PROCEEDINGS OF THE TRI-SERVICE GUN TUBE WEAR AND EROSION SYMPOSIUM

29-31 MARCH 1977

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This volume consits of the technical papers presented at the Tri-Service Gun Tube Wear and Erosion Symposium, held at the Pictatinny Arsenal, Dover, New Jersey, on March 28-31 1977. It was the first Service Symposium hosted at the newly formed Army Armament Research and Development Command (ARRADCOM). (over)

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FOREWORD

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Following the welcoming remarks by Col. P. B. Kenyon, Director LCWSL and Maj. Gen. B. L. Lewis, Commanding General of ARRADCOM, forty papers authored by workers from the three services, industry and academia were presented in five sessions, namely: (I) Overview of the Problems and the Current Programs, (II) Identification, Measurement and Condemnation Criteria, (III) Mechanisms of Erosion, (IV) Propellants and Additives, and (V) Materials, Rotating Bands and Design. This was followed by the summary session, in which the Chairman of each session summarized the salient advancements reported in his session, followed by discussion on the topic of the session with a view to identifying the critical areas in which future R&D efforts were required.

FOREWORD (contd)

From the response of the attendees, it was obvious that all the three services shared concerns about the detrimental effects of erosion on the useful life of barrels and were anxious to exchange ideas and work together to understand and control this complex phenomenon. A lot of information was indeed freely exchanged and a number of concrete suggestions to advance the state-of-the-art were made. The salient points are covered in the Summary (page vii - xiii). One of the points of general agreement was, that in view of the increasing interest and R&D effort in this subject, a similar meeting should be held in not too distant a future, maybe in 1980.

The program committee is grateful to ADPA for making general arrangements for the symposium through Col. P. Skordas, which included a banquet on the 29th March. The guest speaker was Brig. Gen. F. Brown who in his very interesting speech stressed the importance of recognizing the users limitations and requirements, during the development of advanced weapons, by the RDT&E elements of DOD.

Jean-Paul Picard

Iqbal Ahmad

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SUMMARY

Since the 1970 symposium on the gun tube erosion held at Watervliet, there has been increased concern about this problem in all the three services. For example; a number of new R&D projects were initiated in various DOD laboratories, and a number of Army personnel have been involved in preparing an Army wide R&D plan to understand and control erosion in advanced gun tubes. The present symposium was a sequel to such efforts, because the necessity of sharing the results of the work done during the last seven years and identifying high priority areas for further investigations was strongly felt in all quarters. The following is a brief summary of the work reported at the symposium.

It was obvious from the review session, that only a minor effort on the control of erosion existed outside the United States. In general the shorter erosion limited life as compared with the fatigue life of barrels, is accepted by most of the countries as a fact of life, except in a few cases where severity of erosion makes it impossible to develop a successfuly high performance barrel. As stressed in the keynote paper, the fact remains that erosion is imposing very heavy penalties in terms of the cost of replacements (including logistics) of prematurely condemned tubes and is a road block to the realization of advanced concepts such as high muzzle velocity, rapid fire, and extended range barrels.

More than 50% of the guns in U.S. are used by the Army, and therefore Army is more heavily involved in studies of the mechanism and the control of erosion. Next comes Air Force which uses guns with requirements of very rapid fire and high velocity, and hence have erosion limited performance. Navy, currently has very little R&D effort in erosion control because most of the Naval guns use the low temperature, low force propellants.

The necessity of understanding the mechanisms involved in erosion has been generally recognized, but most of the work reported in this symposium related to the control measures. The reason is

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not hard to understand. Erosion is a highly complex phenomenon, in which a number of thermal, chemical and mechanical interactions with the bore surface take place in a very short time, and they vary in significance and intensity along the length of the bore. This makes it very difficult to quantitatively relate these processes with the erosion rates for a specified ammunition and firing schedule. Therefore, as yet all of the approaches adopted to predict a barrel life (which is an important objective of these studies) involve semiempirical, although sometimes sophisticated modern computerized techniques. Progress in the improvement of experimental temperatures in the vicinity of the bore surface, measurement of bore enlargement of the eroded guns (including those with secondary and muzzle wear), simulation of firing conditions in the labs, identification of erosion products on the bore surface, etc. was discussed, which only made one to realize that much more effort is required in this area.

The major developments in the control of erosion were reported to include, use of ablatives, identification of the effects of the positioning and design of the additive jacket on the heat transfer in large caliber guns, low flame temperature high impetus nitramine propellants, substitution of gilding metal bands with Nylon band and modified rifling design to reduce engraving stresses and possible damage to the chromium plating on the bore in small caliber guns, and use of refractory alloy liners and/or coatings.

Use of dimethyl silicon + 5% fine silica powder as ablative was reported by Calspan to have increased the life of M39 barrel by about 300%. Also it increased the "cook off" life of the barrel. One undesirable effect was the formation of silica residue on the bore surface which enchanced coppering. Additives (polyurethane, TiO_2 + Wax, Talc + Wax etc.) continue to be the most important erosion control measure. Work reported by the BRL group clearly demonstrated the fact that in order to achieve optimum performance, it was necessary to carefully design the additive jacket and its positioning with respect to the charge, compatible with the internal geometry of the barrel systems. In addition it was also necessary to use an ignition system, appropriately modified to achieve stable cooler boundary layer. For example, it was shown that TiO_2 + wax containing XM201E2 charge which is base-ignited and has an ignition delay of 200 msec, did not exert any beneficial

SUMMARY (contd)

effect on the wear life of 155 mm barrel. By shortening the ignition delay it was possible to make the additive work effectively. Use of talc, MoO_3 and $CaCO_3$ in admixture with the propellant in 20 mm and 7.62 mm ammunition, was described by the Frankford group to have improved barrel life. However, in each case fouling of the tube was observed. Apparently the formation of a residual inorganic coating helps to reduce the heat transfer to the bore surface. But very little is known about the nature of these residues, and how they reduce erosion. The criteria for the selection of these inorganic additives has not been worked out.

Some interesting propellant formulation work is being sponsored by all the three services, to develop high force-low flame temperature compositions. One of them, reported by the Picatinny group consists of RDX, TAGN and polyurethane which has a flame temperature of 3000°K and an impetus of 400,000 ft-lb/lb. It also does not have the problem of the anamolous pressure exponents shown by a number of other nitromine compositions under an investigation by the Air Force. Presently it is being evaluated by the BRL for use in 105 mm guns. Apprehension of its relatively high erosiveness, was somewhat mellowed by a statement from BRL investigators, that it compared well with conventional propellants with the same flame temperature.

Considerable work has been done and is being supported by the Air Force Armament Labs on the development of plastic rotating bands for 20 mm and 30 mm Gatling guns. Some of this work reported at the symposium highlighted the advantages of these bands, in terms of reduced engraving stresses and consequently increase in erosion life. Most of these guns in which plastic bands have been tested were chrome plated. Apparently because of the easy deformability of the band material, chrome remained on the bore surface longer, providing protection to the bore for extended periods. Another explanation given was that the plastic material filled in the cracks in the chrome plate preventing combustion gases from reaching the bore surface. However, no actual observation of the presence of the plastic material in the cracks was mentioned. Contrary to the Air Force's results, Navy found no improvement of erosion life of 5"/54 gun as a result of substitution of copper with plastic band. Again this emphasized the need of proper designing of the

SUMMARY (contd)

band and selection of the material with appropriate mechanical properties for each gun system. In one of the papers, it was shown that the optimum design for rifling (maximum torque with minimum engraving stresses at the bore) for a plastic rotating band, was with a modified sawtooth geometry. Similar studies are required to be made for other guns in which plastic rotating bands are expected to be used.

Advances in the application of high temperature materials in the form of coatings and liners in steel barrel were summarized in the keynote paper, in which more than half a dozen of materials were identified as having demonstrated erosion resistance. It was shown that for successful application of such materials, it was necessary to take into account the thermal and mechanical compatibility of the jacket and liner/coating. For example, the major reason given for cracking of chrome and tungsten coatings and molybdenum alloy liners was, that they had higher elastic modulus and lower thermal expanion coefficient than the gun steel. To make them perform satisfactorily, they should be placed under a compressive stess. Tantalum and columbium alloy, because of their low elastic modulus, were considered to be more promising. Importance of other properties such as hot hardness and impact strength was also pointed out.

An interesting development described by the Benet Labs related to the improvement of the life of 105 mm M68 (fired with HEAT rounds without additives), from 100 rounds to 430 rounds, by plating only the 36 inch length with 10 mil thick chrome, from the origin of rifling. This also reduced the down bore erosion, because there was no chrome plating to chip off.

An important observation made in connection with the use of refractory metal alloy liners/coatings, was that the development of high impetus propellant was essentially limited by the properties of the gun steel - m.p., high temperature mechanical properties and reactivity with the propellant gases. With the use of liners on

SUMMARY (contd)

coatings of materials with very high m.p. such as those of tantalum or columbium alloys, it would be possible to break the so called "steel barrier", and give freedom to the propellant chemist and ballistician to design propellant charges with flame temperature a few hundred degrees higher, to achieve the force necessary for high velocity and high kinetic energy rounds.

It was suggested that the current technology of the fabrication of refractory alloy liners/coatings was fairly advanced and could be exploited to achieve acceptable wear life in advanced barrels.

From the discussion of papers, summary session and general comments of the attendees, the following appeared to be the overall consensus:

1. Since the 1970 symposium considerable progress has been made in developing the state-of-the-art of erosion control.

2. Use of additives is still the most important method of the control of erosion. However, its effectiveness varies from system to system, and depends on the additive jacket design and its placement with respect to the propellant charge and the ignition parameters. It is necessary that both theoretical and experimental work be performed to optimize these factors for each gun-ammunition system with a view to achieving a stable cool boundary layer along the maximum possible length of the bore. To understand the mechanisms of erosion reduction, it is necessary to identify the interaction of wax and the inorganic particles with the propellant gases and the bore surface including identifying the residues on the bore surface, both in the large and small caliber guns.

3. Definitive studies should be made on the erosiveness of propellants, especially nitramine propellants and improve their compositions to obtain minimum erosion of the bore.

4. Plastic bands are quite effective in reducing the engraving pressures on the bore surface, especially in small caliber guns

and perhaps the plastic material provides lubrication and filling of the cracks on the bore surface. In order to achieve maximum effectiveness in both small and large caliber guns, the band design and material properties of the band and the rifling design for each ammunition gun system should be carefully studied and optimized. Basic data on the interaction of these bands with advanced refractory alloys should be developed.

5. There is a considerable promise in the use of ablatives and this approach should be further investigated.

6. Although steel is still the best material for guns with conventional ballistics (especially in combination with additives), it is unsuitable for guns with high performance requirements. Therefore the technology of providing liners, cladding or coating of refractory alloys must be developed on high priority basis.

7. A number of semi-empirical techniques to predict the bore surface temperature and erosion life of barrels are available. They should be thoroughly reviewed and a tentative and generally agreeable procedure be developed to predict wear life of barrel. This procedure can continuously be modified in the light of new knowledge of the mechanisms that must be developed.

8. Selected experimental techniques to determine the erosiveness of propellants, erodibility of materials, measurements of temperature of bore surface, bore enlargement in eroded barrels, identification of propellant combustion products and the product residues on the bore surface, be continuously improved.

9. A uniform system of reporting the erosion of barrels be developed and its use encouraged.

10. A coordinated DOD wide R&D plan be formulated and actively pursued under a directing committee having responsibility of executing this plan, and having strong influence with the system managers in initiating high payoff projects and curtailing low priority or low yield projects. Most of this work must be performed on carefully selected one or two gun systems so that the variety of data obtained could be easily correlated and analyzed.

SUMMARY (contd)

11. An active means of communication between workers in the three services, about the work performed under various programs, (e.g. through the Joint Technical Coordination Group--Munition Development) in the form of occasional meetings, a quarterly or six-month summary report and exchange of technical memoranda or reports be established.

Iqbal Ahmad

Jean-Paul Picard

SESSION I

SYMPOSIUM OVERVIEW

Chairman: Dr. Jean-Paul Picard

U.S. Army Armament
Research and Development Command

THE PROBLEM OF GUN BARREL EROSION - AN OVERVIEW*

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1. INTRODUCTION

Erosion as illustrated in Figure 1 can be defined as the progressive damage of the bore surface and enlargement of the bore of a gun barrel by normal firing, ultimately resulting in loss in the muzzle velocity, range and accuracy and therefore the effectiveness of the weapon. Normally when the loss in muzzle velocity exceeds 200 ft/sec or when the bullets show excessive keyholing, the barrel is condemned. This could happen, depending upon the severity of erosion, much earlier than the established fatigue life of the tube. The purpose of this paper is to review briefly the salient processes involved in ›sion, and the progress made during the last thirty years towards .. control.

2. HISTORICAL

Since their advent as engines of war, in about the first quarter of the fourteenth century, there have been continuous efforts to improve the performance of guns in terms of throwing heavier projectiles to longer distances with higher accuracy. Early guns were made in the form of a vasi or vaze from cast bronze. They could throw a stone to perhaps a few hundred yards. Later, to increase the range they were made in a tubular form by arranging one or more layers of hand-wrought iron bars or rods side by side in a circle (1), then welding the abutted surfaces together like the staves of a barrel (from where the name 'gun barrel' was derived). The interstices were filled with lead, and then wrought iron hoops were driven over the barrel to give it strength and solidity (Figure 2). By the last quarter of the 15th century barrels were made from cast iron. Also the art of producing gun powder in the form of dense grains was mastered by that time. This led to the fabrication of some huge pieces of artillery. For example Edinburg's "Mon Meg" weighed five tons and could throw 19.5 inch iron balls to nearly a mile. The biggest of all the "Czar Cannon of Moscow" had a 36 inch bore and weighed 40 tons. The "Dull Griete", a giant 13 ton bombard of Ghent, had 25 inch caliber and used stone projectiles as heavy as 700 pounds each. Revolutionary progress was made in the 19th century when because of the advances in chemistry, mechanics, metallurgy and associated disciplines really workable rifled gun, elongated streamlined projectiles (instead of iron balls) through Captain Thomas J. Rodman (USA) a progressive burning gun powder, and through Alfred Krupp the process for fabricating the barrel from cast steel were introduced. This made possible the development of guns

*Keynote paper

with increased chamber pressure, muzzle velocity and consequently the range of fire which naturally increased the rate of erosion to a point that it was recognized as a serious problem. In 1886 Fredrik Abel (2) wrote, "The great increase which has been taking place during the last twenty five years in the power of artillery has brought the subject of erosion of gun barrels into prominance, and it is not too much to say that it now forms one of the chief difficulties to be encountered by the makers of a heavy gun. As far as can be seen at present its sufficient mitigation is the one great difficulty which seems likely to impose a limit on the size and power of ordnance in future".

This statement made about a century ago was true then, and inspite of the introduction, during the first half of this century of smokeless powders, improvements in the gun steel and gun design, various types of coatings and liners was as much true at the end of the second world war, and inspite of the introduction of wear reducing additives in the sixties, was restated with the same urgency in the Triservice Symposium on barrel erosion held at the Watervliet Arsenal in 1970 and it is still true today.

The reason also remains the same, as was in the nineteenth century, i.e., a continuous demand for increased range, rate of firing, accuracy and payload. To give an idea, Figure 3 shows the trend in the increase of the muzzle velocity (MV) during the past six hundred years. Presently some of the in-service barrels are designed for MV exceeding 4000 ft/sec, some advanced concepts of barrel development include evaluations of MV as high as 8000 ft/sec (3). Rate of fire in some small caliber machine guns are as high as 1200 rounds/min. Even some large caliber guns are being designed for sustained, relatively rapid fire rates. Historically mortars and howitzers are known to have little erosion, and therefore to fail by mechanical fatigue, and guns to have usually a shorter erosion life.* But currently, some howitzers such as 155mm XM199 and 8" M201 are being modified for longer range, and therefore have real erosion problems. In isolated cases, the problem of erosion has been observed even in mortars (81mm M29A1). In recoilless rifles the vent bushings are so severely eroded that they have to be replaced after 500 rounds.

Although systematic studies of this problem date back as far as the middle of the 19th century, the most authentic work was done during World War II (1940-45) under the sponsorship of National Defense Research Committee (NDRC) of the Office of the Scientific Research and Development. The results were summarized in an excellent treatise entitled, "Hypervelocity Guns and Control of Gun Erosion" (4). After the war the interest in the subject waned as is evident from the proceedings of 1950 (5) and 1952 (6) symposia. During the Vietnam War, the problem again came to the surface and the subject was reviewed in an interservice symposium held in 1970 at the Watervliet Arsenal (7). Since then there has been a general awareness of this problem, and

*Had it not been for the additives the majority of the large caliber guns would be erosion limited.

while the Army initiated efforts (59) to draw out a well coordinated R&D plan a number of projects on understanding and control of erosion were started by the concerned labs. At present a comprehensive plan prepared by a committee of ARAADCOM personnel is under consideration of DARCOM. Dr. Ward will review its status today.

3. MAGNITUDE OF THE PROBLEM OF EROSION IN THE CURRENTLY USED GUNS

Table I, lists some of the current large caliber weapons which are wear limited (8). It also lists guns which are condemned on the basis of both wear and fatigue. The small caliber, especially rapid fire machine guns, are in general erosion limited. For example 20mm M61, has, depending on the firing schedule, a fatigue life between 3000-12000 rds, but their erosion life is not more than 500-2000 rds. Some of the advanced systems such as 60mm high velocity MCAAAC or 75mm ARES gun cannot be brought to fruition unless satisfactory methods of the control of their erosion can be found.

Erosion is therefore costing the DOD heavily in the prematurely condemned tubes and is a road block in the development of advanced barrels especially those with the requirement of long range, rapid fire rate and high muzzle velocity.

4. PHENOMELOGICAL DESCRIPTION OF EROSION PROCESSES

Figure 4 illustrates the three major elements which take part in the erosion of a gun barrel.

1. The barrel, made of gunsteel (composition and properties shown) with rifled bore surface.

2. The round, which includes the propellant, primer and ignitor. Representative compositions of propellants used in large and small caliber guns are summarized in Table II(a) and II(b) respectively.

3. The projectile with its rotating band which is usually made of copper or gilding metal. The rotating band not only provides spin to the projectile, but also obturates the gases, to achieve maximum pressure in the barrel behind the projectile.

When the gun is fired, the propellant burns and develops high temperature (2500-3800°K) and high pressure (20-80,000 psi) due to the formation of large volume of gases which propel the projectile and give it the required muzzle velocity. CO, CO_2, H_2O, H_2 and N_2 are the major constituents of the powder gases. Also present are NH_3, CH_4, NO and H_2S (from the primer), and a number of other minor species including free radicals, ions and metastable molecules. The amounts of CO, CO_2, H_2O and H_2 are controlled by the water gas reaction $CO_2 + H_2 \rightleftarrows CO + H_2O$. The CO/CO_2 ratio for single base powders is higher (2-3) than that for the double base powders (approximately 1). In other words, single base powders are more reducing than the double base ones. Figure 5 is a typical ballistic curve, showing the variation (9) of temperature, pressure, muzzle velocity and the fraction of propellant burnt as a function of time. In large caliber guns such as 175mm, this time may be 20 milliseconds and in the small caliber rifles, it may be a fraction of a millisecond. During this time, under the pressure of the gases the projectile moves forwards. The rotating band engages with the rifling creating a contact pressure as high as 50 Ksi, with the accompanying increase in temperature at the interface, which can melt the band surface, providing almost hydrodynamic lubrication to the projectile which is accelerating towards the muzzle end. Heat is transferred from the hot gases to the bore surface by forced convection, further raising the bore surface temperature, which not only reduces its mechanical strength, but also promotes chemical interaction. Theoretical calculations and extrapolation of experimental measurements made in the vicinity of the bore surface with various kinds of high response thermocouples, indicate that the first few mils of the bore surface experiences a rapid temperature excursion (Figure 6). In high muzzle velocity rapid fire barrels, it can cause partial melting of the surface.

Some of the important processes involved in the alteration of the bore surface are summarized in Figure 7(a). The reacting parameters include high temperature, high pressure, chemically reactive gases, and rotating band stresses and material. Heat and pressure cycling accompanied by some C and N diffusion, soften the bore surface and induce $\alpha \rightleftarrows \gamma$ and martensite $\rightleftarrows$ austenite transformations. The affected area, called the thermally altered layer (7a4), has a finer grain structure than the original steel, and approximately the same composition. The phase transformations are accompanied by volume changes, inducing stresses in the bore surface causing it to crack. This condition is called "heat checking" (7a1). If the propellant used is a double base, which in general has a high flame temperature and CO/CO_2 ratio of about 1, the surface products are low melting point FeO and austenite. There is a partial melting of the surface, resulting in "pebbling" (7a2). Low flame temperature single-base propellant gases normally react to form the so called "white layer" (7a3) which consists of Fe_3C (cementite), Fe_2N_x(epsilon), Fe_4N (γ prime), minor amounts of Fe_3O_4, retained austenite and some martensite phase formed on rapid cooling. Another layer between the thermally altered layer and white layer, called inner white layer is sometimes observed. It is generally austenite stabilized by the dissolution of C and N and is occasionally partially melted. Apparently the formation of inner white layer precedes the outer white layer. The sequence of these layers is schematically shown in Figure 7a4. The melting point of these products is as low as 1100-1150°C, which is 250-300°C lower than the m.p. of steel. Therefore it is conceivable that high velocity gases following the projectile can sweep away some of these molten or loose products.

Frequently incomplete obturation of gases past the projectile, due to faulty band, or after a certain amount of erosion at the origin of rifling has taken place, can score the bore surface, as well as melt the rotating band surface contributing to coppering. Entrapment of copper in the surface cracks may accelerate their propagation into the body of the barrel. Further the accelerating projectile can swage the lands, and contribute to the muzzle wear. The unburnt propellant particles provide mechanical abrasion of the surface. All these factors, which are summarized in Figure 7(b) contribute to the removal of the material from the bore surface, ultimately resulting in the enlargement of the bore.

The severest conditions occur near the origin of rifling and therefore it is the most affected area in the tube. The rate of erosion, in general, increases with the increase in energy imparted to the projectile and the temperature of the propellant. In other words it increases with increase in the weight of the propellant charge, chamber pressure, muzzle velocity, rate of fire and isochoric flame temperature.

Normally, muzzle wear, which is essentially mechanical in nature, is very small, but in some of the advanced barrels with requirement of longer range, or those with bad O.R. wear, it can become critical enough (Figure 8) to limit the effectiveness of the weapon by causing unacceptable loss in range and increase in azimuth dispersion (10).

Thus in a very short time the bore surface is exposed to 1) high temperatures, 2) high pressure stresses, 3) severe chemical interaction of combustion gases, 4) engraving stress of the rotating band, frictional forces and bore surface band material interaction, 5) transient thermal and mechanical effects such as scoring due to poor obturation of gases, 6) sweeping action of the high velocity gases and 7) abrasion due to unburnt particles. Further complexity is added by the fact that these conditions vary along the length of the tube during the ballistic cycle.

The net result is 1) enlargement of the bore and damage of the bore surface, 2) loss of muzzle velocity, 3) loss of range and 4) loss of accuracy. Also the presence of reactive environments and high temperatures can reduce the normal fatigue life of the barrel.

It may be pointed out here, that in view of the short time for which the projectile remains in a barrel the useful life of a barrel in real time is extremely short. For example, assuming the projectile travel time in a 105mm gun to be 10 milliseconds, and it is condemned after 1000 rounds, due to fatigue, its total useful life is only 10 seconds. Usually large caliber barrels fail earlier because of erosion, unless some control measures such as use of additives, ablatives or liners are used. Small caliber rapid fire barrels also have similar short useful lives. This testifies the severity of conditions to which the bore surface is exposed during firing.

5. SOME MECHANISMS OF EROSION

It is evident from the above discussion that erosion is a result of the combined action of thermal, mechanical and chemical processes. Efforts have been made since 1940, to relate these phenomena quantitatively with the rate of erosion. Two major mechanisms are proposed:

Thermo-mechanical: A number of approaches have been taken first to accurately predict and measure the surface temperatures along the bore, and in the radial direction in the tube. Such calculations and measurements near the bore surface show that the bore surface near the commencement of rifling attains the highest temperatures. Because the slowness of the thermal conduction process, only a very thin surface layer suddenly experiences a transient temperature excursion (Figure 6). Using some simplifying assumptions, such as that the rate of erosion is directly proportional to the depth of penetration of one of a number of critical isotherms, Thornhill (11) was able to correlate the average rate of wear at 1 inch from O.R. and the estimated maximum bore surface temperatures at this point. He showed that below 660°C, erosion was negligibly small, when the temperature was between 660-1000°C, the rate of erosion divided by the square root of the caliber increased steadily with temperature. Apparently 660°C isotherm is associated with the Ac temperature of the phase change of the steel. When the temperature is higher than 1000°C the rate of wear increases more rapidly probably due to the ablation of the relatively low melting erosion products (melting point of Fe_3C is 1150°C). In the case of high performance barrels, where high bore surface temperatures are reached, Jones (12) assumed that even though the overall bore surface temperatures did not reach the m.p. of steel, the projected areas on a surface caused by heat checking, form hot spots as a result of hot turbulent gases and frictional forces. These hot spots melt and are washed away by the hot gases. Jones developed a semiempirical expression which predicted wear rates for 18 Army and 11 Navy guns quite reasonably (Figure 9).

Thermo-chemical-mechanical: Thornhill and Jone's treatments are based on only thermal effects. According to another school of thought, in addition to thermal and mechanical, chemical processes also play an important role. As has been described earlier, the products in the form of white layer are formed by the chemical interaction of powder gases, and their composition depends on the CO/CO_2 ratio and the temperature. The reacted surface melts at a temperature much lower than that of steel, and, is easily sheared away by the high velocity gases. If the bore surface is of a material which did not form these low melting products (e.g. Stellite 21), the erosion is considerably less.

In recent years Richardson and Belton (13) have reported the enhancement of vaporization rates of metals in presence of reactive atmospheres. Formation of gaseous $Fe(OH)_2$ in presence of water vapor was found to be the reason of enhanced vaporization of iron. Contribution of chemical processes is further supported by the fact that propellants with the same ballistic characteristics (isochoric flame temperature, impetus, etc) can have different erosivity. For example, it has been reported that (14) picrite rich propellants are less erosive than non-picrite equivalent propellants. Recent propellant compositions containing nitramines have been found to be more erosive than the equivalent nitrocellulose base propellants. Hence erosion is a product of not only thermo-mechanical, but also of thermo-chemical-mechanical factors.

A critical evaluation shows that although chemical reactions are bound to contribute to erosion, whatever the firing schedule used, it is the relative importance of the chemical effects which vary. With low temperature propellants, the chemical contribution to erosion is more significant. While the high temperature propellants, surface melting becomes a predominant factor. Temperature is of course, the main regulator of all the processes of erosion.

Mechanical swaging of the lands and dissolution of bore surface in liquidified surface of copper rotating bands, are common to both the above mechanism. Other contributing factors include mechanical abrasion by the unburnt propellants and constitutional liquation of grain boundaries due to high heating rates involved (52).

The rates and influence of all the above thermal, mechanical and chemical processes vary along the bore length during firing and also with the history of firing. This makes gun barrel erosion phenomenon to be highly complex. In order to construct a realistic model to predict the barrel life, all these variables have to be taken into account, which is a very formidable task.

6. PROGRESS IN THE CONTROL OF EROSION

From the preceding discussion it will be apparent that the major causes of the erosion in a barrel are:

1. High temperature and high pressure.
2. Chemical interaction of the propellant gases with the bore surface.
3. Rotating band-bore surface interactions: engraving stresses, melting of band surface and interaction with steel, poor gas obturation.

Therefore, the obvious approaches to minimizing erosion include: 1) reduction of the bore surface temperature, 2) developing low flame temperature and less erosive propellants, 3) reducing engraving stresses by proper designing of the rotating band and rifling and use a band material which is inert, provides necessary spin to the projectile with minimum engraving stress and effectively obturates the gases. On the otherhand, 4) use of a gun barrel material, or provide coatings or liners in the gun steel barrels, which are resistant to all the three main causes of erosion.

Reduction of the Bore Surface Temperature

The following four major methods have been investigated: 1) external cooling of the barrel, 2) FISA protectors, 3) smears and propellant additives to provide laminar cooling at the bore surface. The first three methods have been evaluated since World War II, with varying degrees of success. External cooling of barrels is perhaps practical for aircraft barrels. For ground artillery, it may be possible to use water cooling but it presents problems of additional weight and logistics. FISA protector consists of a thin, slightly tapered sleeve of soft steel slid over a complete round of ammunition so that one end surrounds the cartridge case and the remainder covers the projectile up to the bourrelet. On firing the gun, the mouth of the cartridge expands and locks the sleeve, which protects the O.R. from erosion. Limited success in reducing erosion in 0.5, 0.6 inch and 37mm barrels was reported to have been achieved, but it is not widely adopted. Use of smears has been evaluated both in England and U.S. It essentially involves smearing the bore surface with some inert oil or grease ahead of the propellant gases to provide a temporary thermal barrier between them and the bore surface. According to a British report (14), in a 3 inch Mark N.gun, use of 110 gm of silicone oil (viscosity 60,000 centistokes) contained in a PVC capsule situated at the base of the projectile, reduced the heat input to the bore by about 30% and even after 2200 rds (at 120 round per minute, burst length 60 rd), there was no erosion, as compared with 0.12 in bore enlargement, when standard ammunition was used. Serious difficulty arose when the capsule got damaged in handling the ammunition and the oil got mixed with the propellant. Similar work done by Brown (15) et al with the instrumented M39 and M61 barrels showed that use of M55 A2 20mm ammunition containing 5cc of gelled dimethyl silicone (60,000 CS) compound mixed with 5.5% fine silica powder, as shown in Figure 10a, reduced the bore surface temperature by about 150°F at the O.R. and increased the erosion life of the M39 barrel by about 300%. The erosion profiles of the barrels without and with the smear or the 'ablative' as is termed by the authors are shown in Figure 10b. Also use of ablative ammunition increased the safe 'cook off' life of the barrel (almost double). One undesirable side effect was the formation of a silica film on the bore surface, which induced enhanced coppering. Another objection against ablatives is that its use increases the loading density of the powder.

In the mid fifties recognizing the presence of a laminar layer of gas along the inside of gun tubes during firing, Dickenson and McLennon (16) proposed to use polyurethane foam to generate cool boundary layer by its thermal degradation. It was shown that thin slices of the foam cemented inside the front half of the cartridge cans reduced the erosion of 17 pr, 20 pr, 3"/50 and 90mm barrels considerably. In a 20 pr gun, erosion was reduced by a factor of four (17). Tests made in U.S. (18) on 90mm, 105mm, and 120mm tank guns showed similar (Table III) increase in erosion life.

Although in 105mm M68 the wear life did improve, unaccepted dispersion was recorded. Observation of secondary wear has been suggested by Alkidas (19) et al as the possible reason of this dispersion. Navy also reported (49) dramatic improvement of the life of 6"/47 and 16"/55 Naval guns. However, in a Canadian (3) evaluation at very high muzzle velocities (8820 ft/sec) and chamber pressure of 75,000 psi polyurehtane foam proved to be of no use. In all these tests, the bulk density of the foam and its position relative to the propellant was found to be critical to its successful performance. The denser foams were found to be more effective.

Another major contribution to the wear reduction through keeping the bore surface temperature down, by boundary layer cooling, was made in the sixties by Swedish inventors Ek and Jacobson (20). They proposed the use of liners of rayon coated with a mixture of TiO_2 46%, Wax 53.5%, dacron staple 0.5% and stearyl alcohol 1.0%. These liners were placed at the forward end of the case with flaps folded on the propellant charge (Figure 11). They also suggested the use of WO_3/wax as additive. Firing tests showed that TiO_2/wax liner performed better (erosion 0.003 in/50 rounds). Results of a few other calibers are given in Table IV. Unfortunately, the use of this additive is not successful universally. In the long range howitzers (22) (105mm XM204, 155mm XM185 and 8" XM201), the additive did not give satisfactory results. This could be due to the fact that in these guns bag loaded ammunition is used (Figure 12). The additive as it is positioned could be as much as 40cm away from the charge, making it less effective (19). Results such as those obtained with 105mm M68 tank guns and the extended range howitzers, strongly suggest the need of systematic work on the mechanisms involved in the wear reducing action of additives and gain information about gas dynamics in the guns, and develop design of the additive jackets suited to the interior geometry of each gun system.

Work in this direction i.e., understanding the mechanism is continuing at Picatinny Arsenal and BRL for a number of years. Lenchitz and coworkers (23) by using a specially designed erosion gauge, showed that in these additives, wax was the major constituent responsible for the reduction of wear, however, a mixture of TiO_2 + wax was decidedly more effective. It was shown that, of the various oxides, those with higher heat content were more effective. Picard et al (24) evaluated a number of oxides and silicates and found that hydrated magnesium silicate ($3MgO \cdot 4SiO_2 \cdot H_2O$) with a heat content of about 300 cal/gm as compared with 193 cal/gm for TiO_2, was more effective than TiO_2 + wax.

The superiority of talc + wax over TiO_2 + wax additives has been demonstrated in a number of Army and Navy guns. Recently Brosseau and Ward (25) measured the reduction in temperature in the vicinity of the bore surface, with both polyurethane liners and TiO_2/wax additive. They found that the heat transfer to the bore surface by both the additives was about the same as long as they were configured and positioned alike. One important observation was that TiO_2/wax leaves a residue which insulates the bore surface in the subsequent firing. Similar observations have been reported by Navy for talc additives.

It is rather difficult to provide additive liners in the small caliber ammunition. Therefore attempts have been made to mix 1-2% of the additive such as talc and TiO_2 with some success (26).

Alkidas, et al (19) have reviewed the mechanisms involved in the wear reducing action of these additives. No single mechanism can account for the effectiveness of an additive. A number of mechanisms appear to work in combination. Some of the most plausible ones include: the organic constituents of the additive, vaporize and reduce the propellant gas temperature along the bore surface, providing the cool boundary layer; the inorganic particles reduce the eddy turbulance in the vicinity of the wall, act as wax dispersents, and absorb energy in heating up and in some cases undergoing endothermic reactions (dehydration of talc); the inorganic constituents leave thermally insulating residues on the bore surface; the organic constituents by reacting with the propellant can increase the CO/CO_2 ratio in the laminar boundary layer, thereby making the gas less chemically erosive. From these considerations, other inorganic compounds with higher heat content than TiO_2 and talc, capable of endothermically decomposing or reacting with propellants, can be and should be selected for investigation.

Low Flame Temperature Propellants

Use of additives has been a successful expedient method of the control of erosion. However, it will be very desirable if the propellants, with instrinsically low flame temperature and high force could be developed. There is a continuing effort in this direction by all the three Services. M30 propellant, is a product of such an effort. In this propellant by substituting some of the nitroglycerine with nitroguanidine, high force values with low flame temperature (3040°K) and reduced barrel erosion have been achieved (see Table II). However, this propellant has relatively low mechanical strength, and in high pressure guns (e.g. 120mm delta gun, with chamber pressure 80,000 psi) tends to give instabilities. Propellant compositions with thermochemical characteristics similar or better than M30, but with better mechanical strength have been obtained at Picatinny Arsenal (27) by substituting nitroguanidine with nitrate esters such as trimethyloethane nitrate (TMETN), triethylene glycol nitrate (TEGN) and diethyleneglycol nitrate (DTGN) (Table V).

Similar results were achieved in a composition designated PPL-A-2923, which had same impetus as M8 propellant (390,000 ft lb/lb) but the flame temperature was 300°K lower and had also superior mechanical strength and has been suggested as a substitute for M8. Navy has consistently preferred using their NACO propellant which in composition is similar to M10, but has a flame temperature considerably lower than that of M10.

An important recent development is the introduction of a new series of propellants which contain nitramines (either cyclic or linear) with hydrocarbon binders. Because of the low average molecular weight of the combustion products these propellants have high impetus, low flame temperature (< 2600°K) and relatively high cook off temperature. For example, under an Air Force contract (28), a composition containing 9% NC, 64% DMED (1,6 dimethoxy-2,5 dinitraza-hexane) and 16% HMX, was found to have isochoric flame temperature of 2188°K and an impetus of 330,000 ft-lb/lb with an average molecular weight (MW) of 18.3. Numerous other compositions giving high impetus (e.g. 440,000 ft lb/lb) have been formulated. However, these compositions had anamolous pressure exponent. While below 4000 psi, it was 0.49-0.68, it rose to 1.00-1.74 at higher pressures. By controlling the particle size of the oxidizer and the other process parameter, the Picatinny Arsenal group have overcome the problem of pressure exponent in a composition containing triaminoguanidine nitrate (TAGN), RDX (15 μ) and polyurethane (binder). This propellant has a pressure exponent of 0.8. Strauss and Costa (29) reported to have developed a propellant composition incorporating 20% tetraethylene pentamine nitrate (TEPAN), 10% Hycar binder and 70% HMX which has an impetus of 379,000 ft-lb/lb and an isochoric flame temperature of 2784°K, as compared with 364,000 ft-lb/lb and 3040°K for M30. The pressure exponent over the pressure range 3000-30,000 psi was 1.15. However, a study of the erosivity, made of a number of propellants by Picard and Trask (30) have shown that compositions containing nitramine-Hycar-nitrocellulose were far more erosive than the nitrocellulose base compositions of comparable flame temperature. This means that it may not be possible to use them without additives to protect the bore surface. It is therefore necessary that further studies be made to modify these compositions in a way that they become less erosive.

In the small arms ammunition, as mentioned earlier, deterrents like ethyl centratite and additives such as TiO_2 and Talc (1-2%) have been found to reduce erosion (31). These propellants also contain $CaCO_3$ (Table IIb). It has been reported by Devine and Brodman (32) that $CaCO_3$ in M16A1 rifle is a major contributor to barrel fouling at the O.R. At the same time it was noticed that ball propellants with high $CaCO_3$ content gave essentially no erosion at the origin of rifling. Formation of a thin thermally insulating $CaCO_3$ film in the area was suggested as the possible cause. Levy and Stiefel (33) also reported dramatic reduction in erosion when MoO_3 was added to a standard double base propellant. The formation of a thin film of molybdenum at bore surface is a possible reason. It appears that all the inorganic additives whether used in large or small caliber guns, leave a residual film on the bore surface which at least to some extent reduces the heat transferred to the bore surface and therefore erosion.

Reduction of Engraving Pressures on the Bore Surface and Band Material Bore Surface Interaction

The contributions of the rotating band of the projectile, to the erosion of the barrels, in terms of its swaging action on the lands (thereby flattening of the lands or cracking of the chrome plating), and forming low melting and removable alloy with the steels are well documented. Consequently efforts have been made to find ways to reduce the engraving pressure, by optimizing the band design, and evaluating substitute band materials to eliminate the bore-band material interaction. The noteworthy outcome in 1940-45 period has been the development of pre-engraved (PE) projectiles, or a projectile with a pre-engraved band, in which the projectile or the band is provided with teeth to match the grooves of the rifling. In this configuration it is also possible to apply a solid lubricant coating to reduce friction. Test made with 0.5 cal test guns showed steel banded lubricated PE projectiles, gave a two fold increase (4) in velocity life of the gun. The application of this concept is particularly beneficial for chrome plated tubes. With the combination of chrome plating of the bore and use of lubricized PE steel banded projectile, a twenty fold increase in velocity and 8-10 fold increase in the accuracy life of 0.5 cal gun was reported. The main, but not unsermountable disadvantage in its adoption is the necessity of aligning of the teeth on the projectile or the band with that of the rifling at each round of fire.

In the search for better rotating band materials with minimum interaction and coefficient of friction with the bore surface, since WWII, a number of materials have been evaluated. One of them was the sintered iron bands introduced by Germans due to the shortage of copper. Sintered iron bands have been also evaluated in UK, Canada and USA and have been found advantageous in reducing erosion at O.R. but down the bore, because of the abrasive wear, the erosion was found to increase.

Use of organic polymers, such as the thermo-setting plastics was suggested as early as WWI. In 1954 Navy (NWL, Dahlgren) developed the first nylon band for 20mm barrel, which was found to effectively obturate gases, and remain in tact at velocities as high as 3420 ft/sec (34). Accelerated aging tests indicated that although there was some dimensional change, no difficulty arose in chambering of the round (35). However, during the last ten years, a more significant progress has been made in this area under the sponsorship of the Air Force Armament Labs. A number of band materials, designs and techniques of the bonding to the projectiles were evaluated. These are covered in a series of reports written by Philco Ford in 1972, and DeBell and Richardson Inc. (36). Philco Ford developed the 6/12 nylon bands which were specified to have 8800 psi tensile strength and 8600 shear strength (m.p. 406-420°F, coeff. therm. expansion 5×10^{-5} in/in/°F). Rotating band made by DeBell and Richardson Inc. from Nylon 11 and Nylon 12 were injection molded onto the primed (253-P primer) surface of the grooved projectiles and bonded by induction heating. Fired in a 30mm GAU-9/20 barrel, the Nylon band performed fully at 4000 ft/sec MV, while the gilded metal bands were lost at 3900 ft/sec MV. However, in a series of firing tests in 20mm gun system, the projectiles with Nylon 12 bands were found to have short start pressure (11 Ksi) as compared with 14 Ksi with copper bands, with accompanying lowering of peak pressures, muzzle velocity, and accuracy. To compensate for this, use of either a thin web propellant or slightly increased charge has been found necessary. Occasional loss of band during firing is still encountered. Currently, efforts are directed to improve bonding technology of the band with the projectiles and quality control of the product. A three fold improvement in erosion life has been occasionally mentioned, but as yet not documented.

Such plastic bands have also been evaluated in 105mm and 5"/54 (Fig. 13) Naval guns (37). However, as shown in Figure 13, very little improvement in erosion life has been observed. For the plastic band to perform effectively in large caliber guns, the material and design of the band may have to be modified to withstand the stress and temperature at their respective band-bore interface.

Montgomery (38), from the results obtained by the pin-on-disc method has suggested that wear rate of band materials is proportional to the reciprocal of their melting point Tm(in°K), according to the expression

$$\text{Const} = A \exp (B/T_m)$$

when A and B are constants. Nylon with its low melting point can therefore wear much faster than copper or steel, which may be the reason of its poor performance in 5"/54 guns. Probably the increase of the bearing area by increasing the width of the band or using tapered bore may solve this problem.

All the above results on the plastic rotating bands have been obtained using conventional rifling geometry, which has been developed essentially for copper rotating bands. What is the best rifling geometry for plastic rotating bands? To answer this question James T. Healy and Donald P. Haas (39), by analytical studies including finite element computer analysis of barrels, plastic rotating bands and projectiles and lab testing have shown that a saw tooth rifling (Figure 14) will perform best (provide max. torque with minimum stress on the band and the bore surface) in a barrel using 20mm M-56 HE1 projectile with plastic band. Test firing data indicated good high temperature capability and higher muzzle velocity for the same propellant charge. A choked modified conventional configuration was recommended as a strong second choice. More work is necessary to clearly demonstrate the superiority of the sawtooth rifling over the conventional rifling with the same number of grooves.

Erosion Resistant Materials

Since the successful casting of steel barrels by Krupps in the 19th century, steel has been reigning as the best material for all types of guns. It has been modified in composition and by thermo-mechanical treatments, but never replaced. In fact all the developments in propellant formulations has been tailored to the properties of steel - such as the melting point, oxidisability and mechanical properties. However, early in WWII, it was recognized that it only worked well with barrels with conventional ballistic requirements. With barrel designed for high muzzle velocity, extended range or rapid fire with extended bursts, in which large charge to mass ratio or high temperature propellants are required, becuase of its low m.p. and low mechanical properties at high temperatures steel could only be used if the bore surface was protected by liners or coatings of materials with higher m.p. than steel. In 1940-45 systematic and exhaustive work was done to develop materials suitable for high performance barrels (4). Scores of available alloys were screened and new ones were formulated and evaluated using ingeneous experimental devices including firing the powders of potential metals and alloys in admixture with the propellant, and variety of erosion vent plug apparatus and test barrels. The requirements of a desirable material for high performance barrels liners and coatings were identified. They are still valid and are summarized in the following:

	Barrel	Liners	Coatings
Melting Point	High > 1500°C	High > 1500°C	High > 1500°C
Yield Strength at High Temp	High	High	High
Elastic Modulus	High	Compatible with Jacket Same or lower preferred	Compatible with the substrate Same or lower preferred
Fracture Toughness and Impact Strength	High	High	High
Hot Hardness	High (Appropriate)	High (Appropriate)	High (Appropriate)

	Barrel	Liners	Coatings
Chemical Inertness to Powder Gases	High	High unless used as substrate for a coating	High
Coeff. of Thermal Expansion	Low	Compatible with Jacket	Compatible with the substrate
Thermal Conductivity	High	Low	Low
Sp. Heat	High	Low	Low
Reaction with rotating band material	Inert	Inert unless protected with a coating	Inert
Phase Transition	None	None	None

Other factors common to all the three classes are the easy fabricability and availability at low cost.

For monobloc barrels, it is desirable to have high thermal conductivity and specific heat to keep the bore surface temperature low, and low coefficient of thermal expansion and high elastic modulus, so that enlargement of the bore diameter due to heat and pressure is minimum for optimum engraving of the rotating band and gas obturation. However, for the liners and coatings, these parameters have to be compatible with those of the jacket or substrate material.

One of the major outcomes of 1940-45 work was the development of Stellite 21, which is a cobalt base alloy, as a liner material. It was found to have excellent erosion resistance, optimum hot hardness and ductility and fabricability. So it was adopted and is still being used as a liner material in machine gun barrels. However, its major drawback is its low m.p. (1280°C). When a double base propellant was used it failed by surface melting.

Chromium is another excellent erosion resistant material. It also has a high melting point, but unfortunately it is very brittle. Up until now efforts to ductilize it have proved futile. In 1940-45, a number of chromium base alloys were formulated and evaluated. One of the best was Cr-25 Fe-15 Mo. It had excellent erosion resistance. Liners of this alloy inserted by shrink fitting (shrink fit pressure

90,000-100,000 psi) in 0.5 cal steel barrels showed less erosion than Stellite even after 3800 rds. The work was stopped because of the termination of the contract at the end of WWII.

Another material found to be excellent was molybdenum, hardened with 0.1% Co. A 0.5 cal barrel fitted with liners of this material (made in the form of twisted staves) was test fired. The liner withstood more than 2000 rounds without much erosion. However, spalling and cracking of liner and opening of the joints were some of the problems encountered. Most of these problems are connected with lack of suitable fabrication technology. With the current state-of-the-art e.g. advanced powder metallurgy, probably these problems will not arise.

Considerable work was done on optimizing the processes of coating of the bore surface with a number of erosion resistance materials. Most notable is chromium. It can be applied by the cheap electroplating process, and it bonds well with steel. However, it is brittle and develops cracks as a result of firing and engraving stress. Hot propellant gases penetrate these cracks and attack the underlying steel (Figure 15) and finally making the barrel susceptible to bore damage. In fact, frequently the rate of damage is more than in the unplated barrels. Another problem one encounters is that if the coating is not thick enough to thermally insulate the substrate the latter under the engraving stress of the rotating band deforms thereby resulting in cracking of the poorly supported coating. Hardening of the steel surface by nitriding, and using a thicker (5-10 mil) coating (to insulate the substrate from excessive heat) were found advantageous. This will be discussed in details in the session on Materials and Design. In the final stages of these efforts, using a combination of short stellite liner, and chromium coating ahead of the liner, with choked muzzle were successfully used in 0.5 cal machine gun barrels. These barrels show a ten fold increase in life, over the unplated barrels.

Since WWII, nothing extraordinary has happened in the area of barrel material development, firstly due to lack of any urgency (or major conflict) and secondly because of the introduction of TiO_2 + wax additive which many believedto have solved the problem of erosion. However, during the sixties and the current decade, a number of high strength steels, cobalt and Ni base and refractory metal alloys have been evaluated for their erosion resistance. For example erosion studies made at 11TR1 (40) by using an erosion gage apparatus on W, Ta, Mo, Cb, Hf and Zr alloys showed molybdenum alloy to be the best, and Ta-10W was the second best. Liu and Kennedy (41) of G.E. Burlington screened a number of refractory alloys by measuring their physical and mechanical properties and erodibility by vent plugs and concluded that Ta-12W-1.0Re-0.025 C designated an ASTAR 1211, had the best combination of properties for erosion resistance in barrels. Vassallo (42) of Calspan investigated fourteen materials for 75mm high velocity cannon (60 Ksi chamber pressure). Of these materials Ta-10W and Columbium-1%

Zr were found to provide substantial improvement over the vascomex alloy, which is the present barrel material. 0.22 Swift barrels made of A-286 steel were fitted with coextruded liners of TZM, Mo-0.5 Ti, and Ta-10W by Battelle West (43). Ta alloy T-222 could not be coextruded, Similarly 25mm barrels were made with coextruded L605 alloy liner. Of these barrels, the Ta-10W lined barrels gave the best performance (43, 44). The TZM lined barrel did not perform as well. Similar work was sponsored by Rock Island Arsenal and 7.62mm barrels made of Cr-Mo-V steel were provided by coextrusion, liners of a number of high temperature alloys including refractory metal alloys. These barrels were test fired. The data (45) are summarized in Figure 16. (USAWECOM Annular Report S-WE-EX-2-72 FY72 p. 26.) It shows that chrome plated medium alloy steel H11, HS25 and the tantalum alloy liners gave the best results. Table VI lists materials with demonstrated erosion resistance.

Since the 1970 symposium considerable work on the application of chromium, cobalt, Co-Re and cobalt-Al_2O_3 alloy coatings in small and large caliber guns has been done at Watervliet Arsenal. Also duplex coatings of Cr-(Co-Al_2O_3) were evaluated. It has been shown that in 105mm M68, 10 mil Cr is better than 5 mil-(Co-Al_2O_3) duplex coating, and that partial plating up to about 36 inches from the O.R. can eliminate the down bore wear usually shown by full chrome plated tubes (49). Limited work has also been done on coating barrels with W, Ta and Ta-10W, both under Air Force and Army contracts. Chemical vapor deposition processes were used. Although W coatings crack during firing (46), they did perform better than chromium in 0.22 swift barrels. Tantalum alloy (47) coated barrels were not test fired, because the quality of the coating was not satisfactory. Further work to develop the CVD processes of coating Ta-10W alloys is needed.

A critical assessment of some of the material evaluated as liners or coatings in steel barrels, and their performance, shows that discounting all the materials with a m.p. lower than 1500°C, the potential erosion resistant refractory metals and their alloys can be divided into two classes: 1) with elastic modulus higher than that of steel e.g., chromium, molybdenum and tungsten, 2) with elastic modulus lower than that of gun steel, e.g. tantalum and columbium. In the first class, all of the metals behaved very well in the erosion gage tests, but when applied as liners or coatings, they did not do as well and failed usually by cracking. In the second class columbium alloys did not do as well in erosion plugs as tantalum alloys. However, barrels made both with Ta and columbium alloys gave improved life. This can be explained by referring to Figure 17, which shows that at the strains developed during firing the class 1 materials due to high elastic modulus, depending on the coating/liner thickness, experience a much higher stress than steel which is normally used as a jacket and do not effectively transfer the load to the substrate (jacket). Therefore if the material is brittle (e.g. Cr or W), it will crack. Molybdenum liners were made by forging and due to anisotropy had poor transverse strength and therefore under firing and engraving stresses they would crack. Thermal mismatch further aggravates this problem. The only way these coatings or liners could be made successful is by putting them under a compressive stress. The reason why chrome plate has been partially successful as a coating is that its modulus is relatively closer to that of steel. At the bore surface temperature, it may be even closer. In any case, the coating if placed under compressive stress, could resist cracking induced by pressure stresses. Some of the refractory ceramics like oxides or carbides have good erosion resistance, but they are also known to crack. Hopefully by applying requisite compressive stress these refractories, which are much cheaper than refractory metal alloys, can also be effectively used. The extreme sensitivity of the ceramics to stress concentration and flaws is, however, a serious problem, which must first be overcome. Some progress in the use of a ceramic liner has been reported by Fishman and Palmer (48). The lower elastic modulus of Ta and Cb alloys in class 2 assures that their coatings and liners would effectively transfer stresses to the jacket. Besides they are relatively more ductile, and therefore are not prone to cracking. These factors contributed to their reported successful performance, and make them outstanding candidate liner materials.

Another aspect of this problem, is that in order for the coating or the liner to effectively resist the swaging action of the rotating band, it should have an optimum hardness at the temperatures developed in the barrel. The reason of the success of Stellite 21 as compared with the gun steel which has a higher melting point, and other refractory alloys is illustrated in Figure 18, which summarizes the data we have obtained in the Benet Labs on the hot hardness of these materials. Stellite 21, maintained its hardness at least up to 250°C higher temperature than the gun steel. Similarly the refractory alloys like Ta-10W, and TZM maintain hot hardness at temperatures even higher than 1000°C. Hence these are the alloys which have the potential for fighting erosion in hypervelocity and rapid fire barrels such as MCAAAC and 75mm high velocity ARBS guns. However, considerable work is required to optimize the technology of their fabrication as liners or application as coatings in order to develop in them the requisite physical and mechanical properties.

Because of their high cost as compared with steel, they will be of necessity used as liners or coatings. For example, Ta-10W is almost $100.00 a lb. Therefore, especially in large caliber guns, it has to be applied as coatings for which processes such as chemical vapor deposition or molten salt electrolysis will have to be developed. For coatings, it will also be necessary to carefully select the substrate liner material with high temperature hot hardness (so that due to thermal effects it does not deform under the coating) and minimum interaction with the coating during fabrication or in service.

Successful use of refractory alloys, brings out a very important point in connection with the future advancements in the technology of high performance propellants. The impetus of a propellant is given by the expression:

$$F_p = \frac{R}{M_w} T_f$$

where F_p = impetus of the propellant, M_w = average molecular weight of the products of combustion, T_f = flame temperature and R = gas constant. In order to develop propellants of high impetus, the propellant chemist can use two approaches: 1) decrease M_w, or 2) increase T_f. Considerable work is being done in formulating compositions to obtain products with low M_w. However, on the whole it appears that as long as C, H, N, and O compounds are the source of energy in propellants, it will take radically a new concept to approach M_w of 17 or lower. On the other hand T_f is essentially limited by the melting point and high temperature mechanical properties of steel. If high melting point materials such as Cr, Ta-10W, Mo, Tungsten and columbium alloys can be successfully deployed in the form of coatings or liners, the "steel barrier" can be broken and it should be possible to use propellants with a T_f a few hundred degrees higher. This can give a significant increase in F_p and the accompanying advantages of high MV, range and accuracy. However, it will then also become necessary to develop propellants with high "cook off" temperatures.

CONCLUSIONS

Barrel erosion is a highly complex phenomenon, which involves the interaction of a number of severe thermal, mechanical and chemical factors with the bore surface, all acting at the same time and varying in intensity along the length of the barrel and with the variation of the firing condition. Although over simplified thermo-mechanical and thermo-chemico-mechanical models have been suggested and some semi-empirical expression relating to the firing parameters and the rate of erosion have been reported, as yet no reliable comprehensive model capable of predicting the erosion life of a barrel has been developed.

A number of control measures have been developed, of which use of boundary layer coolents like polyurethane foam, TiO_2 + wax and talc + wax additives, ablatives such as dimethly silicone, low flame temperature-high impetus propellants and plastic rotating bands have shown significant improvement in erosion life. But lack of adequate understanding of the mechanisms involved in these methods is a delaying factor in achieving their maximum effectiveness in every gun barrel system. Their poor performance in some systems suggest different requirements of material properties and design for gun systems with different interior geometry and ballistics. Attempts to arrive at propellant composition with low flame temperature and high impetus are also showing promising results. However, some of these are quite erosive, and need further modification, before they can be used with advantage. Gun steel has been and still is one of the most desirable materials for gun barrels, and with the use of the above control measures, and improved fracture toughness and high temperature mechanical properties, the effective life of some of the conventional barrels can be considerably increased. However, to meet the requirements of high MV, increased rate of fire and range of advanced systems, it is necessary to break the steel barrier by using liners or coatings of materials with melting point higher than steel. This will enable the propellant chemist and ballistician to design high impetus propellants at relatively higher flame temperatures. During the last thirty years except for the identification of Ta-10W as a good erosion resistant liner material, no new material has been developed. In spite of the well known excellent properties of chromium, as yet the technology of its application as a coating has not been advanced enough to use it as a reliable erosion protection measure. The technology of the fabrication of liners or coatings of other potential materials like molybdenum, tantalum, columbium, chromium, and tungsten alloys is now considerably advanced and must be exploited.

At the end of five years study in 1945, NDRC planned to construct a high performance barrel called A-Z gun which would embody the best features of erosion control developed at that time. The goal was a 90mm cannon having a muzzle velocity of 4200 ft/sec and max powder pressure of 60 Ksi. The first version included the use of preengraved projectile and chrome plated tubes. Also a future hypervelocity gun was envisioned as a steel tube with a full length seamless hardened molybdenum liner. At that time the technology for the fabrication of the Mo liner was not developed, and therefore A-Z gun remained a dream. But now in view of the advances made in the material fabrication technology, propellants and rotating bands, this dream appears to be within the realm of being realized in more than one way. For example a feasible concept could be, as illustrated in Figure 19, a partial hardened molybdenum alloy liner made by powder metallurgical technique, with a chrome plating ahead of the liner, both placed under a compressive stress in a high strength steel jacket, or possible in a filament reinforced steel (Fig. 20) jacket. The technology of fabrication of such a composite tube has been demonstrated at Benet Labs (50). The other concept can involve the use of low modulus Ta-10W or columbium alloy liners. In that case the use of high compressive stress is not necessary. However, since these materials are expensive, it will be preferable to use them as relatively thin claddings or coatings.

To exploit these technologies to achieve improved performance and increased relatively erosion free life of small and large caliber gun barrels, what is most urgently needed is a well coordinated and adequately funded program with clearly defined commitments and priorities.

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Tables and Figures follow

TABLE I. CONDEMNATION LIMITS OF SOME CANNON TUBES.

Caliber	Wear Limit in(***)	Wear Life EFC Rds	Estimated Fatigue Life EFC Rds	Muzzle Velocity Ft.Sec	Propellant	Projectile	Isochoric Temp T°K	Max Pressure 70°F kpsi
40mm M1	0.052 (13.75)	12,000	12,000	2870	M1(AP-T)	M91(A1)	2433	50.5
76mm M32 Tank Gun	0.100 (24.75)	350+		3200	M30	TP-T M340	3040	52.2
*90mm M41 Tank Gun	0.197 (25.25)	700	3,000	3000	M17(TP-T)	M353	2974	57.1
" " "	0.197 (25.25)	240		3950	M30	HEAT-T M431A1	3040	52.3
105mmM68 Tank Gun	0.075 (25.25)	100+	1,000	4850	M30	APDS-T	3040	58.5
" "	0.075	400‡	1,000	4850	M30	APDS-T	3040	58.5
" "	(25.25)		6,000(est)++	4850	M30	APDS-T M392-A2	3040	58.5
" "	0.075 (25.25)	125+	1,000	3850	M30	HEAT-T M456	3040	58.3
" "	0.075 (25.25)	1,000++	1,000	3850	M30	HEAT-T M456	3040	59.4
**105mm M137E1 How	0.070 (16.0)	20,000	5,000	1621	M1	(HE,M1)	2433	36.7
120mm M58	0.075 (38.25)	250		3500	M17	APT(M358)	3017	54.0

(continued)

TABLE I. CONDEMNATION LIMITS OF SOME CANNON TUBES CONT'D

Caliber	Wear Limit in(***)	Wear Life EFC Rds	Estimated Fatigue Life EFC Rds	Muzzle Velocity Ft. Sec	Propellant	Projectile	Isochoric Temp T°K	Max Pressure 70°F kpsi
**155mm M126E1 Howitzer	0.080 (30.0)	30,000	7500	1841	M1	(HE, M1)	2433	36.4
155mm SP How M185	0.100 (30.0)	3,350(Z7)	5000	2245	M30	M107	2470	32.0
155mm Towed How M199	0.100 (30.0)	1,750(Z8)	11250	2245	M30	M107	2470	32.0
**175mm M113	0.200 (64.50)	400		3000	M6	(HE,M437)	2583	47.2
*175mm M113E1	0.200 (64.50)	1,200++	2350	3000	M6	(HE,437)	2583	47.2
8" SP How M201	0.135 (53.0)	1,500(Z9) 3,000(Z8)	7500	2500 2300	M30A1	M106	3040	39.6

* Plated **Tube primarily condemned on metal fatigue. ***Measured at distance in inches forward of breech shown in parenthesis.

+Without additive ǂWith Polyurethane ++With TiO_2 additive

TABLE IIa. COMPOSITION AND CALCULATED THERMOCHEMICAL VALUES OF SOME COMMON PROPELLANTS FOR LARGE CALIBER GUNS

USE	ARTILLERY							
PROPELLANT	M1	M2	M5	M6	M14	M15	M17	M30
SPECIFICATION	JAN-P 309	JAN-P 323	JAN-P 323	JAN-P 309	JAN-P 309	PA-PD-26	PA-PD-26	
Nitrocellulose	85.0	77.45	81.95	87.0	90.0	20.0	22.0	38.0
Nitration	13.15	13.25	13.25	13.15	13.15	13.15	13.15	12.6
Nitroglycerin	-	19.50	15.00	-	-	19.0	21.5	22.5
Barium nitrate	-	1.40	1.40	-	-	-	-	-
Potassium nitrate	-	0.75	0.75	-	-	-	-	-
Potassium perchlorate	-	-	-	-	-	-	-	-
Nitroguanidine	-	-	-	-	-	54.7	54.7	47.7
Dinitrotcluene	10.0	-	-	10.0	8.0	-	-	-
Dibutylphthalate	5.0	-	-	3.0	2.0	-	-	5.0
Diethylphthalate	-	-	-	-	-	-	-	-
Potassium sulfate	-	-	-	-	-	-	-	-
Tin	-	-	-	-	-	-	-	-
Diphenylamine	1.0	-	-	1.0	1.0	-	-	-
Ethyl centralite	-	0.60	0.60	-	-	6.0	1.5	1.5
Graphite	-	0.30	0.30	-	-	-	0.1	0.1
Carbon black	-	-	-	-	-	-	-	-
Cryolite	-	-	-	-	-	0.3	0.3	0.3
Ethyl alcohol (Residual)	0.75	2.30	2.30	0.90	1.00	0.30	0.30	0.30
Water (Residual)	0.50	0.70	0.70	0.50	0.25	0.00	0.00	0.00
Butyl Stearate								
Isochoric flame temp, °K	2417	3319	3245	2570	2710	2594	3017	3040
Force ft-lbs/lb, x 10^{-3}	305	360	355	317	327	336	364	364
Unoxidized carbon, %	8.6	0	0	6.8	5	9.5	3.9	3.2
Combustibles, %	65.3	47.2	47.4	62.4	58.9	51.0	38.7	41.0
Heat of explosion, cal/gm	700	1080	1047	758	809	796	962	712

(continued)

TABLE IIa. COMPOSITION AND CALCULATED THERMOCHEMICAL VALUES OF SOME COMMON PROPELLANTS FOR LARGE CALIBER GUNS (CONT'D)

USE	ARTILLERY							
PROPELLANT	M1	M2	M5	M6	M14	M15	M17	M30
SPECIFICATION	JAN-P 309	JAN-P 323	JAN-P 323	JAN-P 309	JAN-P 309	PA-PD-26	PA-PD-26	
Gas volume, moles/gm	0.04533	0.03900	0.03935	0.04432	0.04338	0.04645	0.04336	0.04308
Ratio of specific heats	1.2593	1.2238	1.2258	1.2543	1.2496	1.2557	1.2402	
Isobaric flame temp, °K	1919	2712	2647	2050	2168	2066	2433	
Density, lbs/in^3	0.0567	0.0597	0.0596	0.0571	0.0582	0.0600	0.0603	
Covolume, in^3/lb	30.57	27.91	27.52	29.92	29.54	31.17	29.50	

TABLE IIa. COMPOSITION AND CALCULATED THERMOCHEMICAL VALUES OF SOME COMMON PROPELLANTS FOR LARGE CALIBER GUNS

USE	RECOILLESS				MORTAR		
PROPELLANT	M18	T18	T25	T28	M7	M8	M9
SPECIFICATION	PA-PD-123	PA-PD-329	PA-PD-329	PA-PD-329	JAN-P-659	JAN-P-381	MIL-P-20306
Nitrocellulose	98.0	72.0	73.25	67.25	54.6	52.15	57.75
Nitration	13.15	13.15	13.15	13.15	13.15	13.25	13.25
Nitroglycerin	-	19.75	20.00	25.00	35.5	43.00	40.00
Barium nitrate	-	0.75	0.75	0.75	-	-	-
Potassium nitrate	-	0.70	0.70	0.70	-	1.25	1.50
Potassium perchlorate	-	-	-	-	7.80	-	-
Nitroguanidine	-	-	-	-	-	-	-
Dinitrotoluene	-	-	-	-	-	-	-
Dibutylphthalate	-	-	-	-	-	-	-
Diethylphthalate	-	-	-	-	-	3.00	-
Potassium sulfate	1.0	-	-	-	-	-	-
Tin	-	-	-	-	-	-	-
Diphenylamine	1.0	-	-	-	-	-	0.75
Ethyl centralite	-	6.50	5.00	6.00	0.90	0.60	-
Graphite	0.10	0.30	0.30	0.30	-	-	-
Carbon black	-	-	-	-	1.20	-	-
Cryolite	-	-	-	-	-	-	-
Ethyl alcohol (Residual)	1.50	1.20	1.20	1.20	0.80	0.40	0.50
Water (Residual)	0.50	0.30	0.30	0.30	0.00	0.00	0.00
Butyl Stearate							
Isochoric flame temp, °K	3000	2938	3071	3081	3734	3695	3799
Force ft-lbs/lb x 10^{-3}	339	346	353	356	368	382	382
Unoxidized carbon, %	4	3.4	1.8	2.2	0	0	0
Combustibles, %	54.5	59.1	-	-	55.4	37.2	32.8
Heat of explosion, cal/gm	936	910	962	966	1255	1244	1295
Gas volume, moles/gm	0.04068	0.04219	0.04133	0.04157	0.03543	0.03711	0.03618

(continued)

TABLE IIa. COMPOSITION AND CALCULATED THERMOCHEMICAL VALUES OF SOME COMMON PROPELLANTS FOR LARGE CALIBER GUNS (CONT'D)

USE	RECOILLESS				MORTAR		
PROPELLANT	M18	T18	T25	T28	M7	M8	M9
SPECIFICATION	PA-PD-123	PA-PD-329	PA-PD-329	PA-PD-329	JAN-P-659	JAN-P-381	MIL-P-20306
Ratio of specific heats	1.2342	1.2421	1.2373	1.2383	1.2100	1.2148	1.2102
Isobaric flame temp, °K	2431	2365	2482	2488	3085	3042	3139
Density, lbs/in^3	0.0602	1.0583	0.0585	0.0585	-	-	-
Covolume, in^3/lb	27.76	29.13	28.66	28.77	-	26.63	25.97

TABLE II.b. COMPOSITION AND CALCULATED THERMOCHEMICAL VALUES FOR STANDARD PROPELLANTS USED IN SMALL CALIBER GUNS

Specification	MIL - P - 3984				
Propellant	WC846	IMR8138M	WC860	WC870	CR8325
Nitrocellulose	Remainder	Remainder	Remainder	Remainder	Remainder
% Nitration	13.15	13.15	13.15	13.15	13.15
Nitroglycerin	10.0	-	10.0	10.0	-
Potassium Nitrate	-	-	0.60	0.60	-
Dinitrrotoluene	1.0 Max	-	1.0 Max	1.0 Max	-
Dibutylphthalate	4.90	-	7.60	5.20	-
Potassium Sulfate	-	0.70	-	-	0.75
Diphenglamine	0.90	0.70	0.90	0.90	0.60
Graphite	0.4 Max	0.20	0.4 Max	0.4 Max	0.4 Max
Methyl Centralite	-	-	-	-	3.50
Ethylene Dimethacrylate	-	3.25	-	-	-
Tin Dioxide	-	-	-	0.80	-
Calcium Carbonate	0.1%	-	0.1%	0.1%	-
Moisture & Volatiles	1.00	0.80	1.00	1.00	1.00
Density gm/cc	0.980	0.900	0.950	0.950	0.970
Isochoric Flame Temp.°K	2860	2900	2600	2800	2800
Force, ft-lbs/lb x 10^{-3}	334	330	318	327	319
Unoxidized carbon %	0	0	0	0	0
Combustibles %	0.59	0.57	0.59	0.59	-
Heat of exp., Cal/gm	883	-	802	872	-
Gas Vol. Moles/gm	0.042	0.041	0.044	0.042	0.041
Ratio of specific Heats	1.24	1.24	1.25	1.24	1.24

NOTE: Values for propellants are general averages; numbers for individual lots vary.

TABLE III. EFFECT OF POLYURETHANE FOAM LINERS ON THE EROSION LIFE OF SOME ARMY GUNS

	Round	Type	Pressure PSI	MV (ft/sec)	Erosion Life	
					Standard Amm.	Polyurethane Foam
90mm	M318	APT	52,000	3000	700	1900
105mm	M392	APDS	55,000	4850	200	400
120mm	M358	APT	54,000	3500	300	700

TABLE IV. EFFECTIVENESS OF TiO_2 + WAX LINERS IN INCREASING THE WEAR LIFE OF SOME ARMY GUNS

	Round	Type	Pressure PSI	MV (ft/sec)	Additive Wt.(Oz)	Without Rds.	With Rds.
90mm	M431	HEAT-T	53,000	3950	4.0	240	2100
90mm	M353	TP-T	52,000	3000	6.0	700	2100
105mm	M456	HEAT-T	58,000	3850	4.5	125	1000
105mm	M392	APDS-T	55,000	4850	8.0	100	10000*
120mm	M469	HEAT-T	41,000	3750	17.0	350	1750
175mm	M65			3000		375	1200
155mm How	XM119	M30			13.0	700	2100

*Extrapolated

TABLE V. COMPOSITION AND SOME THERMOPHYSICAL PROPERTIES OF SOME ADVANCED EXPERIMENTAL PROPELLANTS WITH LOW T_f AND HIGH IMPETUS

COMPOSITION	M17	M30*	XM35	M26E	M8*	PPL-A-2923
Nitrocellulose (%Ni in NC)	22 (13.15)	28 (12.6)	58.5 (13.15)	68.7	52.15	44.5
Nitrogylcerine	21.5	22.5	-	25.0	43.00	-
Nitroguanidine	54.7	47.7	-	-	-	-
Ethyl centratite	1.5	1.5	1.5	6.0	-	1.5
Cryolite	0.3	0.3	0.3	0.3	-	-
Graphite	0.1	0.1	0.1	0.1	0.1	0.1
Trimethylaethane teinitrate (TMETN)			25			25
Triethylene glycol dinitrate (TEGDN)			10			24
Diethylene glycol dinitrate			5			
Butanetriole trinitrate (BTTN)						5
Total volatile	0.3	0.3	1.0	0.9	0.40	0.01
KNO_3					1.25	
Diethylphthalate					3.00	
Thermochemical properties						
Isochoric flame temp, °K	3017	3040	3030	3132	3695	3386
Force ft-lbs/lb	364000	364500	365700	362800	381500	390.8
Gas volume moles/gm	0.04336	0.04308	0.04339	0.04164	0.0374	0.04149
Unoxidized C %	3.9	3.2	3.7	1.6		0.5
Combustible CO + H_2	38.7	41.2	57.6	56.2	37.2	50.8
Ht of explosion cal/gm	962	974	976	949	1237	1096
Physical Strength	147	323	-	-	-	-
Side impact test oz.						
Tensile stress 145°F psi		2733	2850	4390	609	3669
Compressive stress at 40°F		8.2	42.3	9.2	40.6	48.4

*M30 and M8 propellants are included in this table for comparisons.

TABLE VI. SOME MATERIALS WITH DEMONSTRATED EROSION RESISTANCE

Steels - For Barrels with Low Temp - Low Pressure Rds.
L-605
Stellite 21
Chromium, Cr 60, Fe 25, M 15
Mo + 0.1% Co
W , Tungsten Alloys
Ta - 10W, T111, T222
Cb - 1% Zr, Columbium Alloys

FIGURES

FIGURES (CONT'D)

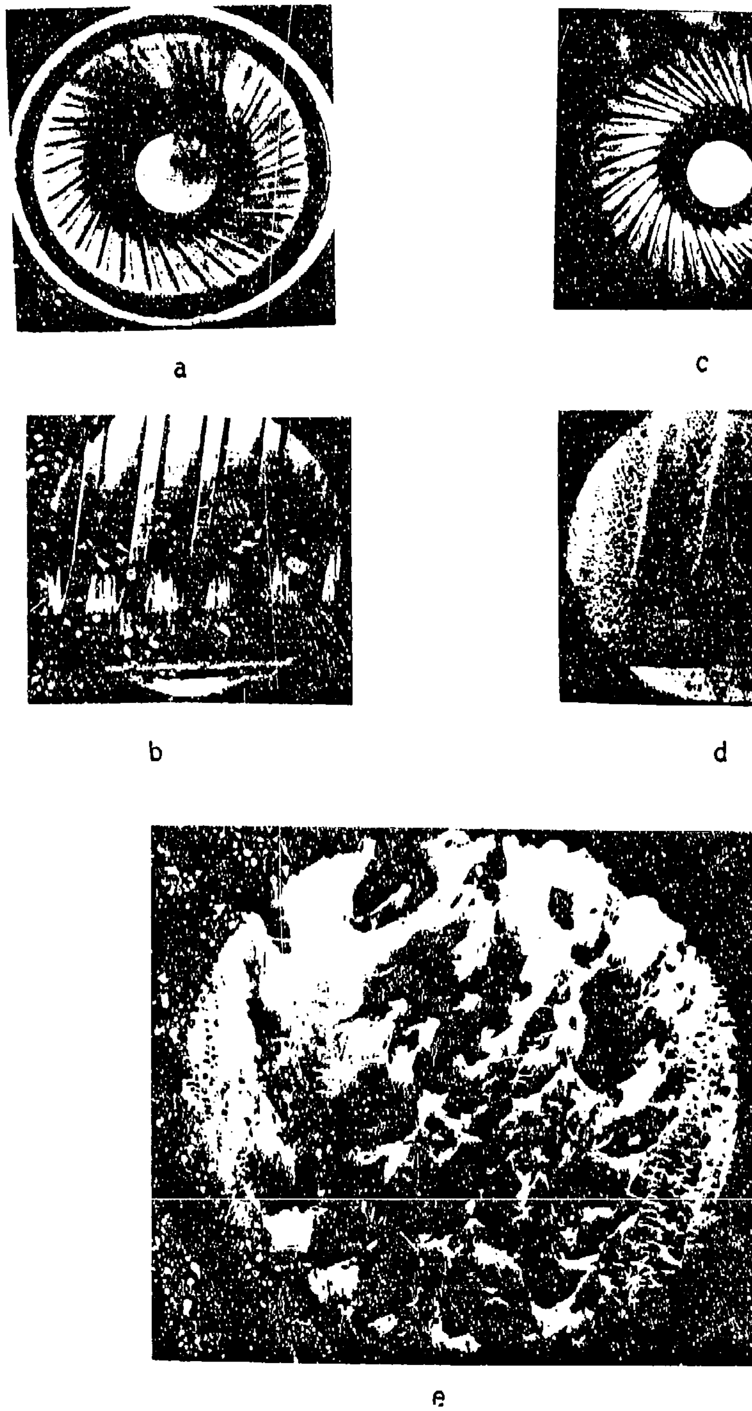


Fig. 1. Progressive damage of the bore surface of 105 M68 barrel in the O.R. area as a result of firing - a,b - after 2 rounds, c,d - after 702 rounds, and e - after 1744 rounds.

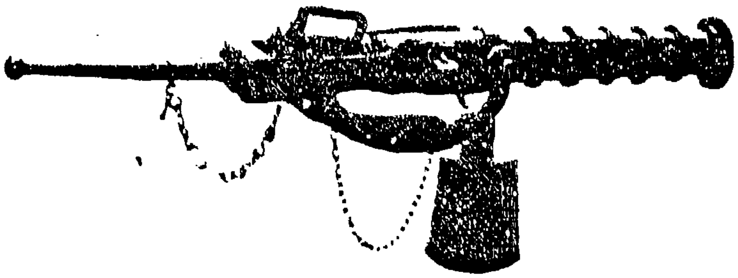


Fig. 2. Patrera, an early (1461-1463) barrel forged from wrought iron bars, hooped together with iron rings.

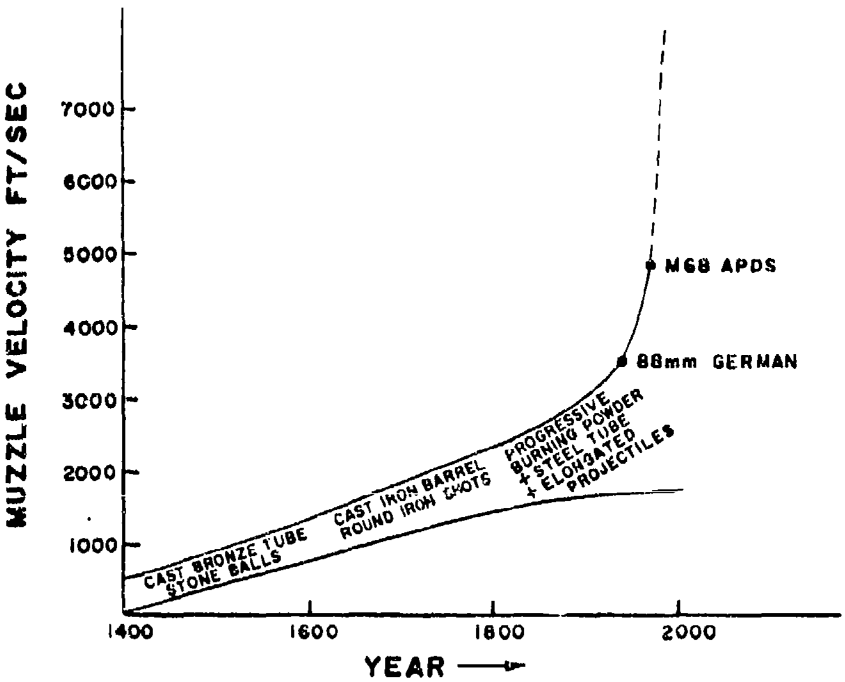
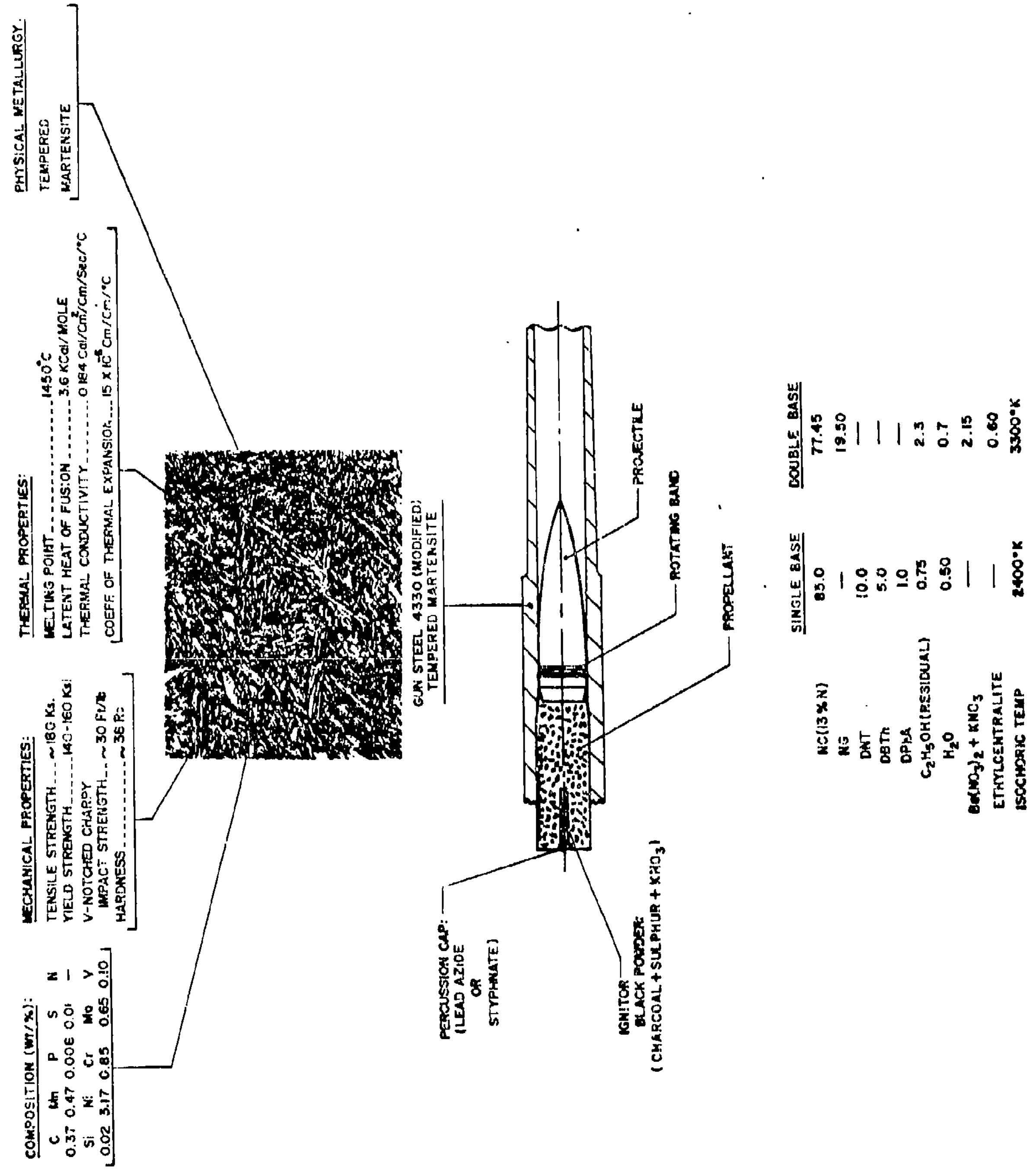
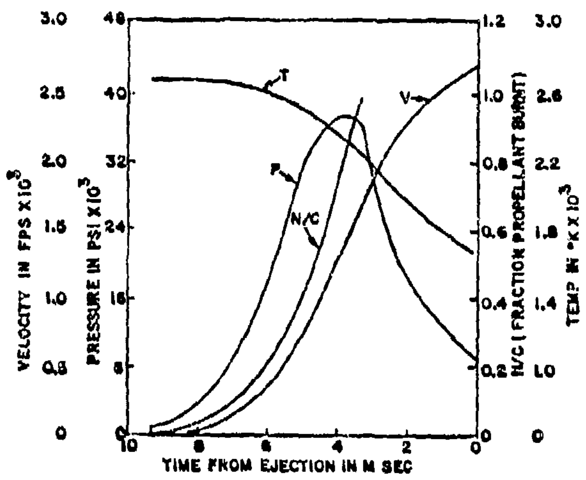


Fig. 3. Trend of the increase in muzzle velocity of guns since 1400 A.D.

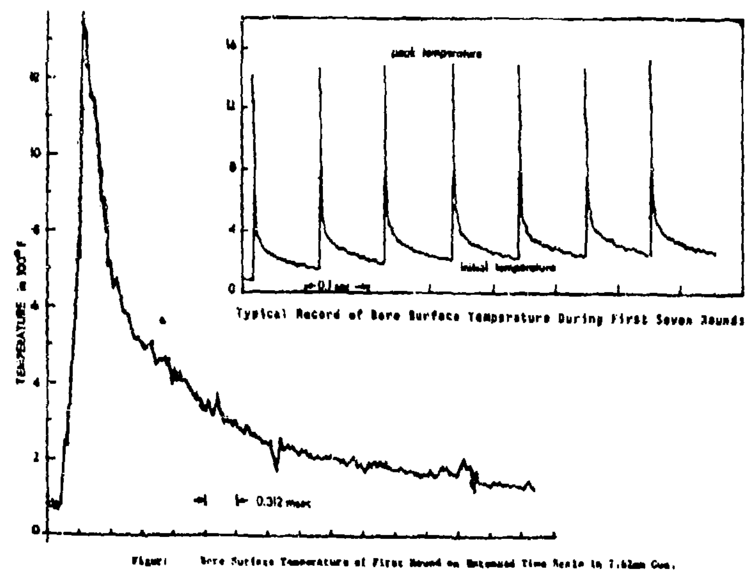


<u>Fig. 4</u>. Elements involved in the process of erosion of a gun.



Theoretical Ballistic Curves for 3" Gun (Cardruck Range).

Fig. 5. A typical set of ballistic curves, showing variation of pressure (P), muzzle velocity(V), fraction of the propellant burnt(N/C) as a function of time, for a 3 in. gun.



Typical Record of Bore Surface Temperature During First Seven Rounds

Fig. 6. Temperature profile of bore surface of a 7.62mm gun as a result of a single shot, and repeated firing.

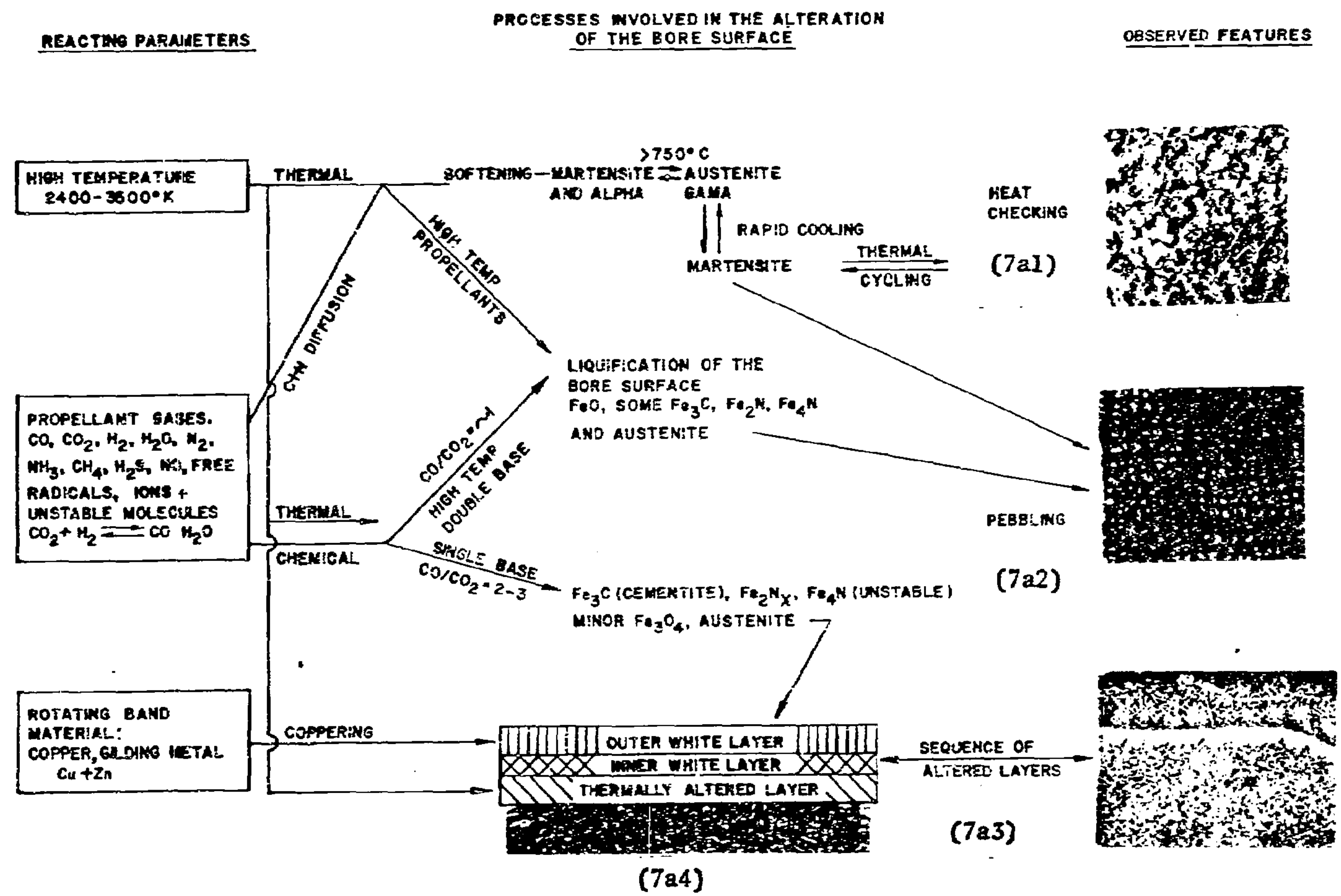


Fig. 7a. Processes involved in the alter ation of the bore surface during firing.

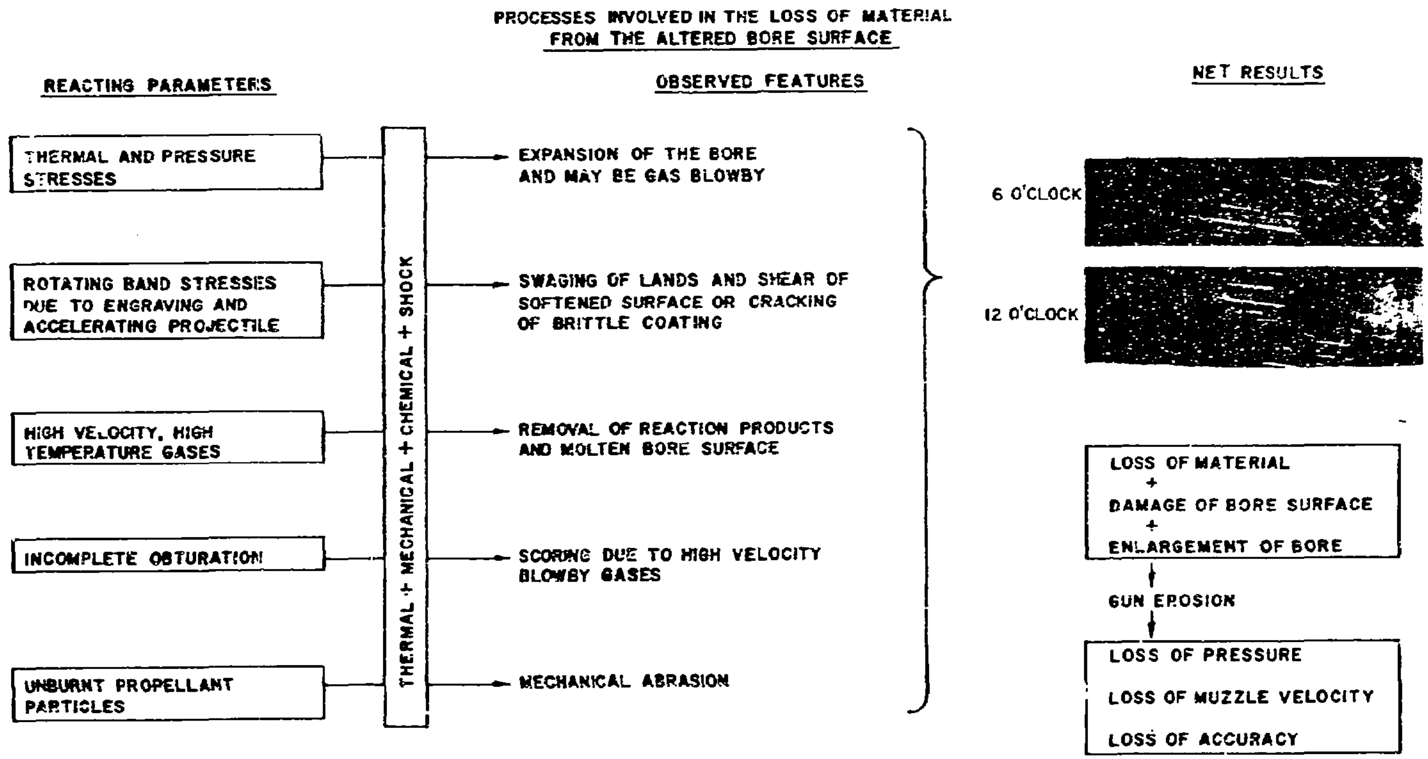


Fig. 7.b. Processes involved in the loss of material from the altered bore surface.

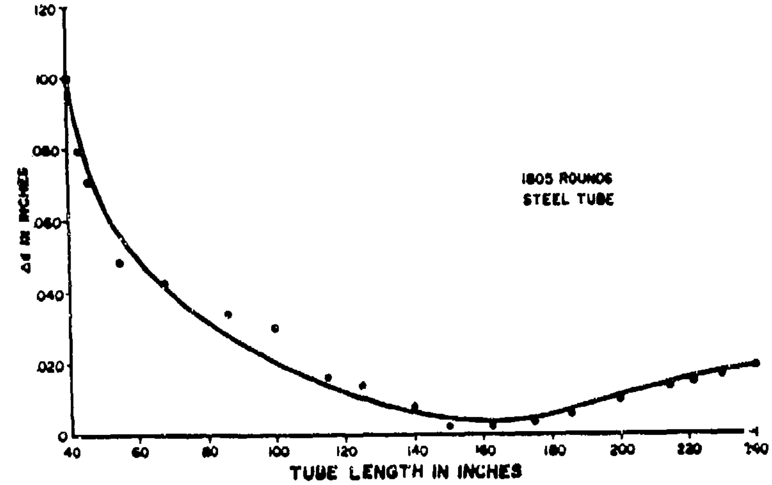


Fig. 8. Erosion profile of one of the experimental 155mm XM199 barrel (ref. 10).

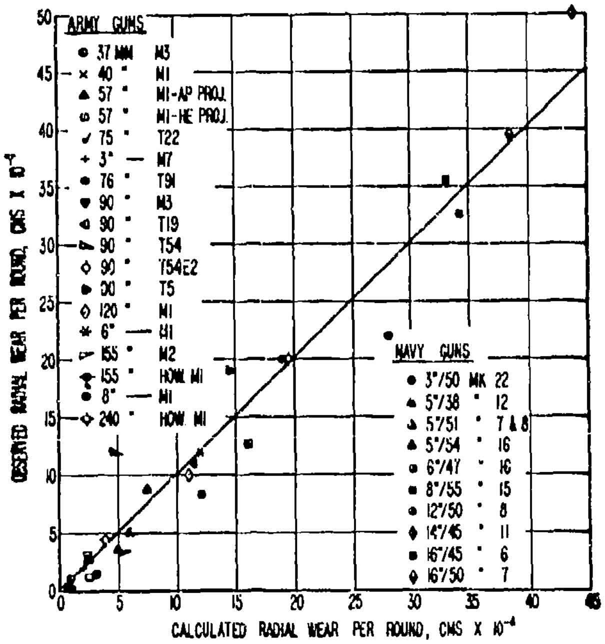


Fig. 9. Calculated wear rates of 13 Army and 11 Navy guns as compared with compared with the observed values

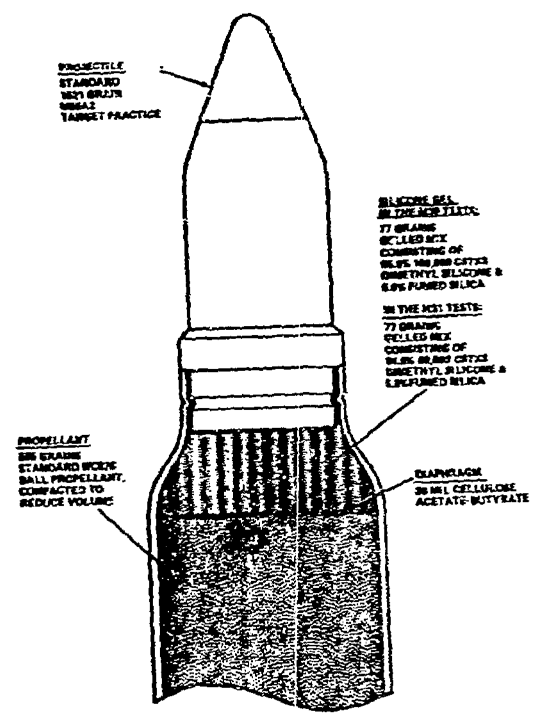


Fig. 10a. Showing the positioning of the ablative in the cartridge.

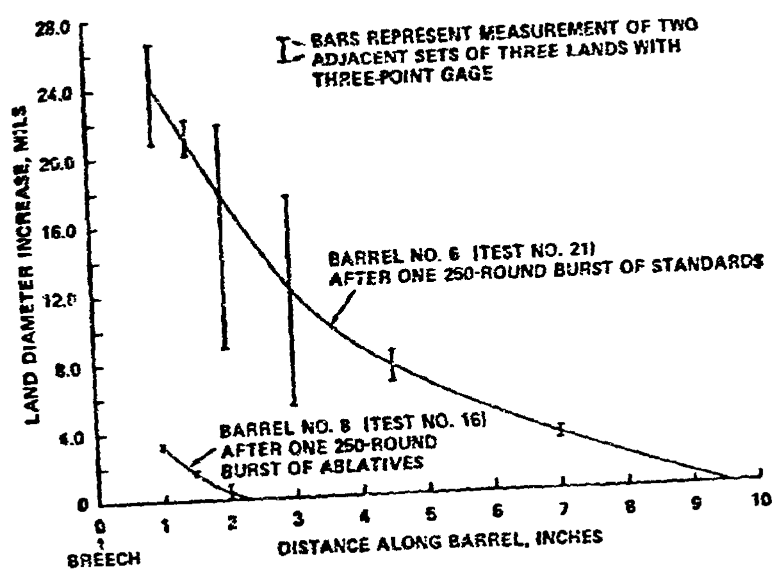


Fig. 10b. Wear reducing effect of the ablative ammunition in M39 gun.

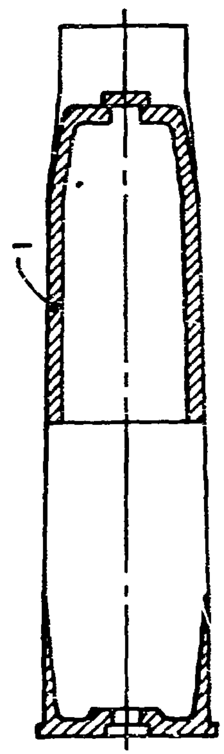


Fig. 11. Showing TiO_2 + wax liner with flaps in a cartridge.

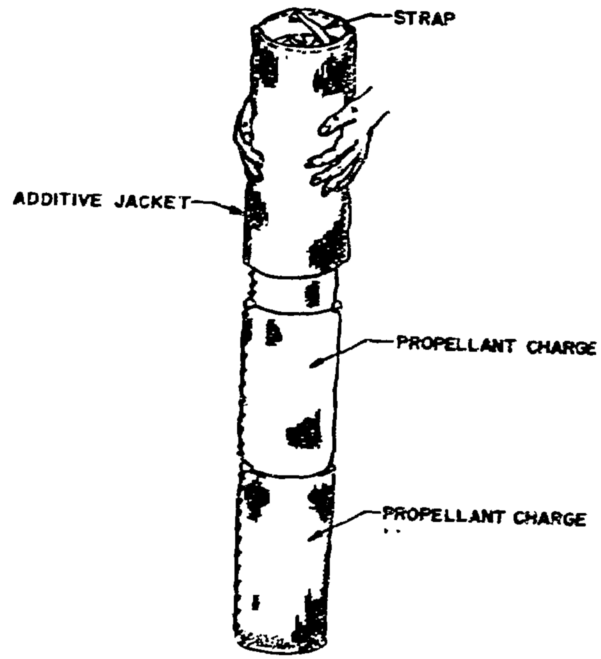


Fig. 12. Additive jacket placement on a bagged ammunition.

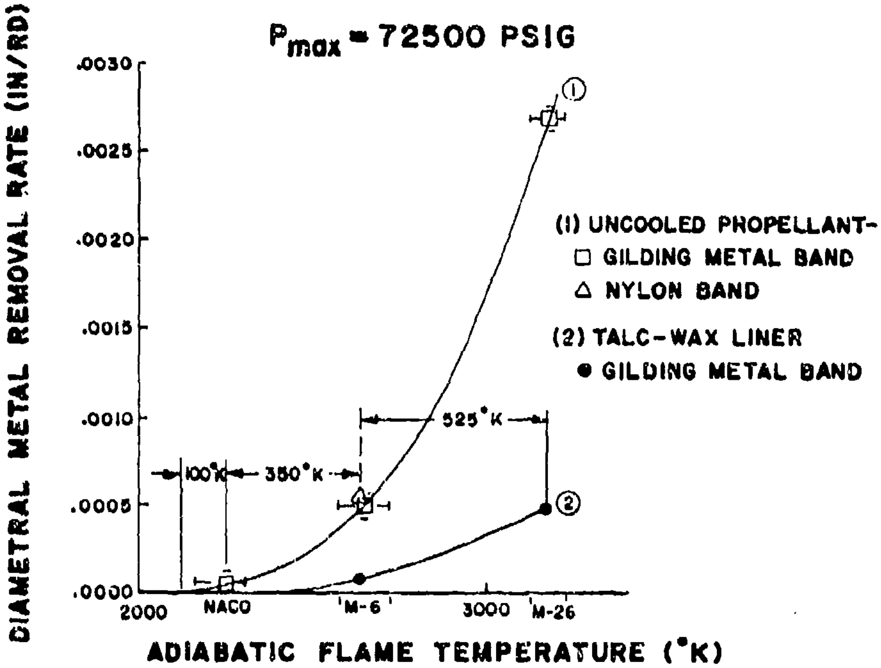


Fig. 13. Comparison of the performance of the plastic bands vs gilded band in a 5"/54 gun

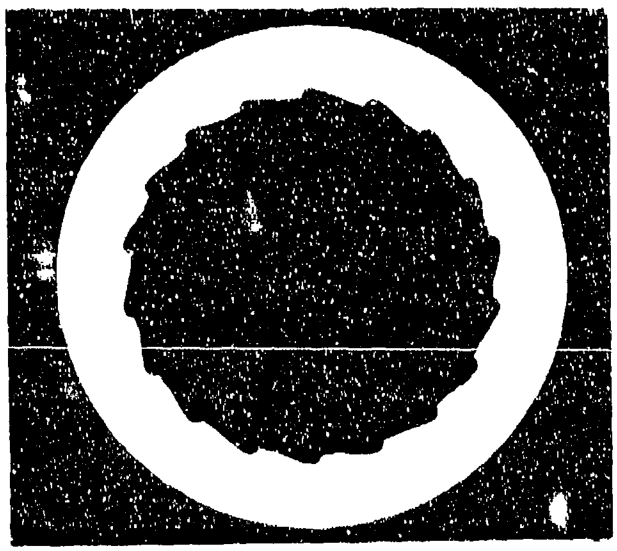


Fig. 14. Sawtooth-Rifling in a 20mm M-61 barrel.

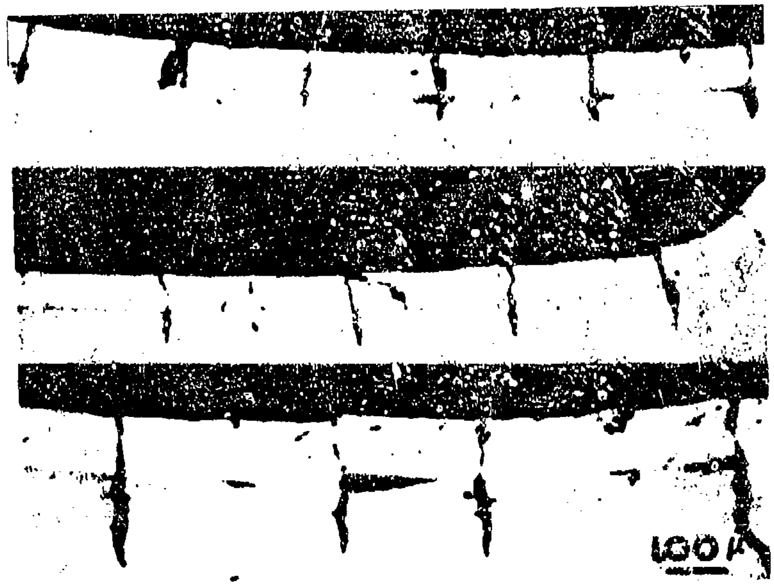


Fig. 15. Micrographs showing the progressive cracking of chrome coating in a 7.62mm gun after 900, 1500 and 3000 rounds (Ebihara, Ref. 7, 1.4-17).

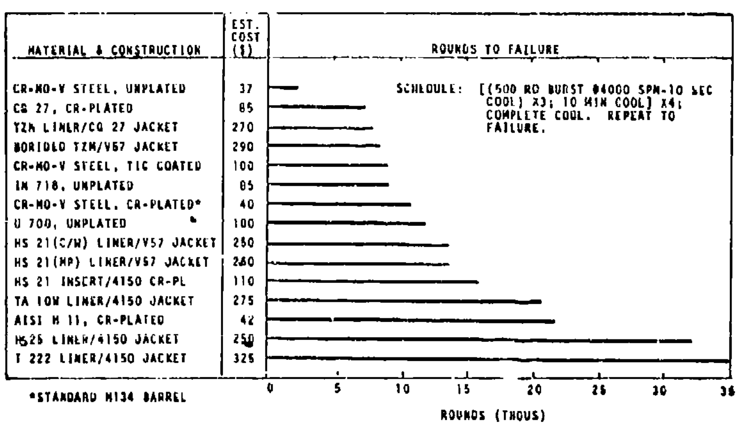


Fig. 16. Comparative performance of a number of liner materials in a 7.62mm M134 gun.

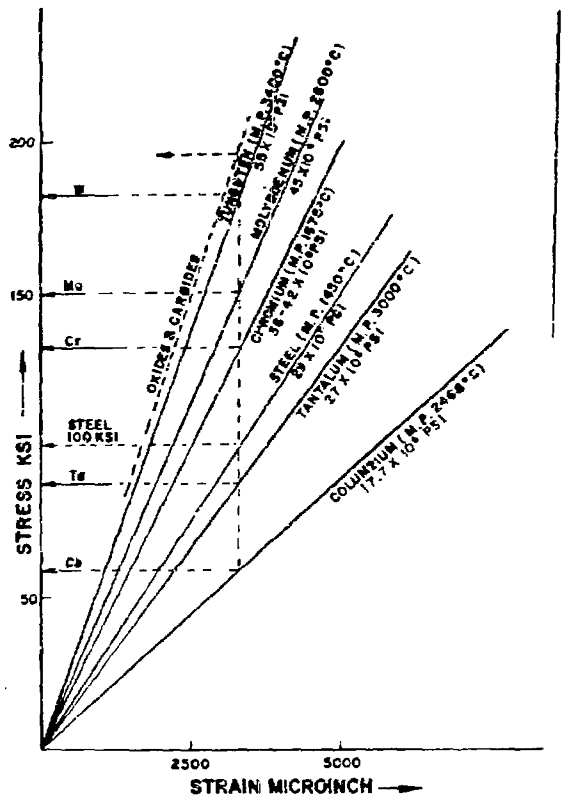


Fig. 17. Stress-strain curves for a number of refractory metals.

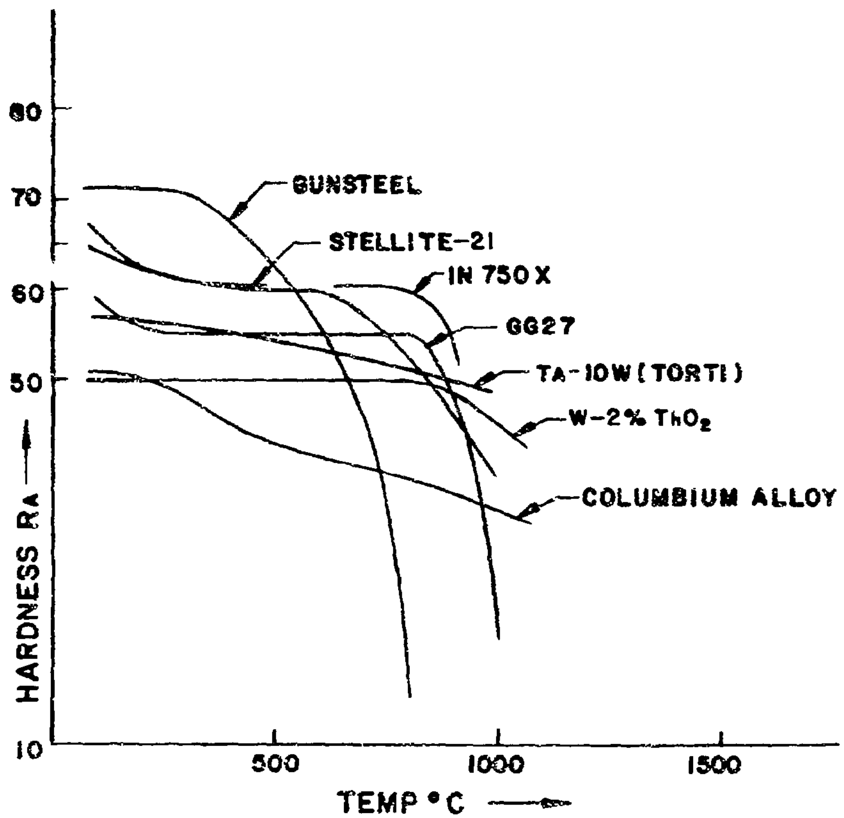


Fig. 18. Hot hardness data for a number of materials obtained with a modified Rockwell hardness tester.

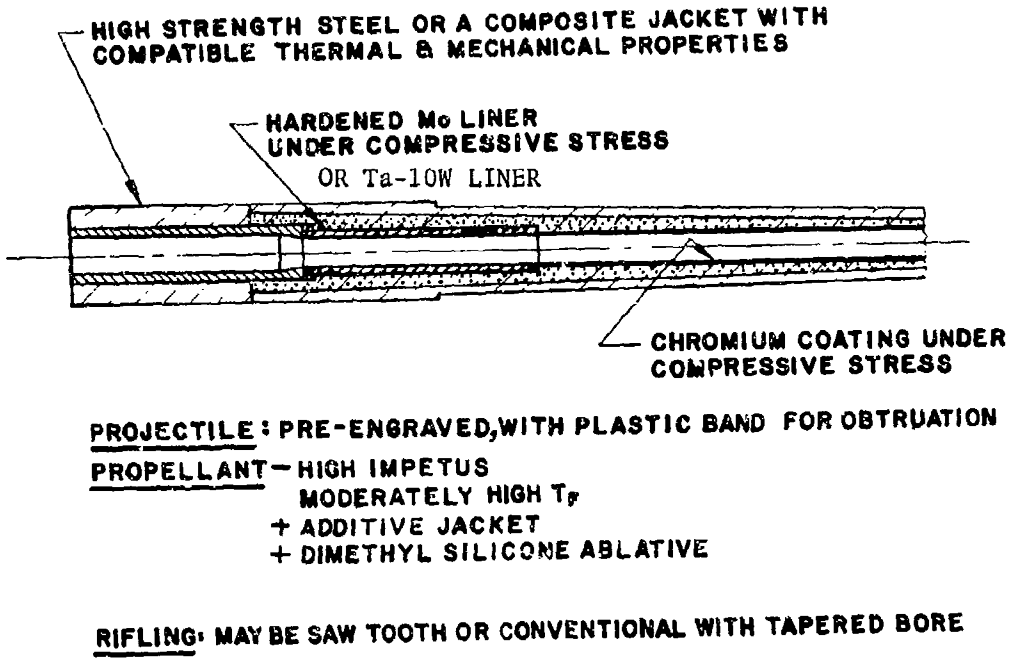


Fig. 19. A concept for "A - Z Gun"

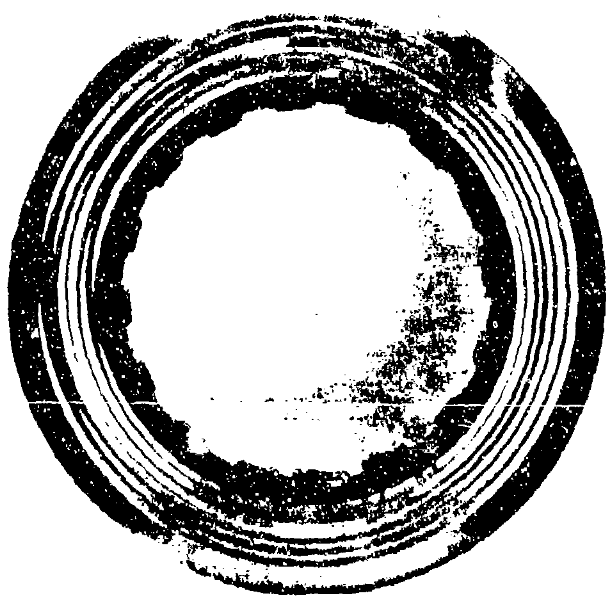


Fig. 20. Section of a 30mm XM140 barrel, with a TZM wire - 316 stainless steel jacket reinforcement, applied by plasma spray - Hot isostatic pressing process.

A NEW INITIATIVE IN GUN BARREL WEAR AND EROSION

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ABSTRACT

The Army recently fielded two wear-limited howitzers, the 155 mm M198 stowed howitzer and the 8-inch M110A1 self-propelled howitzer. During proceedings leading to type-classification of each system, concern was expressed by DA that future gun developments will be plagued by unpredictably high wear rates. Accordingly, DDR&E and DA requested a review of the gun barrel wear and erosion technology program. These briefings failed to satisfy DA and DDR&E staff that the technology program was properly directed. DARCOM was directed to task the ARRADCOM associate director for research and technology to provide a program that addresses both short-term solutions to fielded wear-limited weapons and also extends the technology base in order to forecast and alleviate wear and erosion problems in future weapon developments. The program and the plan was constructed by a group representing ARRADCOM, AMMRC, ARO, AMSAA, and the Navy and Air Force. The plan includes: an assessment of wear and erosion problems in fielded and future Army weapons; a forecast of what the Army development community can do to increase the wear life of fielded howitzers; a summary of the proposed program; and the relationship between the Army program and on-going work in the Navy and the Air Force. The plan was presented by J. R. Ward to HQ, DARCOM on 11 January 1977.

Vu-graph number one is the plan of presentation. This talk is divided into four parts. In the first part we're going to describe gun barrel wear and erosion. In the second part, we will attempt to demonstrate why we think the problem has become of such importance to the Army. We'll then make a technology assessment and forecast what we in the wear and erosion community think we can do in the near term to solve immediate problems. And finally, there will be a detailed analysis of the program. Vu-graph number three lists the people who contributed to the development of the program. I'd like to draw attention to the fact that people were represented not only from ARRADCOM, but also from AMMRC, ARO, AMSAA, as well as representatives from the Navy and the Air Force.

The following vu-graph shows why we feel a new initiative in this problem area is needed. The main thing we want to do is increase the wear life of fielded guns which are now wear limited. Secondly, we want to develop technology to anticipate future wear and erosion problems and then to extend the state-of-the-art gun barrel wear and erosion technology to meet these future requirements.

The next vu-graph defines terms. The point I want to make is that the wear life of a gun barrel is not a function only of the enlargement of the bore; rather, the wear life of the gun barrel is a function of unacceptable ammunition performance, and the unacceptable performance arises because of the in-bore motion of the projectile as it travels through the worn gun tube. Our attention, however, is focused primarily on the thermal, mechanical, and chemical processes that lead to the enlargement and deformation of the gun barrel.

The next vu-graph is an attempt to show how the enlargement of the bore is related to the wear life of the gun tube. Because the erosion is normally most severe at the origin of rifling, the wear life of the gun tube is related to the bore enlargement at the origin. In practice, a gun tube is fired until one of the criteria for unacceptable ammunition of performance occur. At that point, the bore enlargement at the origin is measured and that measurement then becomes the condemnation limit for the gun tube. Also this vu-graph demonstrates the wear is linear with number of rounds fired up to a certain point and then the wear rate decreases. The causes of wear and erosion are summarized in the next vu-graph. They are heat, mechanical forces, and chemical reactions. The major cause of wear and erosion is the heat input to the gun barrel surface. Therefore, our short-term program is designed to either reduce the heat input to the gun barrel surface or to increase the thermal resistance of the gun barrel surface.

The next vu-graph summarizes previous information and serves as the framework of the Army's gun barrel wear and erosion program. At the left is our goal - optimized wear life. As was pointed out earlier, optimized wear life is a function not only of the gun barrel wear and erosion, but also the in-bore projectile motion. Our program, as stated before, is concerned with wear and erosion. In the 6.1 area we're concerned with understanding the mechanisms. In the 6.2 area we're concerned with reducing or controlling gun barrel wear and erosion such that we have fatigue limited gun systems. The mechanisms we need to determine are the interactions between the propellant and the barrel and the interaction between the projectile and the barrel. For each of these categories we find there is thermal, mechanical, and chemical aspects which must be unraveled. The goal is an a priori model of gun barrel wear and erosion such that we will be able to predict the wear life of gun tubes and suggest ways to increase the wear life. In the 6.2 area, the first category constitutes techniques which increase the thermal resistance of the gun barrel. The other techniques, namely wear reducing additives, ablative coolants, non-metallic bands, and less erosive propellants, are designed to reduce the heat input to the gun barrel.

This concludes the first section of the presentation.

The next section is an attempt to describe the nature of the problem or why is gun barrel wear and erosion so topical within the DOD community? The Army elected to develop gun/howitzers. That meant that in addition to firing the long range top zones, the gun had to also fire the shorter range, lower zone charges. This restricted the chamber volume. In order to reach the longer ranges, high-force, high-flame temperature M30 propellant was used. As seen in the vu-graph, it was presumed that the wear life was so much greater than in the fatigue life in the old howitzer systems, that the introduction of the M30 propellant and the use of wear reducing additives would result in a wear life comparable to fatigue life. What happened was that not only were the wear life estimates, optimistic, but the wear life was not measured until wear tests were conducted late in 6.4 development. In addition to the problem with the new extended range howitzers, a problem surfaced in the M68 tank cannon. The secondary wear results from the introduction of wear reducing additives in the HEAT rounds. The wear reducing additives are effective at the origin of rifling, but their effectiveness diminishes down the barrel. This new wear profile altered the in-bore characteristics of the armor-piercing discarding-sabot ammunition. If we were to fire only 392A2 projectiles with TiO_2/wax liners, the wear life of the gun tube and the fatigue life would be the same - 1000 rounds. There's an excellent possibility that the condemnation limit for firing of M392A2 projectiles will have to be dropped to 56mil enlargement at the origin of rifling due to the secondary wear. This would mean that one would only be able to fire 750 training rounds through the M68 tank cannon. So in effect, the M68 tank cannon will also be wear limited in the future if the condemnation limit is dropped from 75 to 56mils.

The next vu-graph shows cost for replacing current wear limited tubes. This vu-graph is intended to show in dollars what we've tried to demonstrate in the earlier figures. The cannon for which we consider to be the cannon requiring immediate attention are shown in the left column. The cost per tube and the number of these cannons ordered to be produced in the next four years is also shown.

The next vu-graph shows future systems and where wear problems may exist. This vu-graph is divided into anti-armor, air defense aircraft and small arms systems. Under anti-armor, we expect by the early 1980's to be fielding a 120mm gun for the next XM1 tank. At present, the wear life requirement is undetermined. The only available data we have for such a gun is the Federal Republic of Germany's 120mm gun on their LEOPARD II with a quoted wear life of 400 rounds. In the future, we expect to field a medium caliber automatic anti-armor cannon (MC-AAAC) within the 60-90mm caliber range. Wear life requirements are also undetermined. At present, in single shot mode, we experience a wear of 1/2 to 1 mil per round. Such a gun will be unacceptable because of this wear rate. In air defense we expect to see the 25mm BUSHMASTER be placed on the new MICV. The wear life requirement is 4000 rounds. A wear test is scheduled for 1977. It is anticipated that the required wear life will be met. Another air defense gun will be the 30-40mm division air defense gun (DIVAD). It is intended to replace the M168 in the VULCAN air defense system. Requests for proposals will go to industry for the DIVAD during 1977. The wear life requirement is 5,000-10,000 rounds desired, less will be acceptable. One of the candidate guns, The Oerlikon KDA-35 has a wear life of 2500 rounds. In aircraft guns, the 30mm XM230 or XM188 will be placed on the new advance attack helicopter. This weapon is to be fielded in 1983. The requirement for 15,000 rounds is easily met by either gun. In the area of small arms, the only potential wear problem that we can see would be in a small caliber weapon, such as a 4.32mm rifle. Future rifles or machine guns in such small calibers will experience erosion problems, thus one of the targets of the erosion technology program will be to understand the model erosion in such small caliber guns. In the far future, the kind of weapons we are looking at are air defense or anti-armor systems whose muzzle velocities approach 10,000ft/s. Examples of guns in the concept stage that will launch projectiles with such velocities are indicated in this vu-graph. The purpose of the technology program is to be able to predict what the erosion will be for each of these concepts, and if necessary, to come up with techniques to reduce the erosion in such guns.

This concludes the second part of the briefing.

The third part of the briefing is an assessment of the current technology in order to show what we feel we can do in the short term. The next vu-graph is the plot of the efficacy of wear reducing additives in rounds fired with M30 propellant. The following points need to be made: first, the TiO_2/wax liner is more effective in cased ammunition than in bagged charges. Secondly, the down tube effectiveness of the TiO_2/wax liner is increased by the use of flaps. The TiO_2/wax liner is more effective than polyurethane foam. Our approach is to increase the effectiveness of fin-stabilized, cased rounds and bagged, separately-loaded charges by mimicking the TiO_2/wax liner with flaps that is used in the M392 round which increased wear life of the M68 tube by a factor of 100. The next vu-graph shows some data which we've already obtained which shows that by mimicking the liner in the 392 round in a bagged charge, we can increase the wear life. The left side of the vu-graph lists charges in four categories. The categories represent similar heat inputs. The charges with the lines highlight charges for which we already knew what the wear life was from previous wear tests. I'd like to draw your attention to the XM203E2 charge. We see that the introduction of the standard TiO_2/wax liner reduces the heat input from 793 joules to 702 joules. This corresponds to an increase in wear life from less than 1000 rounds to 2500 rounds according to our categories. The actual wear life of the XM203E2 charge is about 2000 rounds. By mimicking the liner in the M392, the heat input for the XM203E2 charge was reduced from 702 joules to 650 joules. This translates to doubling the wear life. The actual increase in wear that will be obtained by this technique must be determined by a wear test. This data merely indicates that we do have some basis for feeling that we can increase the wear life of bagged charges by modifying the wear reducing liner. The next vu-graph compares the wear vs distance from the rear face of the tube of the M68 tank cannon firing no additive to that firing the TiO_2/wax additive. We see in curve C that after firing 6,000 rounds, the wear 32 inches from the rear face of the tube is still only 20mil; much less than the wear associated with the HEAT training round. Thus, we feel that by mimicking the TiO_2/wax liner in the HEAT and HEAT training round, we will be able to reduce the secondary wear in those projectiles.

Another technique we intend to employ to increase the wear life of these tubes is to take advantage of chrome plating. A key point that has been learned in the technology program is that the thickness of the chrome plating placed on the steel is important. This vu-graph is a plot of the number of rounds that must be fired to remove chrome plating as a function of the flame temperature of the propellant. We see the following points made: First of all, the lower the flame temperature, the more rounds needed to remove the chrome plating. We also see the larger the caliber of the gun, the fewer rounds needed to remove the chrome plating. Most of this data is for Naval guns. Data for the Army guns is listed for the 105mm M456 round where we see that 150 rounds must be fired in order to remove the chrome plating. When this plating is doubled to 10mil thick chrome, 400 rounds need be fired before the chrome is removed. This graph also shows that if we use a wear reducing additive to reduce the heat input to the barrel, it gives us hope that we will be able to increase the number of rounds that can be fired before the chrome plate is removed. The next vu-graph is a plot of the wear vs distance from the origin of rifling for 12mil thick chrome plate. In one gun we have a fully plated tube; the broken line is the wear profile for a partially plated gun tube. The point we like to make here is that for distances well down the tube from the origin of rifling, the steel is more resistant to wear than is the chrome plate. So our future tube will not only be chrome plated, but we intend to plate only the region where the chrome is more wear resistant than the steel is. Another possible technique which we could exploit to reduce the heat input to the barrel to increase the wear resistance of the steel is the use of non-metallic rotating bands. The Air Force has already demonstrated that non-metallic rotating bands in the rapid fire aircraft cannon increased the wear by at least a factor of two. The Navy has demonstrated that in a rapid-fire 3 inch/54 gun, the use of non-metallic bands increases the wear life of the tube. Unfortunately, we also have data that suggests that the wear of the gun tube firing non-metallic bands in a single shot mode may not be as advantageous for wear at the origin of rifling. The vu-graph for the 20mm barrel and also the vu-graph for the Navy gun that follow, show that in a single shot mode, the non-metallic band offers no advantage over metallic band in a single shot. However, we are also going to test whether the non-metallic band offers any advantage in reducing the wear at the origin of rifling in the M119 and M201 guns. In the technical forecast to follow, we're going to make the assumption that the non-metallic bands offer no improvement.

The next vu-graph is a forecast of what we feel we can do in the M185 cannon. We feel that the modification to the wear-reducing liner will raise the wear life to 4000 rounds. In the M185 cannon or for the zone 7 M199 cannon, the chamber is not filled with propellant, so in addition to using a wear-reducing additive, another technique to increase the wear life is to use a base-ignited cool propellant. Such propellant was used in the center-core ignited M119 charge which we know has a wear life of 5000 rounds. By using a wear-reducing additive with the base ignited cool propellant, we feel we should be able to exceed the fatigue life. By using a properly designed wear reducing liner, we feel we can approach a 15,000 round wear life for either the M185 or the zone 7 M199 cannon. A gap of two years is shown on the chart to allow the charge design community to develop the base ignited cool propellant charge.

In the zone 8 M199 cannon, we feel that by improving the TiO_2/wax liner to mimick the M392 liner we can approach a wear life of 4000 rounds. We further feel that by chrome-plating the tube, we'll be able to increase the wear life to 5000 rounds. We feel that either the use of ablatives, or possibly a novel wear reducing additive coming from our technology work will result in a further increase in the wear life to 6000 rounds. In the future, further development of new propellants such as the high force low flame temperature nitramines will give us an even further increase in wear life such that we will have a gun that is fatigue limited rather than wear limited.

The M201 cannon is already chrome plated. We feel that by the use of an improved TiO_2/wax additive, we'll be able to increase the wear life by a factor of two. Then we forecast either the ablative coolant, the novel additive, or a novel propellant, coupled with the chrome plating we'll be able to increase the wear life approximately to 6000 rounds. The next vu-graph summarizes this technology forecast. We would anticipate that redesigning the TiO_2/wax liner and the use of partial chrome plating would be low risk and give us the wear life estimates indicated. The ablative coolant, a novel wear reducing liner, or a novel propellant would be considered to be high risk. However, if such techniques become feasible we feel that each of these howitzers can be made fatigue limited.

The remainder of the briefing is an outline of the program which we intend to follow in order to address the issues raised. This vu-graph lists the objectives of the 6.1 program. The next vu-graph summarizes the pay-offs which we expect if we could understand the mechanisms by which barrels erode. In the next vu-graph we wish to distinguish between the program being pursued through the in-house army laboratories as opposed to the type of work sponsored by the ARO. The next vu-graph lists the 6.2 objectives. The following vu-graphs summarize work going on in other services. In the Navy there is no formal wear and erosion program, since the Navy does not consider erosion to be a critical problem. The work that is on-going is in support of the Marine Corps. The erosion effort in progress is being performed at Dahlgren and the kinds of work pursued are indicated. Basically, the work is parallel to what the Army is doing and we seek to fund Navy work in FY 78, particularly in the area of ceramic liners. From previous Air Force work which demonstrated that the non-metallic bands increase the wear life of automatic fire aircraft cannon, the Air Force has committed itself to placing non-metallic bands on ammunition fired from all future systems. The Air Force is starting a formal program in FY 77 of approximately \$300,000 to understand the interaction between the rotating band and the rifling with the assumption that if they can find ways to keep the rotating band intact, the wear life that results will be accepted. The program that the Army has put together will draw heavily on this Air Force work. This is one of the reasons that the non-metallic rotating band portion of the Army program will look lower in comparison to other techniques. Another agency sponsoring erosion related work is

DARPA who sponsored a 75mm solid propellant candidate for the AAAC. In this gun both ablative coolant and removable liners are being examined as techniques to bring the wear life of the 75mm cannon to an acceptable limit. DARPA also sponsored work in liquid propellant guns.

The 6.2 program is summarized next: First we have techniques to reduce the heat input. We're looking at nitramines, consolidated propellants, and liquid propellants. The erosion program is not intended to develop the propellant to the point where it could be fielded, but rather, to examine the erosion characteristics of such propellants. In wear reducing additives, as indicated, we will be mimicking the additive in the M392 round and we'll also be looking at talc as a substitute for TiO_2. In order to devise a new wear reducing additive, we're using boundary layer codes developed under NASA sponsorship to model the ablation of nose cones during re-entry. Non-metallic rotating bands are being investigated for large caliber use; for the automatic cannon caliber, we're relying heavily on the Air Force for results. Ablative coolants have been shown to be superior in reducing heat input than wear reducing additives, but packaging the ablative is a problem. We think they would be most effective if the ablative is packed with the projectile. For increase in temperature resistance to the bore surface, we are examining either platings or liners.

The following vu-graph summarizes the fiscal requirements for our program. The funds listed in FY 77 are committed. In FY 78 the numbers in parentheses represent guidance figures as of Dec 76. The remaining numbers represent total requested for the fiscal year indicated. The ARO related work is summarized in a separate vu-graph. I wish to make clear that these numbers represent the funds that have been committed to date. ARO expects to spend up to $600,000 this year and the next in support of gun barrel wear and erosion.

The next vu-graph is the cost in millions of dollars that will have to be spent over the next 20 years in replace 8" M201 tubes. The broken lines represent costs if we can improve the wear life of such tubes. This vu-graph is intended to show why a new initiative in wear and erosion is being pursued. Another important cost will come in the area of wear testing. The next vu-graph lists the number of rounds that would have been required to be fired in the XM201E2 program to learn that the additive was ineffective with the clean-burning igniter. In the future we expect either this technique or a radioactive measurement technique will be a standard procedure to design an optimum wear-reducing additive before we proceed into a full scale wear test in 6.4.

The final vu-graph contains the conclusions. By pursuing the wear and erosion program just outlined, we feel the wear life of fielded howitzers may be doubled. We feel that the M68 tank cannon can be made fatigue limited without resorting to chrome plating by altering the location of the liner in the HEAT rounds. We feel that the wear life of rapid fire automatic aircraft and air defense systems may be doubled with non-metallic rotating bands as shown by the Air Force work. We also feel that the medium caliber AAAC and future hypervelocity anti-armor and anti aircraft weapons will require new coatings or liners, since it appears we're reaching the erosion limits of gun steel even when used with wear-reducing liners. In future hypervelocity systems with muzzle velocity in excess of 5000ft/s it will likely require a combination of novel propellant charges coupled with new materials.

PLAN OF PRESENTATION

- Description of gun barrel wear and erosion.
- Statement of the problem.
- Technology Assessment and Forecast.
- What we propose to do and why.

CONTRIBUTORS TO DEVELOPMENT OF PROGRAM

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P. Parrish

AMSAA

D. Barnhart

NAVY

M. Shamblen

AIR FORCE

D. Uhrig

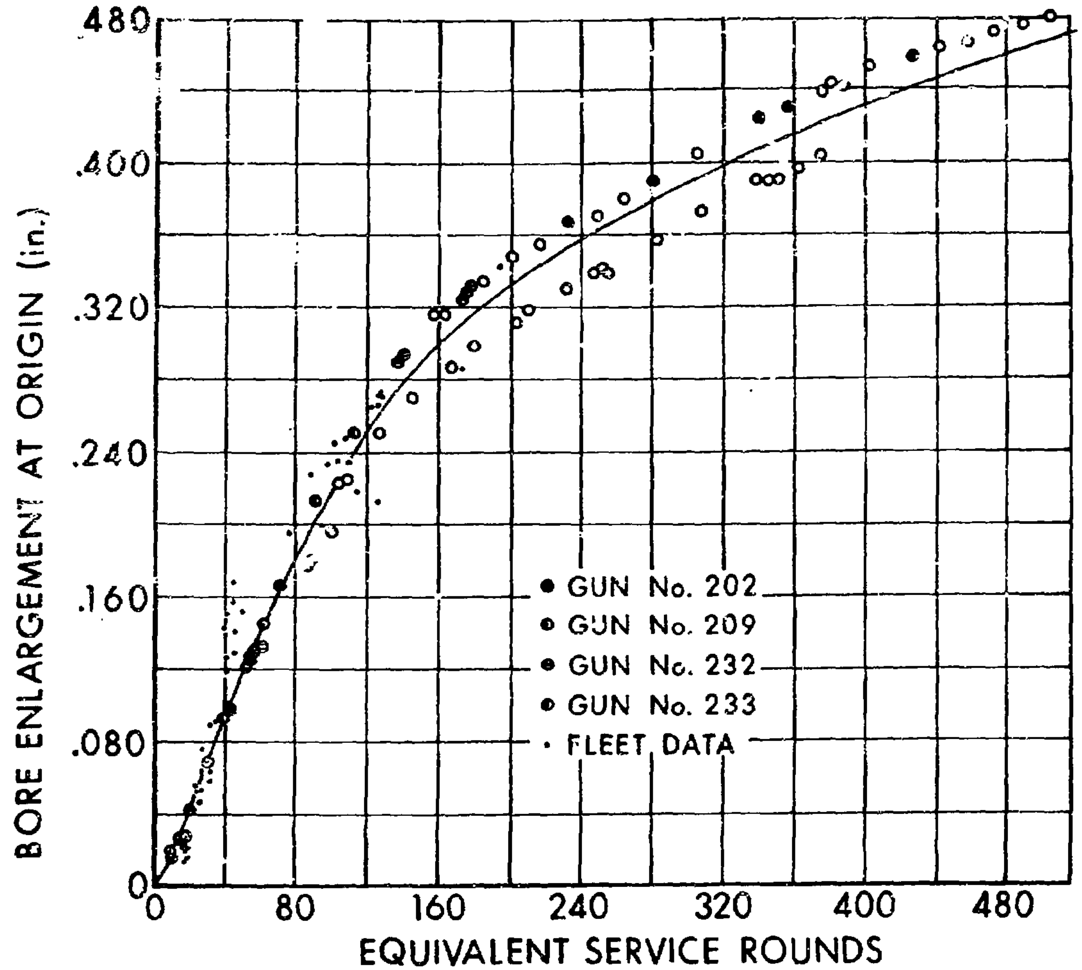
PURPOSE OF NEW INITIATIVE

- Increase the life of fielded wear-limited guns.
- Develop technology to anticipate wear and erorion problems in future Army weapons.
- Perform research to extend the state-of-the-art of gun barrel wear and erosion technology to meet wear life requirements of future Army weapons.

DEFINITIONS

1. <u>Wear Life of a Gun Barrel</u> is determined by unacceptable ammunition performance - muzzle velocity drop, precision, fuze malfunction, stripped rotating band.
2. <u>Fatigue Life</u> - The number of rounds that may be fired before the gun barrel fails catastrophically.
3. <u>Gun Barrel Wear and Erosion</u> - Physical and chemical processes leading to enlargement or deformation of a gun barrel.

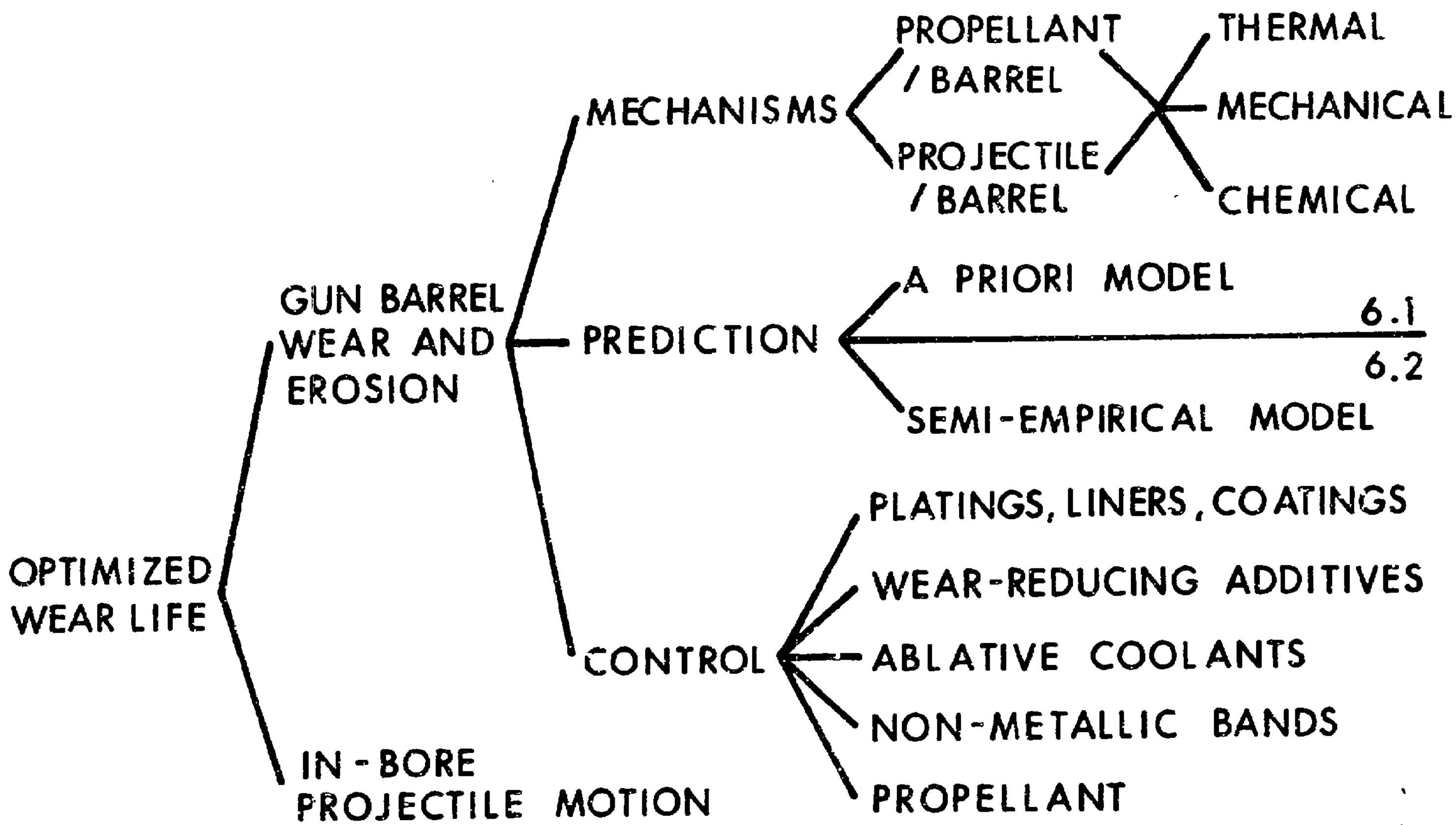
16in./45 CALIBER GUNS MARK 6 AND 8 BORE ENLARGEMENT AT ORIGIN vs EQUIVALENT SERVICE ROUNDS.



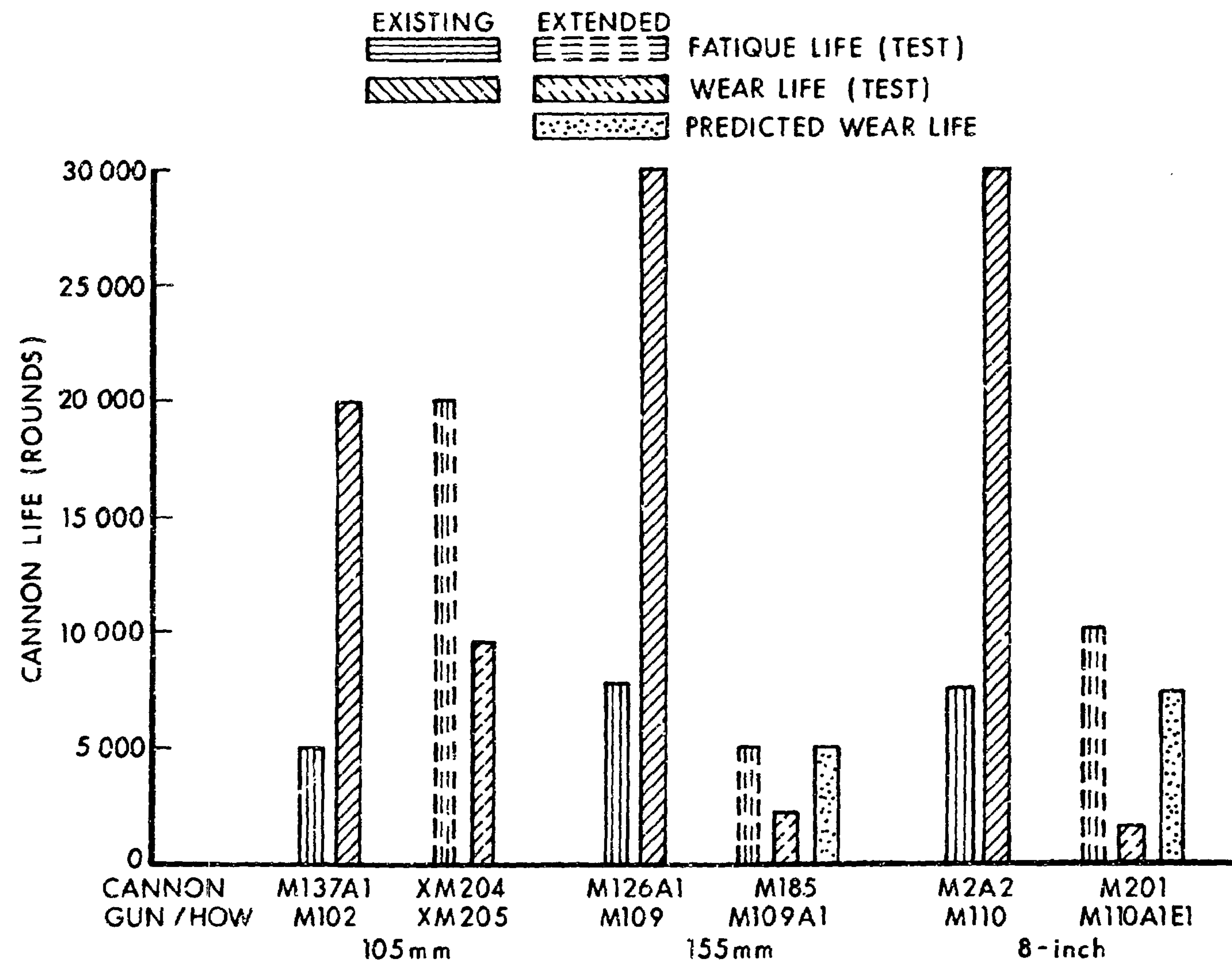
CAUSES OF WEAR AND EROSION

1. THERMAL
   - PROPELLANT
   - ENGRAVING
2. MECHANICAL
   - COMBUSTION GASES
   - ROTATING BAND
3. CHEMICAL REACTIONS
   - COMBUSTION GASES
   - BAND MATERIAL

FRAMEWORK - ARMY GUN BARREL WEAR AND EROSION PROGRAM



COMPARISON OF TUBE LIFE FOR EXISTING CANNON AND EXTENDED RANGE CANNONS



KEY POINTS

- Elected to develop gun/howitzers. High-force, high flame temperature M30 propellant used in top zone charges.

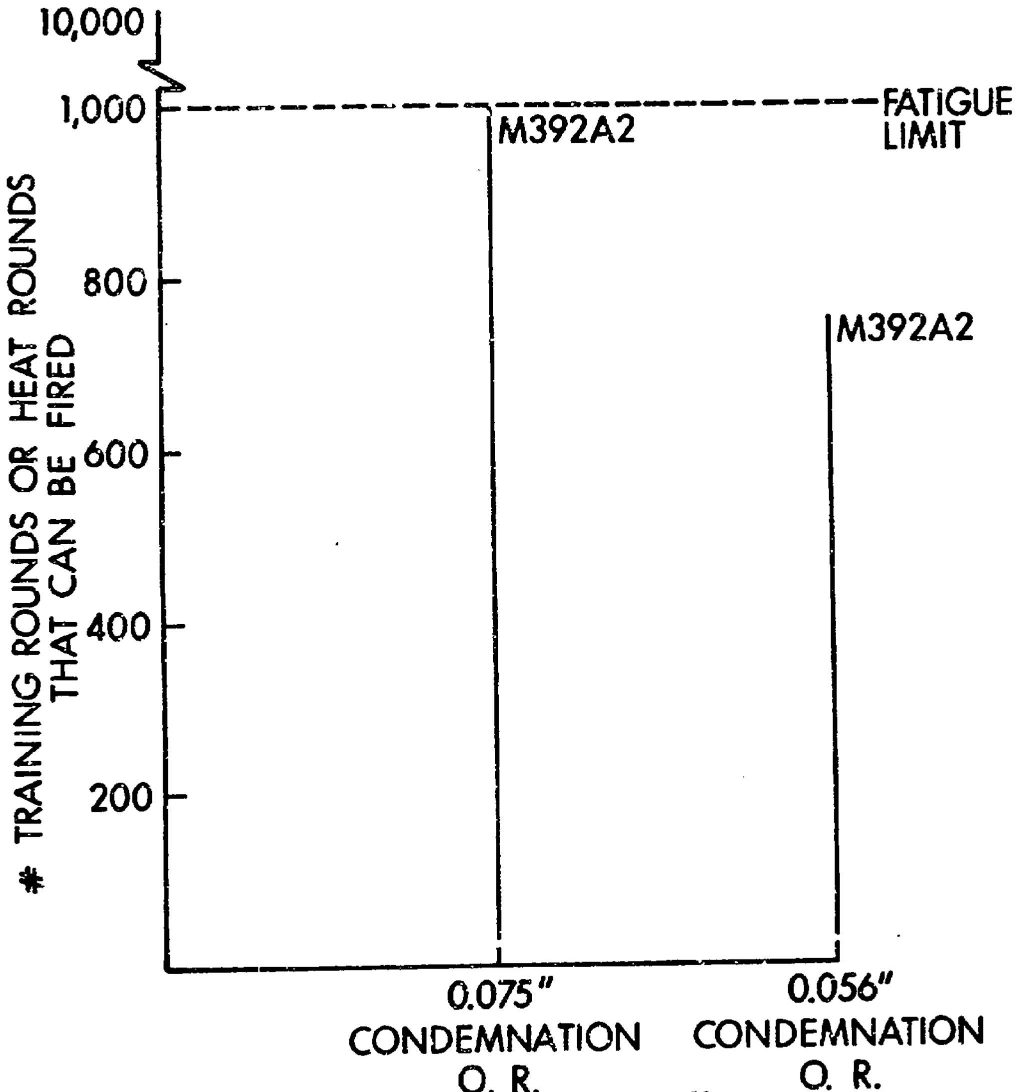
- Presumed wear-reducing additives would keep wear life near fatigue life.

- Wear-life estimates optimistic. Not discovered until wear tests conducted late in 6.4.

KEY POINTS

- Introduction of TiO_2/wax liner in 105mm tank cannon ammunition in 1960's made wear life exceed or equal fatigue life.
- Wear-reducing additives altered wear profile, led to development of secondary wear. Required lowering condemnation limit for discarding sabot, armor-piercing ammunition.
- Secondary wear and its effect on APDS ammunition discovered in after-action reports from Yom Kippur War.

IMPACT OF SECONDARY WEAR ON APDS PROJECTILE



COST FOR REPLACING CURRENT, WEAR-LIMITED TUBES

Cannon	FY 78-81 Existing Orders	Cost/Tube, Thousands	Total Cost Millions
105mm M68	7200	10	72
155mm M185	850	12	10.2
155mm M199	534	15	8
203mm M201	833	39	32.5

FUTURE SYSTEMS

System	Key Date	Wear Life Requirement, Rounds	Available Wear Life Data
Anti-Armor			
120 mm Gun for XM-1	Early 1980s (IOC)	Undetermined	400 (FRG)
MC-AAAC 60-90 mm	1980 (6.3b)	Undetermined	0.5-1.0 mil/rd single-shot
Air Defense			
25 mm Bushmaster for MICV	1980 (IOC)	4,000 required 8,000 desired	Wear test scheduled in 1977
30-40 mm DIVAD Gun to replace M168 in VADS	1977 (RFP)	5,000-10,000 desired	Oerlikon KDA-35 2,500 rds
Aircraft			
30 mm XM230 or XM188 for AAH	1980 (IOC)	15,000	Exceeds 15,000
Small Arms			
4.32 mm FRS		6,000 @ 40 shots/min	4,000

FUTURE HYPERVELOCITY SYSTEMS

(Air Defense; Anti-Armor Systems - Muzzle Velocities up to 10,000 ft/s)

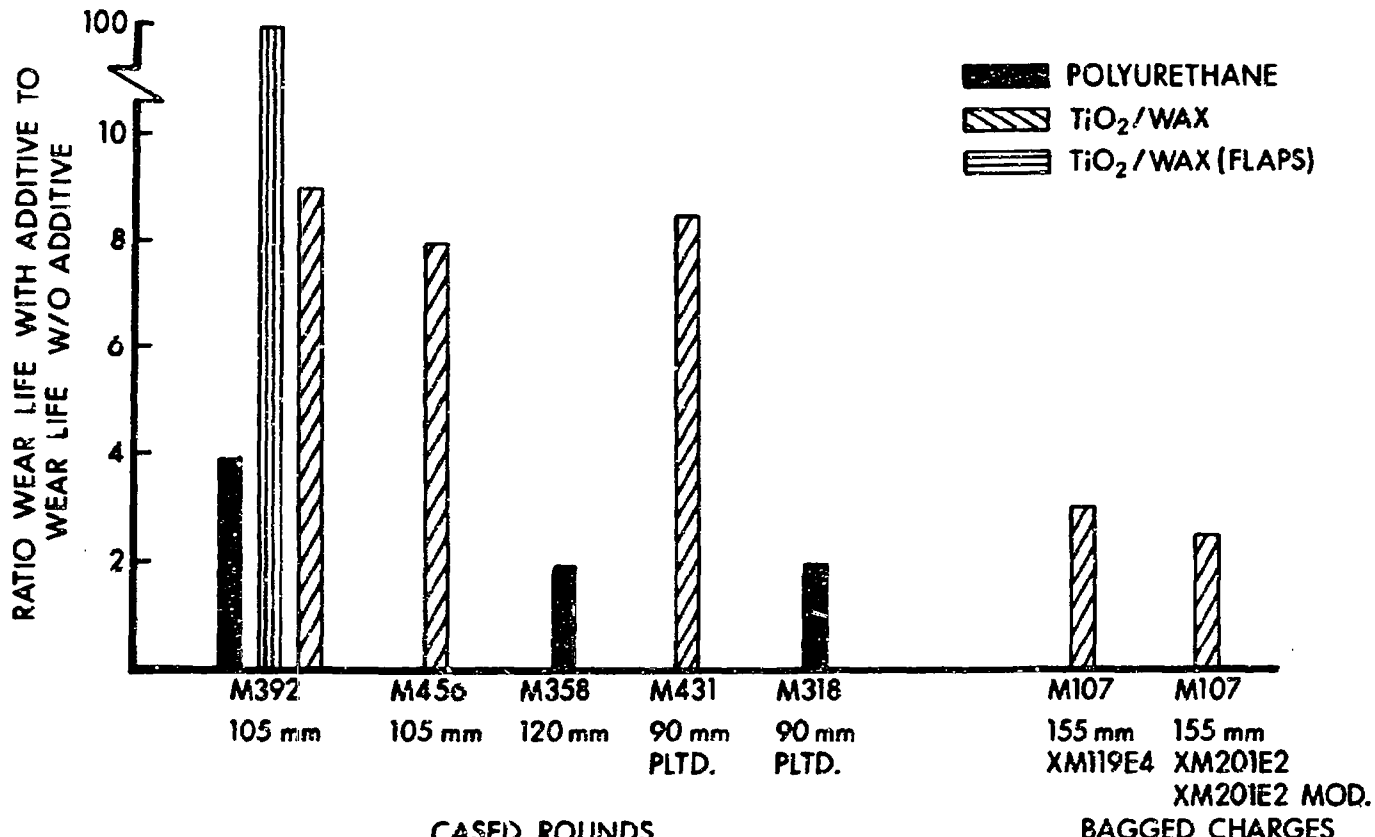
LIQUID PROPELLANT GUNS

CONSOLIDATED PROPELLING CHARGES

TRAVELING CHARGE

LIGHT GAS GUN

EFFICACY WEAR-REDUCING ADDITIVES IN ROUNDS OR CHARGES WITH M30 OR M17 PROPELLANT



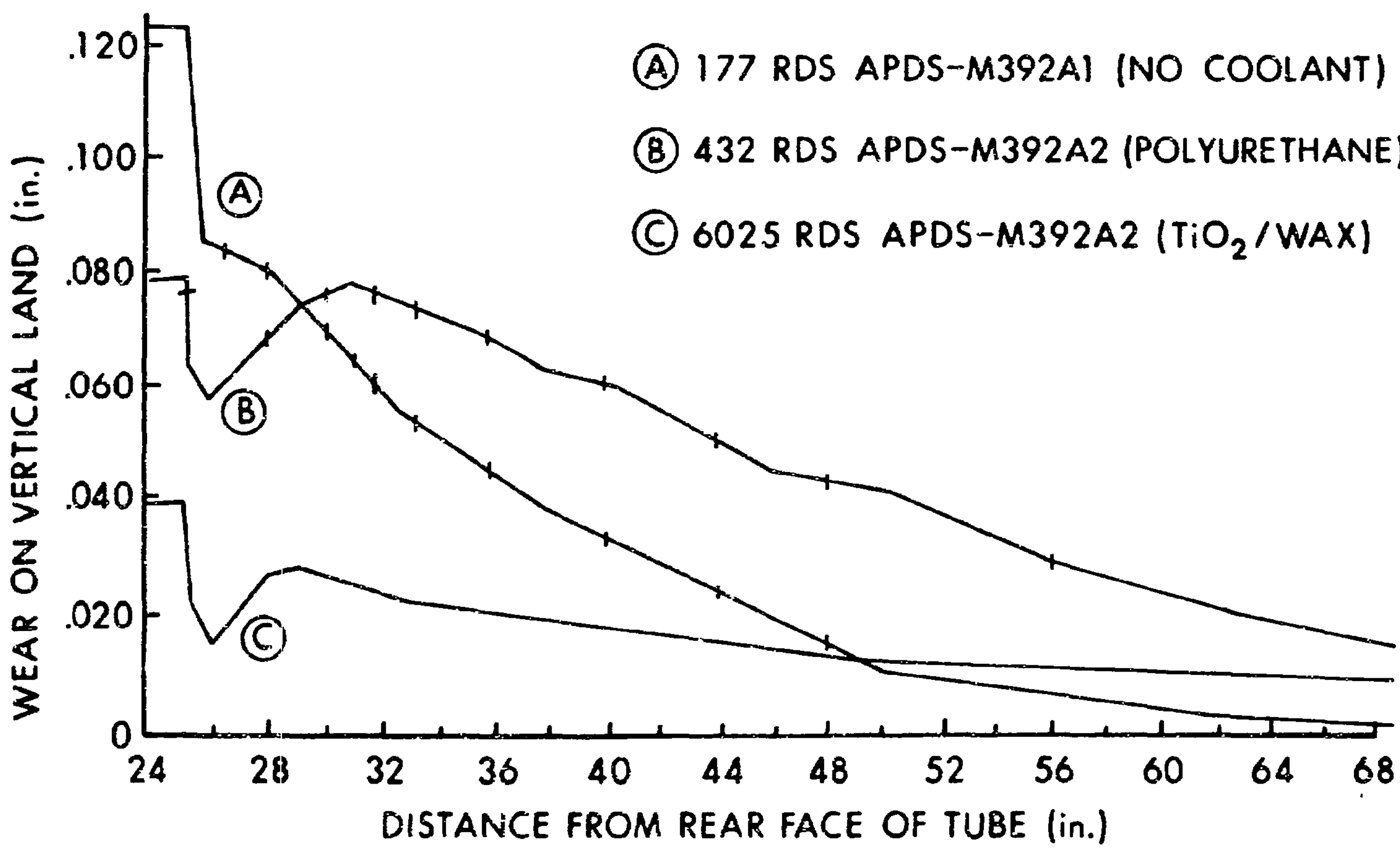
KEY POINTS

- TiO_2/wax liner more effective in cased ammunition than bag charges.
- Downtube effectiveness of TiO_2/wax liner increased by use of flaps.
- TiO_2/wax more effective than polyurethane foam liner.
- Approach:
    - Increase effectiveness of fin-stabilized cased rounds and bagged, separately loaded charges by mimicking M392 round with TiO_2/wax with flaps.

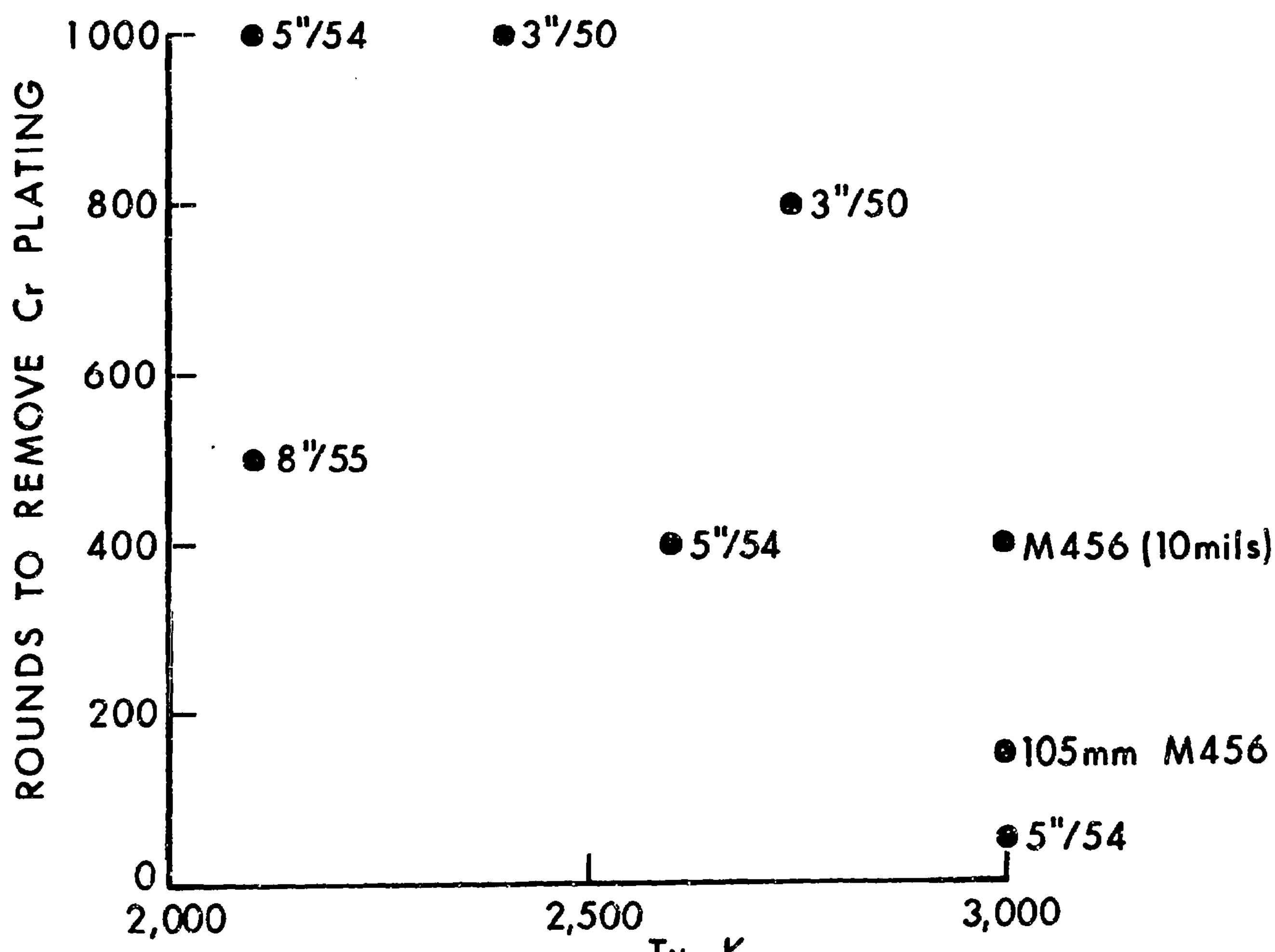
SUMMARY OF HEAT TRANSFER MEASUREMENTS FROM XM201E2 EROSION STUDY

	Charge	Heat Input (Joules)	Est. Wear Life
I.	XM203E2 w/o liner	793	< 1000
II.	XM201E1 w/o liner	750	~ 1000
	XM201E2 w/o liner, M4A1	764	
	XM119E4 w/o liner	764	
III.	XM201E1	701	~ 2500
	XM201E2, M4A1	712	
	XM119E4	702	
	XM203E2	702	
IV.	M119	677	~ 5000
	XM201E2, M4A1, TiO_2/wax cap	671	
	XM203E2 + cap	651	

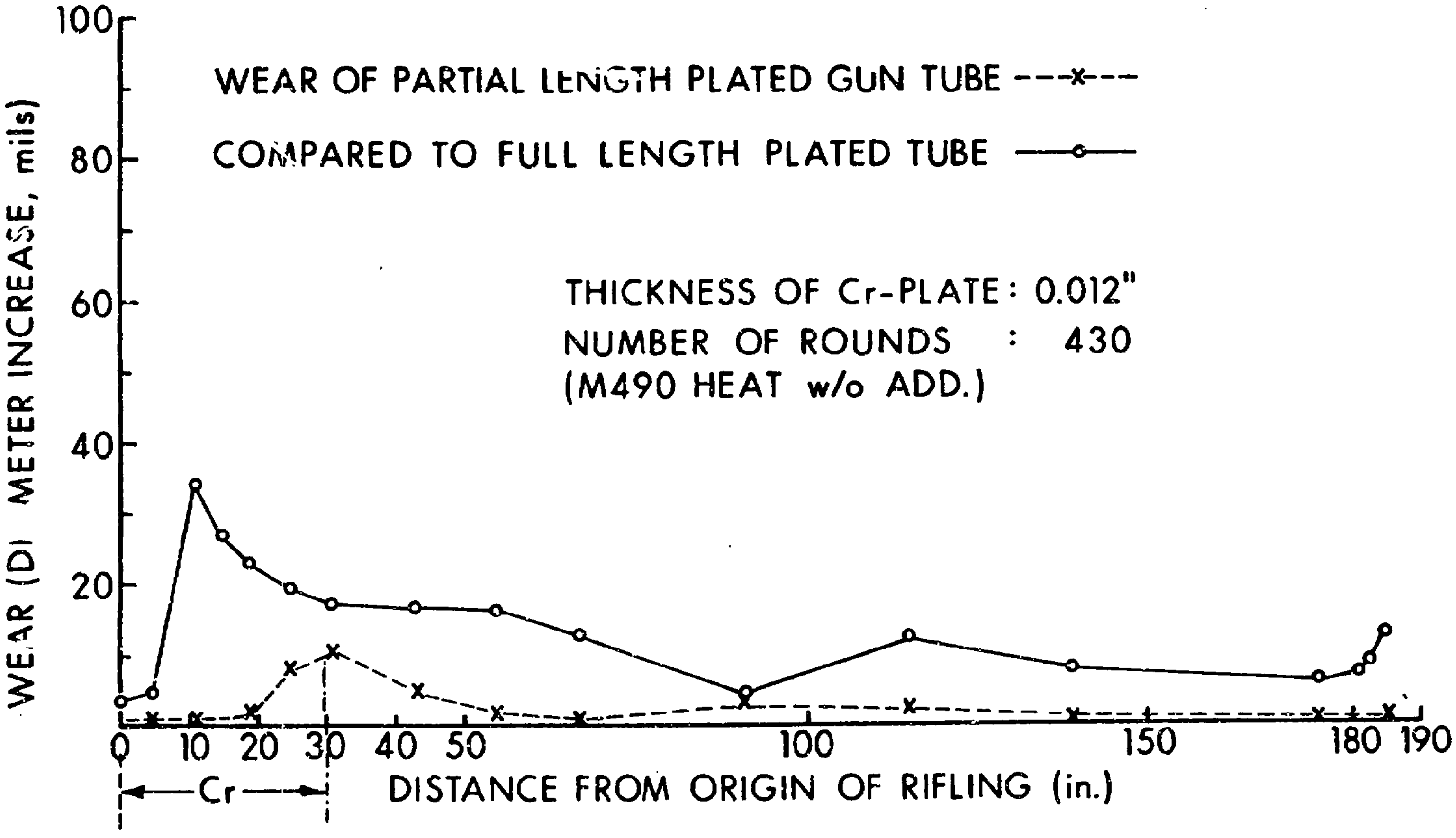
TUBE WEAR vs DISTANCE (RFT) FOR 105mm TUBES, M68



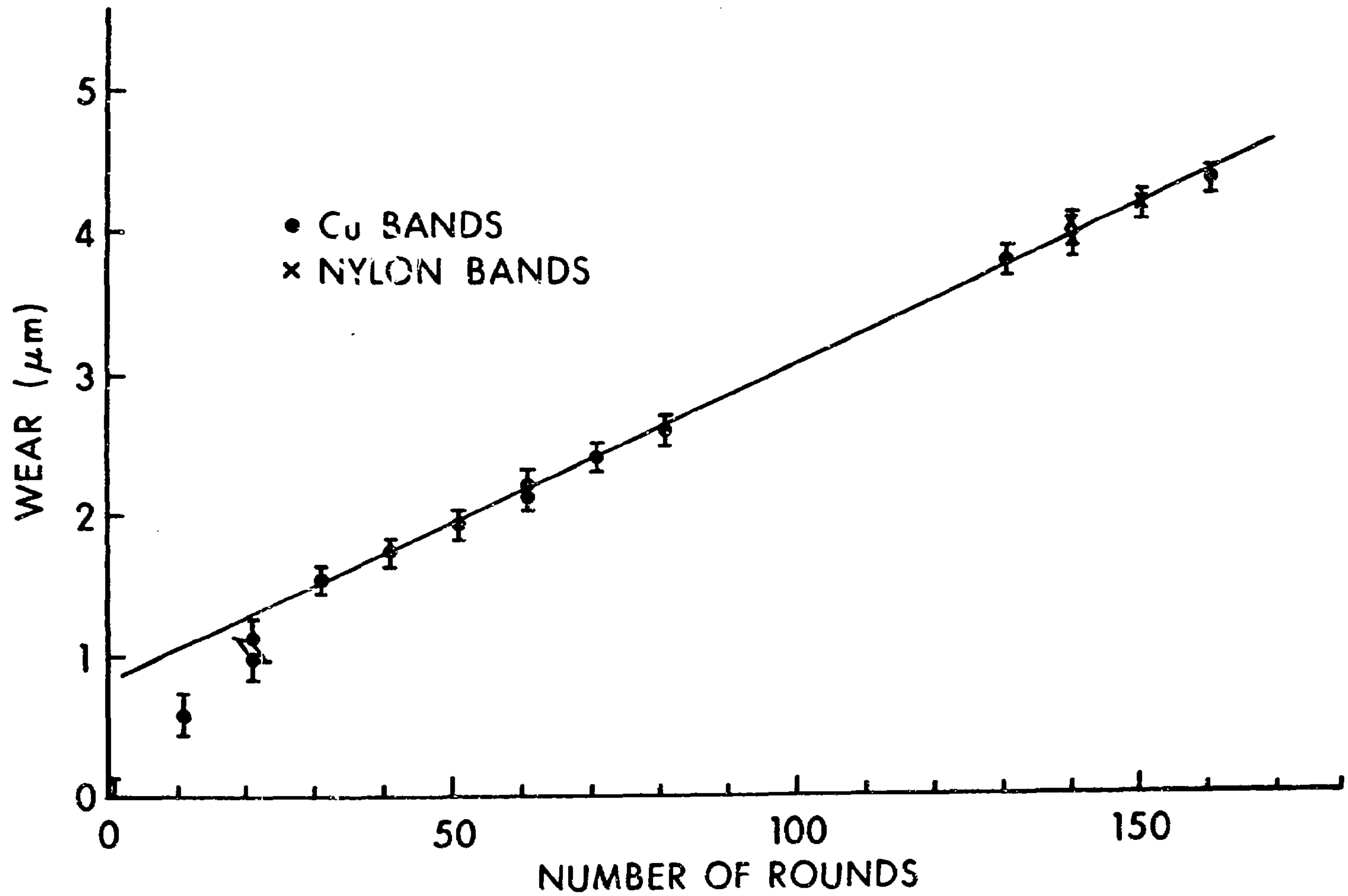
NUMBER OF ROUNDS REQUIRED TO REMOVE
5mils Cr PLATING



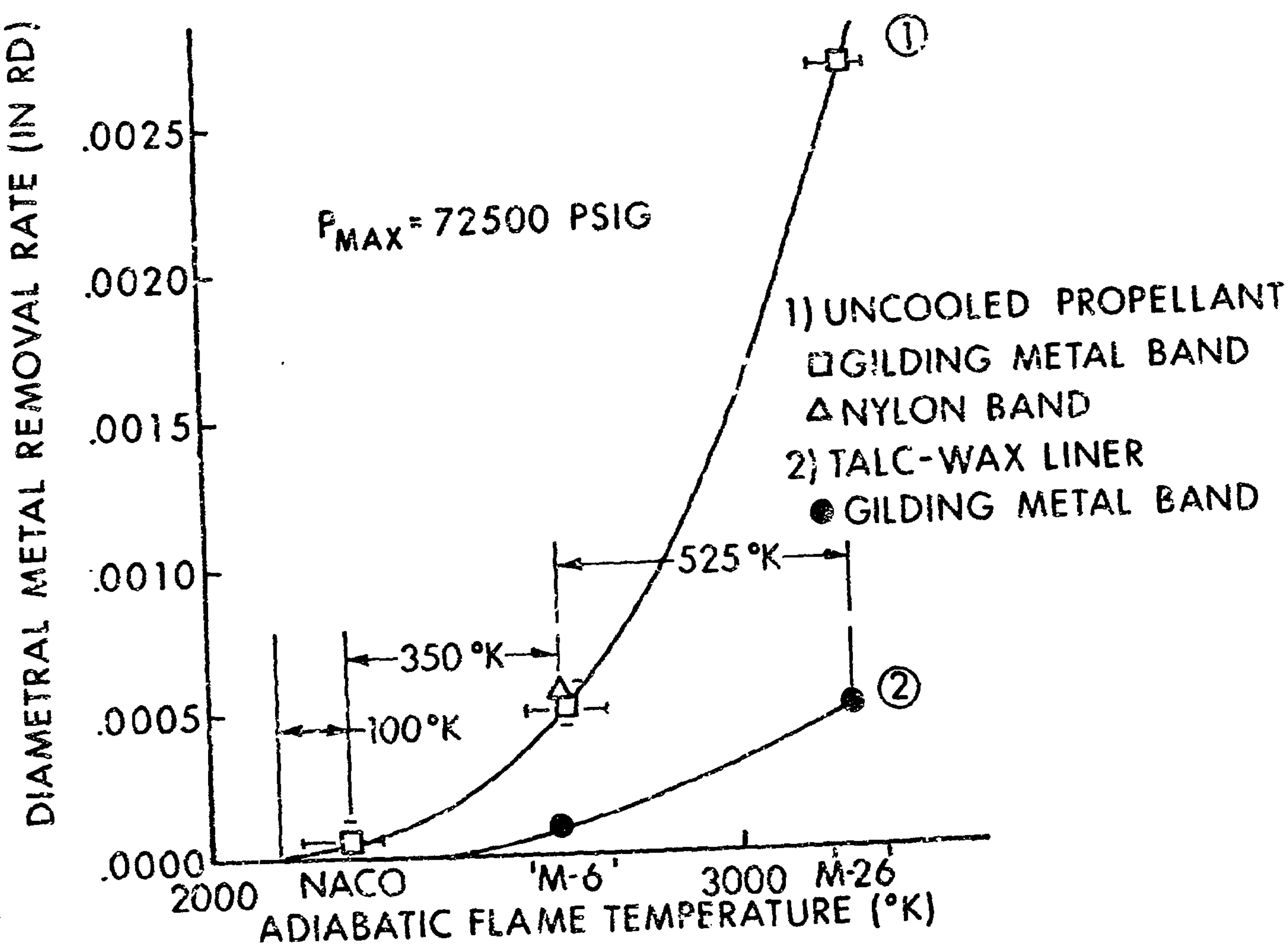
WEAR PROFILES FOR CHROME-PLATED M68 CANNON



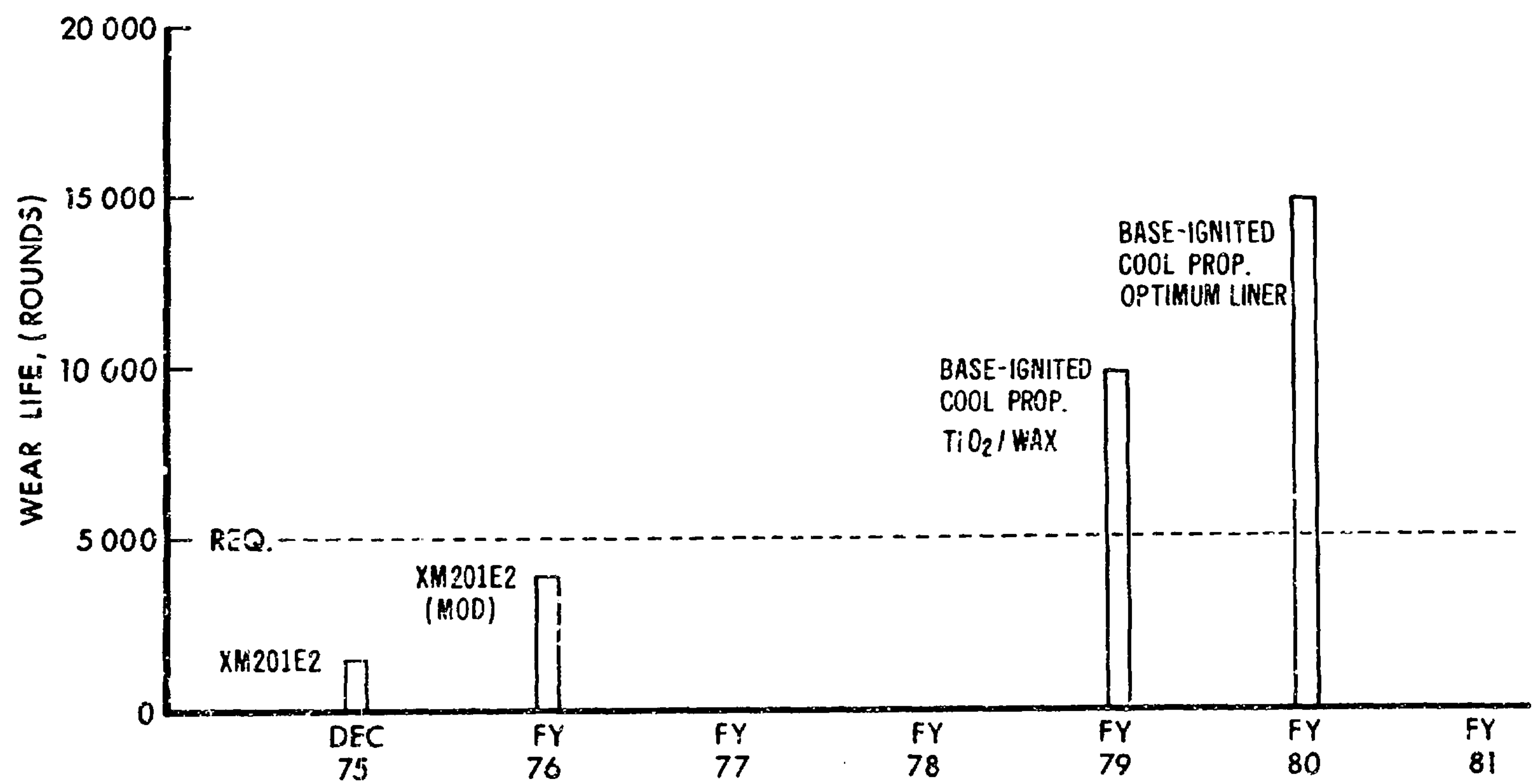
20mm PRESSURE BARREL WEAR AT O.R.



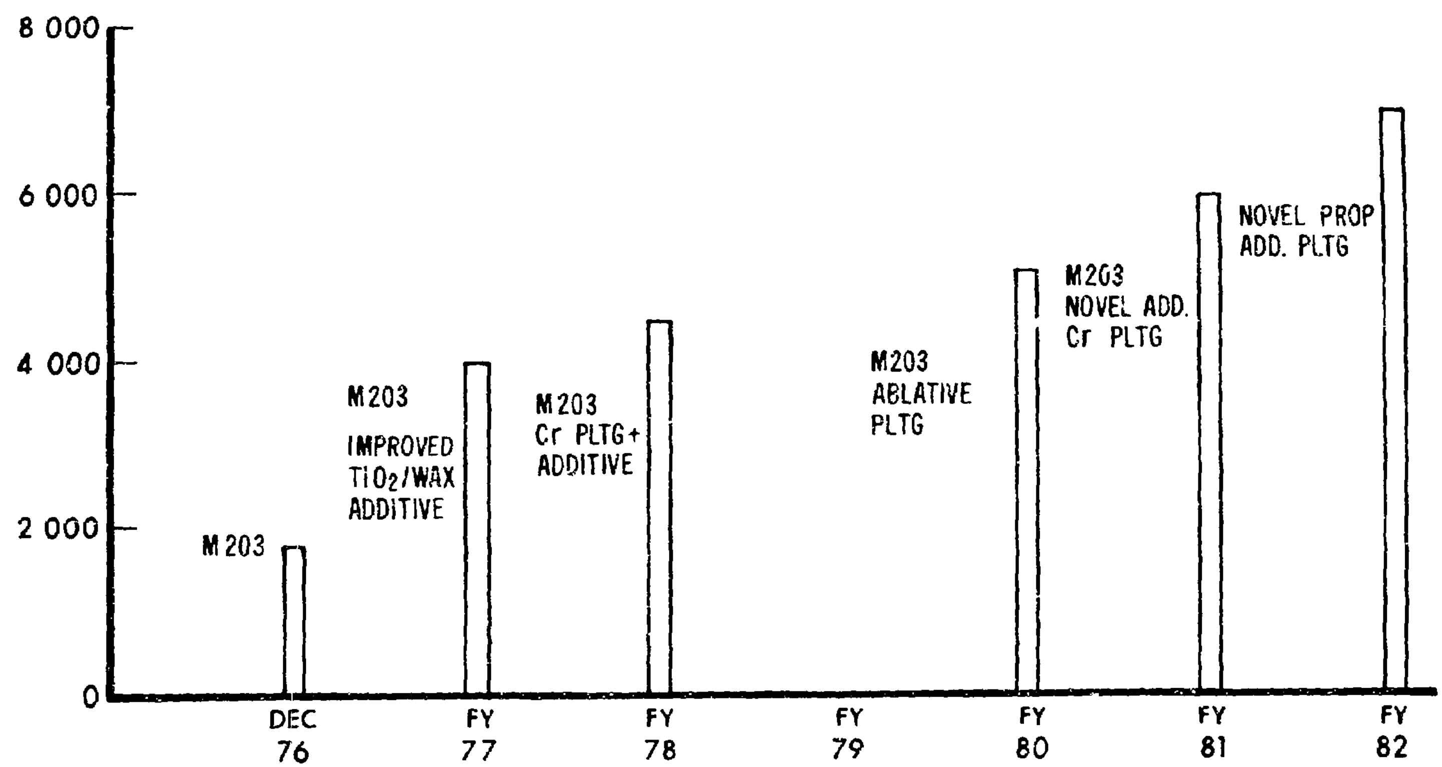
RESULTS-5"/54 HIGH PRESSURE TEST



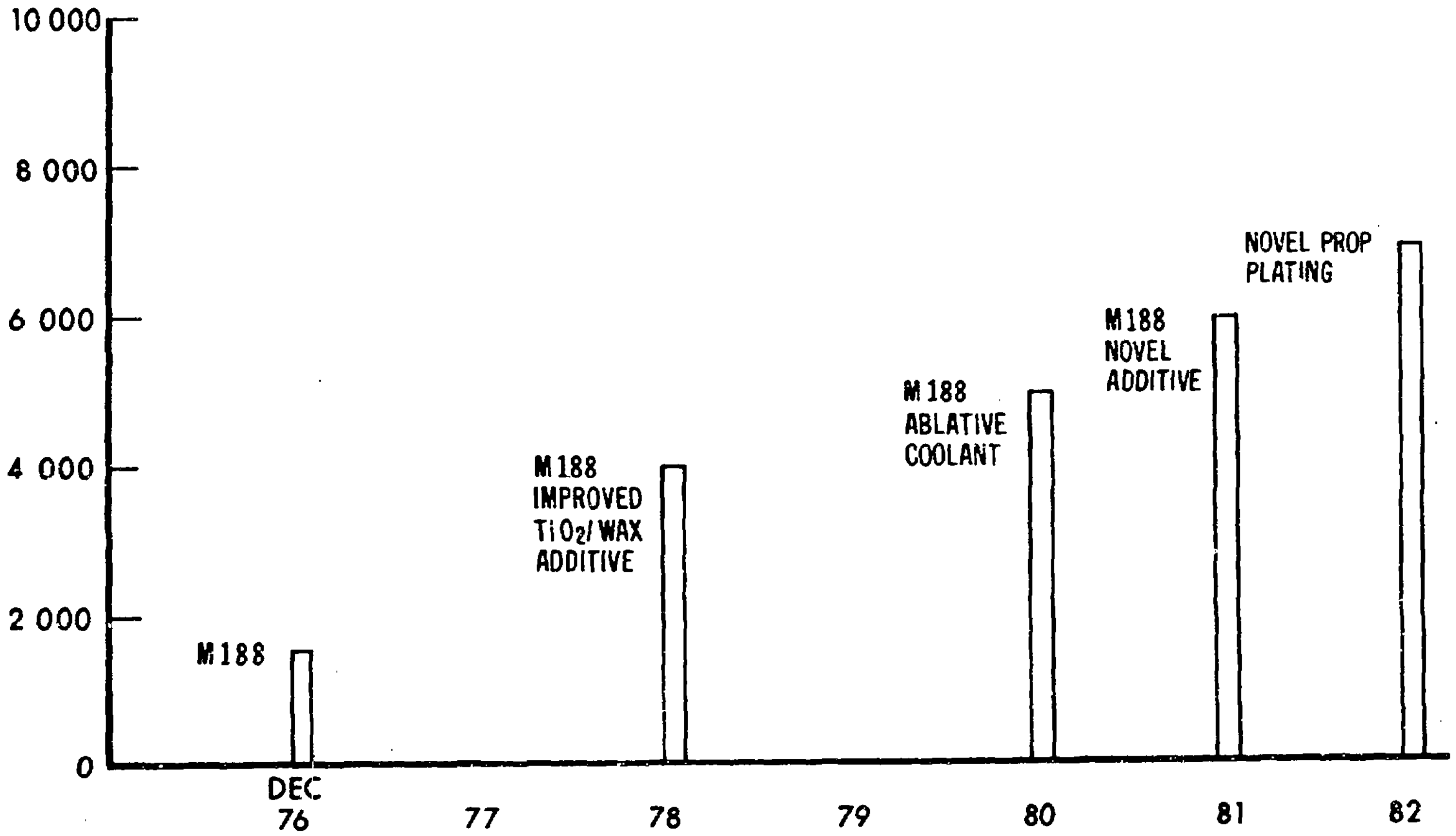
TECHNOLOGY FORECAST OF WEAR LIFE FOR M185 CANNON



EXPECTED WEAR LIFE FOR ZONE 8
M199 CANNON



FORECAST OF WEAR LIFE FOR EXTENDED RANGE FOR M201 CANNON



SUMMARY - TECHNOLOGY FORECAST

Year	Action	Payoff	Risk
1-2	Redesign TiO_2/wax liner in Howitzers.	double wear life	Low
	Redesign TiO_2/wax liner in M490.	reduce secondary wear	Med
	Redesign TiO_2/wax liner in M724.	fatigue-limited M68 cannon	Low
2-3	Redesign Liner and Chrome-Plate Howitzers.	500-1000 round increase in wear life	Low
3	Successfully package ablative coolant for howitzers.	1000 round increase in wear life	Med
4-5	Devise new wear-reducing additive; new propellant, new plating technique.	fatigue-limited M201 gun. Little or no erosion during fatigue life	High

6.1 PROGRAM

<u>OBJECTIVE - Understand the Mechanisms of Gun Barrel Wear and Erosion.</u>

- What is the physical and chemical state of material removed from the barrel surface?
- What is the rate of removal?
- What combinations of mechanical, chemical, and thermal processes lead to removal of material?
- What is the composition of the bore surface after firing?

6.1 PROGRAM

OBJECTIVE	PAY-OFFS
Mechanisms	Anticipate Future Problems.
	Prepare Realistic Requirements for Future Systems.
	Guide Direction of Future 6.2 Program.
	Lead to Novel Techniques to Extend Wear Life or Fatigue Life.
	Eliminate Need for Extensive Wear Testing in Development.

6.2 PROGRAM OBJECTIVES

- Optimize new or existing techniques to increase useful life of gun tubes.

- Prepare semi-empirical models to predict wear life as a function of interior ballistic parameters.

GOAL - Make fielded, wear-limited guns fatigue-limited.

Eliminate future surprises.

NAVY EROSION EFFORTS

- No formal program.
- Work has been done at Dahlgren:
  - Talc/wax additive 5"/54.
  - Heat transfer measurements and calculations.
  - Ablative Coolant 5"/54; 8" - Marine Corps Gun.
  - Measure erosion high-pressure MPWS.
  - Empirical erosion model including effects of wear-reducing additive and firing rate.
  - Non-metallic rotating bands ineffective in single-shot mode 5"/54 gun; effective in rapid-fire 3" gun.
- Assessment
  - Work is parallel to Army (MPWS $\cong$ AAAC; wear-reducing additives).
  - Navy does not consider erosion a serious problem; ongoing work in support of Marines.
  - Seek to fund Navy work in FY 78.

AIR FORCE EROSION EFFORTS

- Formal program ~300K scheduled to begin in FY 77.
- 20-40mm guns; 6000 rds/min; 3380-5000 ft/s; 70,000 psi.
- Ablative ammunition discarded-committed to plastic rotating bands.
- Plastic bands work at 3380 ft/s.
- AF erosion effort aimed at understanding interaction between rotating band and rifling.
- Assessment - AF assumed it knows how to reduce erosion. Erosion program is essentially a rotating band program. Army should consider non-metallic bands in rapid-fire aircraft and air-defense guns.

DARPA

- ABLATIVE COOLANT
- REMOVABLE / REPLACEABLE LINER
- LIQUID PROPELLANTS

TECHNIQUES TO REDUCE HEAT INPUT

- PROPELLANTS
  - NITRAMINES
  - CONSOLIDATED
  - LIQUID
- WEAR-REDUCING ADDITIVES
  - MIMIC M392
  - TALC FOR TiO_2
  - B.L. CODES TO DEVISE NEW ADDITIVE
- NON-METALLIC ROTATING BANDS
  - LARGE CALIBER
  - AUTOMATIC CANNON
- ABLATIVES – PACKAGE WITH PROJECTILE

PROGRAM FOR INCREASING TEMPERATURE RESISTANCE OF BORE SURFACE

1. Plating
   - Electroplating
   - Chemical Vapor Deposition
2. Liners
   - Fixed
   - Replaceable

PLATINGS AND LINERS

1. Cr-plating gun tubes under high temperature and pressure to induce residual compressive stresses in chromium to prevent cracking.
2. CVD coat barrels with Ta-10W, Nb alloys for increased temperature resistance and wear properties.
3. Permanent Liners - Will most likely have high melting points, low tensile strength, low ductibility therefore residual compressive stresses needed achievable through shrink fitting or filament winding.
4. Replaceable Liners - Made from gun steel, plated or unplated, provisions will have to be made for easy removal.

ARMY LABORATORIES 6.1 PROGRAM

Title	FY 77	FY 78	FY 79	FY 80	FY 81
I. Mechanisms and Modeling Erosion in Laboratory Devices					
Mechanisms of Propellant/ Barrel Interactions	150	(165)165	170	170	170
Investigation of Gun Barrel Erosion in a Ballistic Compressor	120	(130)130	140	140	140
. Investigation of Gun Barrel Erosion in a Shock Tube Gun	100	(110)185	200	250	250
II. Research in Support of Modeling					
. Erosion Process Characterization	110	(120)140	160	160	160
Thermodynamic and Kinetic Studies Related to In-Bore Chemical Interactions	106	(150)200	250	250	250
. Heat Transfer and Bore Friction in Gun Barrels	185	(145)250	250	250	250

ARMY LABORATORIES 6.1 PROGRAM
(Continued)

Title	FY 77	FY 78	FY 79	FY 80	FY 81
Non-Equilibrium Heating and Cooling	44				
III. Research in Methods to Control Erosion					
Physics of Coatings/Platings for Gun Barrels	200	(200)220	300	300	300
	1015	(1020)1290	1470	1520	1520

6.1 ARO-D - Related to Gun Barrel Wear and Erosion

	FY (Thousands of $) 77	78
Metallurgy and Materials Science	133*	74*
Engineering Sciences	43*	
Mathematics	53*	53*
Chemistry	204*	170*
TOTAL	432*	297*

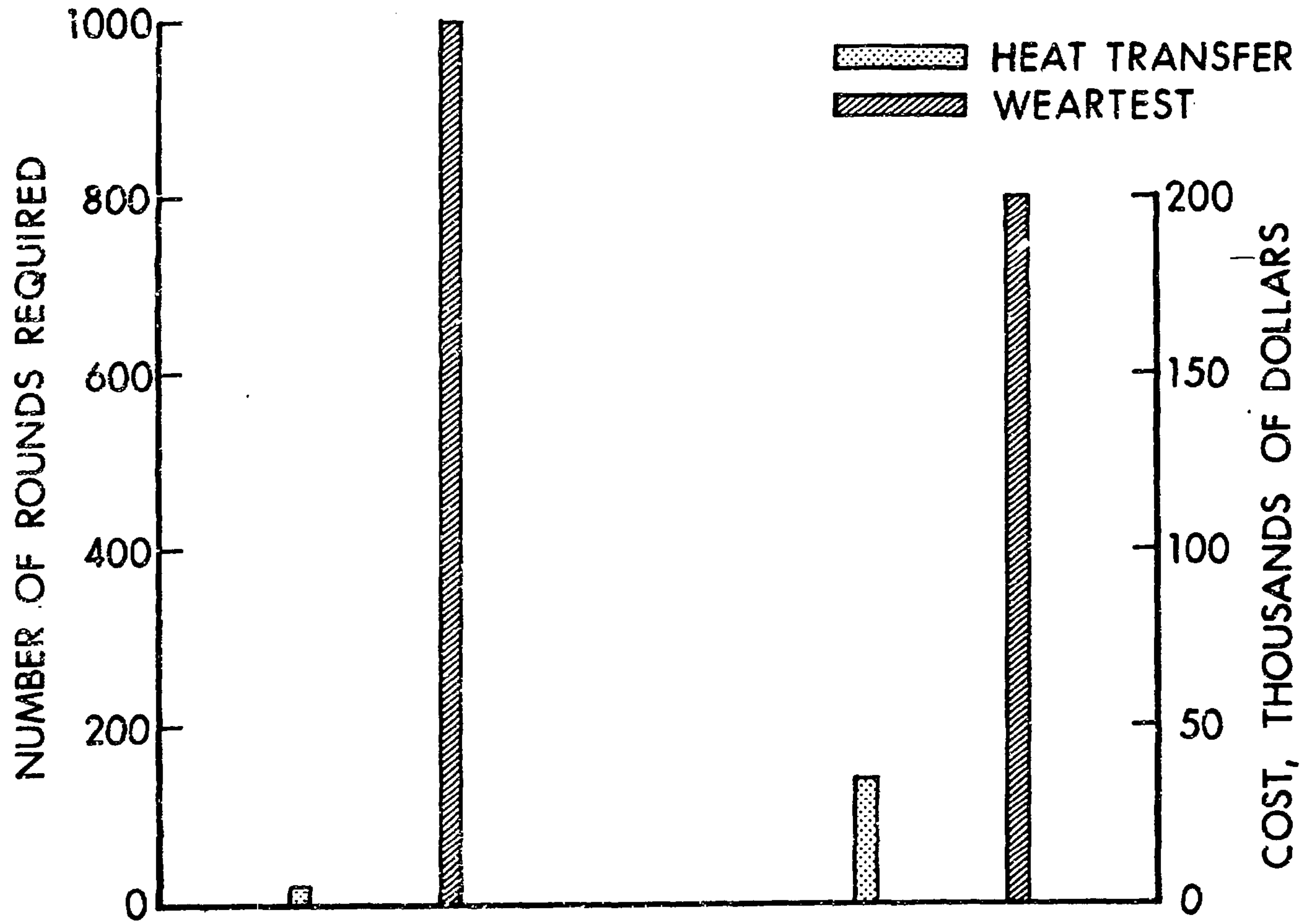
*Committed to Date

+ARO has identified erosion and wear as an area of increased program thrust and is actively seeking proposals which address areas pertinent to the problem.

6.2 PROGRAM

	FY 77		FY 78	FY 79	FY 80
Platings, Liners, Coatings	310	(600)	800	1000	1000
Non-Metallic Rotating Bands	40		50	150	
Wear-Reducing Additives and Ablative Coolants	130	(130)	200	200	200
Semi-Empirical Models	275		300	300	300
Wear Measurement Techniques	75	(75)	75	0	0
Erosivity of Novel Propellants	0		150	150	150
	830		1575	1700	1650

COMPARISON BETWEEN WEARTEST AND HEAT TRANSFER TECHNIQUE IN XM201E2 EXPERIMENTS



CONCLUSIONS

1. The wear life of fielded howitzers may be doubled.

2. The M68 tank cannon may be made fatigue limited without resorting to chrome plating.

3. The wear life of rapid-fire, aircraft and air-defense systems nearing type classification may be doubled with non-metallic rotating bands (Air Force work).

4. MC-AAAC and future hypervelocity anti-armor and anti-aircraft weapons will require new coatings or liners.

5. Future hypervelocity systems with muzzle velocities in excess of 5,000 ft/s will likely require combinations of novel propelling concepts such as liquid propellants or consolidated charges coupled with new materials.

REVIEW OF THE AIR FORCE
PROGRAM IN GUN BARREL LIFE

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Technical Director
Guns, Rockets & Explosives Division
Air Force Armament Laboratory

INTRODUCTION

The purpose of this paper is to summarize the work done by and for the Air Force in gun barrel erosion and related fields during the past twenty five years - the approximate time the Air Force has had its own R&D capability. It is not the intent to present a lot of technical detail, tables, or numbers, but rather a qualitative discussion of what we have done, why it was done, what the general result or end use of the work was, and what we are doing now, plan to do in the future, and why.

EARLY WORK

When the Air Force began to aggressively pursue its own munitions R&D programs (about 1951) we had two aircraft guns in large numbers, the Cal. 50 and the 20mm M24. Since by that time it had already been determined that the days of the Cal. 50 were numbered no further barrel work was done on it. The M24 (an Hispano Suiza design) was a different matter, however. At that time we had them in all of our first line bombers and many of our fighters. We also had a few percussion fired M3's and the Navy had large quantities of both M3's and M24's under their identification of MK14 and MK16. The driving installation, however, was the B36. It had sixteen guns in twin gun turrets. The nose guns carried 450 rounds each, the tail gun 960 rounds each and the remaining twelve guns 600 rounds each. The barrels were of steel, unplated and unlined. Life of the barrels was short and unpredicatable. Since the replacement of a barrel in this gun required complete disassembly of every component, the "simple" matter of changing a barrel amounted to a complete rebuild of the gun. As a matter of fact the threads sometimes seized into the receiver so firmly that the receiver was destroyed during a barrel change and a barrel change sometimes occurred in as little as 600 rounds. The gun life was supposed to be 5000 rounds. It was desired, of course, to extend barrel life, to 5000 rounds if possible. At least we needed extended life, predictable life, and some measure by which to replace barrels. An extensive program was undertaken which covered several years and well over a million rounds of erosion testing. This is probably the most extensive barrel erosion program ever undertaken anywhere and any serious student of the subject would be well advised to research this data. The firing was done at Aberdeen under project TS3-3014 and the reports are probably available only at Aberdeen.

This erosion testing was initially carried out on several firing schedules and various types of failure occurred - some barrels failed by excessive gas erosion near the breech, some by flattening of the lands further towards the muzzle. If anything was consistent in this early work it was that barrel life was inconsistent. We later standardized on what was considered a severe but realistic schedule to simulate an air battle; 40 rounds every thirty seconds with complete cooling after 600 rounds. As the program progressed several decisions were made that we are still living with - fortunately they were good decisions. First the barrel alloy was standardized as the Chrome Moly Vanadium alloy still in use. Second, nitriding, as used in many foreign weapons, was of absolutely no value in this gun. Third, chrome plating is effective. Fourth, chrome plating process is important. Fifth, if a plated bore is faulty, chemical stripping of the chrome can cause catastrophic hydrogen embrittlement. Finally, we were able to devise a simple type of erosion gage (M10) to reject barrels at or near their wear out point regardless of firing schedule, barrel alloy, or type of erosion.

M39 GUN, M50 SERIES AMMUNITION

When we first started with the M39 gun and the M50 series ammunition we had more problems. The rate of fire was 1500 shots per minute, double that of the M24 and the muzzle velocity was 500 fps higher. We had an additional problem - the M39 is a revolver and failure of the drum-to-barrel seal caused breech face erosion of the barrel before the bore wore out - often in less than 100 rounds. We used M39 barrels for fence posts all over the base. The only redeeming feature of this gun (as far as barrel life was concerned) was that the cook off problem limited the number of rounds carried to less than 200. Of course we used the now standard chrome moly vanadium barrel alloy and chrome plated the barrel. The seal leakage problem was solved by redesign of the seals. What really saved the day, however, was the advent of ball powder. Ball powder increased barrel life to the point where barrels are replaced at 1200 rounds in combat. In training use, the barrels are gaged with the M10 gage, and last over 1200 rounds. This is considered acceptable life for a gun in this type of service.

BALL POWDER

As stated earlier ball powder, or ball propellant as it is now called, was a significant factor in increasing barrel life. Let's briefly examine why this is so. In automatic aircraft cannon the primary mode of barrel failure is hot gas erosion near the breech. It is logical to assume that the majority of this erosion occurs when the pressure is highest, consequently the temperature highest and the heat flow highest. The actual gas temperature at any time is somewhere between the isobaric and isochoric flame temperatures, depending on the pressure. Both the isobaric and isochoric flame temperature of the base grain of ball propellant is higher (due to high nitroglycerin content) than the IMR type propellant previously used. Ball propellant, however, is geometrically regressive burning. Large amounts of deterrent are infused into the spherical grain to reduce the initial linear burning rate to counteract this geometrical regressivity. Fortunately this deterrent lowers both the isobaric and isochoric flame temperatures so that at the time of maximum pressure, consequently maximum heat transfer, the actual gas temperature is lower for ball propellant than for IMR, although the mean flame temperature and impetus are as high or higher. Figure 1 is a plot of flame temperatures vs % deterrent for this propellant. Although the average concentration is 7% the concentration near the surface is believed to be 18 - 20%. Figure 2 illustrates actual flame temperature and % propellant burned vs projectile travel for a 20mm round. Note that the actual flame temperature is approximately 500 ° F lower at the point of maximum erosion and peaks at about 300° F lower than conventional propellant. These factors, together with its high loading density make ball propellant the best of all currently available types for aircraft guns. A word of caution on the versatility of ball propellant is in order. Because of the nature of the deterrent process, it can only be infused into the grain a few thousandths of an inch. This is adequate to counteract the geometric regressivity in samll grains. It will obviously not work for 8" gun propellant. There is some practical limit to the size in which ball propellant will function. This may be between 20 and 30mm, since we have not seen satisfactory performance in the GAU-8 gun.

THE M61

The M61 followed shortly after the M39 in the development cycle of aircraft guns. It also used chrome moly vanadium barrels, chrome plated, and M50 series ammunition with ball propellant. Although its muzzle velocity was 100 fps higher than the M39 its firing rate was "only" 1000 shots per minute per barrel. During development it was determined that its barrel life was adequate and no large effort was ever made to increase its life. The barrel work done on the M24 and the development of ball propellant are still paying dividends twenty to twenty-five years later. M61 barrels are routinely replaced at 25,000 rounds/set (4,166 rounds per barrel).

NEW PROBLEMS

After fiscal year 1956 there was no significant exploratory or advanced development work done on guns and ammunition for a full ten years. In the late 1960's, as a result of the Southeast Asia conflict, new requirements were generated for both air-to-air and air-to-surface gun systems. Significant in these new requirements were desires for increased range and shorter time of flight. The best route to these two goals is higher muzzle velocity.

During our attempts to raise velocity from our operational values of 3200-3400 fps to the neighborhood of 4000 fps, we encountered severe bore coppering problems. Depending on variables such as copper alloy, bore material and condition, barrel length, and propellant composition, this problem becomes intolerable at some velocity between 3600 and 4000 fps. This problem was not without precedent and a corollary existed in the leading of barrels that occurred during the final years of the last century when velocities exceeded 2000 fps. The solution to the leading problem was, of course, to convert from lead to copper with its higher melting temperature. The obvious solution to the coppering problem, and one applied throughout Europe was to convert from copper to iron. Although this solves the coppering problem, it worsens erosion - and erosion was expected to again be a problem as we increased velocity and maintained high firing rates. In summary then our new developments faced a coppering problem and the expectation of increased erosion.

NEW APPROACHES

Realizing that barrel life had always been a limiting factor in both muzzle velocity and rate of fire, the Air Force took a multiple approach route to solve our potential problems. General areas identified for emphasis were plastic rotating bands, low flame temperature propellants, chemical vapor deposited (CVD) tungsten plating, retractory lined barrels, composite barrels, and ablative cooling. It was not expected that any one of these efforts would solve the erosion problem; however, it was hoped that a combination of two or more would get us through the next generation of guns. Of the several programs started, several were carried to logical conclusions, some were abandoned for various reasons, and others are still being pursued. It is not the intent of this paper to cover these programs in detail since they are all documented in formal reports, but to summarize their results, status, and potential payoff.

PLASTIC BANDS

The problem of bore coppering appeared early in our renewed program, first in 10mm, then in 25mm, as we approached 4000 fps. Of course we expected the problem, being familiar with earlier high velocity work and current problems in Cal. 17 by government, industry, and private individuals. Rather than trying iron bands with their known problems we elected to stay with copper until other technology evolved. The most promising concept appeared to be plastic bands. Various plastics had been used for sabot launching of projectiles and models from light gas guns at velocities in excess of 30,000 fps, and performed much better than other materials. Plastic sabots had been fired through rifled barrels at velocities approaching 10,000 fps and had engraved and spun up perfectly. No bore incompatibilities had occurred. We decided to concentrate on plastic bands for our new requirements. The idea of using plastic for rotating bands was new, the Navy had done some outstanding work on the subject in the early 1950's. Figure 3 illustrates these early Navy bands in both 20 and 30mm. As a matter of fact, we were able to obtain some plastic banded 20mm MK100 series projectiles fabricated in 1954. We fired some of these projectiles at velocities up to 3800 fps (the maximum capability of our 20mm test fixture) and they functioned perfectly. This also tended to relieve some of our concern regarding shelf life. These nylon banded projectiles were twenty years old!

Review of Navy reports of the era showed that plastic bands increased barrel life by a factor of three over copper bands. If these bands were so good why didn't the Navy use them then? The principal reason was probably because both Navy guns firing MK100 ammunition (MK11 & MK12) fired from the closed bolt, and the hot chamber will melt plastic bands. As a matter of fact, this is the major plastic band fault discovered to date, but since Air Force guns (excepting M39) present and future will be open bolt this poses no problem.

There are several advantages to plastic bands, and many different materials, designs, and retention methods; however, since we are concerned here today only with bore wear (and coppering) discussion will be limited to these areas. The results of our various plastic band programs have shown that they do in fact increase bore life by a factor of at least three as the Navy earlier reported. As a matter of fact, in the GAU-8 system, conventional gas erosion is virtually zero (Figure 4). A surprising side effect is that velocity doesn't change during a long burst (Figure 5). A net result of these two facts is that no fire control compensation need be made as a function of rounds fired or barrel temperature. Figure 6 shows GAU-8 projectiles manufactured by Aerojet (double band) and Honeywell (single band).

Since plastic bands virtually eliminate erosion, at least in our existing guns, the question of why and how naturally arises. Since the substitution of plastic for copper reduces erosion by a factor of three, one might conclude that two thirds of barrel erosion is caused by copper bands. We know this is not true, the majority of erosion is gas erosion. We therefore must assume that there are some mechanisms whereby plastic bands reduce gas erosion. One can easily postulate three such mechanisms all of which probably contribute: (1) The plastic bands do a better job of sealing the high pressure gases, reducing turbulent blow by over the bore surface; (2) The plastic smears onto the bore surface and cools or reduces heat input through ablation; and (3) The plastic infuses into the microcracks on the bore surface preventing crack growth and propogation. The first mechanism is probably not dominant since copper bands are dimensioned until they completely seal a new barrel; besides this would only account for groove erosion, not land erosion. The second probably is important, but not dominant. Although we have no detailed data there does not appear to be any great difference in total barrel heating with plastic bands. The third is probably the dominant mechanism. Anyone who has seen sections of gun bores during the various stages of erosion can envision how this mechanism works; especially for chrome plated barrels. On the plated barrel the first rounds fired generate a maze of microcracks, which under magnification look like a dried mud flat. Gas from subsequent rounds, hot and containing ionized monotomic gases, enter these cracks and erode the substrate steel. As firing progresses cavities grow under the chrome until it fails and spalls off, exposing more substrate which rapidly erodes. It is easy to envision plastic being forced into these cracks, preventing the cavitation and preserving the chrome plate. All we know for sure is that plastic bands do provide great reduction in erosion. We can postulate as to how and why but have had neither the money nor the manpower to prove or disapprove our theories. We are at present simply accepting the fact and specifying plastic bands on all new ammunition.

PROPELLANTS

Since we know that the primary cause of barrel wear in aircraft cannon is hot gas erosion, it appeared logical to spend considerable effort on those gases and their source - the propellant. During the combustion process, there are many intermediate compounds and elements existing including free monatomic oxygen. Steel melts at around 1700°K and will burn in the presence of O_2 somewhere above 1100°K. The isochoric flame temperature of conventional NC - NG propellants ranges from about 2400°K to 3600°K. Since a few hundred degrees makes a significant difference in erosion rates, Air Force specifications have been not to exceed 2800°K isochoric. This results in actual gas temperatures always less than 2400°K. The actual temperature of the walls and the amount of heat transferred is a function of many things including surface roughness, which of course accounts for the exponential function in erosion rate in older barrels.

Since muzzle velocity, cartridge volume and barrel life are all very important in aircraft systems, our propellant developments have been aimed at those with isochoric flame temperatures of less than 2800°K and specific energies greater than 330,000 ft lbs/lb. Figure 7 illustrates the relationship between flame temperature, specific energy, and molecular weight of combustion products for several existing and developmental propellants. Our present efforts are centered on the tripple base and cyclic nitramines shown. We are now initiating a program on linear nitramines which fall to the left of the MW=18 line.

BARREL MATERIALS

An historical method of increasing barrel life has been to develop barrel materials, plating, liners, or treatments which were less subject to gas erosion and/or rotating band wear. The standardization of Chrome Moly Vanadium for AF guns in the early 50's was mentioned earlier. This was the best of the "standard" alloys then available. More recently, new tool steels and "superalloys" have become available which show promise of making better barrels. We did some development in this area but did not carry it to conclusion because of higher priority work and the success of plastic bands. This work should be carried to completion.

Chrome plating of bores became standard in Germany during World War II and in this country some ten years later. This is relatively simple and very cost effective method of increasing barrel life. In an attempt to find an even better plating material, we conducted a program for several years on chemical vapor deposited (CVD) tungsten. Because of its high melting point, it seemed like an ideal bore surface material and the CVD process was inexpensive. Unfortunately, it did not work. The crystaline structure and physical properties were such that it cracked easily and flaked off much as did chrome. We did this work with copper jacketed bullets, perhaps if it were repeated with plastic bands or jackets, it would be successful. This should be tried, but we have no funds or plans to pursue it at this time.

The use of high temperature or refractory metals for liners looked promising. There are about six metallic elements which are available in reasonable quantities at reasonable cost and have melting points significantly above that of steel. Sorting through these with a simple hot gas erosion test orifice, we came upon a promising alloy of tantalum + 10% tungsten. Two problems immediately surfaced - how do you rifle the stuff and how do you install the liner. The installation was solved by resorting to coextrusion methods developed by the nuclear industry. The rifling problem was solved by the development of a unique, practical, and inexpensive electrochemical machining process. We built some barrels by this method, but the effort was suspended before all test work was completed, again because of the success of plastic bands and the press of higher priority work. This work also should be continued but we have no plans to do so at this time.

During the late 1960's we conducted a study to determine if it was feasible to build an equilibrium temperature gun - that is one that could fire indefinitely at a militarily useful rate and velocity with only normal radiation and convection cooling. It appeared that this was almost possible. The key, of course, was the barrel - one that would operate with a bore surface temperature near the actual propellant flame temperature yet would still have sufficient strength to withstand firing stresses. The solution appeared to be a composite barrel of three layers, a refractory liner, a ceramic insulator, and a steel jacket. Figure 8 shows computed temperature profiles across such a barrel compared to a standard type barrel and an actively cooled barrel. Note that the temperatures appear to be within reasonable working limits for available materials. We decided to build such barrels and test them. Figure 9 shows a cross section of such a barrel which was again fabricated by the coextrusion process and rifled by the electrochemical method. Again we did not complete this program for reasons stated above.

ABLATIVE COOLING

Another idea which looked promising was that of ablative or "smear" cooling. The concept was to place a material between the propellant charge and the projectile, which, when the round was fired, would coat the barrel with a material which would protect it from the hot propellant gases during the early part of the ballistic cycle. This was done using the 20mm M50 series ammunition. The ablative selected was a rather thick silicone fluid. Although simple in theory some problems arose. First, since the available chamber volume of the 20mm is already full, it was necessary to find a more energetic, consequently hotter, propellant to compensate for the volume occupied by the ablative. After developing a suitable propellant change and determining an appropriate quantity and quality of fluid, we began a firing program and found the heating and barrel wear had moved down bore towards the muzzle. Figure 10 illustrates an M61 after alternately firing 200 rounds each ablative ammunition, standard ammunition and cycling dummy rounds. The original is in color and clearly shows a cool breech and a hot mid barrel region for the ablative ammunition. A redistribution of the barrel mass from the breech region to the hot spot would probably compensate for this and reduce the down bore wear (which appeared to be from heat softening of the lands) but since the weapon was in the inventory in large numbers, we decided it was not worth it and the program was terminated. It does work and should be considered in future high velocity systems where breech area erosion is expected to be a problem.

ON GOING AND FUTURE EFFORT

Of the barrel wear programs, we have undertaken in the past several years, two are continuing: Plastic bands and high energy low flame temperature propellants.

Plastic bands have been so successful we consider them to be one of the greatest advancements in ammunition since smokeless powder and feel that all ammunition should use plastic bands (or jackets). One thing for certain is that we don't understand everything we know about them. The question of why they work was discussed earlier. We have so far used them in barrels designed for copper bands and built them to the approximate configuration of copper bands. We are now studying rifling configurations and band configurations to take full advantage of the plastic material. We are investigating alternate retention methods. We are looking at various materials and glass contents. In short, we are trying to understand them better.

We will continue our work on high energy low temperature propellants. The effect of a few hundred degrees reduction in flame temperature on barrel wear is well known. Although our present systems have acceptable barrel life future systems may need a few hundred degrees.

Our anticipated next generation of guns will have muzzle velocities at least in the 4000 fps range. We are working on ammunition intended to have 5000 fps muzzle velocity. We expect to again face the erosion limit. We are counting on plastic bands and high energy low temperature propellants. We may also have to resurrect some of our earlier uncompleted projects such as refractory liners and composite barrels. One thing has always been certain in the gun business - no matter how good a gun is someone always wants it to weigh less, have a higher muzzle velocity, and a higher rate of fire; and this means barrel problems.

FLAME TEMPERATURE vs % DETERRENT
BALL PROPELLANT

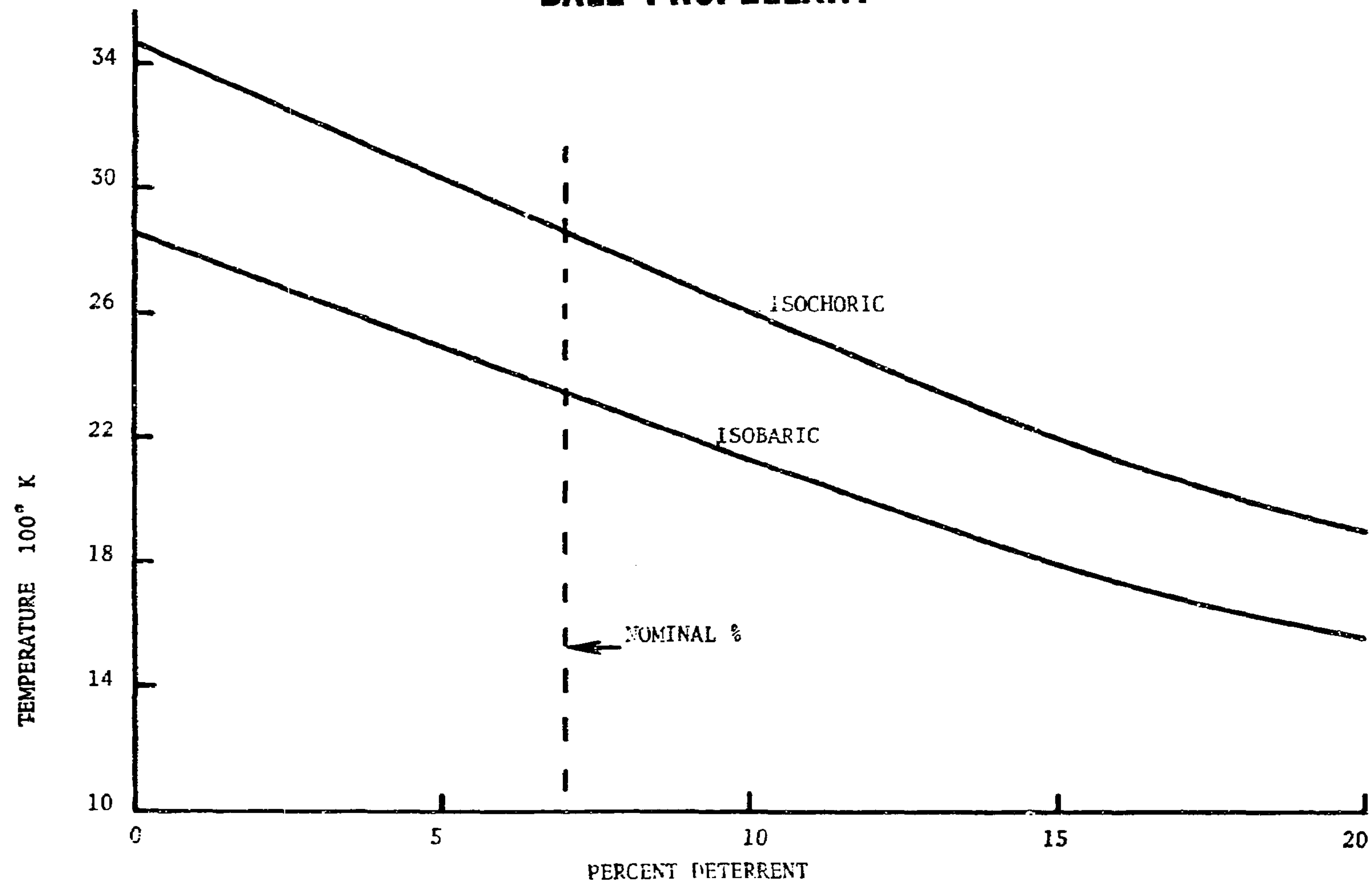
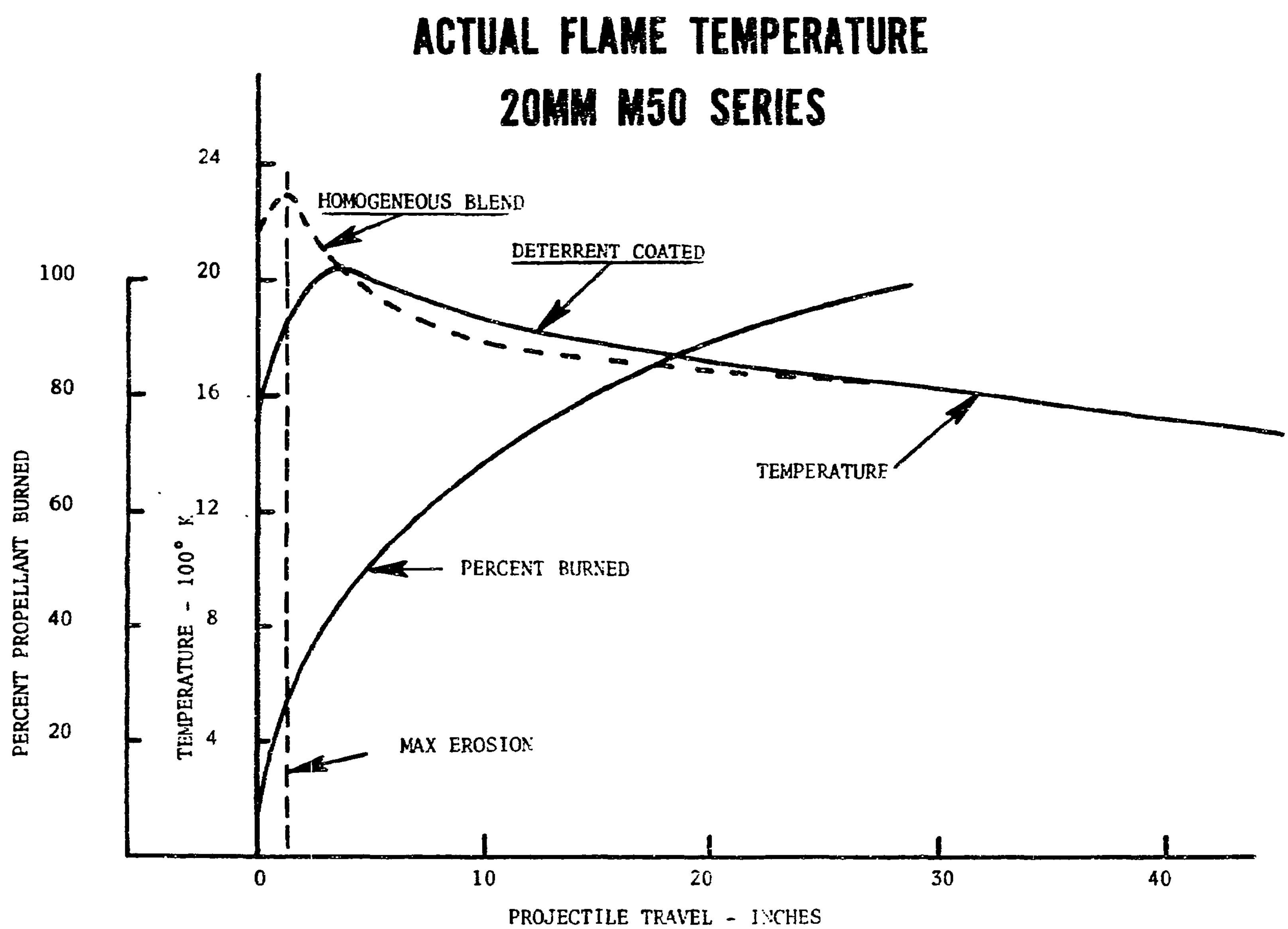
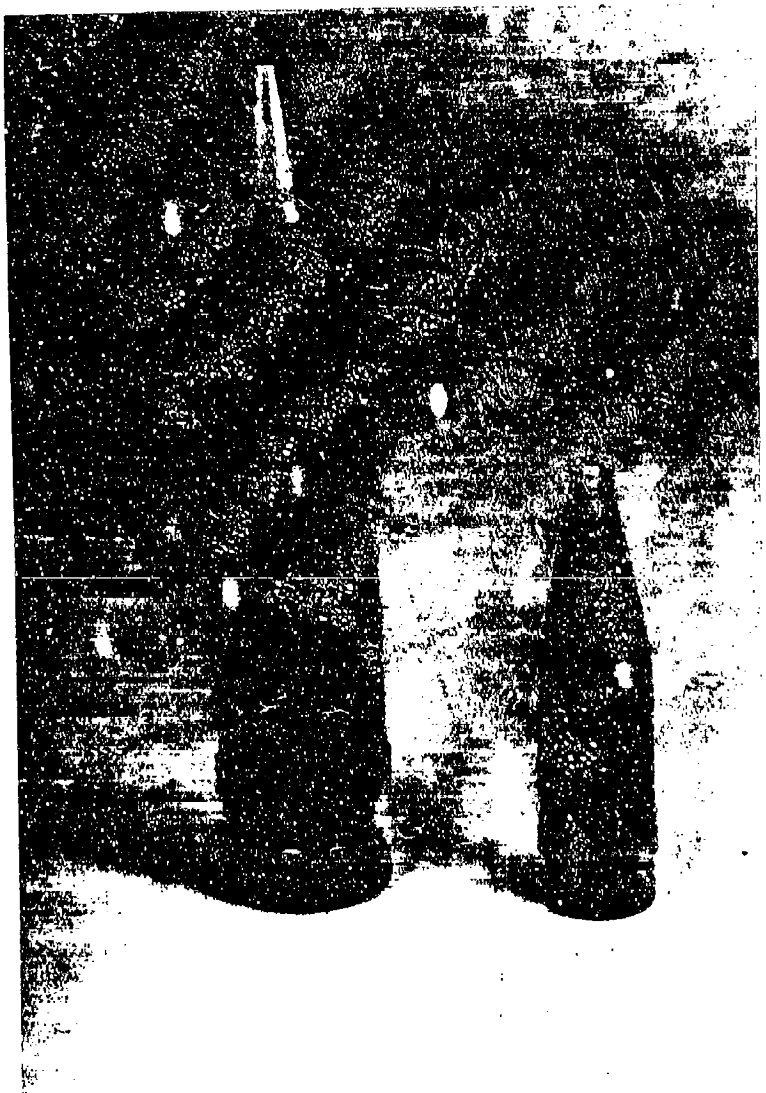


FIGURE #1



ACTUAL FLAME TEMPERATURE
20MM M50 SERIES
HOMOGENEOUS BLEND
DETERRENT COATED
TEMPERATURE
PERCENT BURNED
MAX EROSION
PERCENT PROPELLANT BURNED
100
80
60
40
20
TEMPERATURE - 100° K
24
20
16
12
8
4
0
10
20
30
40
PROJECTILE TRAVEL - INCHES

FIGURE #2



NAVY PLASTIC BANDS
ABOUT 1954

FIGURE 3

PROJECTILE VELOCITY vs GUN ROUNDS
GAU-8 GUN SERIAL 0001

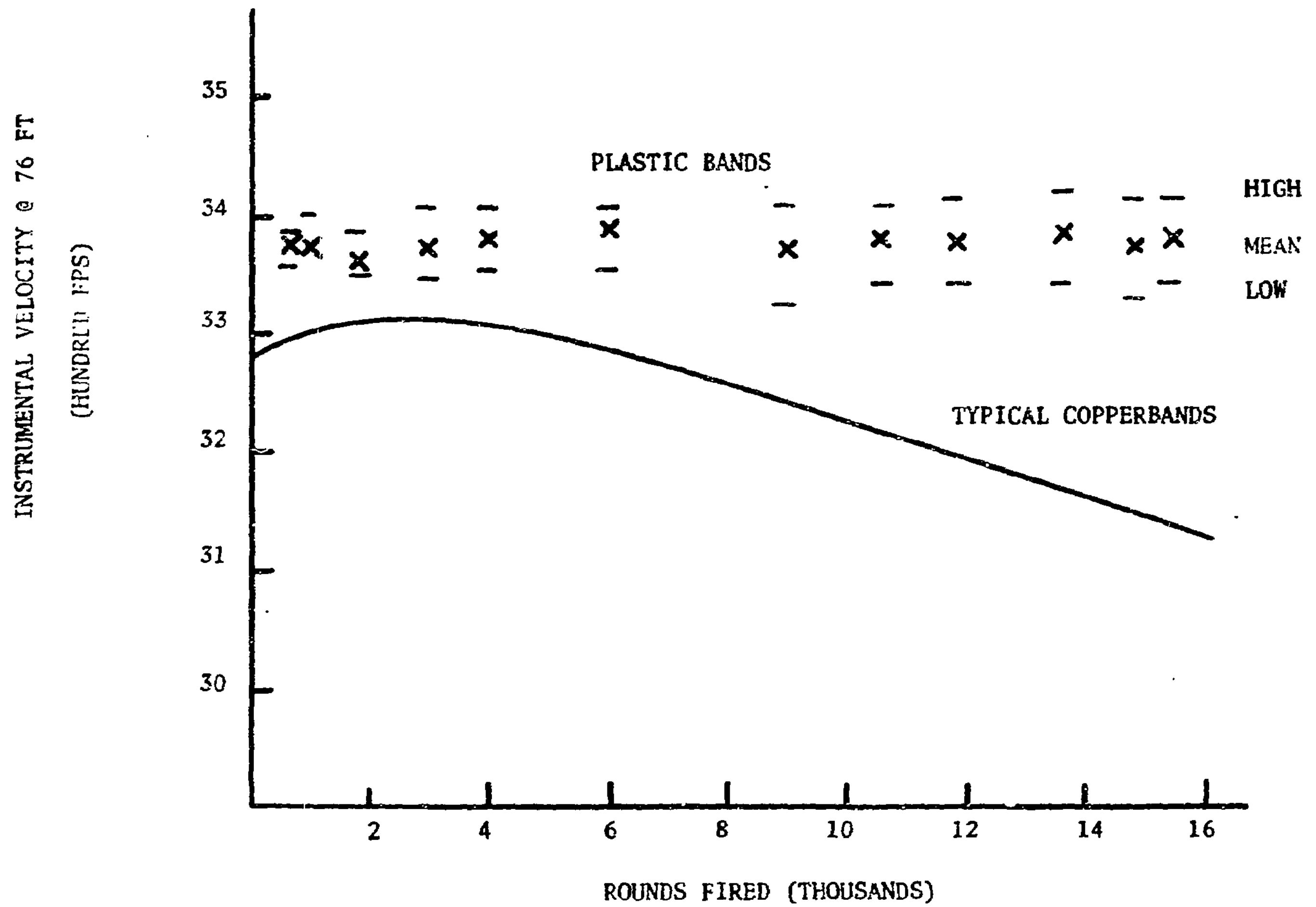


FIGURE #4

PROJECTILE VELOCITY vs BURST LENGTH FOR BURST # 137 MEASURED AT 912 IN. FROM MUZZLE EVERY 10th ROUND PLOTTED

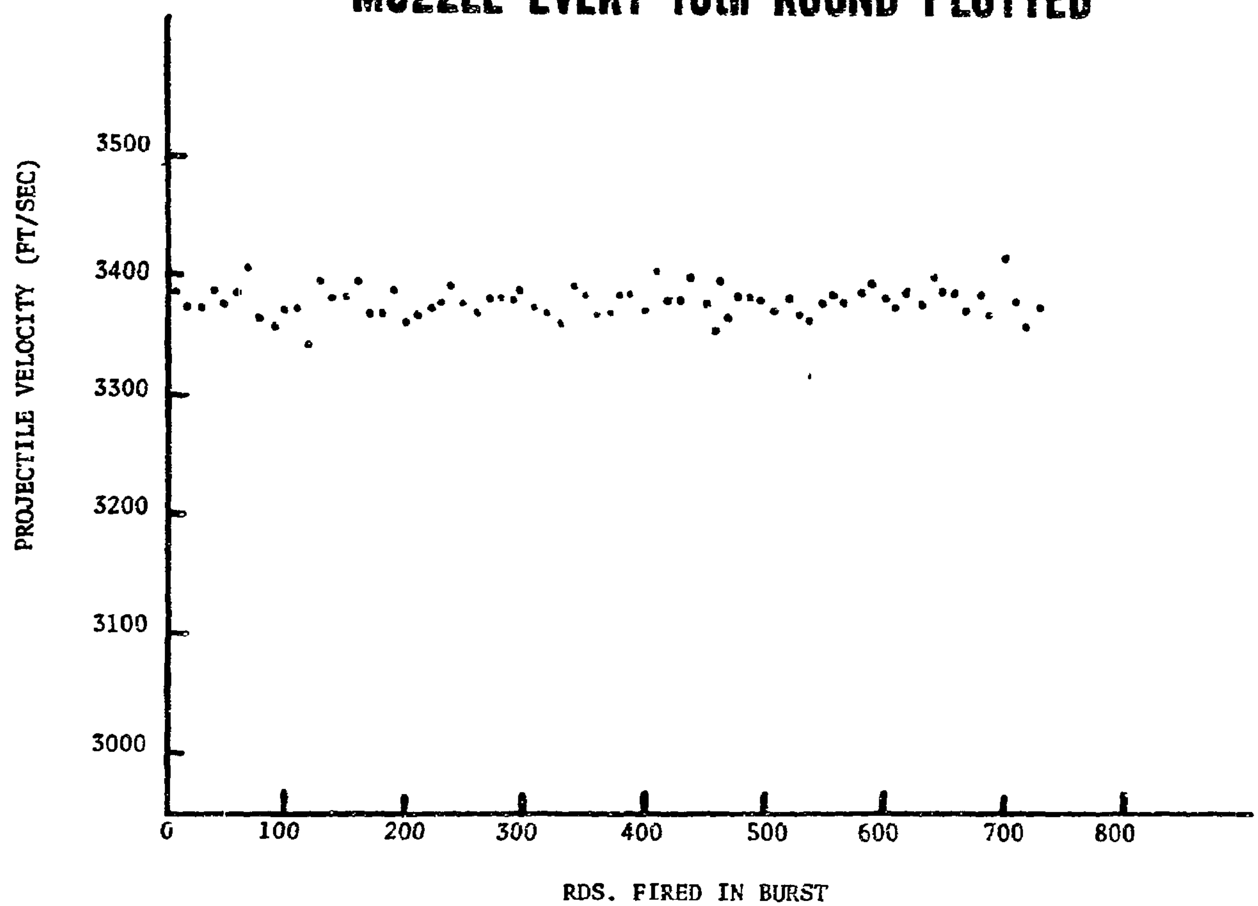


FIGURE #5

GAU-8 PROJECTILES
HONEYWELL (L) & AEROJET (R)

FIGURE 6

GUN PROPELLANTS - ENERGY vs FLAME TEMP.

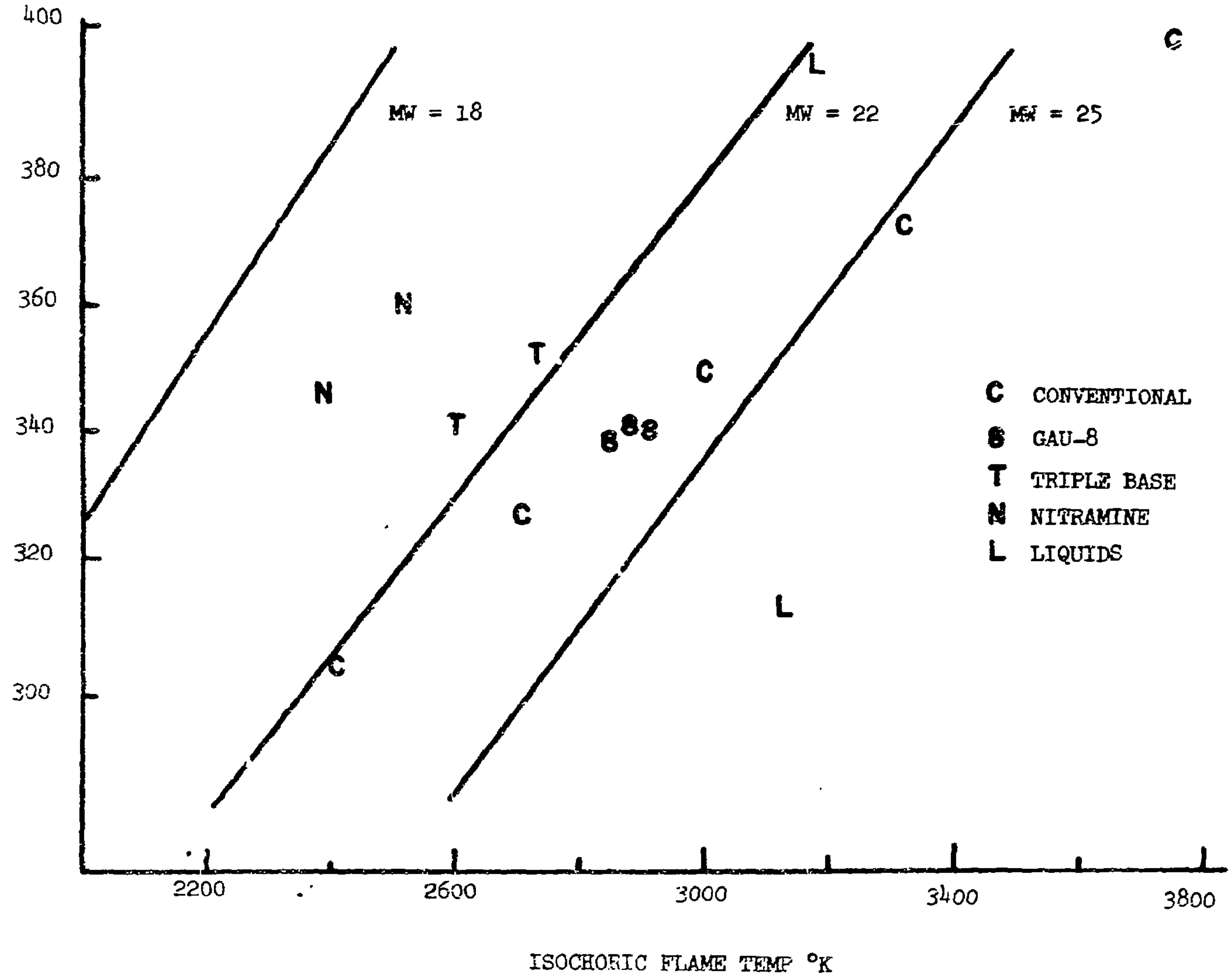
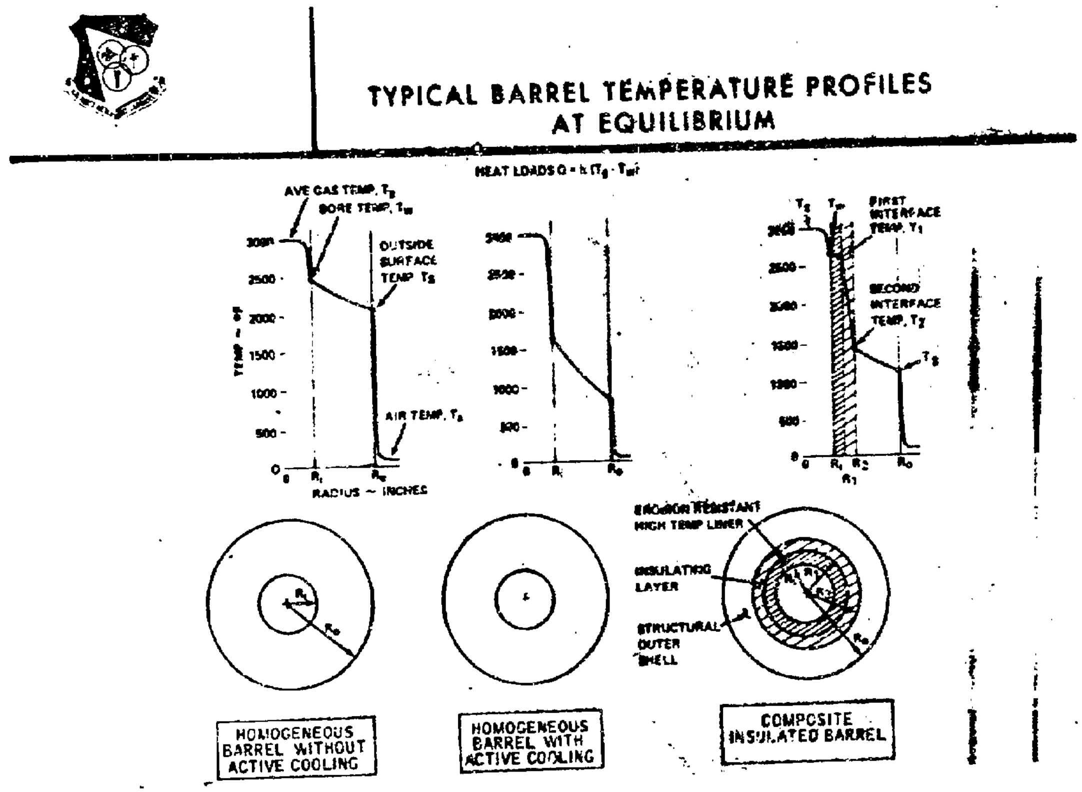
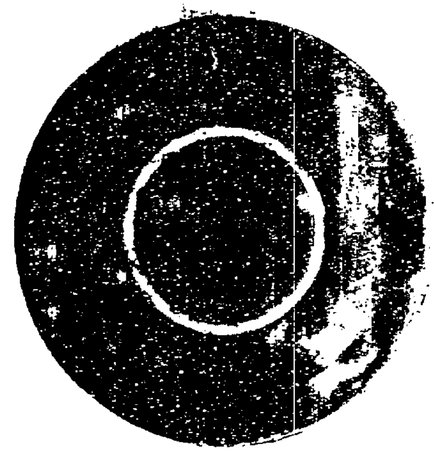


FIGURE #7



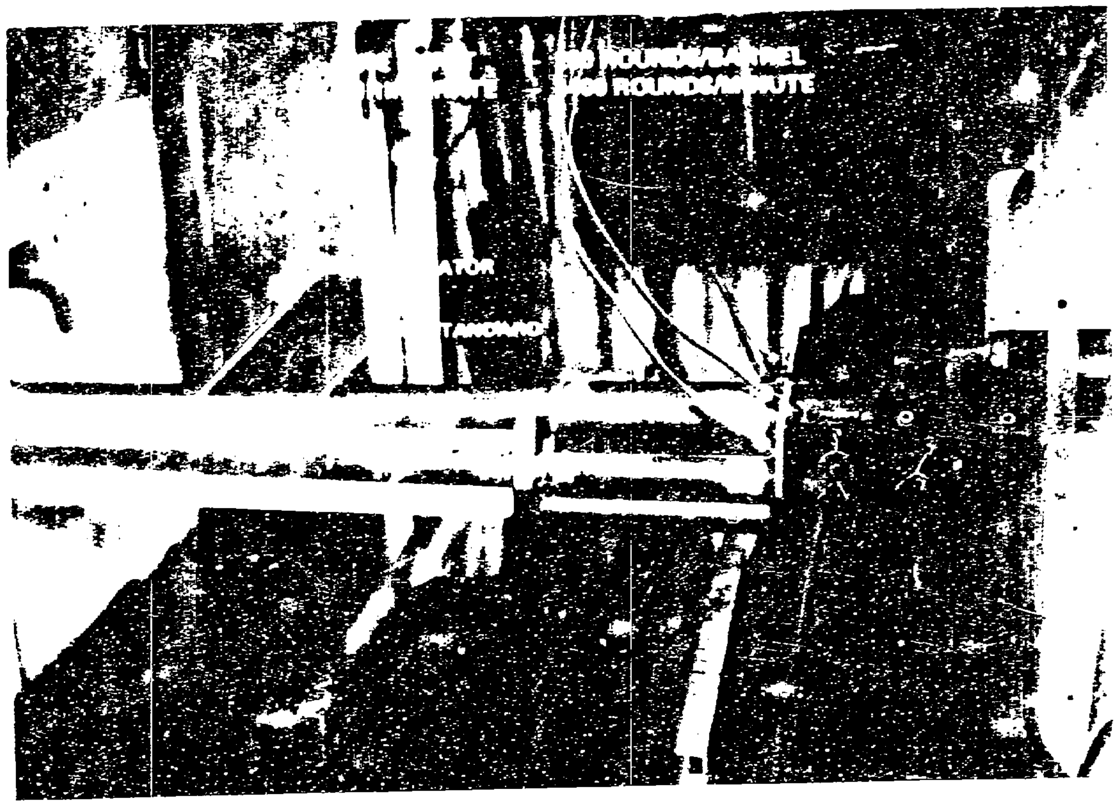
EQUILIBRIUM TEMPERATURE PROFILES

FIGURE 8



CROSS SECTION OF COMPOSITE BARREL

FIGURE 9



M61 FIRING ABLATIVE AMMUNITION

FIGURE 10

OVERVIEW OF EROSION IN U. S. NAVAL GUNS

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ABSTRACT

The modern history of erosion in U. S. Naval Guns will be reviewed. The Navy's use of low and high flame temperature propellants and chrome plating, the importance of rotating band barrel interface on "end of life" criteria and its experiences with plastic rotating bands on 3 and 5 inch projectiles will be reviewed. The effects of fatigue and erosion lives on the operational effectiveness of the fleet will be discussed.

INTRODUCTION

The Navy has no formal or informal program addressing wear and erosion in Naval Guns. However, a great amount of work has been conducted under individual projects directed toward specific applications for various gun systems which has significant impact on gun tube wear and erosion technology. No attempt will be made to present the detailed supporting data from each program to support the various observations and conclusions.

To maximum extent possible, an attempt will be made to reference the latest or summary reports, papers, etc., in order for one to obtain the data in a particular area of interest.

This paper will attempt to present an "overview" of wear and erosion in major Naval gun barrels (tubes). The effects of wear and fatigue lives of guns on the operational effectiveness of the fleet are given along with the Navy's experiences with low and high flame temperature propellants, chrome plating and rotating band design including plastic rotating bands.

HISTORY OF EROSION IN NAVY

The apparent Navy philosophy on the selection of propulsion packages is shown in Figure 1. From about 1900 until the 40s, the Navy essentially had one composition of propellant, called PYRO. Essentially, this was an M-6 or medium flame temperature propellant. A variety of weapon systems existed, ranging from 3" to 16". The Navy has always had a very strong emphasise on having smokeless propellants. The erosion lives of Naval guns range from approximately 4000 rounds to 300 rounds for the 3" to 16", respectively. The targets were primarily other ships and aircraft. The Navy only relied upon bag charges where necessary, i.e., in the much larger guns. Cased ammunition was employed for obvious advantages on rate of fire and mechanical handling systems. Barrel life was extended by the band-barrel interface design features. Even historically, the logistic problem of regunning ships was very costly. The main change in philosophy occurring during World War II was that flash suppression became very important. The enemy at sea employed gun flash to aim torpedoes to sink ships.

Since World War II the Navy has introduced NACO propellant, a low flame temperature propellant, which resulted in longer barrel wear life. Also the Navy is attempting to introduce a high performance charge for the 5"/54 gun system using M26, a high flame temperature propellant, with a talc wax liner. The missions have changed slightly, in that shore bombardment and anti-aircraft are the major roles.

APPARENT NAVAL PROPULSION PACKAGE SELECTION PHILOSOPHY
(GENERAL SERVICE)

1900-1943	1943-1944	1945-1976
ONE COMPOSITION (PYRO)	SAME	TWO COMPOSITIONS (PYRO, NACO) TRANSITIONING TO NACO
MEDIUM FLAME TEMPERATURE (2500-2650°K)	SAME	LOW FLAME TEMP. (2180°K)
13 WEAPON SYSTEMS (3"-16")	SAME	6 WEAPON SYSTEMS
STRONG EMPHASIS ON SMOKELESSNESS	SAME	
NO FLASH PRECAUTIONS	FLASH SUPPRESSION	SMOKELESS - FLASHLESS
P_{max} = 48,000 PSI (31.0 MPa)	SAME	SAME
4000 - 300 EFC (3"-16") 1400 EFC (6")	SAME	12000 - 1600 EFC (3"-16") 8000 EFC (6")
EFC EXTENSION BY BARREL-BAND DESIGN FEATURES	SAME	SAME
SHIPS AND AIRCRAFT TARGETS	SAME	SHORE BOMBARDMENT - AIRCRAFT
BAG CHARGES ONLY WHERE NECESSARY	SAME	SAME

EFC = Equivalent Full Charge

FIGURE 1

The prime difference between Army and Navy guns is that the Navy guns have accompanying handling systems and nearby ammunition storage, thus permitting high rates of fire for sustained periods; whereas the Army guns do not. The high rates of fire and sustained periods introduced two unique problems: that of hot gun cookoff of the projectiles (which can occur in a very short time) and very high wear rates. The erosion and fatigue lives of Naval gun barrels are presented in Table 1.

PROPELLANTS AND COOLANTS

After World War II, the U. S. Navy developed 3"/50 and 3"/70 gun systems. In developing these guns, various wear reducing techniques were evaluated because of extreme rapid fire ability (i.e., sustained firing of 90 -120 rounds per minute per barrel for 3"/70). CORDITE N, a medium cool burning propellant was developed for use in these guns. Other features, such as gain twist rifling, Probert rifling, plastic and metal bands were also tried, references (a), (b) and (c). During the early 1960s, NACO (Navy cool) propellant (flame temperature about 2200°K) was introduced in the Navy gun systems. Use of this propellant greatly extended the wear lives of the gun. The extended wear life introduced a fatigue problem in 5"/54 gun systems because of loose liner design gun barrel. The fatigue problem has been eliminated by a return to the monoblock, autofrettaged gun barrel design using modern gun steels. Additional details concerning propellants being employed by the Navy may be found in reference (d).

The use of cool propellants to reduce wear in Naval guns had no significant disadvantages because the gun chambers were large enough to permit the small additional volume of cool propellant required over medium or hot propellants to obtain the same levels of ballistic performance. Additional advantages, to that of reduced wear, were obtained: low probability of flash (cooler gases) and low amounts of smoke. The difference in erosion rates for the hot, medium and cool propellant are shown in Figures 2 and 3 as measured in the 5"/54 gun system). Figure 2 presents the metal removal rate vs maximum bore surface temperature as measured by bore surface thermocouples, reference (e). A summary of wear test results in 5"/54 guns is shown in Figure 3. The advantages of cool propellants are obvious. The effects of rapid, continuous fire or wear are shown in Figure 4.

In an attempt to increase ballistic performance of the 5"/54 system, a hot propellant (M26) with a talc-wax liner has been employed along with a new plastic banded projectile. The limited wear and temperature data has been obtained, reference (e) and (f),

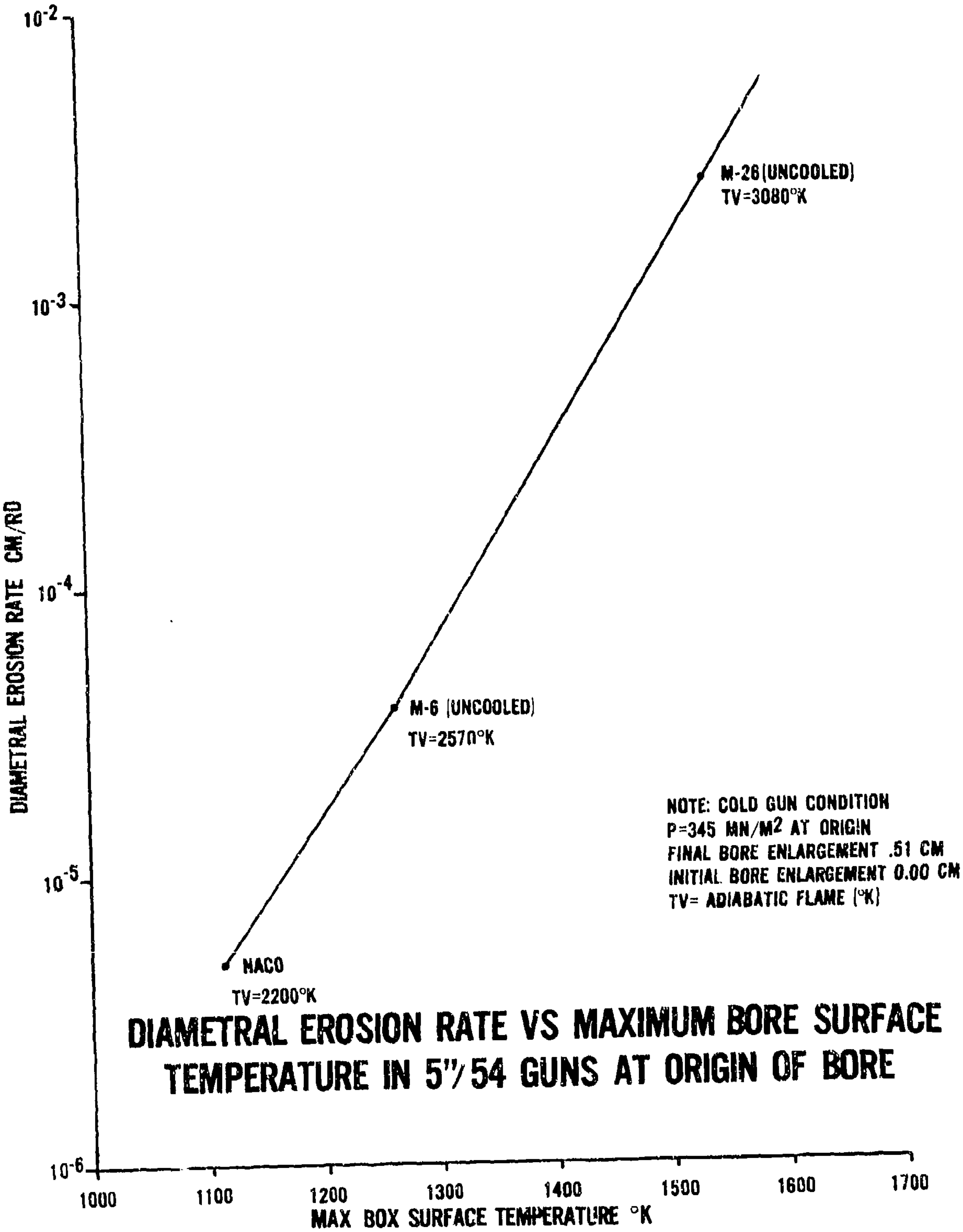
TABLE 1

EROSION & FATIGUE LIFE OF NAVAL GUN BARRELS

GUN	ALLOWABLE OBE (MAX) MM (IN.)	MAX VEL. LOSS (AV) M/S (FT/SEC)	FATIGUE LIFE NO. ROUNDS	PROPELLANT	ESTIMATED EROSION LIFE NO. ROUNDS SF	RF
3"/50	3.02 (.119)	24.68 (81)	1000 - 5000*	PYRO	4200	2050
				NACO	12000	4200
5"/38	5.33 (.210)	79.24 (260)	6000	PYRO	4600	--
				NACO	12000	--
5"/54	8.35 (.336)	108.20 (355)	2500 - 8000*	PYRO	1500	800
				NACO	10000	6000
6"/47	7.11 (.280)	63.70 (209)	4500	PYRO	2300	--
				NACO	6500	--
8"/55 (BAG)	7.26 (.286)	55.16 (181)	4500	PYRO	1170	--
				NACO	3320	--
8"/55 (CASE)	8.12 (.320)	32.00 (105)	1500 - 3000*	PYRO	1000	500
				NACO	3000	1500

OBE = ORIGIN BORE ENLARGEMENT
SF = SEMIAUTOMATIC MOUNT (SLOW FIRE)
RF = AUTOMATIC MOUNT (RAPID FIRE)

* DEPENDS UPON DESIGNS (MK & MOD's)



M-26 (UNCOOLED)
TV=3080°K
M-6 (UNCOOLED)
TV=2570°K
NACO
TV=2200°K
NOTE: COLD GUN CONDITION
P=345 MN/M2 AT ORIGIN
FINAL BORE ENLARGEMENT .51 CM
INITIAL BORE ENLARGEMENT 0.00 CM
TV= ADIABATIC FLAME (°K)
DIAMETRAL EROSION RATE VS MAXIMUM BORE SURFACE TEMPERATURE IN 5"/54 GUNS AT ORIGIN OF BORE
DIAMETRAL EROSION RATE CM/RD
10-2
10-3
10-4
10-5
10-6
1000
1100
1200
1300
1400
1500
1600
1700
MAX BOX SURFACE TEMPERATURE °K

FIGURE 2

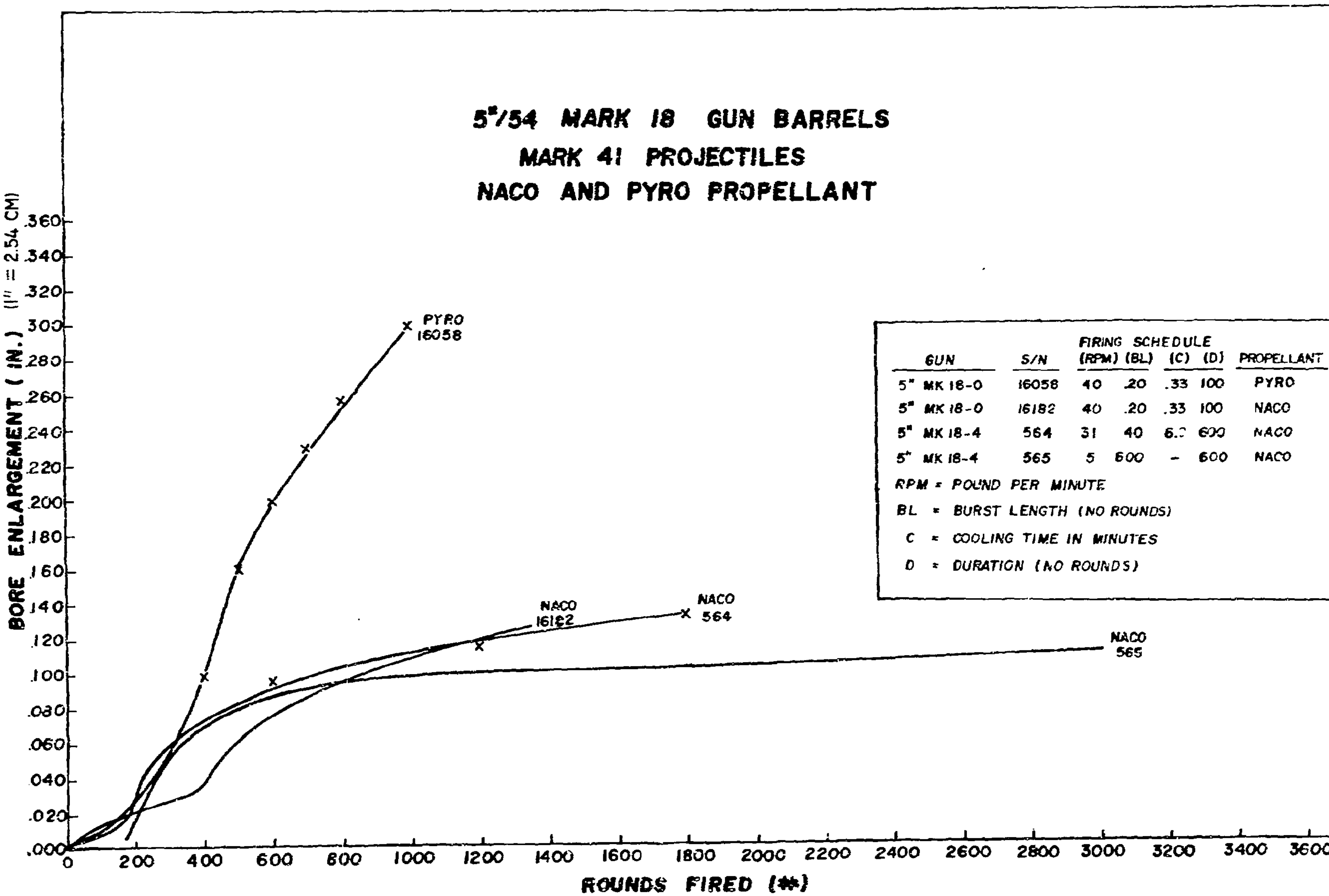


FIGURE 3

ORIGIN OF BORE ENLARGEMENT vs. ROUNDS FIRED

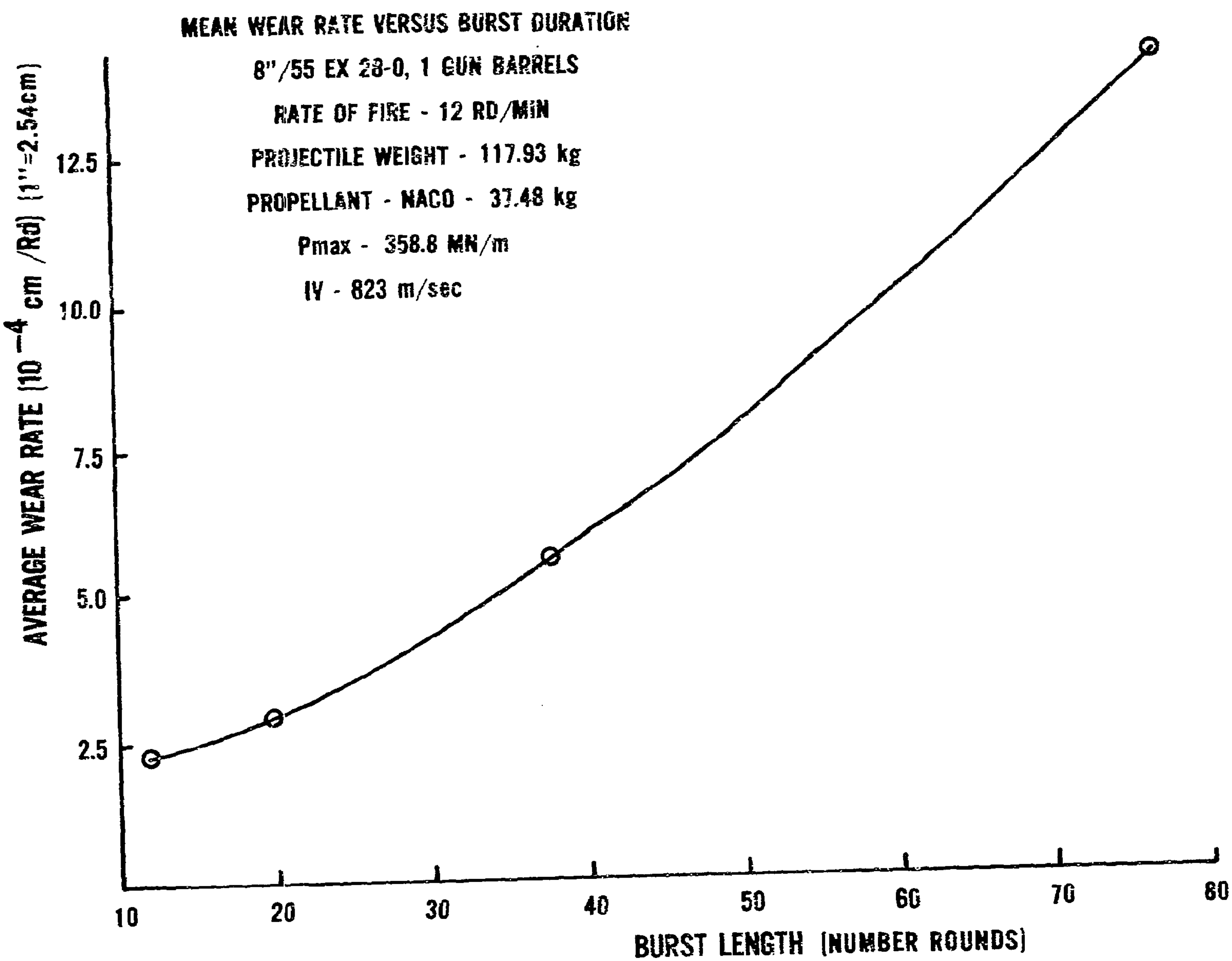

MEAN WEAR RATE VERSUS BURST DURATION
8"/55 EX 28-0, 1 GUN BARRELS
RATE OF FIRE - 12 RD/MIN
PROJECTILE WEIGHT - 117.93 kg
PROPELLANT - NACO - 37.48 kg
Pmax - 358.8 MN/m
IV - 823 m/sec
AVERAGE WEAR RATE (10 −4 cm /Rd) (1"=2.54cm)
12.5
10.0
7.5
5.0
2.5
10
20
30
40
50
60
70
80
BURST LENGTH (NUMBER ROUNDS)

FIGURE 4

which indicates that the added coolant reduces wear to about the same level as a medium flame temperature propellant.

A silicon oil coolant has been investigated primarily as a means of reducing gun barrel heating for safety reasons, reference (g), (to reduced gun barrels heating in order to prevent or lengthen the time required to cookoff gun ammunition). Bore surface temperature data indicate a significant wear reduction effect from the silicon oil. However,limited testing to date has been primarily conducted with NACO propellant which produces no measureable wear of the gun bore under the test condition with or without the silicon oil coolant.

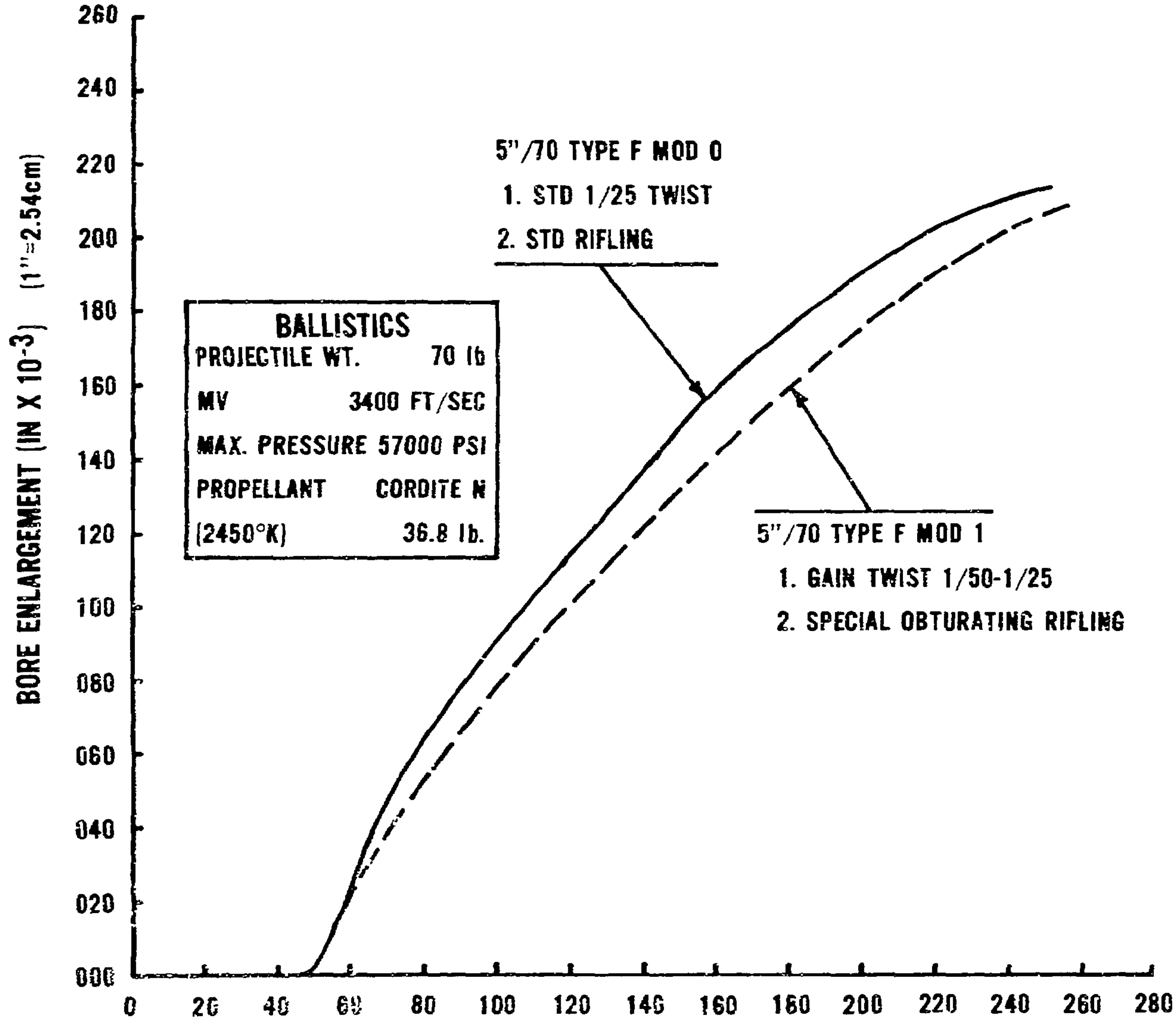
Analytic methods for predicting erosion of various propellants as well as the effects (both heating and erosion) of the coolants have been developed as published by references (e) through (h).

RIFLING, CHROME PLATING AND BAND DESIGN

As a result of various testing, the Navy has accepted standard uniform twist rifling with 0.12 mm (0.005") chromium plating, references (i) and (j). Gain and uniform twist with disappearing rifling at the muzzle to smooth the rotating band on the projectile body was investigated in the 3"/70 gun systems. A slight advantage in range was obtained. Gain twist rifling was shown to have no significant advantage over uniform twist rifling relative to gun tube erosion, Figure 5.

The use of chromium plating on Navy gun tubes has proven to have significant advantages. The primary advantage is the protection of the bore surface from the salt-sea environment. Chromium plating does provide a significant erosion reduction as shown in Figure 6. As much as 500 to 1000 rounds of cold gun firing may be required to remove the chromium plating. However, it must be noted that high flame temperature propellants remove the chromium plating much quicker. Also high rates of firing and sustained firing remove the chromium plating in a small number of round (see Figure 3). Manufacturing and 3"/50 firing experiences indicated that the 0.12 mm (0.005") thickness chromium plating was near optimum thickness for bore protection and retaining the plating during firing.

The effect that rotating band design has on gun erosion is discussed in detail in references (h) and (k). Band design can significantly increase the amount of allowable erosion before it is necessary to condemn a gun tube as shown in Figures 7 and 8. Band design and material can effect the frictional heating of the bore surface, thereby effecting the erosion. Experiments with



RAPID FIRE EROSION TESTS NPG RPT 627 OF AUG 1950
(10-15 RD/MIN)
FIGURE 5

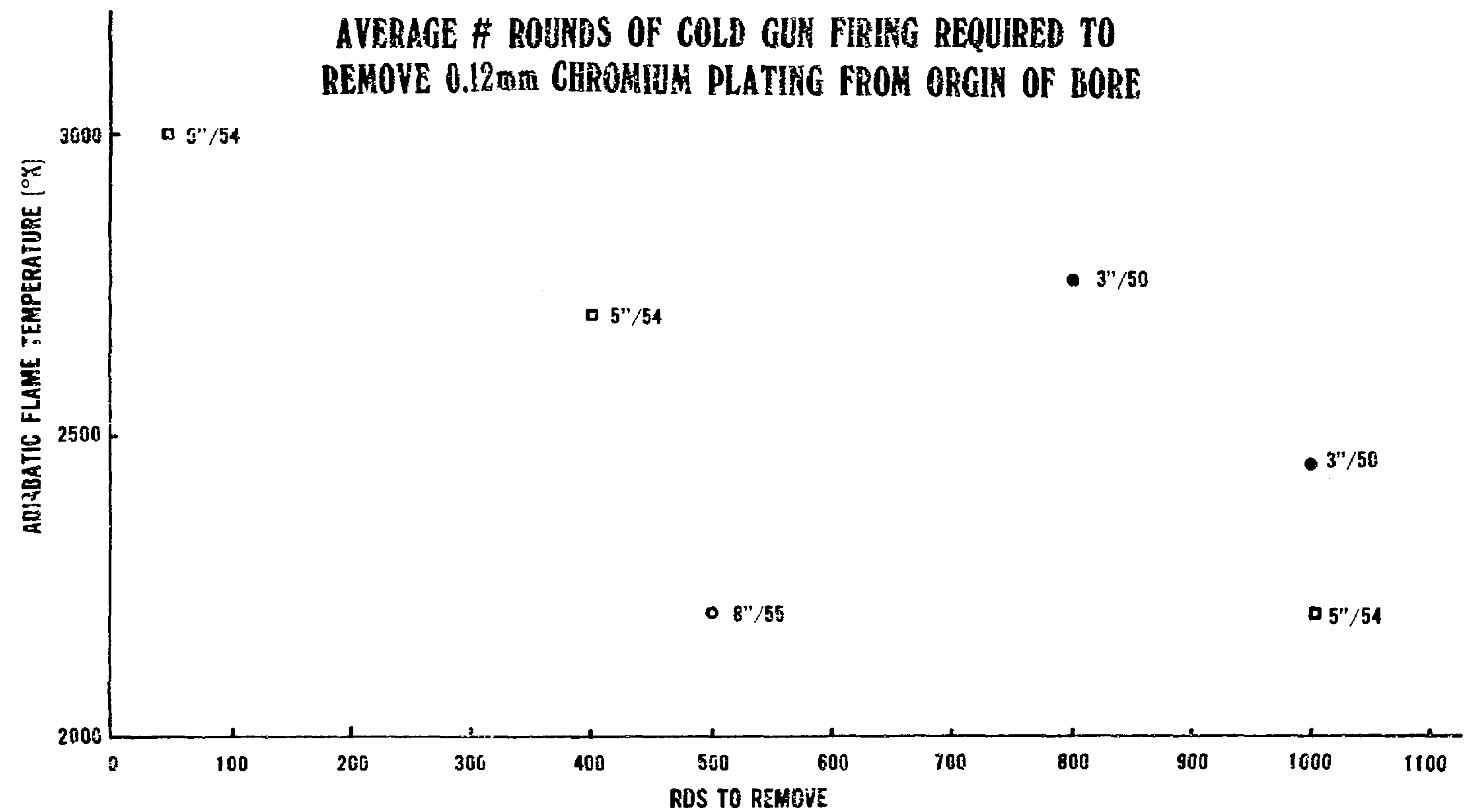
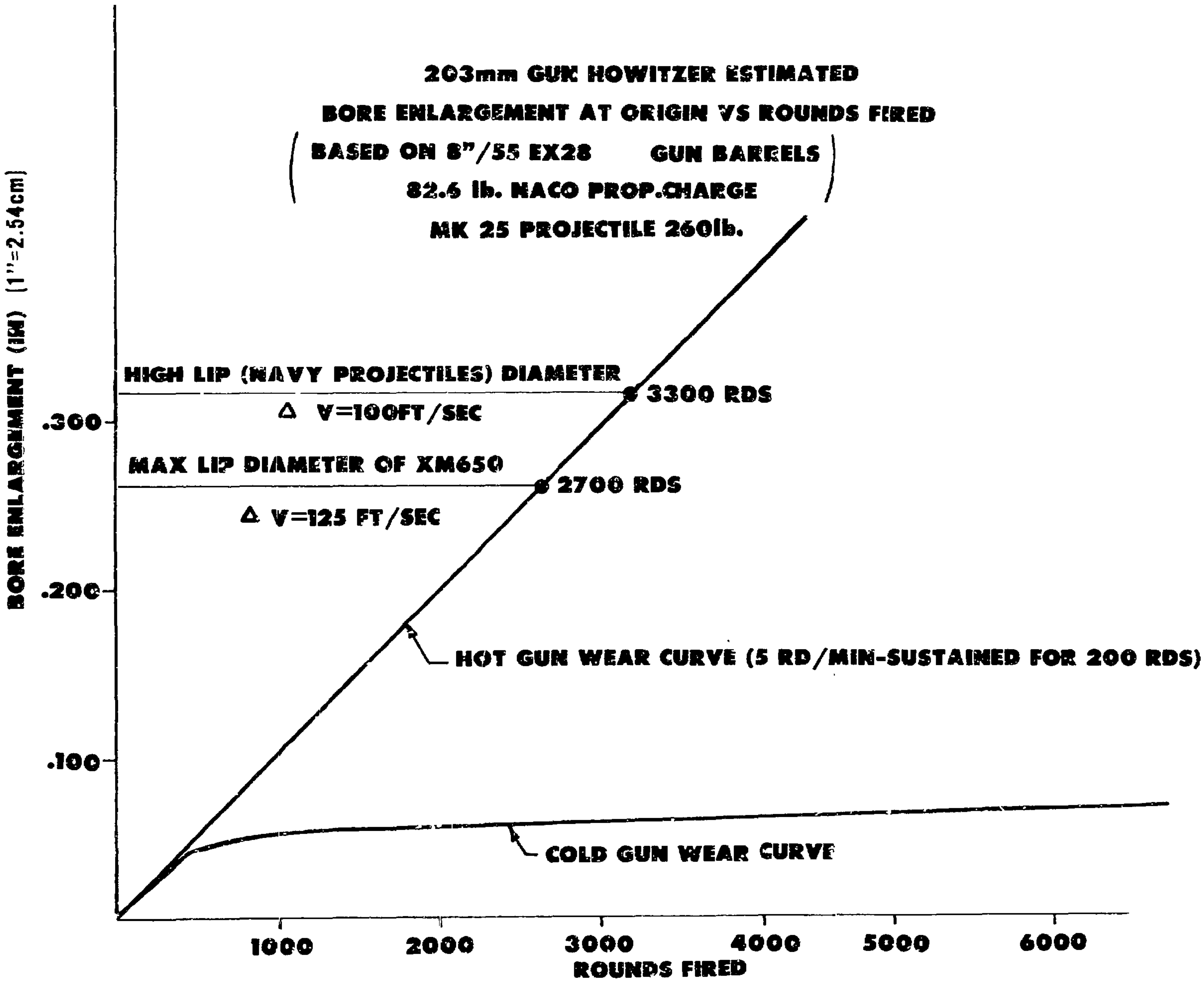

AVERAGE # ROUNDS OF COLD GUN FIRING REQUIRED TO
REMOVE 0.12mm CHROMIUM PLATING FROM ORGIN OF BORE
ADIABATIC FLAME TEMPERATURE (°K)
3000
2500
2000
5"/54
5"/54
3"/50
3"/50
8"/55
5"/54
0
100
200
300
400
500
600
700
800
900
1000
1100
RDS TO REMOVE

FIGURE 6



203mm GUN HOWITZER ESTIMATED
BORE ENLARGEMENT AT ORIGIN VS ROUNDS FIRED
(BASED ON 8"/55 EX28 GUN BARRELS
82.6 lb. NACO PROP.CHARGE
MK 25 PROJECTILE 260lb.)
HIGH LIP (NAVY PROJECTILES) DIAMETER
Δ V=100FT/SEC
3300 RDS
MAX LIP DIAMETER OF XM650
Δ V=125 FT/SEC
2700 RDS
HOT GUN WEAR CURVE (5 RD/MIN-SUSTAINED FOR 200 RDS)
COLD GUN WEAR CURVE
BORE ENLARGEMENT (IN) (1"=2.54cm)
.300
.200
.100
1000
2000
3000
4000
5000
6000
ROUNDS FIRED

FIGURE 7

plastic rotating bands in the Navy has given mixed results which can be explained. In 5"/54 system firings, plastic banded projectiles gave similar results to that of metal rotating banded projectiles as shown in Figures 9 and 10 and discussed in references (h) and (1). However, for 3"/50 gun systems less wear was observed as shown in Figure 11 and can somewhat be explained by the substantial difference in frictional heating caused by band pressure.

FLEET OPERATIONAL IMPACT OF EROSION

The high cost of gun barrel replacements, logistics and ship gun-line availability associated with short erosion and fatigue life gun barrels became a severe problem in Southeast Asia. Table 2 shows the effects of a 1500 and 6000 round wear or fatigue life gun barrels on ship availability. In fact, the Navy had the exact problem depicted, not from a wear problem (because of the use of NACO) but from a fatigue problem (loose liner gun barrel design). Gun barrels of monoblock design were air lifted to sea to obtain additional gun line availability. The effects of propellant selection (erosion life prediction) on the ship available for the 8"/55 gun system is shown in Appendix A..

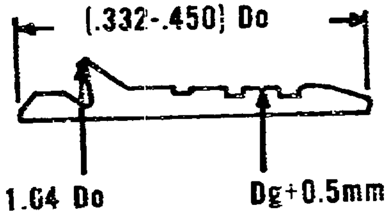
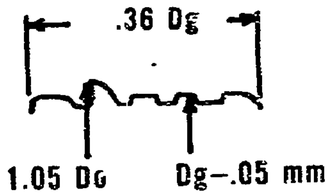
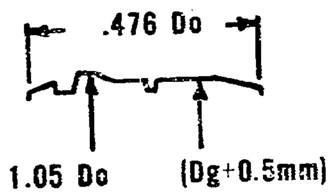
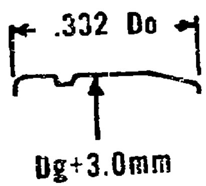
SUMMARY

The U. S. Navy has found that the use of cool propellant (NACO) gives very long gun barrel erosion lives without loss of ballistic performance even under the extremes of rapid sustained firing programs. Coolants, both talc-wax and silicon oil, are effective in reducing the heat input to the gun barrel,thereby reducing erosion effects and safety hazards. Prediction models have been developed which enable the weapons designer to evaluate propellant charge alternatives and their effects on erosion and gun barrel heating.

Chromium plating is desirable both to protect the bore from the salt water environment and as a means to extend the wear life (by 500 to 1000 rounds). The cooler the propellant the longer the chromium plating remains on the bore surface. Additional wear life can be obtained by the design of the rotating band. Concerning erosion reduction with plastic bands, in the Navy's 5" system where the band pressures are fairly low, plastic band seems to perform exactly the same as our metal band. Previous work, however, in 3"/50 has shown that the plastic bands do significantly reduce wear.

For the Army's sticker problem, plastic bands may be desirable because of their low friction.

In overall conclusions, the Navy has obtained from our various programs an awareness that cool propellants are an effective means

SHAPE	GUN SYSTEM	% ALLOWABLE BORE ENLARGEMENT
	NAVY METALLIC BAND	4–6
	175mm M 437	3
	120mm M-73	5
	155 mm M101 (GUN)	2

Dg=DIA GROOVES
Do=BORE DIAMETER

CONDEMNATION CRITERIA FOR ROTATING BAND DESIGNS

FIGURE 8

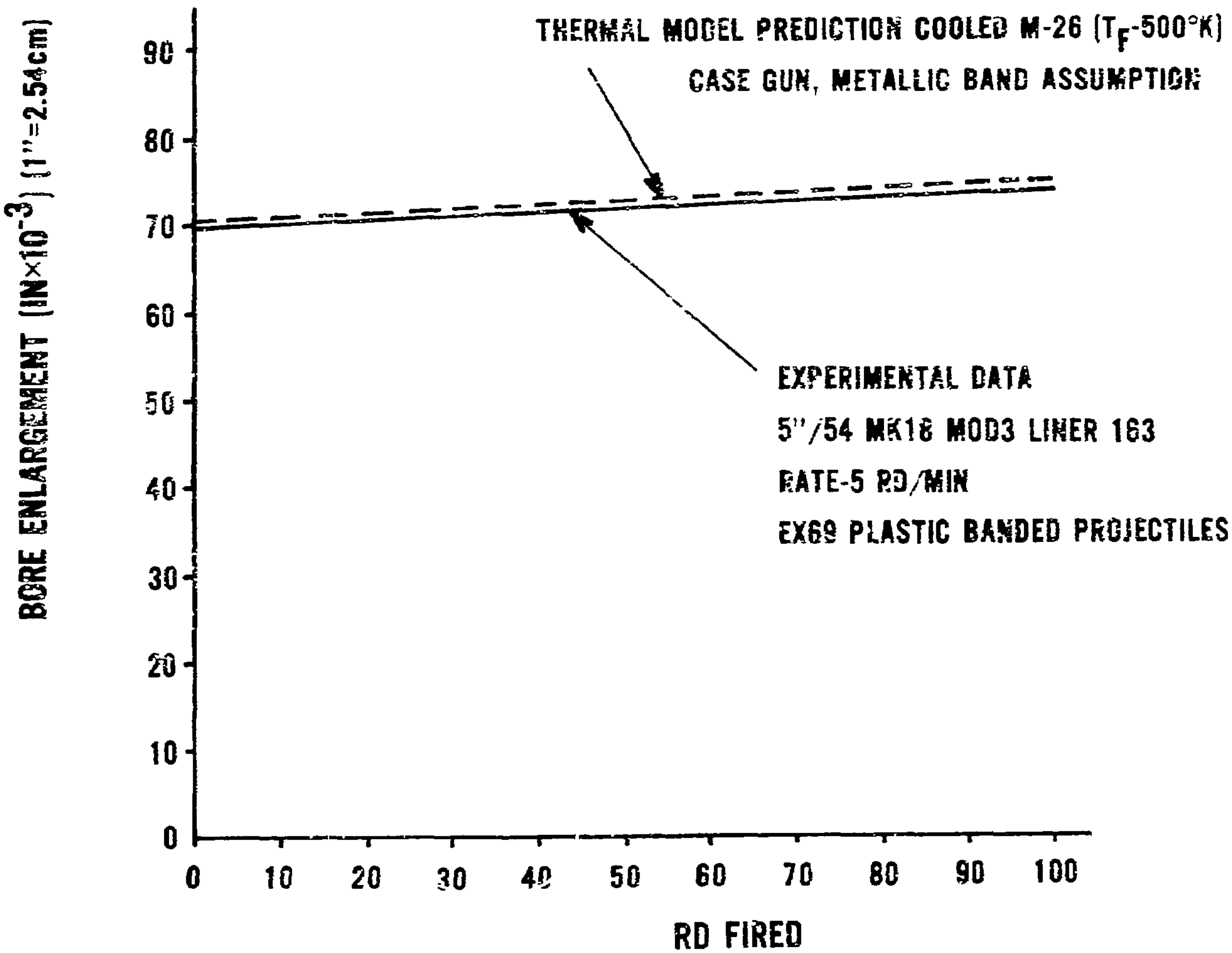
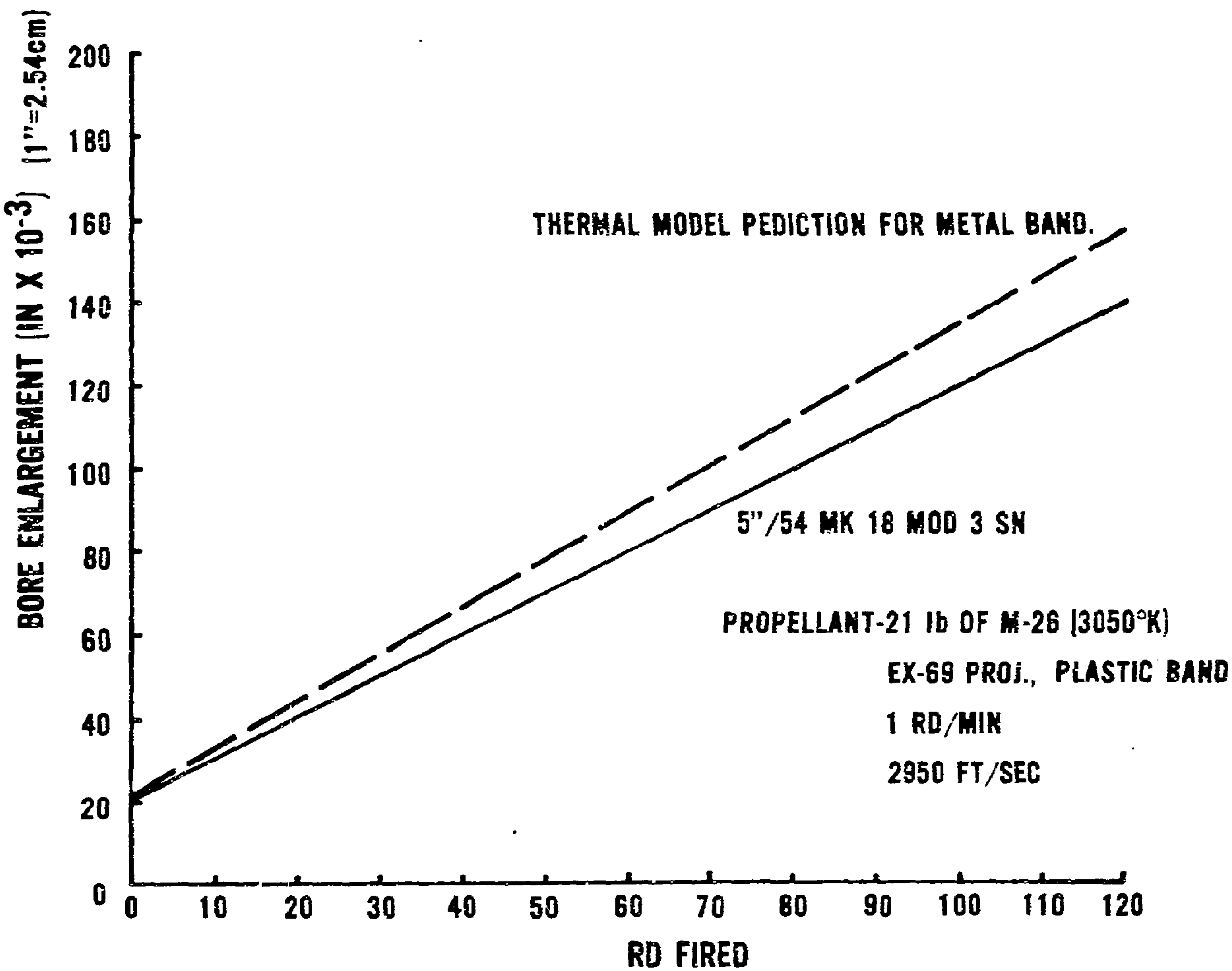
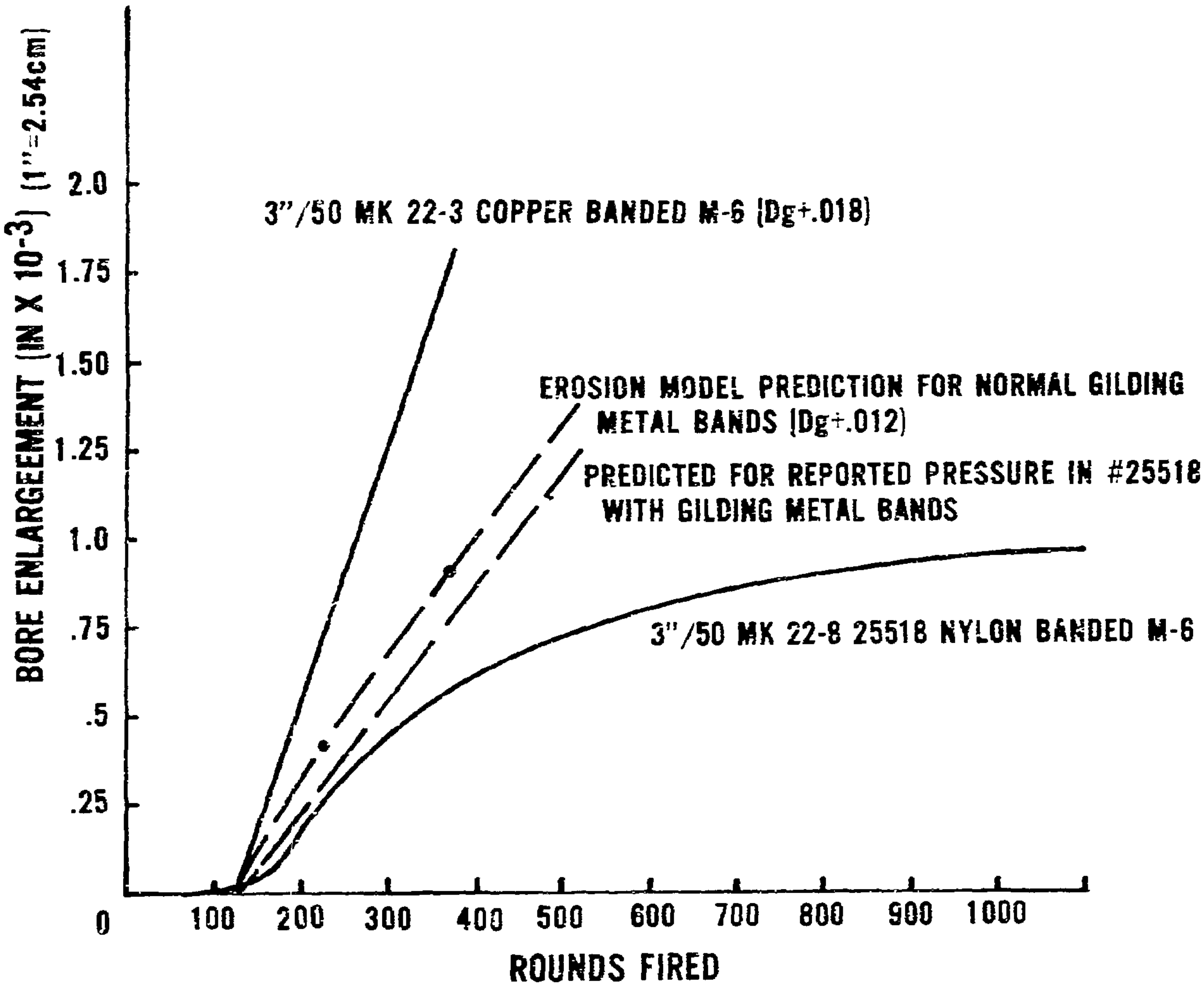

THERMAL MODEL PREDICTION COOLED M-26 (T_F-500°K)
CASE GUN, METALLIC BAND ASSUMPTION
EXPERIMENTAL DATA
5"/54 MK18 MOD3 LINER 163
RATE-5 RD/MIN
EX69 PLASTIC BANDED PROJECTILES
BORE ENLARGEMENT (IN×10^-3) (1"=2.54cm)
0
10
20
30
40
50
60
70
80
90
100
RD FIRED

FIGURE 9



BORE ENLARGEMENT (IN X 10-3) (1"=2.54cm)
200
180
160
140
120
100
80
60
40
20
0
THERMAL MODEL PEDICTION FOR METAL BAND.
5"/54 MK 18 MOD 3 SN
PROPELLANT-21 lb OF M-26 (3050°K)
EX-69 PROJ., PLASTIC BAND
1 RD/MIN
2950 FT/SEC
0 10 20 30 40 50 60 70 80 90 100 110 120
RD FIRED

FIGURE 10



3"/50 RAPID FIRE TESTS WITH NYLON AND GM BANDS

(55 RD/MIN, 175 RDS)

FIGURE 11

TABLE 2

IMPACT ON SHIP AVAILABILITY OF VARIOUS CHARGE ASSEMBLIES

SOUTHEAST ASIAN SCENARIO

AVAILABILITY (SHIP DAYS)

FATIGUE LIFE	GUN LINE	REPLENISHMENT	IN TRANSIT (TO REGUN & RETURN)
6000	60	30	30
1500	35	14	71

NOTE: A 120 CALENDAR DAY PERIOD IS ASSUMED.
AN AMMUNITION EXPENDITURE OF 300 RDS/GUN/DAY IS ASSUMED.
TRANSIT TIME TO AND FROM SUBIC & REGUNNING - 10 DAYS.

of controlling erosion and are acceptable provided the gun system is designed for their use.

Attention to rotating band design and use of chromium plating can effectively double or triple the useful life of a gun barrel. Long fatigue and erosion lives of Naval gun barrels are required for operational effectiveness.

REFERENCES

(a) NPG Report No. 1450 of 10 April 1956.

(b) NPG Report No. 1479 of 23 July 1956.

(c) NPG Report No. 1224.

(d) Mitchell, Stephen E., "Effect of Navy Gun Propellant on Gun Tube Wear and Erosion Symposium", Mar 1977.

(e) NWL TR-3028, Sep 1973.

(f) NSWC/DL TR-3202, Oct 1974.

(g) Boyer, C. T. and Russell, L. H.,"Simulation of Gun Barrel Heating and Associated Ammunition Thermal Response".

(h) Smith, C. and O'Brasky, J. S., "Gun Wear Estimation Method", Tri-Service Gun Tube Wear and Erosion Symposium, Mar 1977.

(i) Jeansen, Carl F.,"A Treatise on Rifling of Guns", Dept of Navy Bureau of Ordnance, June 1935.

(j) NPG Report No. 627 of Aug 1950.

(k) Shamblen, M. C. and O'Brasky, J. S., "End of Life Criteria", Tri-Service Gun Tube Wear and Erosion Symposium, Mar 1977.

(l) Foster, John S., O'Brasky, J. S. and Smith, C. S., "Heating and Wear Effects of Smear Coolant in Navy 5"/54 Gun, JANAF Propulsion Symposium, Dec 1976.

APPENDIX A

ADVANCED 8" AMMUNITION; PRELIMINARY EROSION LIFE ESTIMATE*

1. BACKGROUND

A gun erosion model was developed under the 203mm Marine Corps Gun Howitzer program. This model provides an estimate of new gun erosion rate accurate to ±15% under both cold gun and rapid fire conditions for any propellant, and pressure level. The model is most accurate for erosion rates between 0.002-0.03 mm/round (1.0 and 15.0 x 10^{-4} in/round). At lower erosion rate values the model predicts more erosion than actually occurs. The gun erosion model has been validated by using available data from 15 U. S. Army weapons and 13 U. S. Navy weapons of between 3" and 16" caliber operating between 275-86 - 517.24 MPa (40,000 - 75,000 psig) breech pressure and using propellants with flame temperatures ranging between 2000 to 3150°K.

The problem of estimating the erosion life of a gun under a given firing schedule somewhat more difficult. As a gun erodes the projectile seats further down bore. The geometric change produces two effects. First the chamber volume is increased reducing the peak pressure and, thus, reducing the heat input. Second the rotating band length which must pass over the origin of bore position is reduced thereby reducing friction heat input generated by the rotating band. The net effect of these two influences is to reduce the erosion rate. Based on historical data, the relationship $N = 2\Delta D_0 \max/W$ seems to give an accurate representation of the weapon erosion rate where

$(\Delta D_O)_{max}$ = the maximum bore enlargement, i.e., the minimum projectile rotating band diameter
W = the new gun erosion rate

2. OBJECTIVE

The objective of this task is to make preliminary estimates of the erosion lives of the 8"/55 EX 32 gun barrel when fired under a variety of firing schedules with three different propellant selections. The effect of using a silicon-oil coolant in the weapon is also to be estimated.

*Memo from DG-22 (Jim O'Brasky) to DG-50 (Doug Payne) dtd 10 Feb 77

3. INPUT DATA

NOS/IH supplied the following data:

Propellant	Charge Weight (Kg-lb)	Flame Temperatures (°K)
M1AI	39.46 (87)	2230
M1	38.10 (84)	2420
M6	36.74 (81)	2550

The weapon mean maximum breech pressure was given as 357.24MPa(51,800 psig) and the muzzle velocity for a 117.93 Kg (260 lb) projectile was given as 899.11 m/s (2950 ft/sec) for all solutions.

4. APPROACH

The erosion model was applied to calculate the new gun erosion rate for four different firing schedules using three propellants with and without coolant. The gun barrel erosion life was calculated. An estimate was then made of the effect of the gun barrel life on ship availability times using a Southeast Asia(and South Atlantic) and a Mediterranian (and North Asia) senario in a two week, four week and a 52 week engagement.

5. FIRING SCHEDULES

Four firing schedules designated S1, S2, S3 and S4 were defined. These schedules represent best, worst and average case conditions.

The S1 schedule represents cold gun firings (say o.1 rd per min). This condition is a best case.

The S2 schedule represents a sustained shore bombardment schedule. The firing rate is 1 round/minute and the number of rounds fired is the ship magazine capacity of 300 rounds.

The S3 schedule represents a surge short bombardment schedule. The firing rate is 6 rounds/minute. The number of rounds fired is 75. The S2 and S3 schedules should be considered average short bombardment schedules.

The S4 schedules represents a worst case schedule. The number of rounds fired is 75 at the rate of 12 rounds/minute. Seventy-five rounds are the ready service capability of the 8" MK 71 gun mount.

6. SCENARIOS

The two senarios considered each assumed an expenditure of 300 rounds/day and a 300 ship magazine capacity. It was assumed that one day is required for re-arming. Gunline refers to the number of ship days available on the gun line during the period being considered. Since a multi-mission capable ship can perform other combat missions while transiting to and from the rearming station. The sum of Gunline and Re-Arm is the number of days in the combat zone the ship is available for missions other than gun fire. The term in-transit refers to the time required to steam back to port, regun and return.

The Southeast Asia (South Atlantic) senario assumes 4.5 days steaming to port, one day in port and 4.5 days steaming to return to the operational area.

The Mediteranian (and North Asia) senario assume two days steaming to port, one day in port and two days steaming to return to the operational area.

7. RESULTS

The erosion life estimates (rounds) for the several propellants are given below. The cooled data is designated with a "C".

Propellant	Schedules S1	S2	S3	S4
M1A1	2318	1540	1496	1254
M1	1411	907	882	725
M6	1053	663	644	524
M1A1"C"	11129	8377	8237	7284
M1"C"	6589	4794	4693	4071
M6"C"	4784	3410	3332	2865

Note: The accuracy of these estimates is about +30%, -5% the four significant figures are retained to allow future method tracability.

The effect of gun barrel erosion life on ship availability days is given below.

Southeast Asia Senario

Life, Erosion (rd)	Days Gun-Line	Re-Arm	Transit	Campaign Length
600	3	1	10	Two Weeks
1200	4	3	7	"
1800	6	5	3	"
2400	7	6	1	"
3000 & greater	8	6	0	"

SOUTHEAST ASIA SENARIO

Life, Erosion (rd)	Days			Campaign Length
	Gun-Line	Re-Arm	Transit	
600	4	3	20	Four Weeks
1200	8	7	13	"
1800	9	9	10	"
2400	10	8	10	"
3000	10	9	9	"
3600	12	11	5	"
4200	14	13	1	"
4800 & greater	15	13	0	"
600	56	28	281	365
1200	85	64	216	"
1800	104	87	174	"
2400	116	102	146	"
3000	125	113	127	"
3600	133	121	111	"
4200	138	128	99	"
4800	142	133	90	"
5400	140	132	93	"
6000	149	142	74	"

MEDITERRANIAN SENARIO

600	4	2	8	Two Weeks
1200	5	4	5	"
1800	6	5	3	"
2400 & greater	7	7	0	"
600	7	4	17	Four Weeks
1200	9	7	12	"
1800	11	9	8	"
2400	11	10	7	"
3000 & greater	12	11	5	"
600	91	46	228	365
1200	122	91	152	"
1800	136	114	115	"
2400	146	128	91	"
3000	152	137	76	"
3600	156	144	65	"
4200	160	148	57	"
4800	162	152	51	"
5400	164	155	46	"
6000	166	158	41	"

8. CONCLUSIONS

a. The conditions specified using uncooled propellant gives average erosion lives ranging from 650 to 1520 rounds. The impact of these life levels on ship availability is such that at best 37% of the ship time will be spent on the gun line. The cooled propellant solutions range between 3350 and 8300 rounds or 42-45% of the ship time on the gun line. It is thus concluded that a cooled solution should be used.

b. In hot gun conditions M6"C" propellant gives a much shorter erosion life than M1A1"C" and M1"C" and a much higher probability of cookoff also exists. Thus, M1A1"C" and M1"C" are the only reasonable solutions. If M1A1"C" can get the performance it is a much preferable solution from flash and blast, cookoff and barrel life consideration.

c. The gun barrel for the MK 71 gun mount should be changed from a 3000 round safe fatigue life loose liner to a 7000 safe fatigue life monobloc.

GUN BARREL EROSION CONTROL IN FOREIGN COUNTRIES

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Erosion of Gun Barrels in Foreign Countries is a much older problem that it is in the U.S. At least that is what one can infer from this picture (1) of a tribarrel cannon f.r 'enry VIII of England. Going back to the 20th century, we ..d, as J. Warmsley already pointed out, that basically the same problems persist there as they do here and that similar solutions are being sought. Overall however, one can observe that erosion does not quite take such an important place abroad as it does here, simply, I think, because firing during the training of troops is not that extensive. It appears that the French for instance would accept a wear life of 200 rds for a tank cannon; something we probably would not find acceptable, even so the 105mm M68 once had a life of only 100 rds.

A limited review of some of the efforts ongoing abroad shall be presented here and is best grouped into Small and Large Caliber Weapons.

Among the Small Caliber Weapons, the two main systems which have a severe wear problem are the New Assault Rifle for Caseless Ammo and the 27mm Automatic Cannon for the MRCA.

The British claim a life of 3000-4000 rds for their 4.85mm caliber weapon without a bore coating. Wear is observed at both breech and muzzle, leading to increased dispersion. Cr-plating is being considered to increase barrel life. We also hear, that a tungsten coating by CVD is being considered. It is expected that they will experience similar problems we have encountered in coating the interiors of such small caliber barrels reliably.

The German approach seems to differ from this. The Germans were quite successful in increasing the barrel life of their 7.62mm weapon by changing the rifling contour from the standard land and groove configuration to a so called polygonal profile. We suspect that a similar approach for the 4.32mm weapon was not successful. They also will try Cr-plating, are interested in our Cobalt-Iron coating but will definitely keep on trying to improve the geometry of the rifling and the wear characteristics of the steel by improved manufacturing practices such as button rifling, where a subcaliber bore will be swaged over a mandrel and simultaneously autofrettaged. The Germans consider 6000 rds for an Assault Rifle a realistic life. The life as of 1 1/2 years ago was only 2000 rds.

The weapon which was shown to us had some other advantages however, one of which was, what seemed to be, a very successful caseless ammo, as shown in this picture (2).

The 27mm MRCA Automatic Cannon, for which Mauser is the main contractor, has indeed a very serious erosion problem. The barrel life is quoted as 1200 rds, however some barrels last 2000 rds, some 300. Ion-nitriding has been tried, but was found to be worse than the untreated barrel. This is most likely due to the lower melting point of Iron Nitride. A Niobium alloy liner has been tried. Often it is cheaper to design for a quick change with a cheap material than to go to an exotic material which necessitates also exotic processing and machining. Measurements have shown, that the heat penetrated zone in this barrel reaches to a depth of 0.3mm. This is just about the land height in a barrel of this caliber and this might be the reason why Cr-plating has not been tried or if tried, was unsuccessful. It is difficult to maintain an accurate rifling profile by chrome plating to the same thickness as the rifling depth. It is said that Cr-plating is not being considered because of fear of hydrogen embrittlement. This is difficult to understand.

In this connection it should be mentioned that the Germans seem to actively pursue the idea of using an austenitic steel as a liner and substrate for Cr-plating. An austenitic steel of course would not undergo the phase changes on heating and cooling, often considered to be the cause for the failure of Cr.

Of the large caliber weapons, to our knowledge only the 90mm tank cannons and the new 120mm smooth bore gun are Cr-plated. Erosion in other weapons does not seem to be so serious that it cannot be taken care of by changes in propellant formulations, granulations or with additives.

Even so, for the 120mm, in order to achieve acceptable wear lives, both Cr-plating, as well as charge changes, were used.

The contractor for chrome plating stresses the importance of electropolishing before plating in order to remove any distorted layer from the machining process. This is considered to be important for the durability of the Cr-layer.

The German specifications for Cr plating prescribe the steel removal to be half the thickness of the final Cr-plate.

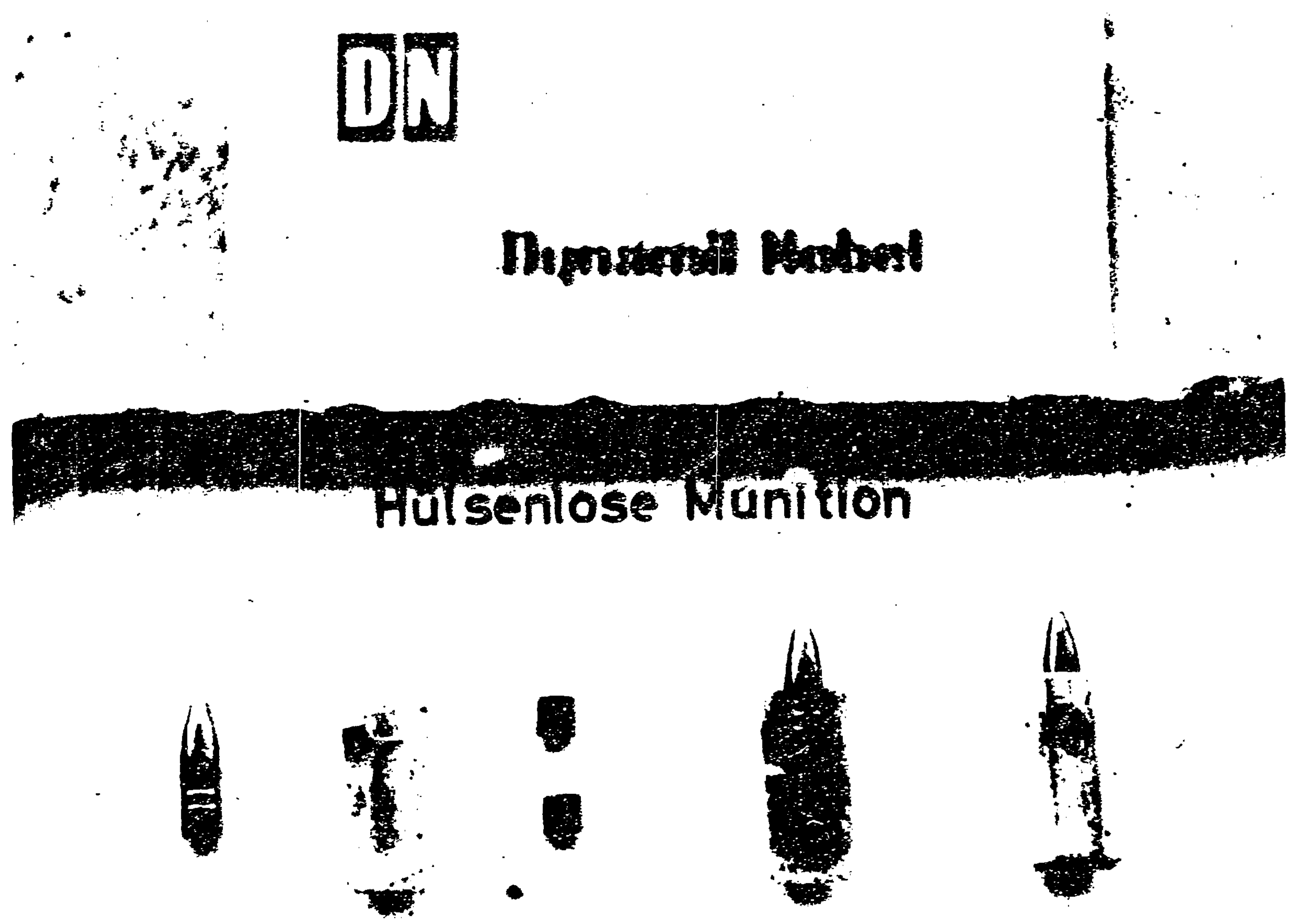
In their development programs, the FRG has an applied technology program to exploit existing technologies from other fields of industry for the application of erosion resistant liners for gun barrels. The processes which are under consideration are:

1. Explosion bonding
2. Roll bonding
3. Co-forging
4. Centrifugal Casting

It seems that all these processes have been tried at some time or other in the U.S. Nevertheless technology does progress, and it seems that the centrifugal casting process has met some success. The materials under consideration are alloys of the stellite family with various W-additions to raise the melting point. A careful selection of liner and jacket has to take place to match coefficients of expansion, otherwise cracking will occur on cooling.

A longer range program is also envisioned and considers Welding, Plasma Spraying, Vapor Deposition both Physical and Chemical, again for bonding liners to jackets or for creating the liner in situ (coatings). Other ideas which are being "kicked around" are Ion plating, Sputtering, and Vacuum coating with $MoZrO_2$. This sounds very ambitious. How much of this is or will materialize, only time can tell.

PICTURE 1



PICTURE 2

SESSION II

IDENTIFICATION, MEASUREMENT AND CONDEMNATION CRITERIA

Chairman: Morley Shamblen

Naval Surface Weapons Center
Dahlgren Laboratory

METALLOGRAPHIC CHARACTERIZATION OF ERODED GUN BARRELS

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ABSTRACT

Under rapid-fire conditions, a gun barrel experiences repetitive high rate loading, thermal shock and chemical attack. An adequate characterization of the damage to the barrel material necessitates the definition of service conditions in terms of stresses, temperatures and the chemically active species. However, because of the complexity of the various factors involved during firing, only indirect methods of assessing such parameters have been found to be useful. Considerable information was obtained by metallographic examination of test-fired gun barrels and controlled laboratory tests. Transverse sections of test-fired 7.62mm barrels were examined. Microstructures at several locations in the radial direction were compared with microstructures of test samples which were annealed isothermally at elevated temperatures for various time intervals. Radial microhardness profiles were taken to corroborate the microstructural study. Results are presented to show how the temperature distribution in the bore surface region can be estimated. Also, comparisons of the crack patterns in the bore region of two different alloys, test-fired under identical schedules, were made to determine the temperature distribution. Similar analysis was made on barrels after testing at different firing schedules.

INTRODUCTION:

Erosion in gun barrels refers in general terms to the deterioration and enlargement of the bore which eventually cause loss of accuracy, yaw, and loss of velocity of the projectile. High temperature caused by the burning of the propellant makes the barrel material at the bore surface vulnerable to intense chemical attack by the burnt gases, repetitive mechanical and thermal stresses, structural changes, and phase transformations of the materials. Although pressure and chemical reactivity are the forces which cause barrel deterioration, the rate at which erosion takes place is decided by temperature. If the product of erosion (the material removed from the bore by erosion) could be obtained in the state in which erosion occurred and could be examined for metallurgical changes, specific statements about service conditions could be made. However, this is not possible. Also, instrumentation to measure directly the bore surface temperature during firing has not so far produced reliable data. A critical assessment of the factors which cause erosion and the damage (type and intensity) to the barrel material in service is important for continued efforts to combat erosion.

A logical approach for the understanding of erosion phenomena in small arms rapid-fire gun barrels has been to test fire barrels made of different materials under specific firing schedules, obtain as much data as possible during test firing by instrumentation, and to metallurgically examine the fired barrel by sectioning and metallography. Intelligent piecing together of measured data, metallographic observations and deductions therefrom coupled with the results of controlled tests on materials in the laboratory will present a picture of erosion that will set guidelines for the choice of barrel material, selection of type and amount of surface protection, and design of barrel configuration for specific use in the field.

During the past few years, 7.62mm M60 barrels were made of a variety of materials and were test fired according to several schedules as part of programs on barrel materials and fabrication techniques. Some of the barrels were instrumented to record the outside surface temperatures during test firing. Analytical studies were performed to estimate the bore surface temperature by solving heat transfer equations. Extensive metallographic examination of the sections of test-fired barrels was performed. Only brief references will be made to the thermal analyses study since it has been reported earlier.[1] The results of the metallographic examination will be detailed in this paper and generalized conclusions will be drawn at the end.

PROCEDURE:

7.62mm M60 barrels were fabricated from heat treated low alloy steels and superalloys. Table 1 shows the alloys and their heat treatment prior to fabrication. They were test fired in the unplated condition according to several firing schedules. Unless otherwise stated, the information in this paper will focus the attention on a firing schedule to be described as six 125 rds bursts with 10 sec intermediate cooling followed by complete cooling. The barrels were test fired until retirement. The barrels were sectioned transversely and longitudinally and the sections were prepared for metallographic study. Microhardness surveys were taken on the transverse sections. The metallography of test fired Cr-Mo-V steel barrel was coupled with controlled heat treatment study of Cr-Mo-V Steel specimens and comparison of microstructures.

TABLE 1

MATERIAL	HEAT TREATMENT
1. Cr-Mo-V Steel	Heat at 1700°F for 2 1/2 hrs, Air Cool " " 1575°F " " Oil Quench " " 1210°F " " Air Cool
2. Pyromet X-15	Heat at 1700°F for 1/2 hr, Water Quench " " 1025°F " 4 hrs, Air Cool
3. CG-27	Heat at 1875°F for 2 hrs, Oil Quench " " 1450°F " 16 hrs, Air Cool " " 1200°F " 16 hrs, " "
4. Armco 22-4-9	Heat at 2150°F for 1 hr, Water Quench " " 1400°F " 14 hrs, Air Cool
5. A-286	Heat at 1800°F for 1 hr, Oil Quench " " 1325°F " 16 hrs, Air Cool
6. Inconel 718	Heat at 1875°F for 1 hr, Air Cool Hold at 1325°F for 8 hrs, " " Furnace Cool to 1150°F @ 100°F/hr Hold at 1150°F for 8 hrs, Air Cool
7. Udimet 700	Heat at 2150°F for 4 hrs, Air Cool " " 1975°F " " " " " " 1550°F " 25 hrs, " " " " 1440°F " 16 hrs, " "

Controlled heat treatment study was performed on small specimens of Cr-Mo-V steel heat treated the same way as the gun barrels were. The heating of the specimens was carried out in a tube furnace with air as ambient. The temperature of the specimen was monitored by a thermocouple welded to the specimen. The temperature measurement was correct to within ±5°F. The temperature of the specimens rose to 1300°F in about 2 1/2 minutes and to the specified temperature in about 2 more minutes. After the specimens were held at temperature for desired lengths of time, they were oil quenched, aircooled or furnace cooled. The specimen surface was then ground to remove the decarburized layer and was prepared for metallography.

Temperature measurements were made on the outside surface of special M60, 7.62mm, rapid-fire gun barrels (one piece construction) during test firing under different schedules. The barrels were made of Cr-Mo-V steel and were in the unplated condition. Chromel-Alumel thermocouples were welded to the surface at various locations along the length of the barrel and the emf's (which were later converted to temperatures) were measured by a high-speed recorder. From these values, the temperatures at various radial locations were calculated by the implicit finite-difference method.

RESULTS AND DISCUSSION:

The recorded outside surface temperatures of a Cr-Mo-V steel barrel on a schedule of six bursts of 125 rounds with 10-second intermediate pauses are shown in Figure 1. The barrel wall is thickest in the breech end and thinnest at the muzzle end. Figure 2 shows the time-temperature graph at 2" from the 'Origin of Rifling' (OR) for four different schedules. The effect of firing schedule is obvious from the figure. Figure 3 shows the measured outside surface temperature profile at 2" from the OR along with the calculated temperatures of the "bore surface."

Some explanation is necessary about the phrase "calculated bore surface temperature." The temperatures at various radial locations after four bursts of 125 rounds with an intermediate cooling for 10 seconds are shown in Figure 4. Curves like this were generated for several points along the time-temperature profile for the firing schedule. The implicit finite-difference method is applicable to regions very close to the bore surface. A small region still exists where accurate calculations are possible only if the nature of the slope can be defined. The bore surface temperatures quoted in this report do not refer to the "skin" of material at the bore surface. Also, each time a shot is fired, the bore surface temperature reaches a peak and quickly drops to a stable value. Attempts have been made to measure this temperature by carefully designed thermocouples positioned at the bore surface.[2] A record of the measurements is shown in Figure 5. These peak temperatures are probably of value to study the chemical reactions occurring in the bore; but, for mechanics considerations, only the lower end of the envelope is relevant because of the very short decay times.

The following general comments are given with respect to heat transfer characteristics in the barrel: The temperature gradient across the barrel section becomes insignificant at the end of each cooling interval. The fact that the outside surface gradually becomes heated as the firing continues and thus the temperature gradient is gradually decreased is considered important because of the following reasoning: The thermal stresses and the pressure stresses operate in opposite directions. When the outside surface is cold, the thermal stresses partially nullify the pressure stresses. But, when the outside surface is hot, the nullifying effect is decreased. Furthermore, this action occurs at a time when the barrel is mechanically weak. During service, the barrel is heated and cooled between the ambient temperature and temperatures as high as 1400°F. From this observation, the importance of the study of thermal fatigue in gun barrels is evident.

Inasmuch as microhardness is an indication of the microstructure of the material, changes in microstructure can be detected by microhardness measurements. Since severe erosion is experienced in the breech end of the barrel, transverse sections 1/2" apart at 0-4" from OR were prepared from a test fired Cr-Mo-V barrel and radial microhardness profiles were recorded. Measurements as close (0.010") to the bore surface as physically possible without violating rules of microhardness measurements were made. Figure 6 shows the radial profiles of microhardness at various distances from OR. The greatest change is recorded at 2" from OR. The inserts in this figure show the microstructure of the material at specified locations. Figure 7 is actually a heat transfer analyst's delight. It is an etched longitudinal section of a Cr-Mo-V steel barrel which was fired for 4500 rds in Schedule I. The macroetch reveals the heat affected region, the boundary of which is to be treated as having experienced temperatures in excess of 1210°F for long enough times to cause structural changes. Figure 8 is a microhardness profile graph for sections of barrels made of Pyromet X-15, A-286, and Cr-Mo-V Steel at a distance of 2" from OR. The same sections of barrels made of other alloys (Table 1) did not show any change in microhardness. Quenched and tempered low alloy steels when reheated to temperatures lower than their tempering temperatures and cooled, do not lower in hardness or change in microstructure. Some of them are likely to get harder due to the formation of secondary carbides. A similar behavior occurs in superalloys when they are reheated to temperatures lower than the first ageing temperature. These facts, when viewed alongside the temperatures in the heat treatments of the alloys in Table 1, will put the temperature near the bore surface of the M60 barrel for the particular schedule at 1325-1425°F.

A close examination of the microstructure in the heat affected zone of the test-fired Cr-Mo-V steel barrel along with the knowledge of this microhardness (Figure 6) leads one to believe that subcritical isothermal transformation has taken place to varying degrees, depending on the longitudinal and radial distance along and away from the bore. The microstructure is characterized by spherodization of the tempered martensite. The microhardness readings corroborate with the spheroidized structure. An attempt was made to duplicate the microstructure in the laboratory by controlled reheat treatment of specimens of quenched and tempered Cr-Mo-V steel. Figure 9 is a collection of a few of the resultant microstructures out of several attempted heat treatments. Out of the many, the one that came close to duplicating the microstructure along with hardness in the heat affected zone near the bore surface was a spherodize anneal at 1375°F. Decarburization which would cause lowering the hardness in quenched and tempered low alloy steels, is a remote hypothetical possibility. Although both oxidizing and carburizing conditions seem to exist near the bore surface, the overall effect actually appears to be carburizing on the surface. Knoop hardness measurement taken close to bore surface (0.005"), however suspect, indicates a slight increment in hardness of the material.[3] This is further corroborated by the microstructure (Figure 10) near the bore surface.

Microstructural changes during reheating of heat treated alloys are functions of time as well as temperature. The real life time of a barrel during 4500 rds in the specific schedule is only a few minutes. In addition, the effect of temperature is felt in the environment of pressure. The rates of heating and cooling are far from equilibrium rates. In backtracking from available evidence to the causes, e.g., temperature, one must clearly distinguish between, say, the actual bore surface temperature and the temperature to which the material behaves as if it has been exposed. The distinction is important enough that it deserves scrutiny. The ultimate goal in understanding the erosion phenomenon is to incorporate protection techniques to minimize erosion damage. In practical terms, assessment of what happens to the material and what causes it will help in the prevention of such a thing's happening. Due to the short duration and the nonequilibrium conditions involved, the hardness change and the microstructural change indicate that the material near the bore surface in the breech end of barrel behaves as if it is exposed to a temperature of 1325-1425°F. The macroetched profile in Figure 7 shows that different locations either along the length or away from the bore surface have experienced the effects of subcritical temperatures to different lengths of time. Two facts are worthy of mention. A chrome plated Cr-Mo-V steel M60 barrel under the same firing schedule can be fired for 9000 rounds before its heat affected zone will appear similar to that of an unplated barrel. An H-11 chrome-plated 7.62mm M134 barrel has twice as long a useful life as a chrome-plated Cr-Mo-V steel barrel. H-11 material under dynamic loading conditions has an edge of about 50°F over Cr-Mo-V steel in the sense it retains its yield strength at high rates of loading up to a higher temperature than does Cr-Mo-V steel.[4]

This examination of the pattern of cracks emanating from the bore surface of barrels made of different materials and test fired under different schedules has resulted in some interesting observations concerning the state of stress and the temperature. Of necessity, the observations are qualitative nevertheless valuable. Two prominent cases will be discussed in detail. The first is the case of a barrel made of A286 alloy test fired at two different schedules: I.six 125 rds bursts with 10-second pauses followed by complete cooling and II. twenty 20 rds bursts with 4 second pauses followed by complete cooling. Figure 2 shows the time-temperature graphs for the outside surface temperature of barrel at 2" from OR for these two schedules. A transverse section of a barrel tested according to schedule I taken at 2" from OR shows (Figure 11) intergranular cracking around the bore surface and not extending very deep radially. The fine slip lines near the grain boundaries in the cracked region coupled with the lowering of the hardness in that region would suggest localized deformation. The succeptibility of iron-nickel and nickel-base alloys to intergranular cracking in corrosive environment at elevated temperatures is well known. Because of intense intergranular cracking and pulling away of grains from the bore surface the material did not show any radial cracks. Another material, Armco 22-4-9, which is more resistant to intergranular cracking shows (Figure 12) radial cracks in a section taken at 2" from OR. It would have been interesting to test fire a barrel made of this material at a slower schedule. But, brittleness of this material caused catastrophic failures which ruled out further testing of this material. An identical section of a barrel tested according to schedule II shows (Figure 13) transgranular cracks travelling at 45° to the bore surface (hoop stress axis). Figures 14 and 15 show the microstructures near the bore surface of transverse sections at 2" from OR taken from barrels made of Inconel 718 and Udimet 700 respectively. Both barrels were test fired according schedule I for about 9000 and 11,200 rounds. The Inconel 718 alloy shows radial intergranular cracks and the Udimet 700 alloy shows transgranular cracks at 45° to the hoop stress axis. Figure 16 is a micrograph of a transgranular 45° crack in Udimet 700 barrel at a higher magnification.

Figure 17 is a schematic representation of fatigue crack propagation in polycrystals. Crack propagation by fatigue takes place by two stages under a wide range of stressing and environmental conditions. Stage I, typically at low strains, propagates on a plane oriented at about 45 degrees to the stress axis and changes direction slightly at grain boundaries. Stage II, typically at high strains, propagates at 90 degrees to the stress axis. Stage I propagation rate is extremely slow and can be absent if the strain level is high enough. Laird (5) discusses in detail the features of these two stages in his excellent paper. The strain level near the crack tip is a function of applied stress and the strength of the material. The latter is a function of temperature: the higher the temperature the lower is the strength, etc., Figure 18 shows the variation of yield strength with temperature for A286, Inconel 718 and Udimet 700. The dashed horizontal line is general hoop stress level in an M60 barrel. The stress state is not as simple as is stated. However, a stress level is indicated for qualitative considerations only.

Based on the above mentioned facts the hypothesis concerning the temperature to which the material near the bore surface at 2" from OR in an M60 barrel reacts is as follows: In the case A286, for schedule II the strain level is well below the level corresponding to a temperature of about 1300°F whereas, for schedule 1 it hovers around general yield levels which would correspond to a temperature around 1400F. This projection corroborates well with temperature calculations near the bore surface (Figure 3) and hardness measurements (Figure 7). A similar argument about the crack patterns in Inconel 718 and Udimet 700 barrels test fired at the same schedule would lead to an estimate temperature of about 1450°F for the bore surface for schedule I. The strain level in Inconel 718 is "high" and for Udimet 700 is low. The difference is caused by the fact that at the same stress level at about 1450°F the Inconel 718 alloy is near general yield and Udimet 700 alloy remains well below yield.

From the forgoing discussion, it would be presumptuous to state that a complete analysis taking into consideration all the factors has been made. The tendency of the materials to microstructural changes, microhardness changes, stage I and II and fatigue cracks have been viewed alongside the measured and calculated temperatures, yield strength values and available knowledge about fatigue crack growth phenomenon. The study, by no means, is exhaustive or accurately quantitative. The available facts point to the general vicinity of about 1350-1450°F for the temperature of near-bore surface region in an M60 barrel for schedule I .

CONCLUSIONS:

1. Microhardness measurements, microstructure observations and analysis of fatigue cracks near the bore surface were performed on sections of test-fired 7.62mm rapid fire gun barrels made of a variety of materials. Results indicate that the material in the bore surface near the breech end behaves during schedule I as if it had been exposed to temperatures not higher than about 1400°F.

2. Precise determination of radial profiles of carbon content in a test fired Cr-Mo-V steel barrel would assist in the establishment of chemical conditions existing in the barrel during service.

3. Microstructural stability and mechanical strength under high rates of loading are important criteria for selection of materials for gun barrels.

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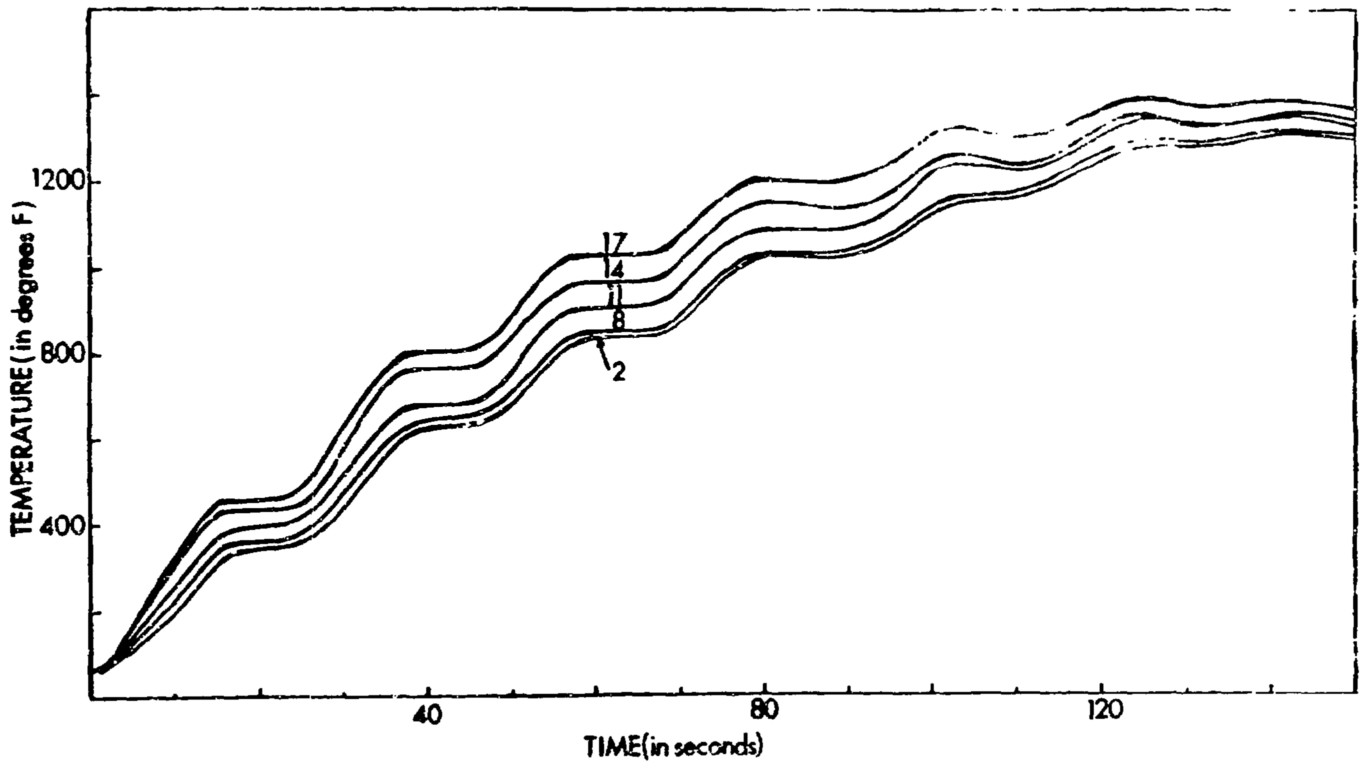

FIGURE 1. RECORDED TEMPERATURES OF THE OUTSIDE SURFACE OF AN M60 BARREL DURING FIRING. THE NUMBERS ON THE LINES REFER TO THE DISTANCES FROM THE ORIGIN OF RIFLING. THE SCHEDULE IS SIX 125 ROUND BURSTS WITH 10 SECONDS PAUSES.

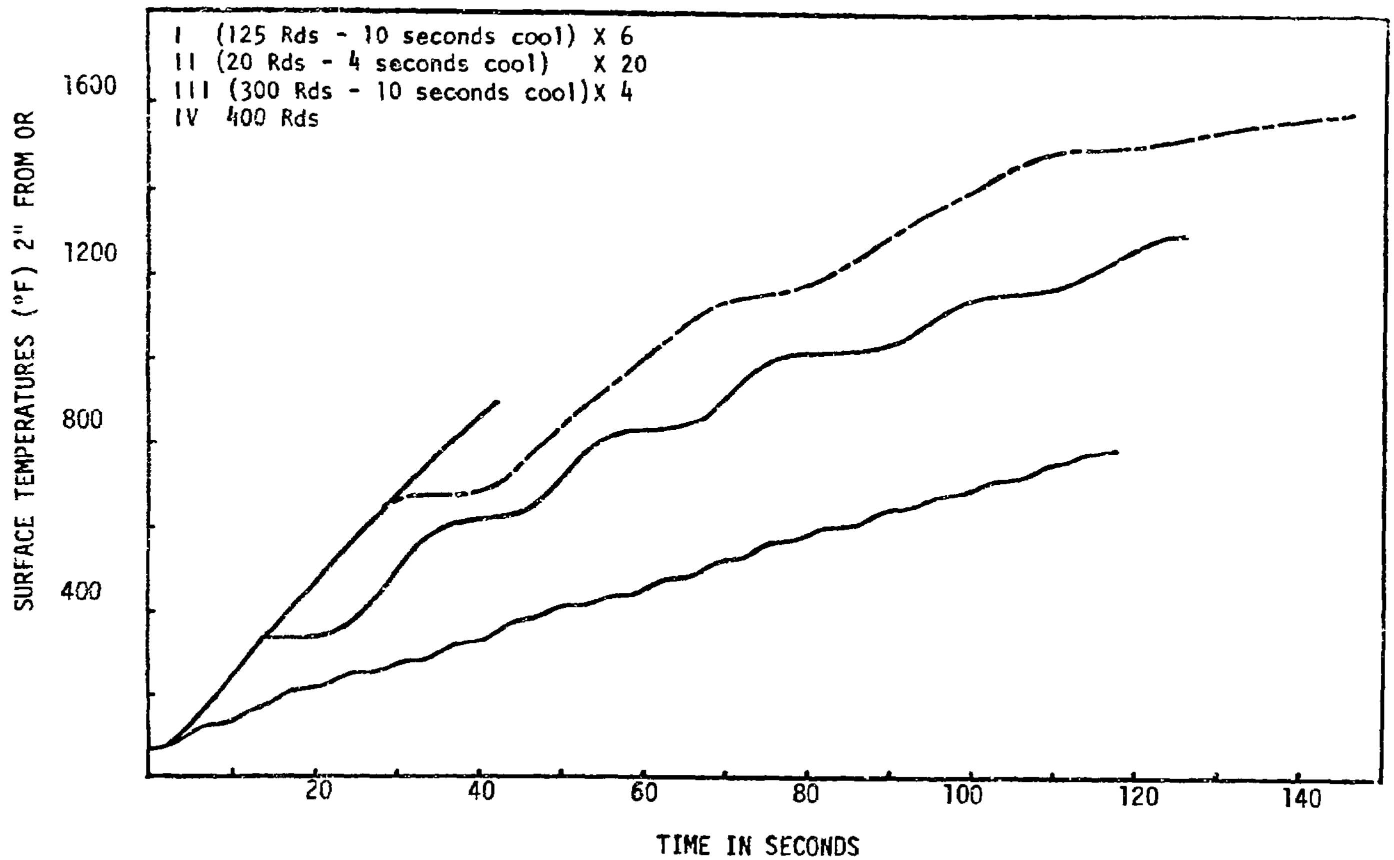

FIGURE 2. RECORDED TEMPERATURES OF THE OUTSIDE SURFACE OF AN M60 BARREL AT 2" FROM THE ORIGIN OF RIFLING FOR DIFFERENT FIRING SCHEDULES.

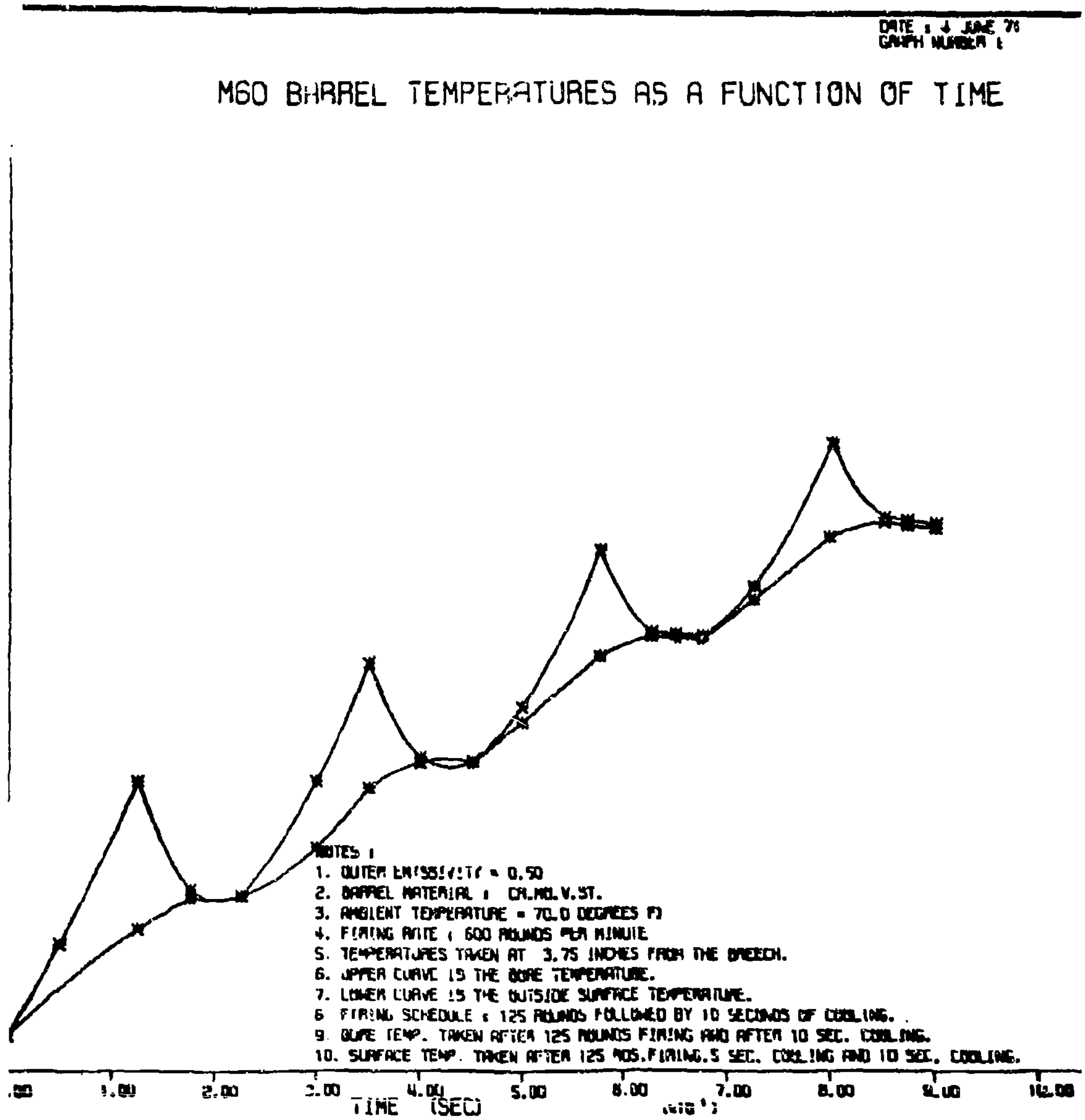

FIGURE 3. CALCULATED VALUES OF BORE SURFACE TEMPERATURES AND MEASURED VALUES OF OUTSIDE SURFACE TEMPERATURES OF AN M60 BARREL.

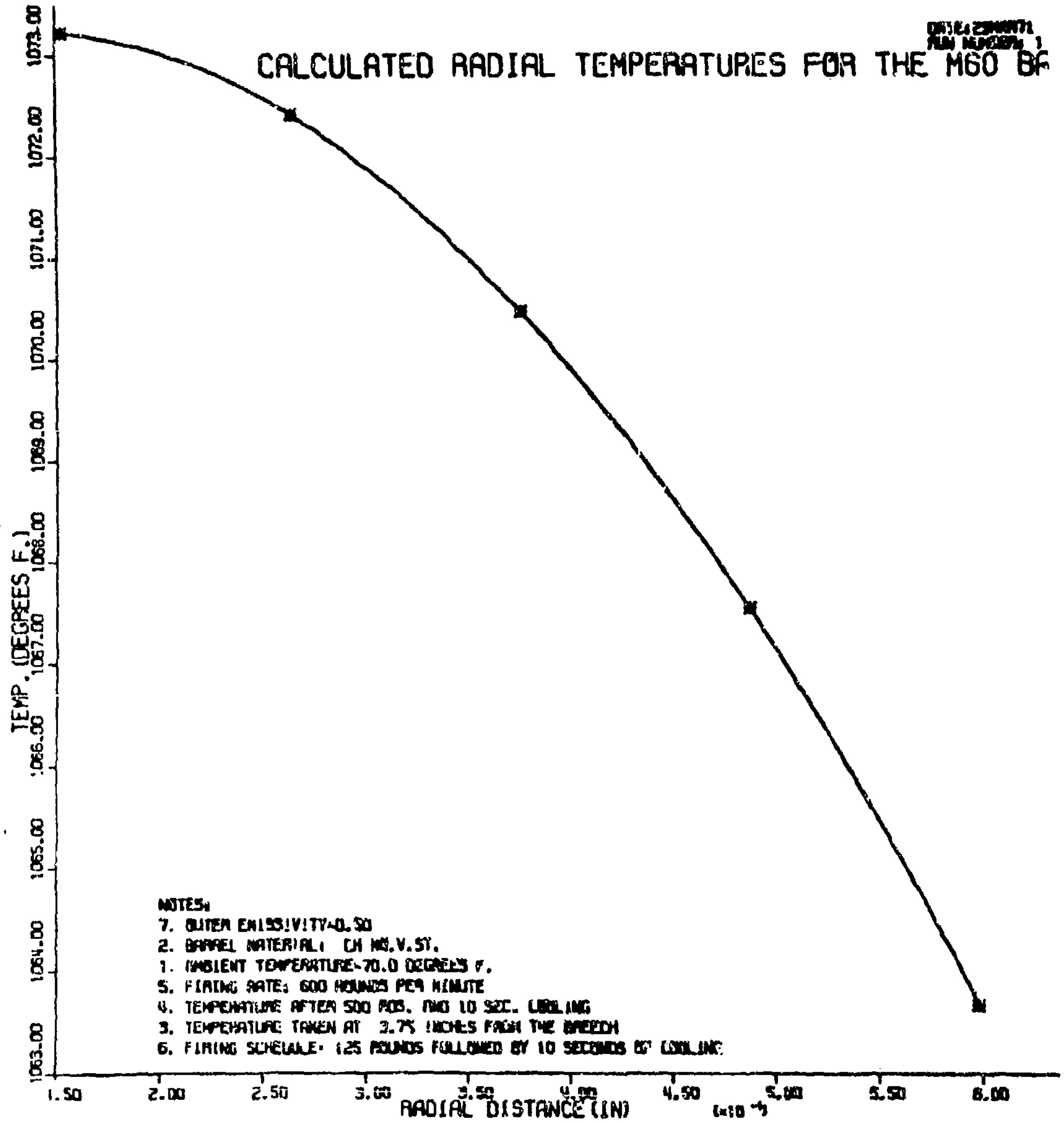

FIGURE 4. CALCULATED VALUES OF RADIAL TEMPERATURES. THE RADIAL DISTANCES ARE MEASURED FROM THE AXIS OF THE BARREL.

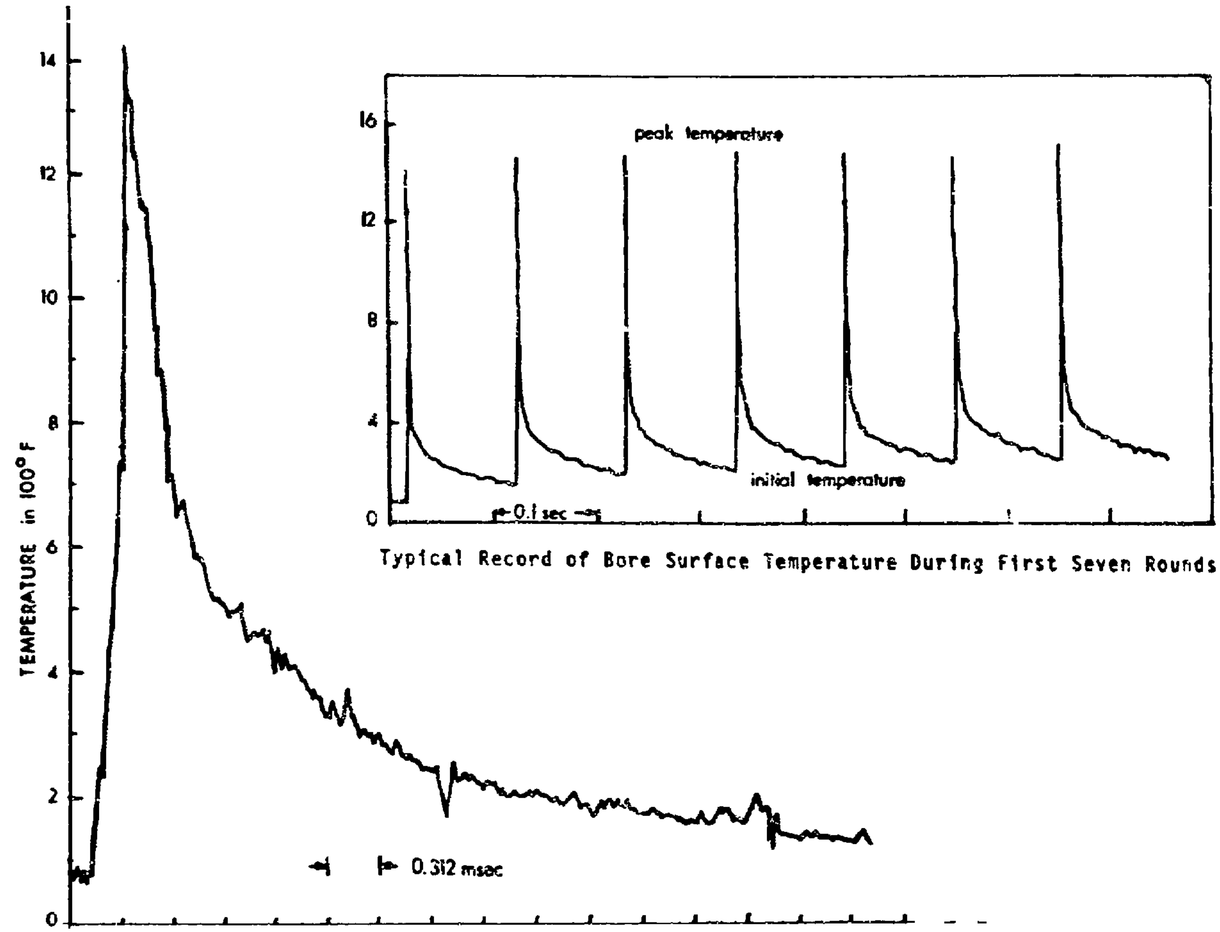

FIGURE 5. BORE SURFACE TEMPERATURE OF FIRST ROUND ON EXTENDED TIME SCALE IN AN M60 BARREL.

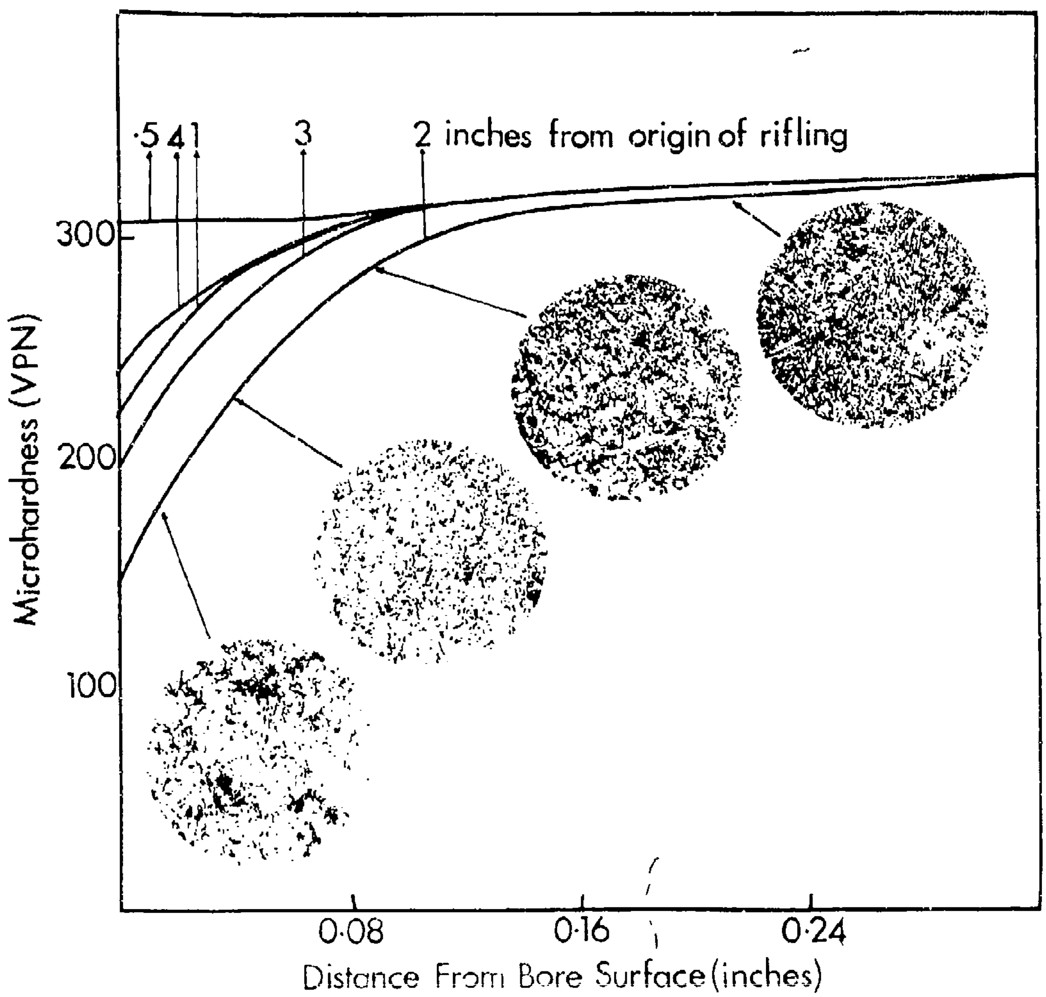

FIGURE 6. RADIAL MICROHARDNESS TRAVERSES ON TRANSVERSE SECTIONS OF TEST-FIRED CR-MO-V STEEL BARREL. INSERTS SHOW THE MICROSTRUCTURE OF THE MATERIAL AT SPECIFIED LOCATIONS.

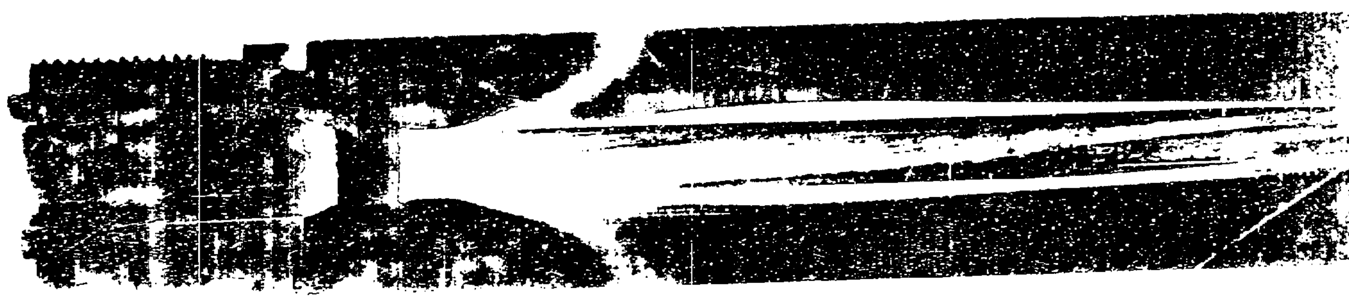

FIGURE 7. LONGITUDINAL ETCHED SECTION OF TEST-FIRED CR-MO-V STEEL M60 BARREL SHOWING THE THERMALLY ALTERED LAYER.

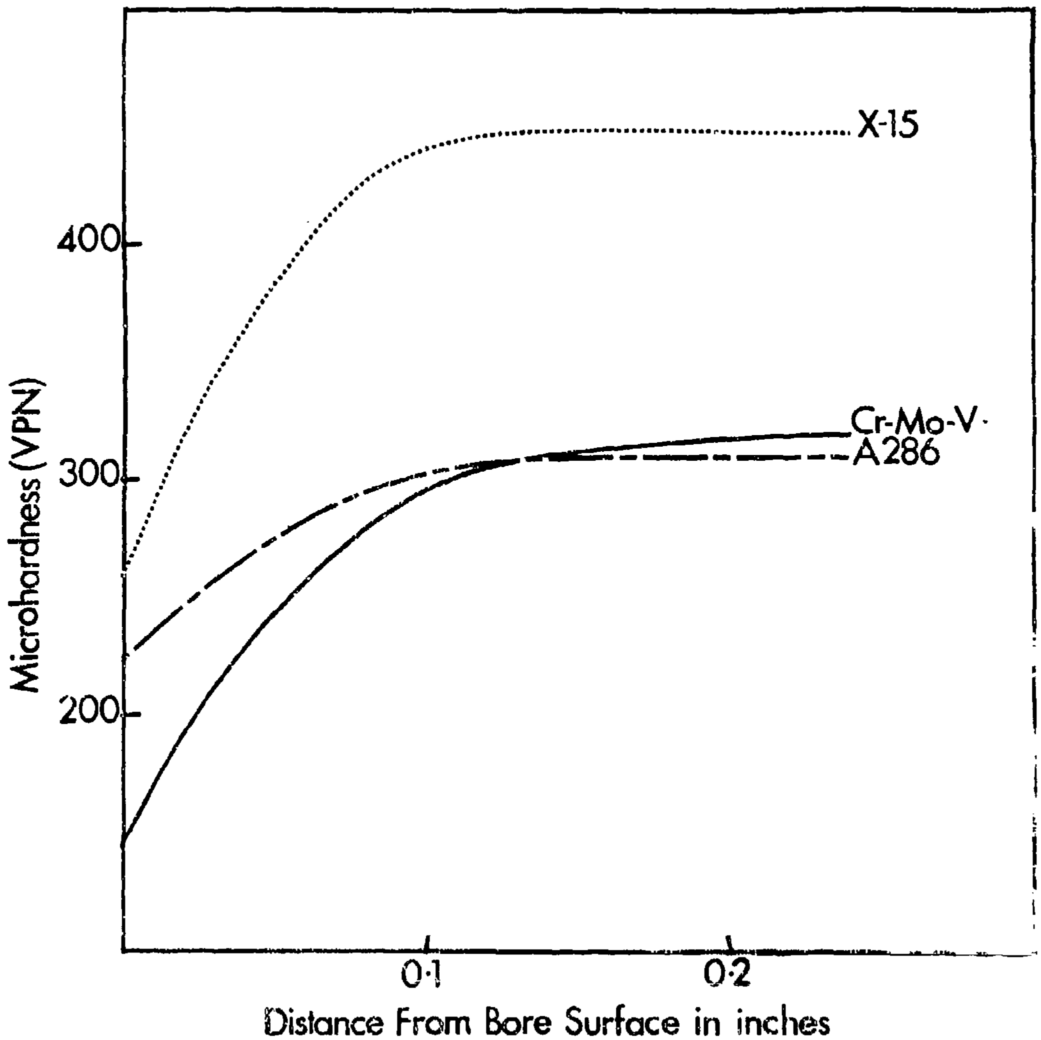

FIGURE 8. RADIAL MICROHARDNESS PROFILE AT 2" FROM ORIGIN OF RIFLING ON SECTIONS FROM TEST-FIRED M60 BARRELS.

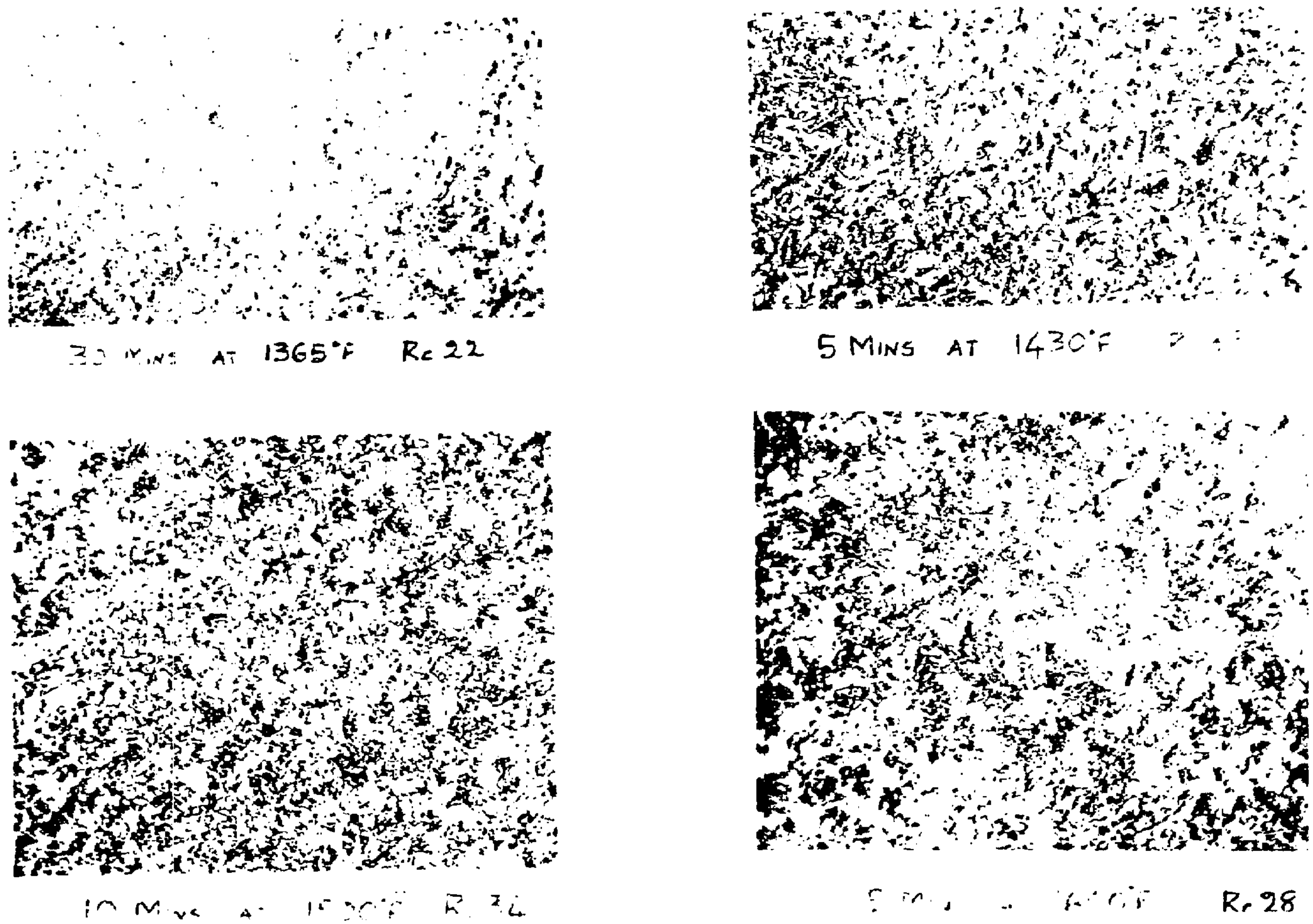

FIGURE 9. MICROSTRUCTURES OF REHEATTREATED CR-MO-V STEEL SPECIMENS.

FIGURE 10. MICROSTRUCTURE OF THE BORE SURFACE REGION OF A TEST-FIRED CR-MO-V STEEL BARREL.

FIGURE 11. MICROSTRUCTURE OF A286 BARREL SECTION NEAR BORE SURFACE. SCHEDULE I. 4000 ROUNDS.

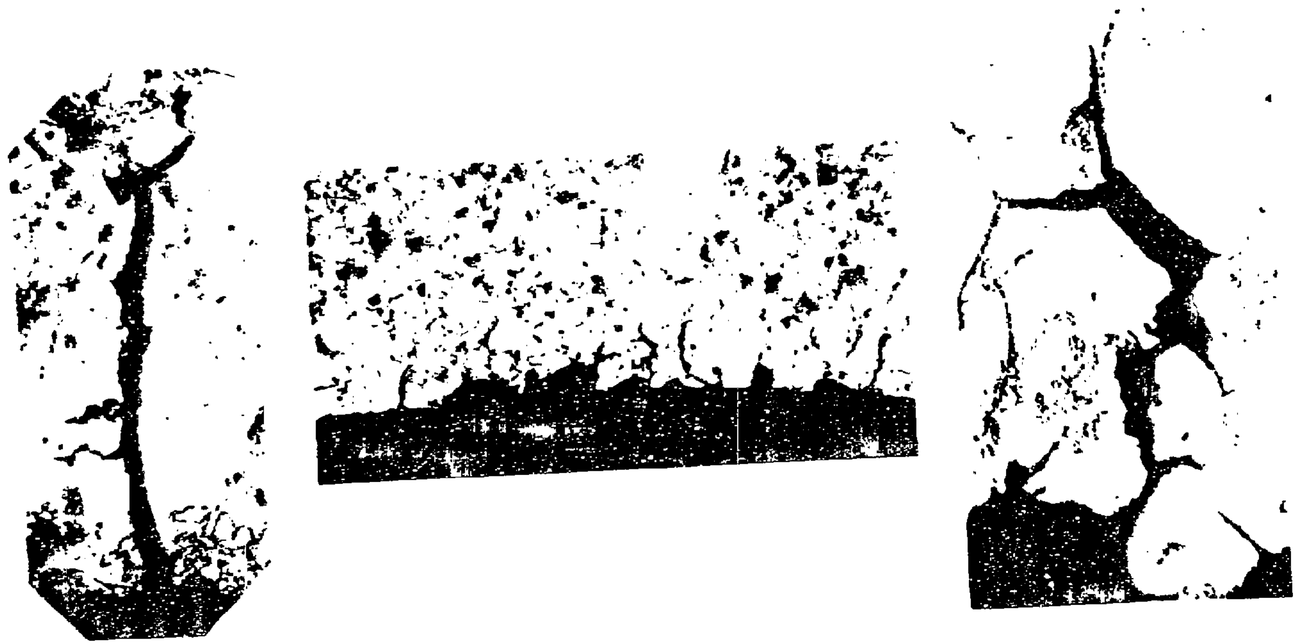

FIGURE 12. MICROGRAPHS OF BORE SURFACE OF A TEST-FIRED ARMCO 22-4-9 ALLOY BARREL. SCHEDULE I. 4200 ROUNDS.

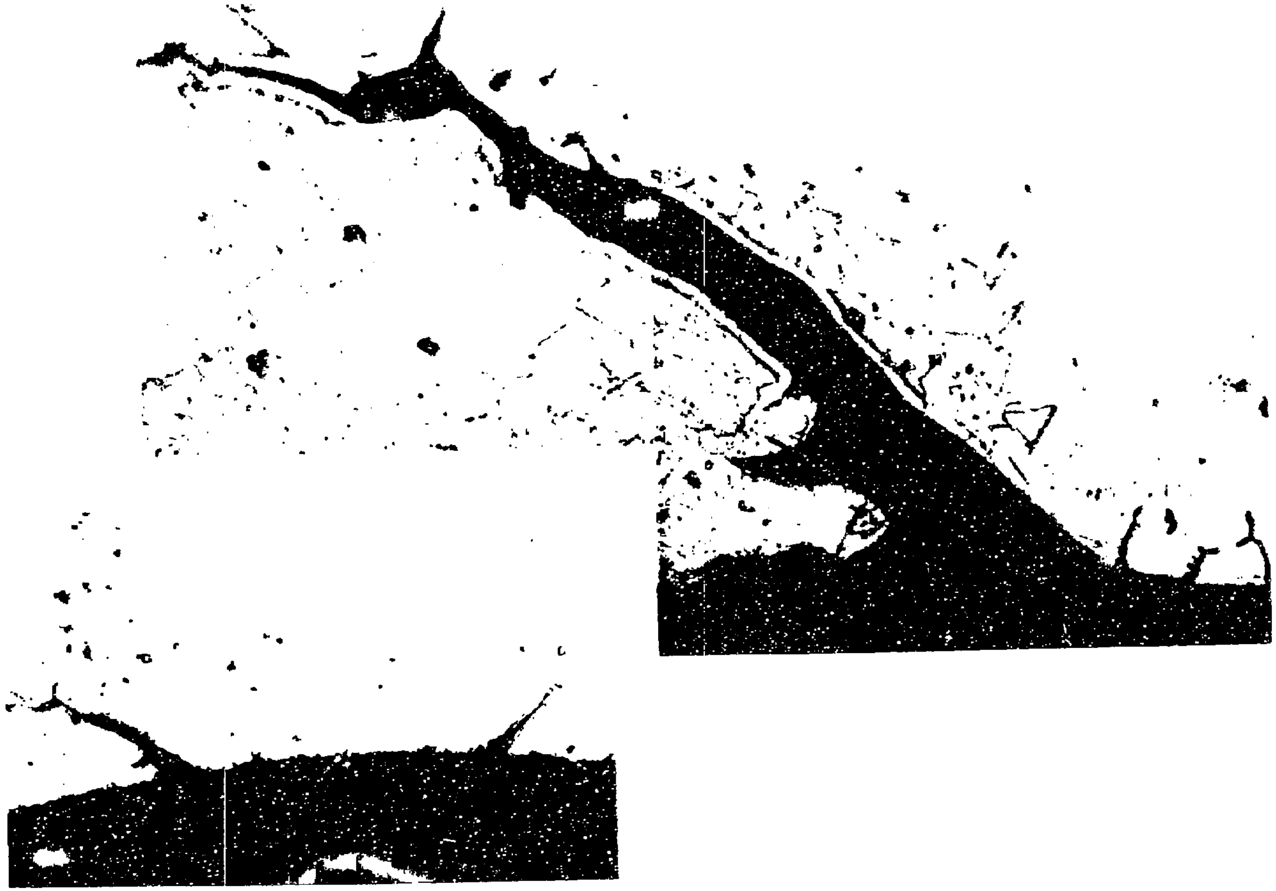

FIGURE 13. MICROSTRUCTURE OF A286 BARREL SECTION NEAR BORE SURFACE. SCHEDULE II. 8000 ROUNDS.

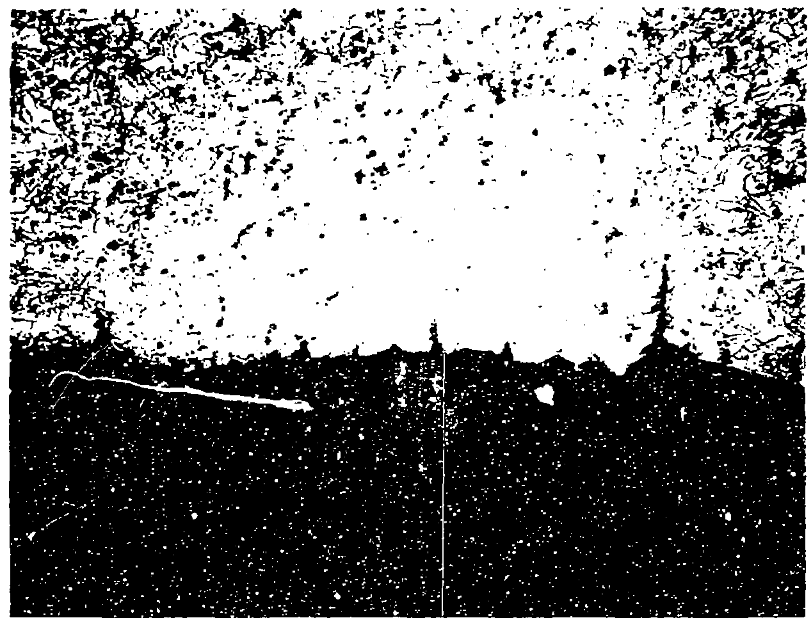

FIGURE 14. MICROSTRUCTURE OF INCONEL 718 BARREL SECTION NEAR BORE SURFACE. SCHEDULE I. 9000 ROUNDS.

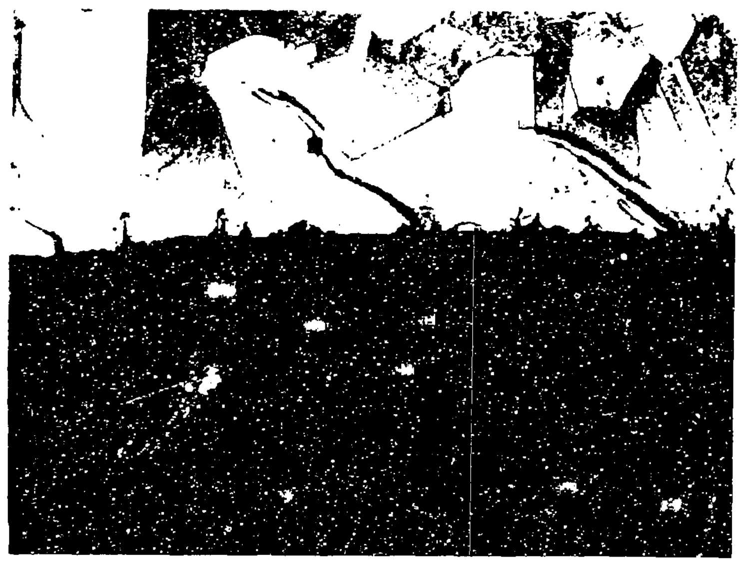

FIGURE 15. MICROSTRUCTURE OF UDIMET 700 BARREL SECTION NEAR BORE SURFACE. SCHEDULE I 11,200 ROUNDS.

FIGURE 16. 45° FATIGUE CRACK IN UDIMET 700 BARREL AT A HIGHER MAGNIFICATION.

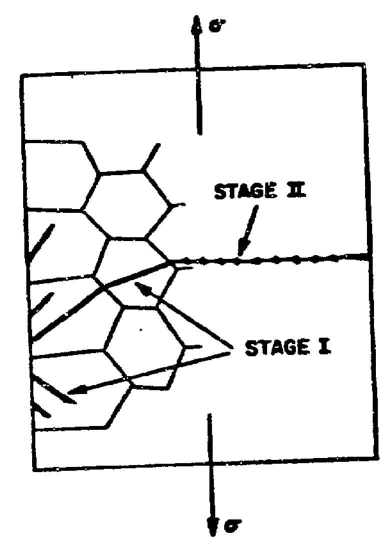

FIGURE 17. SCHEMATIC REPRESENTATION OF FATIGUE CRACK PROPAGATION (AFTER LAIRD).

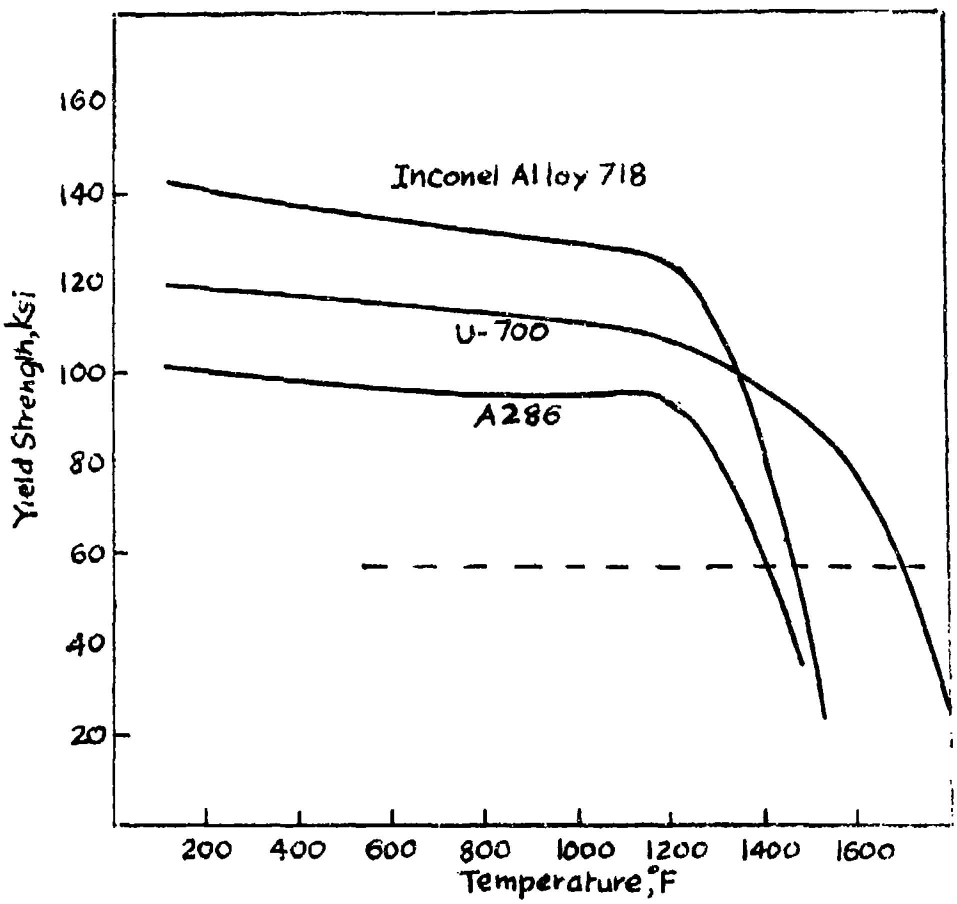

FIGURE 18. YIELD STRENGTH VS TEMPERATURE FOR THREE SUPERALLOYS. THE DASHED LINE IS THE GENERAL HOOP STRESS LEVEL IN THE M60 GUN BARREL.

SECONDARY WEAR CHARACTERISTICS
AND EFFECTS ON BALLISTIC PERFORMANCE
OF THE 105MM M68 GUN

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In the spring of 1974, several incidents of erratic performance of M392A2 APDS (Armor Piercing Discarding Sabot) ammunition were reported. This erratic performance was in the form of grossly excessive target impact dispersion and extreme in-flight projectile yawing. At the direction of the US Army Material Command, an investigation to isolate the causes of this erratic flight performance was undertaken by various ARMCOM activities. This paper describes the study conducted by Benet Weapons Laboratory to evaluate tube wear conditions and their influence on ballistic performance of the APDS projectile in the 105MM M68 Gun.

When the M68 Gun was first fielded, tube wear advanced at the rate of approximately 0.001 inch for each round fired. Figure 1 shows land wear as a function of distance from the rear face of the gun tube for an M68 Gun in early test firing. Note that wear at the origin of rifling after 60 rounds is approximately 0.050 inch. Introduction of wear reducing propellant additives dramatically decreased tube wear rates. Figure 2 shows gun tube wear of 0.050 inch after 802 rounds of additive-equipped M456E1 HEAT (High Explosive Anti-Tank) ammunition. During these test firings, a variation in gun tube wear profile was noted with the evolution of a second land wear maxima This maxima occurred in the region from 4 to 15 inches from the origin of rifling and was, generally, approximately 15 percent lower in amplitude than the wear at the origin. No effect on gun tube accuracy was noted during test firings.

The life of the M68 Gun Tube was established as 0.075 inch diametral land wear at the origin of rifling, this being the accuracy limit for the APDS projectile and corresponding to an accumulative firing history well within the fatigue limits of the gun tube. With the proliferation of propellant additive-equipped ammunition, it has been necessary to address fatigue criticality with the current dual tube condemnation criteria of 0.075 inch wear or 1,000 rounds.

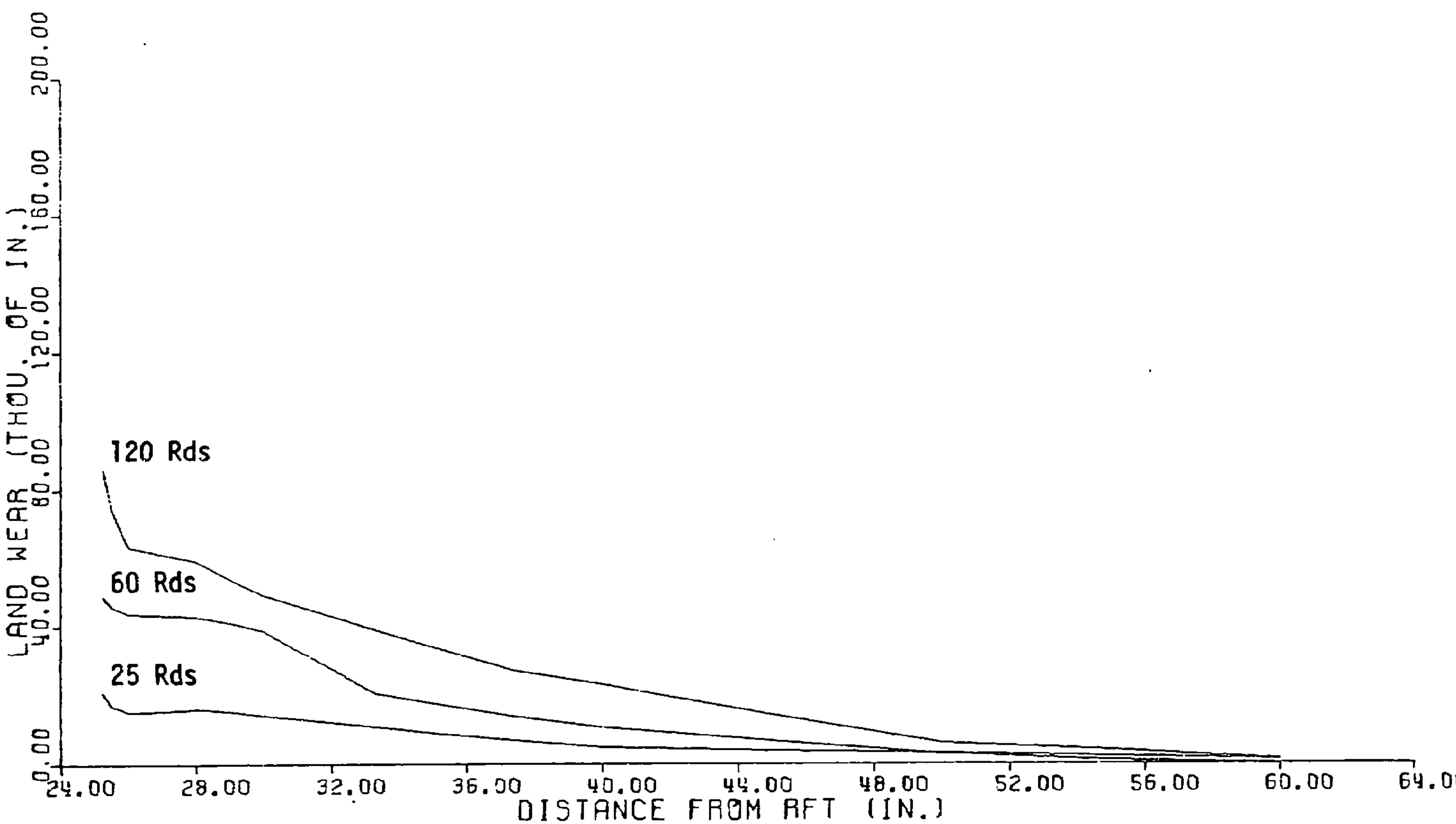


Figure 1. M68 Gun Tube Land Wear with Non-Additive Ammunition

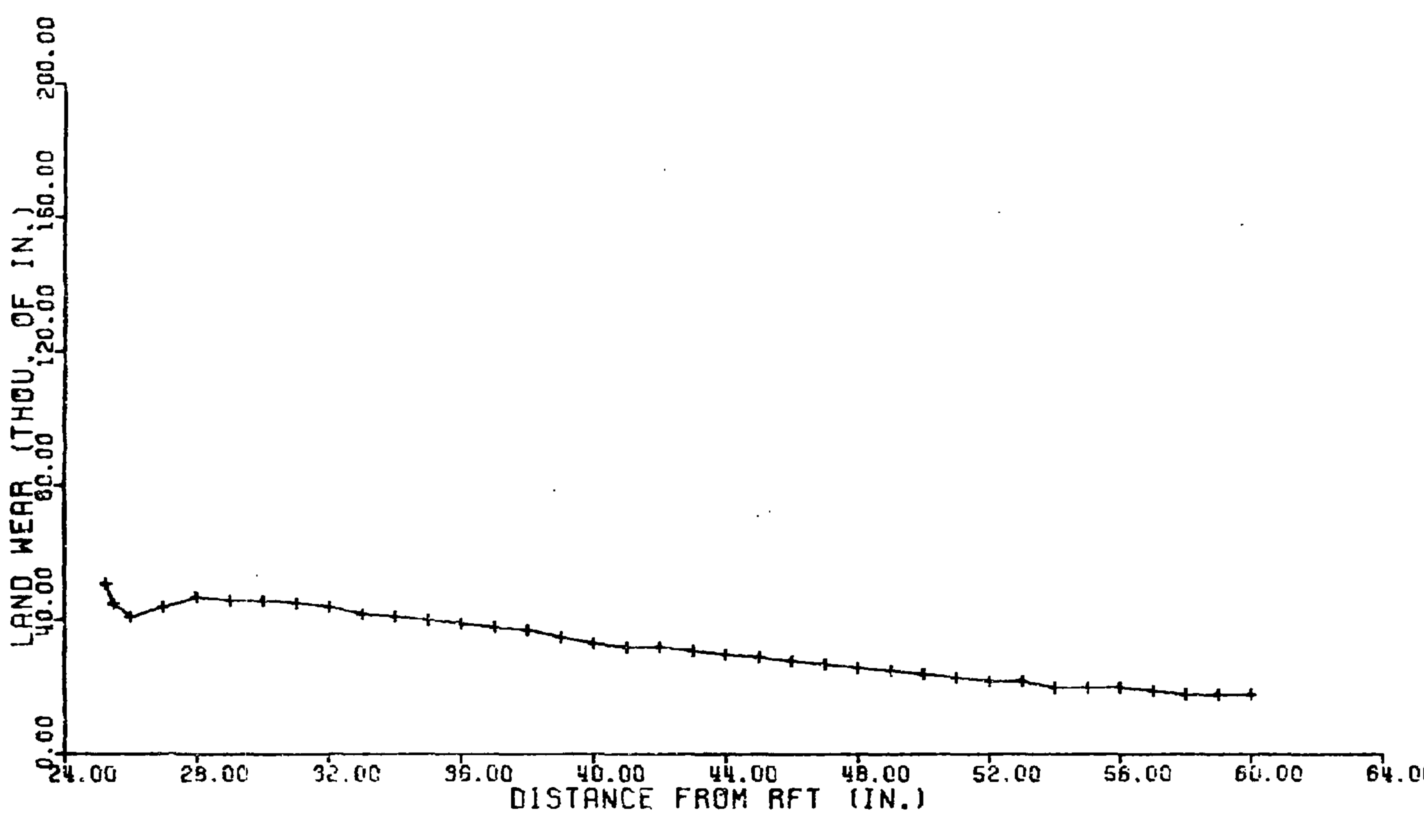


Figure 2. 105MM M68 Gun Tube Wear with Additive Ammunition (circa 1965)

In describing tube wear, the amplitude of land wear at the origin of rifling and the second wear maxima are referred to as primary and secondary land wear, respectively. There is no assigned groove wear criteria; for consistency, groove wear at the origin is referred to as primary groove wear and at the second maxima as secondary groove wear.

With evidence that secondary land wear exceeded primary land wear in several of the tubes reported to have fired APDS ammunition erratically, a field sample was taken to determine the wear condition of gun tubes in the US tank fleet. This sample consisted of over 2% of the total tube population and was taken from tubes in service in the Continental United States (CONUS) and US Army Europe (USAEUR). Conditions where secondary wear exceeded primary wear were found in the majority of gun tubes sampled and in the last quarter of wear life, this condition was predominant. Models of tube wear were developed from the survey data. Figures 3 and 4 show mean and 90% tolerance (90% confidence) intervals for land and groove wear in the fourth quarter of tube life.

It is important to note that high secondary wear conditions result from the erosive characteristics of HEAT-type ammunition and are dependent on the combinations of ammunition fired and rates of fire. Once high levels of secondary wear have been initiated, they are compounded by any type of ammunition. The APDS round is sensitive to bore wear conditions created by HEAT ammunition. Figure 5 shows advanced secondary wear in a fourth wear quarter M68 Gun tube.

The tube wear models developed from the field survey data were used to specify one gun tube typical of each wear quarter. Test firings showed a 10% erratic flight rate with the fourth wear quarter tube and no erratic flights in the remaining tubes. It was concluded that typical fourth quarter secondary wear conditions influence APDS flight performance. Concurrent testing, conducted by the Army Material Systems Analysis Activity (AMSAA), which eventually assessed the entire stockpile of M392A2 ammunition, indicated that inherent ammunition anomalies resulted in erratic projectile flights independent of tube wear conditions.

With stockpile surveillance testing in progress, personnel from Picatinny Arsenal pursued testing to identify and correct conditions in the APDS projectile which caused erratic flights. Benet Weapons Laboratory conducted a series of tests to evaluate wear resistant plating and to isolate tube wear conditions which induced erratic projectile flights and develop criteria for removing tubes with adverse wear conditions from field service.

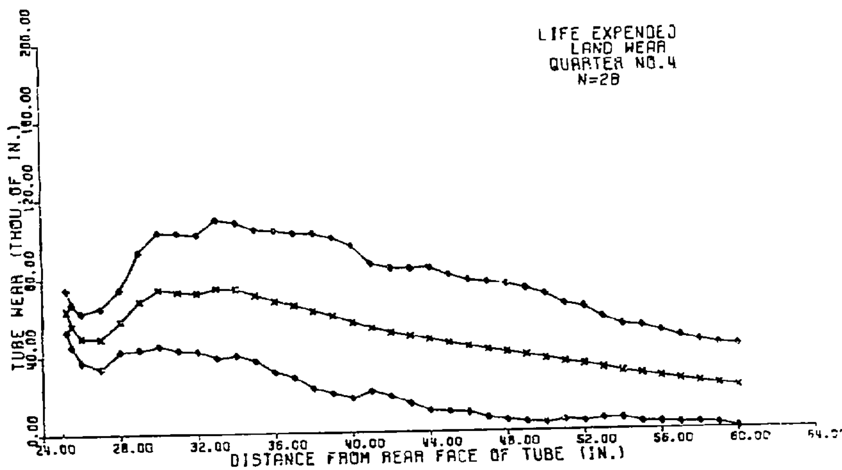


Figure 3. M68 Gun Tube: Mean Land Wear with 90/90 Tolerance Limits - Fourth Wear Quarter

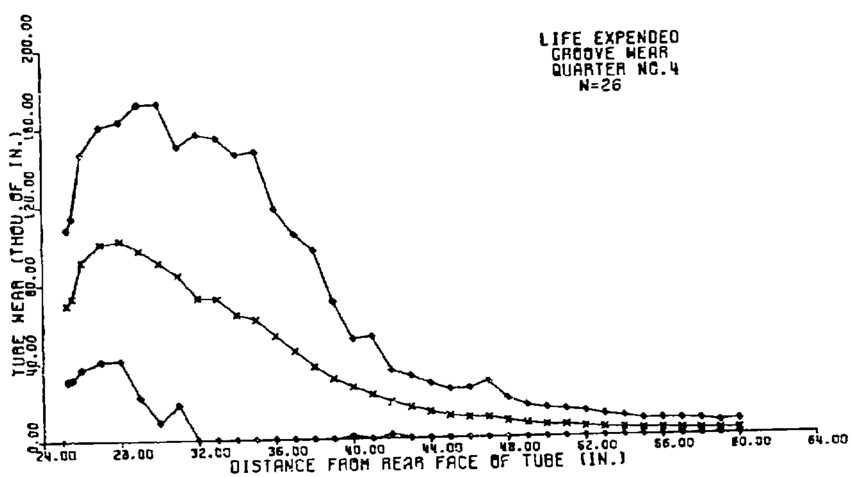


Figure 4. M68 Gun Tube: Mean Groove Wear with 90/90 Tolerance Limits - Fourth Wear Quarter

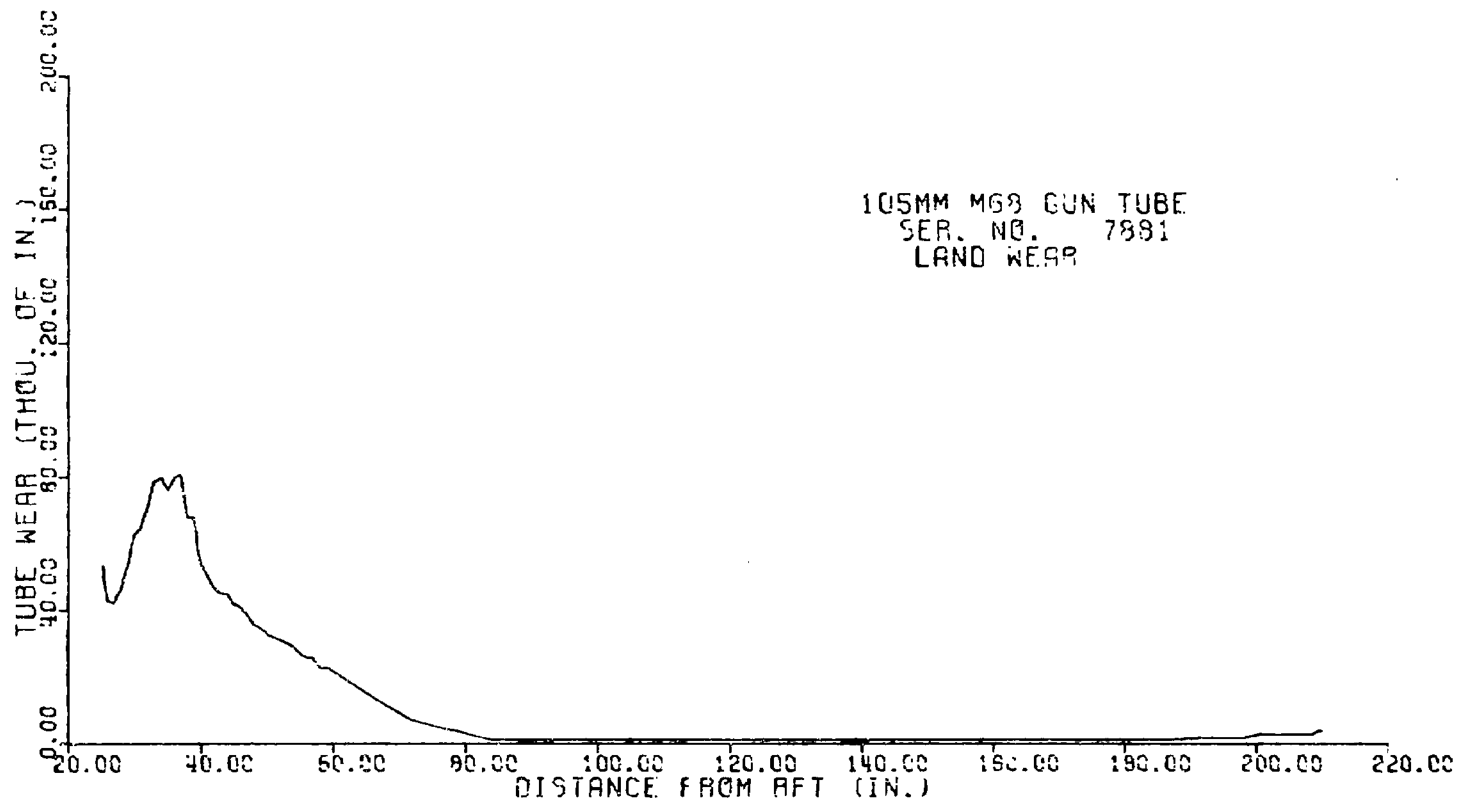


Figure 5. Advanced Secondary Land Wear

The wear resistant plating evaluation consisted of a test to compare the performance of full and partial bore length chrome plated tubes with a standard production tube. While a 0.010 inch thick layer of chrome plate had been shown to confine primary and secondary tube wear to very low levels, the possibility of degraded accuracy performance with APDS ammunition had not been addressed. In the comparison test,a tendency towards greater dispersion and higher muzzle velocities with both chrome plated tubes was noted. It was decided that bore wear coatings should not be considered until the interaction between platings and the APDS projectile was established in an exploratory development program.

Gun tube wear testing consisted of a designed factorial screening experiment, examining wear conditions in fourth quarter tubes, and verification testing. In the screening experiment, four wear characteristics (primary and secondary land and groove wear) were examined. These characteristics were treated as qualitative (high/low) variables and fourth wear quarter gun tubes representing the sixteen possible combinations of these variables were selected from field service. Two ten round replicates were fired using APDS ammunition which had shown sensitivity to fourth quarter tube wear in AMSAA testing and was considered typical of serviceable ammunition in the US stockpile. Figure 6 shows the outcome of the two test replicates as erratic flights in each ten round group for each of the tubes tested.

Regression analysis of the results of the screening experiment indicated that primary and secondary land wear and secondary groove wear influenced projectile flight stability. Primary groove wear and interactive effects were found to have no significant influence on flight stability. Pairwise comparison of the three parameters under consideration indicated that they influenced the outcome equally. With this information at hand, and drawing from other available firing data, primary and secondary land wear of 0.072 and 0.100 inch, respectively, and secondary groove wear of 0.120 inch were hypothesized as wear limits beyond which a gun tube would induce erratic projectile flight.

Two subsequent test firing phases were conducted to verify the results of the screening experiment and to evaluate the influence of a projectile modification which had been found to reduce the incidence of erratic flights in ammunition testing. The projectile modification consists of a press-fitted aluminum plug installed in the sabot base as shown in Figure 7.

In the first of these two phases, US production ammunition which had demonstrated a low failure rate in AMSAA testing and UK produced L36 APDS ammunition were fired. The L36 is essentially identical to the M392A2 with the exception that it is loaded without propellant

				GROOVE WEAR			
				PRIMARY			
				LOW		HIGH	
				SECONDARY		SECONDARY	
				LOW	HIGH	LOW	HIGH
LAND WEAR	PRIMARY	LOW	SECONDARY LOW	1/1	0/0	1/2	3/1
			SECONDARY HIGH	1/1	9/1	4/3	10/10
		HIGH	SECONDARY LOW	4/2	8/4	3/2	2/5
			SECONDARY HIGH	6/5	5/6	4/2	10/10

Figure 6. Erratic APDS Projectile Flights in Two 10 Round Replicates of Factorial Screening Experiment

APDS-T PROJECTILE

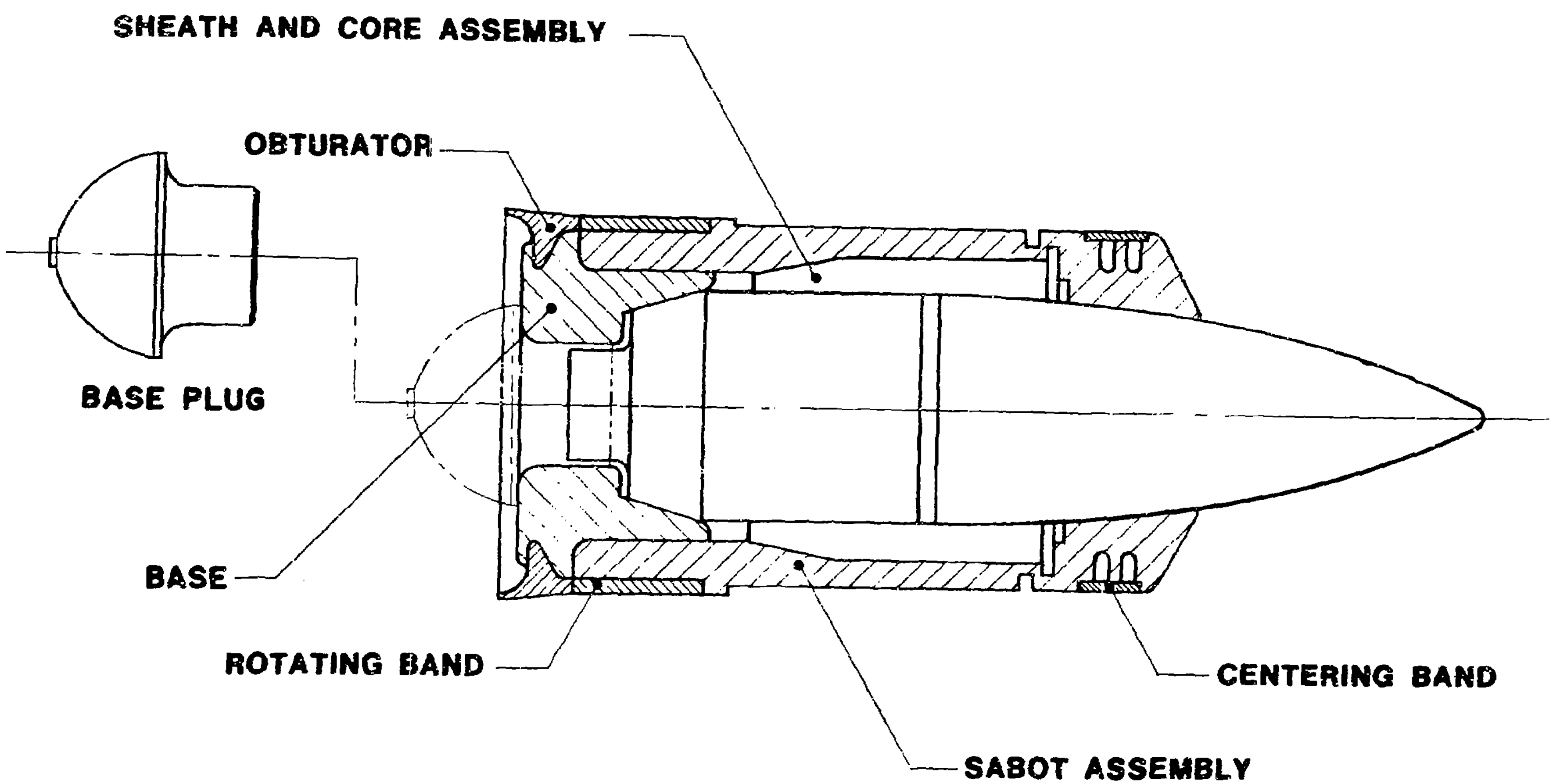


Figure 7. Sectional View of APDS Projectile Showing Base Plug Modification

wear additive. The L36 has shown a significantly lower incidence of erratic flight when fired in tubes with wear conditions which induce erratic flight of the M392A2 projectile. The following table summarizes the outcome of this phase:

TUBE WEAR	AMMUNITION FAILURE RATES			
	US	US + PLUG	UK	UK + PLUG
69/95/113	0.10	0.20	0.33	0.80
71/107/155	0	0	0	0.50
60/77/178	.30	0	0.1	0.40

The tube wear shown for each of the three tubes represents primary land/secondary land/secondary groove wear in thousandths inch. Smear camera photographs showed fragmented sabots in fifteen of the twenty-six rounds of plugged UK ammunition. This test phase was the first time plugged UK ammunition had been fired.

The high incidence of sabot failure in UK ammunition was examined in the second verification phase by firing the tube from the first phase in which all plugged UK rounds had failed with unplugged UK ammunition and a second less severely worn tube with plugged UK ammunition. The following table summarizes results of this firing:

TUBE WEAR	UK AMMO	FAILURES
76/89/214	w/o Plug	10%
68/68/87	w/Plug	0

These results are taken to indicate that the base plug induces sabot failure of UK (L36) ammunition when fired in tubes with high secondary wear levels.

To relate secondary wear conditions to characteristics which can be determined by field measurement using pullover gages, an expanded sample consisting of wear measurements from 231 third and fourth quarter gun tubes was used to project secondary wear levels as functions of primary land wear. Figures 8 and 9 show data and maximum likelihood estimates for secondary land and groove wear. Included on these figures are the 0.100 inch secondary land wear and 0.120 inch secondary groove wear constraints discussed previously.

It is apparent that reductions in allowed levels of primary land wear to exclude undersirable levels of secondary wear must be considered under several conditions. Total elimination of undesirable wear conditions, using primary land wear as the only criteria, would require elimination of the third and fourth quarters of tube wear life and a substantial portion of the second wear quarter. Any less extreme reduction in allowed primary land wear below the 0.072 inch

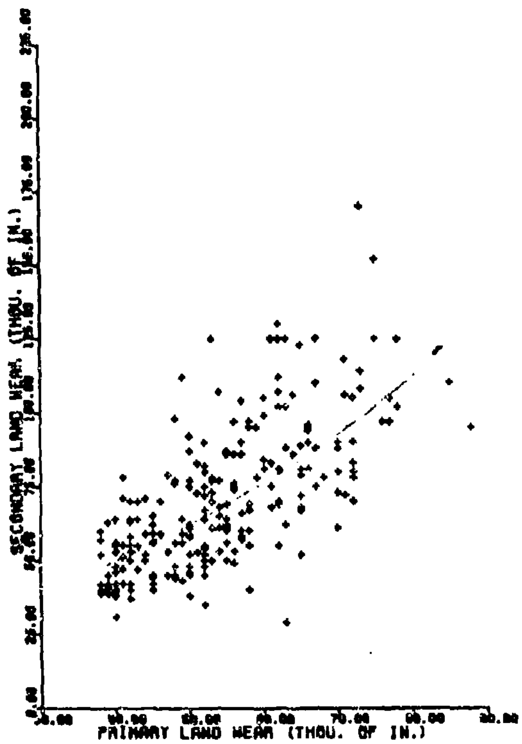


Figure 8. Secondary Land Wear vs Primary Land Wear--Third and Fourth Wear Quarters

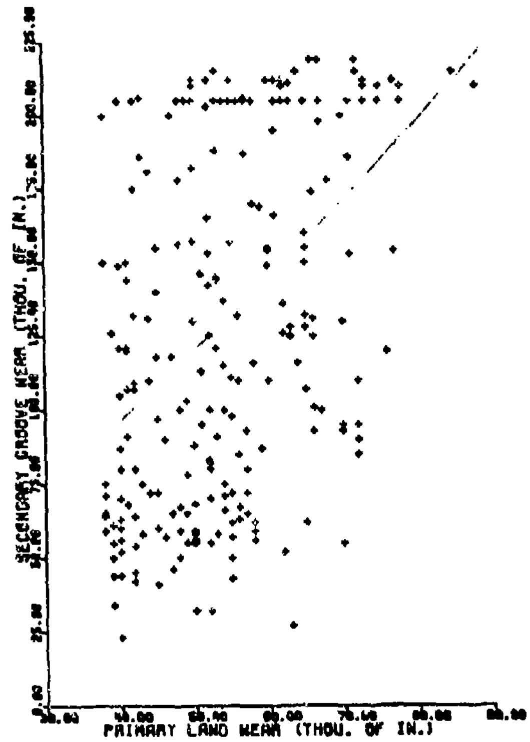


Figure 9. Secondary Groove Wear vs Primary Groove Wear--Third and Fourth Wear Quarters

constraint, will clearly condemn tubes with wear conditions within the secondary wear constraints and retain tubes with wear in excess of these constraints.

The distribution of primary land wear measurements was estimated from a data base which includes the service history of each M68 Gun tube fielded since type classification of the weapon. A continuous record of firings, and other life cycle information related to cannon tubes, is maintained by using units on DA Form 2408-4, the Weapon Record Data card. This form is submitted semiannually by active units and is retained by the Product Assurance Directorate at Watervliet Arsenal. In 1974, a program was initiated by Benet Weapons Laboratory to develop a computerized data retrieval system for 105mm M68 Gun tube firing records. These records are reviewed, information is converted to a coded format, and data is sorted and stored on magnetic tape for computer anaysis and retrieval. This data base provides a continuous, complete record of the service history of each gun tube, limited only by the accuracy of the firing records as submitted. Currently, the data includes nearly 500,000 line entries. Work is now in progress to refine the existing data base, include additional information as new records are received, prepare supplemental information from manufacturing records, and develop software for use in a wide variety of analyses.

To estimate the wear life distribution of the fielded gun tube population, an auxillary data file, ordered on reported vehicle (tank) serial numbers, was examined. The vehicle serial number is usually reported with the tube serial number on the weapon record data card. Vehicle serial numbers were chosen as the basis for estimation to avoid introducing certain procedural errors during the data development. Figure 10 shows the cumulative wear life distribution of 3,183 tubes, nearly one-half of the total population. Note that approximately 83% of these tubes are below the midpoint of tube wear life (0.0375 inch).

Using the estimates of secondary land and groove wear and the primary land wear distribution, estimates of combinations of primary and secondary land wear and secondary groove wear can be developed. For a level of primary land wear (L_1) between 0 and 0.075 inch, a new condemnation point (t) is selected. The question is how many tubes with secondary wear beyond the assigned limits (0.100 inch land and 0.120 inch groove) will be condemnation at new condemnation point (t) and how many tubes with wear within the limits will be retained? If P_{KG} represents the probability of keeping an acceptable ("good") tube, P_{CB} represents the probability of condemning an unacceptable ("bad")

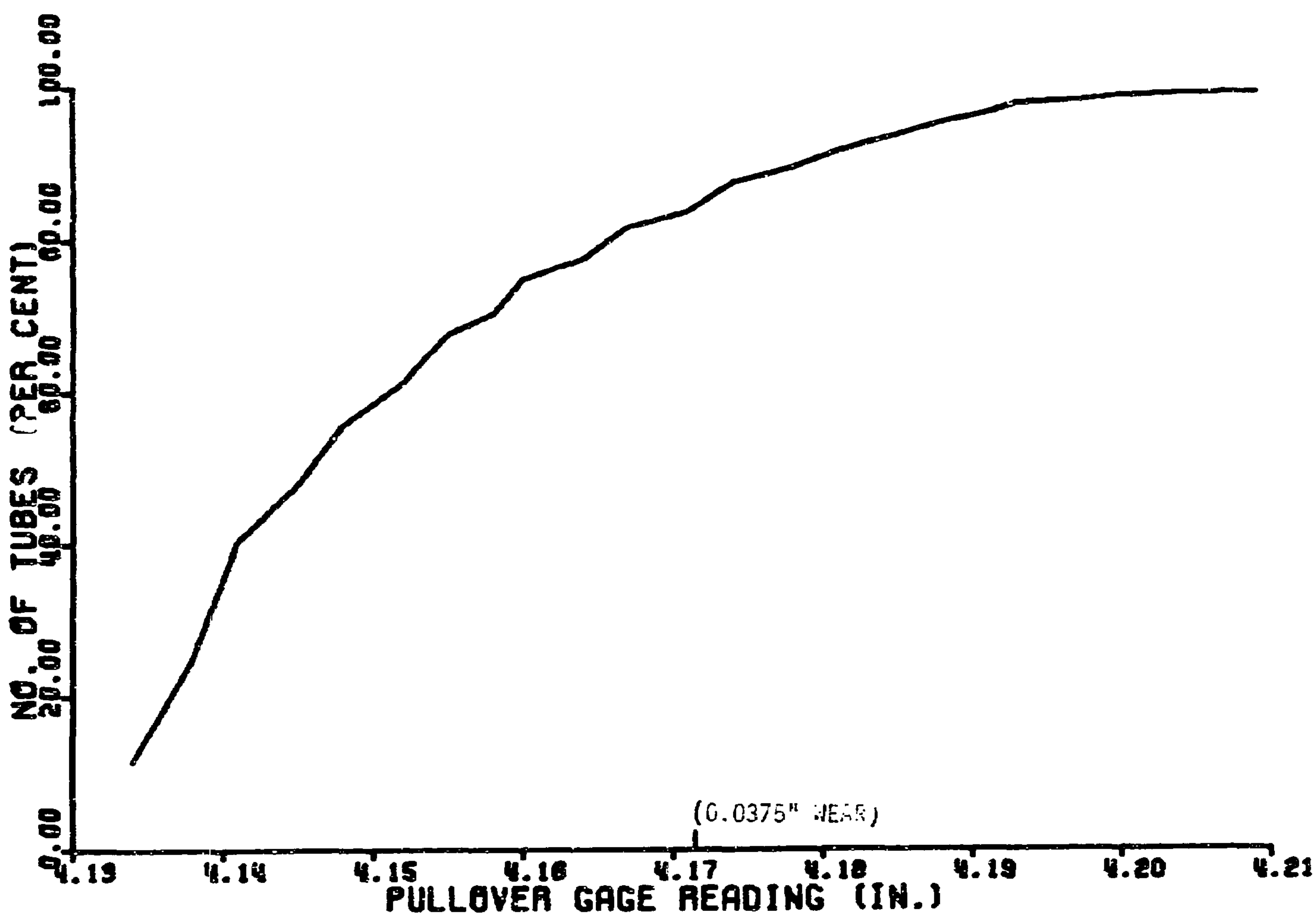


Figure 10. Cumulative Wear Life Distribution of 105mm M68 Gun Tube Population

tube, L_2 represents secondary land wear, and G_2 represents secondary groove wear, the conditions to be considered are:

$$P_{KG}(t) = Pr\,[L_1 < t,\ L_2 < 0.100,\ G_2 < 0.120]$$

$$P_{CB}(t) = Pr\,[L_1 \geq t,\ (L_2 \geq 0.100 \cup G_2 \geq 0.120)]$$

The estimates of L1, L2, and G2 described above were used to generate estimates of P_{KB} and P_{CG} for values of t between 0 and 0.075 inch (corresponding to a range of 0 to 100% of the current tube wear life). These estimates were computed using Monte Carlo simulation on 10,000 samples. Selected numerical results are shown in the following table:

WEAR LIFE REDUCTION (%)	TUBES RETAINED (%)		TUBES CONDEMNED (%)	
	GOOD	BAD	GOOD	BAD
20	92	6	1	1
25	91	5	2	2
30	90	4	3	3
35	88	3	4	5
40	87	2	6	5
45	86	2	7	5
50	82	1	11	6

Based on these results, a revised tube life criteria, consisting of wear condemnation at 0.056 inch primary land wear for combat service, 0.075 inch (with a warning of the possiblility of erratic discarding sabot ammunition performance) for training, and an Equivalent Full Charge (EFC) limit of 1,000 rounds, has been formulated. Borescope condemning criteria, to condemn tubes with excessive secondary wear and primary land wear below the 0.056 inch limit, has been prepared.

To this point, the existing population of gun tubes has been discussed. In addressing the long term influence of a tube wear life reduction, the fatigue life of the gun tube and anticipated charges in the types and combinations of ammunition fired in the tube warrant consideration. The recently fielded M724A1 TPDS (Training Projectile Discarding Sabot), which will be used extensively in training, has shown a very low wear rate in early testing. This wear rate is low enough that tubes firing this round, and a reduced proportion of HEAT-type ammunition, will reach their fatigue limit within the reduced wear limit and exhibit less extreme secondary wear conditions. The following table shows estimated ammunition consumption ratios:

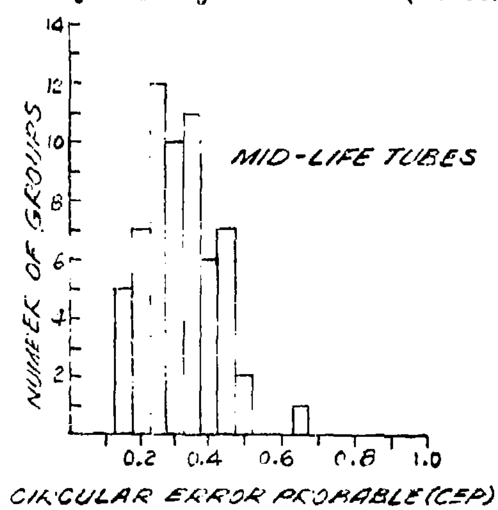
	HEAT	HEP & WP	APDS & TPDS
Before 1975	73%	26%	<1%
1975	75	19	6
Future	40	24	36

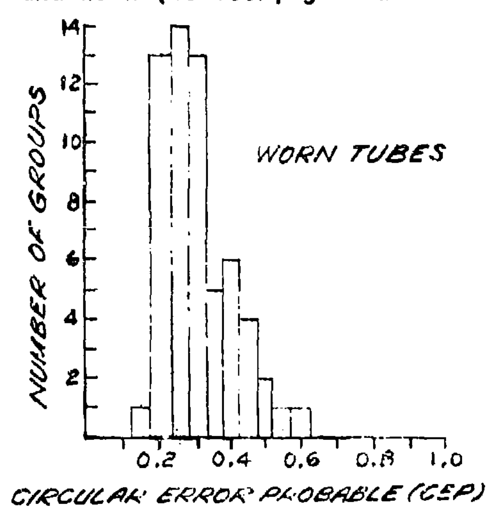
Tube wear for the future training mix of ammunition is estimated as:

409 Rounds TPDS	0.005" Wear
454 Rounds HEAT-TP	.034"
274 Rounds HEP-TP	.002"
1,137 Rounds Total	0.041"
(1,000 EFC Rounds)	

As mentioned earlier, it has been found that once high levels of secondary wear have been initiated the condition is compounded by any type ammunition. While a single test in which equal quantiti of M724A1 and HEAT-TP ammunition produced unexpectedly low levels o secondary wear, the influence of the M724A1 will be fully realized only after the current population of gun tubes has been displaced by tubes with a firing history including a lower proportion of HEAT-type ammunition.

There is no evidence that, given normal projectile flight, secondary wear conditions influence target impact dispersion. Good lots of ammunition have shown consistent dispersion characteristics in typically worn gun tubes through 0.056 inch primary land wear. The following histograms show the results of AMSAA stockpile reliability testing in mid-life (35-60%) and worn (65-100%) gun tubes:





Miss rates for bad lots sampled and lots exhibiting satisfactory (non-erratic) flight characteristics are shown in Figure 11.

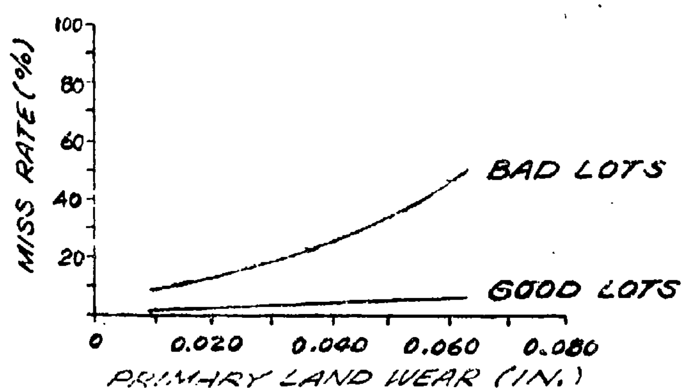


FIGURE 11. MISS RATE OF GOOD AND BAD AMMUNITION VS WEAR IN AMSAA TESTING

This information indicates that, while the rate of erratic flight in anomalous M392A2 ammunition will increase with increasing levels of tube wear, tube wear will have little influence on the dispersion characteristics of normally functioning ammunition.

In summary:

-High secondary bore wear conditions are prevalent in the field population of 105mm M68 Gun tubes.

-Secondary wear conditions are the result of the erosive characteristics of HEAT-type ammunition and influence the flight behavior of APDS ammunition.

-While a deterrent to tube wear, the interaction of chrome plating with the APDS projectile may influence flight behavior and should be investigated before production plating of M68 Gun tubes is considered.

-A tube wear life reduction, in conjunction with additional borescope condemnation criteria, will improve the combat effectiveness of the US tank fleet.

-Proportionally increased use of M724A1 TPDS ammunition will alleviate high secondary wear conditions.

-Dispersion characteristics of APDS ammunition do not appear to be influenced by secondary wear conditions.

-With ammunition stockpile renovation and reduced tube condemning limits for combat service, the erratic APDS flight problem is under control.

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ANNULAR GROOVE VENT EROSION IN 81MM MORTAR TUBES

(Evaluation of erosion resistant coatings using a newly designed erosion tester)

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HISTORICAL BACKGROUND

A. Erosion in General

The literature has shown that studies on gun bore erosion date as far back as the 19th century. Through the years advances in modern tecnnology have brought forth new materials of high performance for the development of improved military systems. However, in spite of these notable achievements - erosion is still existent in modern day weaponry. While the surface characteristics of bore damage vary considerably from one system to another, the net result from the occurrence of bore erosion is a loss of service life in all weapon systems. Unfortunately, erosion problems are not always anticipated during the early development of new weapons. And it seems that when researchers do tend to alleviate the problem of erosion in one weapon system, a new system comes into existence with another erosion problem. Although quite remote from the general problem of gas wash or scoring type of erosion in rifled gun systems, the present investigation is another story on an experimental effort to combat erosion of bores due to design changes in a weapon system.

B. Annular Groove Erosion in 81MM Mortar Tubes

Until recent years, erosion of cylinder bores had only been encountered in gun systems and recoilless rifles. Erosion of bores in Mortar systems would not normally be expected in view of the relatively low chamber pressures and low projectile velocities associated with the weapon. However, due to changes in the design of the tail fin assembly in the 81MM M29A1 Mortar System, excessive damage to the tube bore surface has been found during its early firing life which has rendered the tube unserviceable after approximately 7000 rds as compared to the normal service life of 20,000 rds before condemnation.*

*As a result of the annular groove erosion, a maximum wear limit of .020" on the diameter of the bore has been established before condemnation of the Mortar Tube.

C. Nature and General Cause of Erosion

The surface damage due to erosion occurs in the form of 3 annular grooves presumably due to the jet impingement of hot gases and associated burning propellant particles flowing from the vent holes of the tail fin assembly of the mortar round. The major change in the system which created the erosion problem was that the size of the vent holes decreased from .196 inch to .125 inch diameter which obviously increased the velocity of the eroding gaseous atmosphere at the point of contact on the bore surface.

Figure 1 shows a mortar tube with a section cut to reveal the 3 annular grooves of erosion. Figure 2 shows an illustration of the 8 rows of 3 jets of igniting gases impinging on the bore surface as one looks through the bore. Figure 3 attempts to illustrate the flow pattern of the eroding gaseous atmosphere emanating from the 24 holes of the tail fin assembly (apart from its projectile) upon a sectional view of the bore surface.

From a single round of firing a very slight degree of damage is caused by the jet of hot gases and propellant particles streaming from each of the 1/8" holes of the tail fin assembly. The bore damage caused from a single jet of hot gas only covers a circular area of approximately 1/2 inch diameter. However, with the firing of several thousand of rounds which are loaded in the mortar tube in a random position (circumferentially) the resulting accumulated surface damage is formed in a pattern of 3 circumferential annular grooves spaced opposite to the 3 rows of holes on the tail fin assembly.

The 81MM Mortar System is designed to fire rounds using various increments of propellant (i.e. from 0 to 9). Increments from 1-9 require a corresponding number of propellant bags which are physically placed around the tail fin assembly (inset in Figure 1). The chamber pressures increase with increasing increment (i.e. 900 psi with increment 0 and 8400 psi with increment 9). Along with burning time and flame temperature, the amount of bore erosion in rifled gun systems is also increased with increasing chamber pressures. Suprisingly, however, in the case of the 81MM Mortar System damage to the bore surface is most severe when a "0" increment round is fired (i.e. low pressure) because no bags are placed around the tail fin assembly to obstruct the jet stream of gases from directly impinging on the bore surface. The low pressure referred to, of course, is misleading since its the peak chamber pressure. The pressure value of importance is that which is pinpointed on the bore surface from the gas jets.

D. Erosion Characteristics in the 81MM M29A1 Mortar System

A severely eroded 81MM Mortar Tube (which was fired in the field) was metallographically studied in an attempt to determine the significant factors leading to the annular groove erosion which form the 3 rings. Figure 4a shows a sectional surface view of the 3 annular grooves. Figure 4b shows the longitudinal cross-sectional profile of the forward eroded groove. While visual examination of the eroded surface shows a severe pitted condition which appears to be caused by purely mechanical forces, microscopic cross-sectional examination (see Figures 4c and 4d) has revealed patches of the typical white layer (presumably due to thermal cycling) normally found in large caliber rifled gun tubes.

OBJECTIVE

The primary purpose of this investigation was to search for a surface coating which could be economically applied to the mortar bore which would resist such erosion and increase the service life up to its original performance. In order to achieve such an objective, a number of coatings had to be tested and evaluated.

THEORETICAL CONSIDERATIONS IN THE FORMATION OF PITTING EROSION

Unfortunately, a mechanism for such erosion was not easily obtainable, since we were dealing with a jet stream of hot gases and associated propellant particles with various degrees of burning and trajectories at point of impact. Furthermore, an exact analytical solution appeared impossible, even if treated as a simple mechanism of erosion.

However, since it appeared that the particular pitted surface damage encountered in the 81MM Mortar System was primarily formed by abrasive or cutting forces, it was considered appropriate to review the open literature as a guide in approaching a solution to the problem. A review of the literature on gun bore erosion offered no help, even though the grooved erosion was somewhat similar to the erosion sometimes formed at the end of cartridge cases. By the same token, none of the investigations reported in the open literature could be matched with the combined factors existing in the Mortar System, but it was hoped that some of the erosion studies could be of some help to us. As one might expect, the studies reported in the literature on impact erosion varied considerably.

For example, some investigators have studied the impingement of abrasive particles on a metal surface, some looked at the effects of steel or glass spheres striking a surface and some studied the collapse of water droplets which causes cavitation erosion. Still others have looked at striking abrasive particles in a fluid stream, while some have studied the effects of hot turbine gases on a metal surface.

Suprisingly, some of these study areas did reveal some similarities in the behavior and damage characteristics of metal surfaces and warrant some discussion.

Finnie[1-5] et al appeared to be the first to analytically study the manner in which abrasive particles erode the surface of a ductile metal and was quick to point out that a general disadvantage of erosion data in the literature is that particle velocities as distinct from fluid velocities, had not previously been measured. He points out also that ductile surfaces should be given separate attention from brittle surfaces since their erosion behavior is significantly different.

In solving the equations of motion of a single abrasive particle striking a ductile surface, it was shown that the volume Q removed by a given mass of abrasive grains with velocity V and angle α is estimated to be

$$Q = C \frac{MV^2}{4} \frac{1}{p} f(\alpha)$$

where p is the flow stress (relative to the surface). Finnie reported good correlation of wear with experiments showing the greatest wear to be at angles 17°-20° (see Figure 5). However, the equation does not hold very well for angles nearer 90° which is what we are dealing with in our present case. The three possible effects which invalidate the simple theory for ductile surfaces at angle 90° is:

a) Not all particles will strike the surface at the same angle.

b) The analysis considers a smooth surface and after the surface is roughened the particle-surface interaction is changed.

c) The surface becomes cold worked and embrittled and no longer behaves in a predictable manner.

One point of interest in the case of brittle surfaces (Figure 5), is that 90° angle of impingement produces the greater amount of wear. It will be shown later that some of our own results have shown similar behavior.

Engel[6-7] reported that the development of a pit-depth vs velocity equation for collision of rigid spheres on a surface does not hold in every case where cold working of the surface occurs. This has also been pointed out by Moore, et al[8] in their studies of worn metal surfaces using a blunted tool.

As previously mentioned, the above studies cited from the literature involve process conditions which are quite remote from the environmental factors which exist in the Mortar System. To begin with the particles are neither jagged abrasives, metal spheres or water droplets but propellant platelets or discs which are at an indeterminate rate of burning at the point of contact with the surface. Also, the propellant particles are traveling in their associated hot gaseous atmosphere when impinging on the surface. Thirdly, the time cycles involved are in milliseconds and the particle velocities (although unknown) are undoubtedly far greater than any of those cited in the literature. Early attempts to measure the velocity of propellant particles in the erosion gage by high speed photography techniques were unsuccessful.

In spite of these differences pointed out in the Mortar system compared to the particle-surface actions cited in the literature, our results will show that the behavior of Mortar steel surfaces and some of the wear characteristics exhibit surprising similarities.

FIELD TESTING

Early efforts in searching for a suitable coating to protect the bore of the 81MM Mortar Tube from such erosion consisted of electro-depositing various tube bores with candidate coatings which were then scheduled for field testing using primarily "0" increment rounds (i.e. the most damaging rounds). Since electrodeposited chromium was presently being used as the acceptable erosion resistant coating in high velocity production weapon systems, it was naturally selected as a first choice for test in the Mortar System. Other selected coatings were cobalt-alumina (Al_2O_3) and also a proprietary chromium coating called "Armoloy".

These preliminary efforts to apply wear resistant bore coatings and test firing mortar tubes in the field have been extremely costly and time consuming because of the large number of rounds required and difficulties encountered with field firing schedules. In view of the latter it was decided to devise a laboratory method of testing so that a variety of coatings could be evaluated concurrently prior to field testing.

EXPERIMENTAL DETAILS

Erosion gage - An erosion tester or gage was designed and developed which utilizes the actual tail fin assembly inside a chamber holding eight test specimens at the same time to correspond to the 8 rows of holes. (See cut away illustration in Figure 6.) The actual details of construction of the erosion gage will be reported under a separate cover and will not be discussed here. The primary change made in the firing action, is that the projectile has been removed from the tail assembly which is secured to remain stationary during the firing cycle.

The erosion rate of the test material can be increased so that less rounds are required, by merely reducing the distance of the jet stream by moving the specimens closer to the tail fin assembly. Upon firing the ignitor charge, the mixture of hot gases and associated burning particles are radially discharged upon the specimen surface.

Fin Assembly - The M170 fin assembly consists of the M285 ignition cartridge containing M9 propellant and having a charge weight of 108 grains (1 grain = .0648 gram).

Physical Properties of M9 Propellant - The propellant is in the form of platelets measuring .059 in. in diameter and .010 in. thick. The bulk density is 46 lbs/cu-ft.

Calculated Thermochemical Values for M9 Propellants (as per Spec MIL-P-20306)

Isochoric flame temp, °K	3799
Force, ft-lbs/lb x 10^{-3}	.382
Unoxidized carbon, %	.0
Combustibles, %	32.8
Heat of explosion, cal/gm	1295
Gas volume, moles/gm	0.03618
Ratio of specific heats	1.2102
Isobaric flame temp, °K	3139
Covolume, in.^{3}/lb	25.97

Plating Conditions - The bath formulas and plating conditions employed for preparing the surface coatings selected for evaluations are given in Table I and II respectively.

Post Heat Treatment - All coatings with the exception of Co-Fe were thermal treated in an atmosphere furnace for hydrogen relief after plating at 450°F (232°C) for 4 hrs to insure against embrittlement The Co-Fe coatings were thermally treated at 675°F (357°C) for 4 hrs for the purpose of attempting to impart some ductility to the relatively brittle coatings in the as plated condition.

Nitride Surfaces - The 76-hour nitriding cycle for the 81mm Mortar specimens consisted of treatment at 975°F for 15 hours at 24 to 28% dissociation and 1020°F for 61 hours at 80-84% dissociation.

Coating Thickness and Wear Measurements - The deposit thickness of iron group metals for this test were 6-8 mils. In the case of chromium deposits, two thicknesses were tested, which were 0.2 mils and 2.0 mils. The thickness of deposits were measured with micrometers and in some cases later verified by measurements of the coatings using the microscopic camera. Wear measurements of all specimens were taken every 125 rds and conducted with a ball micrometer and coordinate measuring instrument (Sheffield Cordex 300 Coordinate Measuring Machine) which had a 1/8" ball tip. The procedure was to slide the test specimen back and forth across the worn depression of the surface until the tip reached the lowest point which measured the greatest point of wear due to firing.

Weight Measurements - Weight loss determinations due to erosion during firing, were conducted on a Mettler analytical balance.

Micro Hardness Measurements - Were made on cross-sections of the specimens with a Wilson Tukon microhardness tester using a 100 g load and are reported on the Knoop scale. Measurements on the soft steels were taken with a 25g load. Hardness measurements given are those taken before firing unless otherwise stated.

Photomicrographs - Cross sections of specimens were mounted and polished using diamond abrasives. The polished specimens were etched with a solution of 60 parts lactic acid, 30 parts HNO_3 + 5 parts HF, by swabbing 10-15 sec. The photomicrographs were made with a Polaroid camera using a Leitz MM5 Research Metallograph.

Test Specimens - Consisted of both SAE 4340 steel strips (1/8 in. thick) and cut sections of mortar tubes which both measured 3/4 in. wide and 5 in. in length.

Lab. Firing Tests - The firing tests were limited to 1500 rds which produced over 10 mils of wear on the radius of unprotected mortar steel. Mortar tubes in the field are condemned when any portion of the bore shows a wear of 20 mils on the dia, (i.e. 10 mils on the side). However, the 1500 rd test was selected to permit a suitable range of wear values for evaluating the various condidate erosion resistant coatings. Since the erosion tester held 8 specimens during a firing test, one of the specimens was always unplated mortar steel which acted as the control specimen.

RESULTS AND DISCUSSION

In reviewing the literature, one will find that considerable doubt and controversary exists on the validity of data from erosion vent studies when comparing results with actual field behavior of eroded surfaces and it is agreed that some of the doubts have considerable merit. However, as previously mentioned, in this present lab study our erosion gage consists of the actual field system with the absence of the mortar projectile. In spite of two primary changes made in the use of the erosion gage for the purpose of accelerating the erosion process, microscopic examination shows a close similarity in the erosion characteristics produced by the two systems. One of these changes included a decrease in the jet distance from the vent holes to the specimen surface which increases the velocity at point of impact. The second change effectively focuses the surface damage on the same circular location rather than dispersing the surface damage when the rounds are circumferentially loaded at random which results in the formation of annular grooves which have been shown previously.

The data compiled from firing tests using the above described erosion tester is presented as follows:

A. Chamber Pressure

Since the test specimens were placed closer to the tail fin assembly and no serious attempt was made to match the chamber volume of the erosion tester with that of the field Mortar System it was desirable to measure the peak chamber pressure in the tester to determine how close it was to the field system. This pressure was measured at a point away from the jet blast in order to compare the pressure value with that measured in the actual mortar. Suprisingly, the peak pressure value was approximately the same as the 900 psi measured in the field weapon for a "0" increment charge.

B. Measure of Erosion (wt loss vs dimensional change)

While weight loss of a given specimen may be considered a more exact method of determining the extent of erosion in most cases, it has turned out to be completely unsatisfactory in our present study. Weight loss measurements were found to be inadequate because the surface damage not only included material loss but surface displacement. This surface displacement or distortion is associated with the cold working which was encountered. This continual distortion from additional firing, eventually became part of the material loss as fracturing occurred.

Thickness change was a more accurate method of measuring wear. On a macroscopic scale, the regression of surface pits eventually leads to a profile which is somewhat dished with new pits reforming as firing continues. The deepest point that the 1/8" dia. ball tip (of the coordinate measuring instrument) rested at, represented the most meaningful wear value. Figure 7 shows the ball tip bridging across the series of pits which range in size from approximately 1-5 mils in depth.

C. Comparison of Field Mortar with Erosion Gage

The erosion of a mortar section which has been fired approximately 7500 field rds using "0" increment charges has been compared with that of an average mortar steel test piece fired 1500 rds in the erosion gage. The results are shown in Table III, whereby the field data on the left side of the table is compared with lab data on the right. The results show that the erosion gage increased the erosion rate on mortar steel by a factor of approximately 4.6 to 1 simply by decreasing the jet distance by 4/10 inches and loading the rounds in a fixed circumferential position so that the gas jets line up on the same spot of each specimen. However, if we compare the distance of the top and bottom jets of either system, the difference is only .140 in. (rather than 0.40 in.) and the erosion rate is still a factor of about 4.5 to 1. In view of this observation, it was decided to combine the data and plot wear rate vs jet distance which is shown in Figure 8. The bottom or rear vent distance of the erosion gage is 0.260 in. closer than the top vent of the field mortar system in addition to the fixed circumferential loading of the rounds - but still the wear rate is the same for the 2 systems (connected by the dashed line). This indicates that length of bore travel through the vent hole is more effective than the jet distance in increasing the impact velocity and subsequent erosion rate. However, the longer bore length is still considered relatively short to significantly increase the particle velocity. Another possibility is that more solid particles pass through the forward vents compared to the rear vents due to incomplete ignition which is discussed below. Dealing with ingition systems is more complicated than dealing with propellants in chamber systems. However, based on the observation in Figure 8, and the collection of additional data, it appears that some empirical equation can be developed to calculate erosion in the present system.

D. Pit Formation

It was previously stated that erosion of mortars was due to the impingement of hot gases and associated propellant particles. Observations in our laboratory indicated that the mechanical force contribution to the surface damage was due to unburned as well as partially burned propellant particles striking the surface. After certain intervals of firing, some of these unburned propellant discs were found embedded in the pitted surfaces of the specimens. Some other unburned discs were scattered about the surrounding area of the erosion gage set-up. The surface damage due to solid particles is also confirmed by the grain refinement and subsequent microhardness increase observed along the periphery of the pits during metallographic examination (see Figure 9). It is obvious that severe plastic deformation takes place during the impingement of these unburned and partially ignited propellant particles apparently driven through vent holes by the expanding gases of the inner propellant particles which are first ignited in the cartridge housing.

It is strongly suspected (on the basis of the similarity in surface damage) that this behavior of unburned propellant particles impinging on the surface also occurs in field mortars with "0" increment charges.

E. Relationship of Hardness to Erosion or Wear

Since it was found that mechanical forces played a major role in the present wear problem, the effect of hardness of a steel surface vs erosion was examined. Figure 10 shows the behavior of 3 types of steels in which hardness is shown to be a significant factor with nitrided surfaces providing the best wear resistance.

The effectiveness of hardness is apparently also demonstrated by the "S" shape of the curves which shows the wear rate to decrease for a period and then increase again. This is attributed to the cold working of the surface which occurs as the degree of pitting becomes extensive. As firing continues, it is speculated that the distortion and plastic deformation of the surface peaks or crests approach a severity whereby fracturing occurs resulting in a sudden increase in the rate of wear as shown by the curves.

The indication that hardness of a surface effectively increases the erosion resistance would lead us to prematurely conclude that a quick solution to the problem is to nitride the bore surface of mortar tubes. Other considerations and findings, however, have ruled out such a decision. These are as follows:

1. The process of nitriding was found to be expensive and time consuming.

2. Surface cracks initiated at the surface, during firing, which propagated through the substrate (i.e. non-nitrided portion of the steel).

3. At the early stage of firing, the nitrided surface is very resistant to wear, however, as firing continues, the brittle casing quickly spalls and fragments leading to a rapid rate of wear and surface regression.

F. Evaluation of Surface Coatings

In view of the beneficial effects of hardness which had been demonstrated earlier, serious considerations were given to this property in our search for a suitable electrodeposited coating.

The erosive behavior of 8 candidate coatings have been studied in this present investigation. The coatings for this study were selected on the basis of cost, ease of application and resistance against surface damage. The comparative performance of 6 of the most promising coatings evaluated during the 1500 rd test using the lab erosion gage, is presented in Figure 11. The curves represent the average wear vs rds for all of the specimens tested. A minumum of 3 samples were tested for each average presented (with the exception of the Armoloy and Cr on Co coatings).

1. Chromium - Based on the observation of the harder surface providing the highest resistance to wear, electrodeposited chromium should have proved to be the leading candidate coating since it was the hardest coating. A review of the curves in Figure 11, however, shows that chromium (0.2 mils thick) showed approximately 25% improvement in protecting steel. Previous tests with chromium deposits

which were 2 mils or greater in thickness failed prematurely by excessive spalling and were withdrawn early during the 1500 rd test. Field tests also showed chromium coatings (2-5 mils thick) to perform poorly in a similar manner. An explanation for this behavior can be offered if we look at the wear plot from Finnie et al in Figure 5, which shows a brittle surface to result in greatest wear at a 90° angle of impingement. In reality, electrodeposited chromium can be classified as a brittle surface and therefore explains such a behavior. Figure 12 shows a cross-section of chromium on mortar steel with initial "V" notch segments of chromium progressively sheared away.

Figure 13 shows a surface view of a pitted mortar section. The white patches represent the chromium still remaining.

2. Armoloy Chromium - Coatings of armoloy chromium (which were deposited in a proprietary electrolyte) 1 mil thick were shown to be very promising. However, only one specimen was available for test firing above 1200 rds which is indicated by the dashed line. Other specimens flaked prematurely, and it could not be determined whether the failure was due to the material property or plating defects. In view of the electrolyte being of a proprietary nature and inconsistencies were encountered in the deposition process, the study of these deposits were temporarily suspended.

3. Cobalt System of Coatings - We have already discussed the evaluation of nitrided steel, and conventional and armoloy chromium deposits as candidate materials. The remaining materials for discussion are referred to as the cobalt system and can be classified as ductile coatings in contrast to chromium. The wear plots for these coatings are shown separately with mortar steel (which includes their hardness) in Figure 14 in order to simplify the discussion of their evaluation.

In studying the performance of these coatings, it was observed that the erosion resistance generally increased with increasing microhardness but the hardness was not the sole factor in controlling their overall performance. Coatings had to be sound (i.e. free from voids, stress, cracks, and be crystalline), possess high shear and tensile properties and be highly adherent to their substrates in order to be highly resistant to erosion.

a. Co-Fe - The process controls and mechanical properties of Co-Fe alloys have been reported previously from this lab by Sadak and Sautter[9]. In view of some of these properties it was decided to include Co-Fe as one of the candidate coatings. However, a comparison of the wear curves shows cobalt-iron to be the most inferior among the cobalt system. However, this behavior has been attributed to plating problems in which difficulties were encountered in achieving homogeneous and sound deposits of the alloy. Recent improvements in the plating process controls have indicated that Co-Fe (5-12 wt %) should prove to be a strong candidate as an erosion retardant coating in 81mm mortars.

b. Cobalt - The preformance of pure cobalt was comparable to nitrided steel and eroded approximately 1/2 the amount as mortar steel. The influence of adhesion and soundness of the deposit on its resistance to erosion was clearly evident by the testing of an earlier group of cobalt whereby the erosion resistance was only approximately 25% better than mortar steel. In this case the surface regressed by the flaking of large chips of the deposit.

c. Cobalt-Alumina - ($Co-Al_2O_3$) - Dispersion strengthened cobalt alloy was selected as a candidate coating on the basis of its temperatures. The concept of preparing dispersion strengthened alloys by electrodeposition techniques was first reported by Sautter[10] using nickel, and later by Greco and Baldauf. The properties of electro-deposited $Co-Al_2O_3$ alloys were reported by Sadak and Sautter[12] and more recently by Chen and Sautter[13].

Dispersion hardened cobalt deposits for the present test consisted of approximately 2 v/o of finely dispersed alumina (having a particle size of .05 microns) in a cobalt matrix. This alloy was found to be one of the best erosion resistant materials for this test. $Co-Al_2O_3$ alloys would be expected to perform better than pure cobalt, since their hardness is higher both at room and high temperature and their shear and yield strength are superior. The importance of obtaining sound and homogeneous deposits cannot be overstressed, however, since a large variation in the erosion rate results when the process controls are not under close observation. Non-homogeneity frequently leads to spalling of large fragmentation rather than the fine microscopic surface regression normally observed.

d. Chromium on Cobalt - We have shown previously that chromium deposits on steel (2-5 mils thick) showed poor performance in mortars due to its inherent brittleness resulting in progressive and spalling of the coating. Thinner deposits (i.e. 0.2 mils) directly on steel offered better protection but the decrease in wear was only approximately 25%. Furthermore cobalt deposits by themselves were classified as fair among the other coatings (i.e. the wear rate was 50% compared to mortar steel). Nevertheless, Figure 14 shows Cr on cobalt to offer the highest resistance against erosion when compared with all other coatings. This infers that by combining the 2 coatings we have capitalized on their erosion resistance beyond their protective quality as individual coatings. This means that some combined effect must exist at the interface of Cr and Co. In addition, the chromium deposit was only 2-5 tenths of a mil thick over 6 mils of cobalt. Thin deposits of Cr are relatively strong compared to thick deposits and do not shear or spall readily. However, thin deposits directly on steel did not perform as well, probably because when the thin Cr flaked away, the underlying steel pitted and the surface regressed at a relatively high rate but thin Cr on cobalt appeared to shear away in a gradual manner with the underlying cobalt still being very resistant to the impinging gases. It must be pointed out that the sampling of Cr on Co is limited and efforts in this area must be continued to explain the combined effect of Cr on Co as on erosion resistant material.

CONCLUSIONS

It should be apparent from the above study that the evaluation of and a selection of a coating for the protection of mortar bores against annular groove erosion, is incomplete. However, based on the present data which has been evaluated, two coatings can be recommended for further study. These are:

a) Co-Al_2O_3 (5-7 mils thick)

b) Chromium (0.2-0.5 mils) on Cobalt (5 mils)

Continued firing in the erosion gage with these coatings should provide sufficient data to determine the best coatings to be recommended for field testing.

One can conclude from the present data, however, that:

a) the erosion gage has shown to be a useful tool for evaluating erosion resistant coatings for mortar bores.

b) the comparison of the surface damage characteristics produced by the gage and field mortar are very similar.

c) the gage accelerates the erosion rate compared to field the system by a factor of approximately 4.5 to 1.

d) the cobalt system of coatings will significantly reduce the annular groove erosion in mortar bores.

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TABLE I - PLATING BATH FORMULATIONS

Bath No	Coating	Bath Formula	Compound Conc. g/1
1	Cobalt (Co)	Cobalt Sulfate ($CoSO_4 \cdot 7H_2O$)	300
		Cobalt Chloride ($CoCl_2 \cdot 6H_2O$)	50.6
		Boric Acid (H_3BO_3)	31.4
2	Cobalt Alumina ($Co\text{-}Al_2O_3$)	Cobalt Sulfate	314
		Boric Acid	31.4
		Cobalt Chloride	50.6
		Aluminum (Al_2O_3) (.05u particle size)	25
3	Cobalt-Iron (Co-Fe)	Cobalt Sulfate	314
		Boric Acid	31.4
		Cobalt Chloride	50.6
		Ferrous Sulfate ($FeSO_4 \cdot 7H_2O$)	25
4	Chromium (Cr)	Chromic Anhydride (CrO_3)	250 g/1
		Sulfuric Acid (H_2SO_4)	2.5 g/1
5	Armoloy Cr	*Armoloy Salts	454 g/1

*Supplied by Armoloy Inc., Fort Worth, Texas

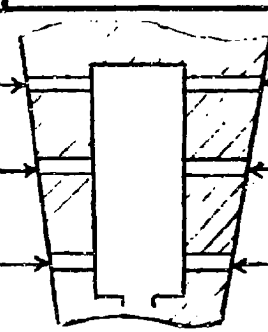
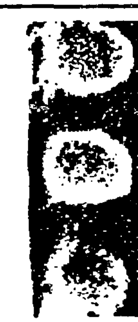
TABLE II - PLATING CONDITIONS

Bath	Current Density	pH	Be'	Bath Temp
Cobalt	5.4 amp/dm^2 (50 amp/ft^2)	2.0	30	60°C (140°F)
Cobalt-Alumina 50 g/l (.05 micron)	5.4 amp/dm^2 (50 amp/ft^2)	2.0	30	60°C (140°F)
Cobalt-Iron (6 wt %)	5.4 amp/dm^2 (50 amp/ft^2)	3.0	30	60°C (140°F)
Chromium	32 amp/dm^2 (300 amp/ft^2)		21-22	54°C (130°F)
Armoloy Cr	32 amp/dm^2 (300 amp/ft^2)		21-22	54°C (130°F)

TABLE III

TESTING ON MORTAR STEELS

COMPARISON OF GAS JET DISTANCE - WEAR RELATION FOR FIELD MORTAR VS LAB EROSION GAGE

FIELD MORTAR SYSTEM AFTER 7500 RDS			M170 TAIL FIN (IGNITION CHAMBER WITH VENT HOLES)	EROSION GAGE AFTER 1500 RDS		
SURFACE VIEW OF EROSION	AVG WEAR ON RADIUS (MILS)	DIST. OF JET TO BORE SURFACE (IN.)		DIST. OF JET TO BORE SURFACE (IN.)	AVG WEAR ON RADIUS (MILS)	SURFACE VIEW OF EROSION
	14	.850		.450	12.1	
	9	.920		.520	8.7	
	3	.990		.590	2.7	

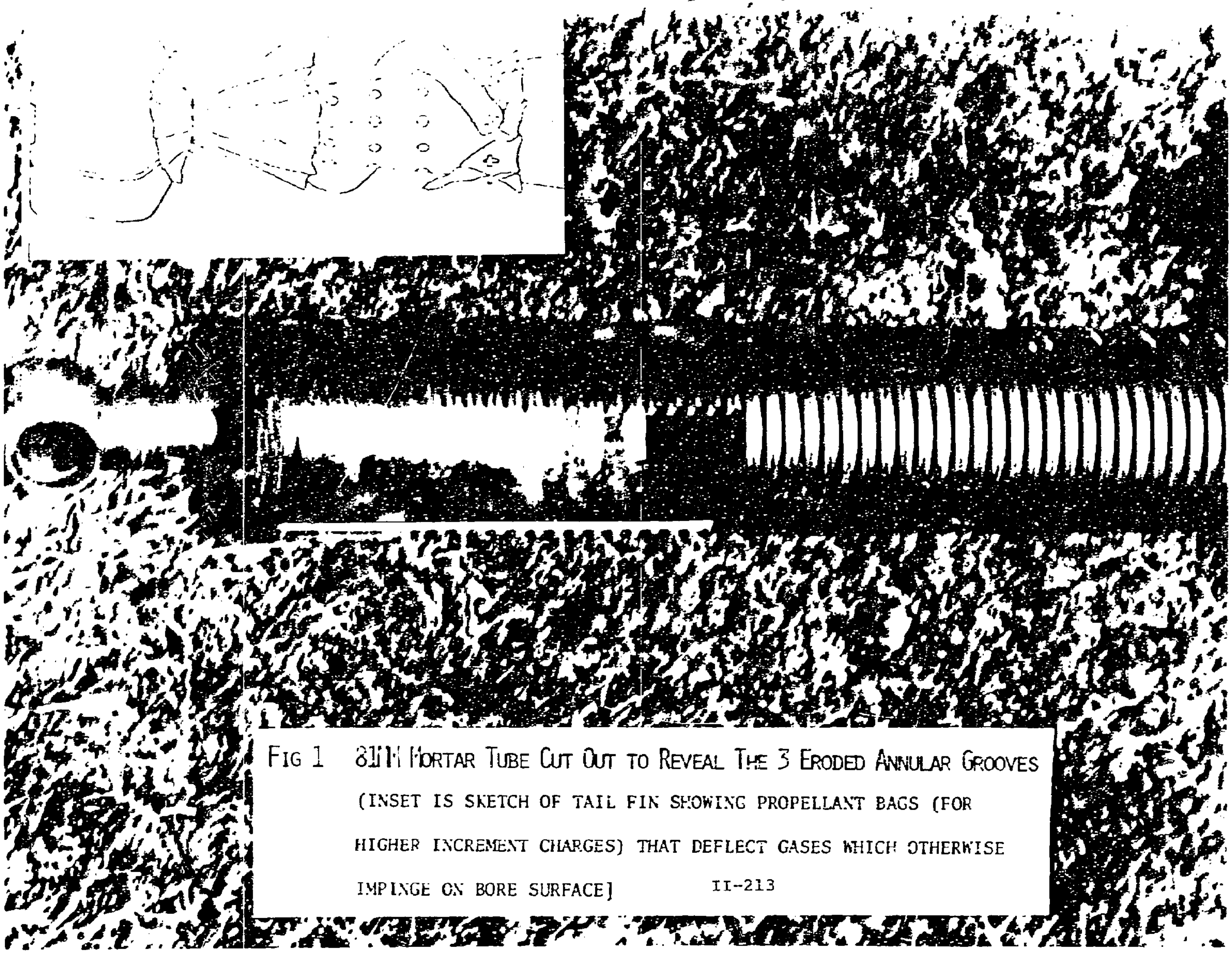
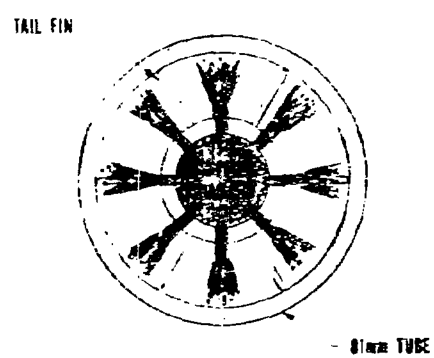
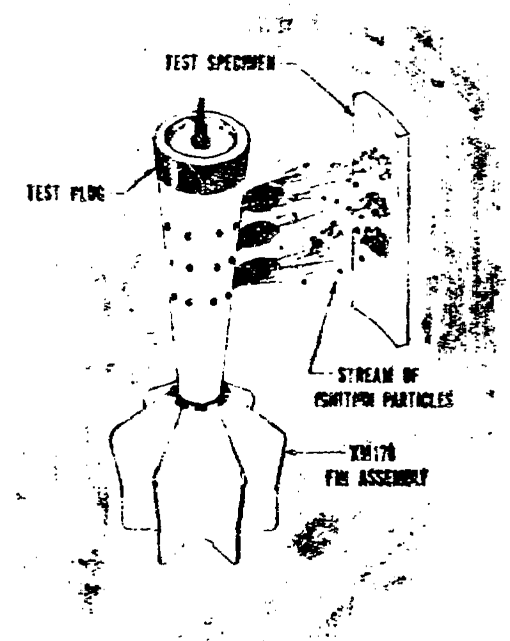


Fig 1 81MM Mortar Tube Cut Out to Reveal The 3 Eroded Annular Grooves (INSET IS SKETCH OF TAIL FIN SHOWING PROPELLANT BAGS (FOR HIGHER INCREMENT CHARGES) THAT DEFLECT GASES WHICH OTHERWISE IMPINGE ON BORE SURFACE]



OVERHEAD VIEW OF
THE 8 JETS OF IGNITING GASES
IMPINGING ON THE BORE SURFACE

FIG 2

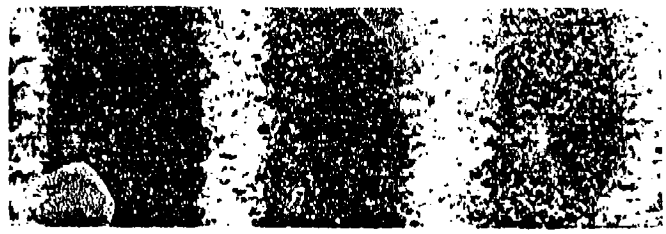


SECTIONAL VIEW OF IGNITING GAS JETS
IMPINGING ON TEST SPECIMENS

FIG 3

(A)

SURFACE VIEW OF THE
3 ERODED ANNULAR GROOVES
(DARK BANDS)



(B)

LONGITUDINAL CROSS-SECTION
SHOWING PROFILE OF THE
FORWARD ERODED GROOVE

x7

(C)

MICROSCOPIC SURFACE VIEW
OF FORWARD GROOVE SHOWING
WHITE LAYER ON PEAKS

(D)

MICROSCOPIC SURFACE VIEW
OF THE FORWARD GROOVE
SHOWING SEVERAL CRACKS

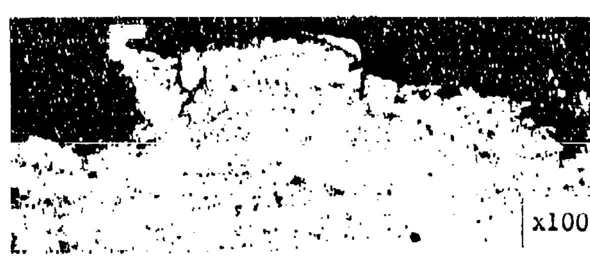


FIG 4 ERODED VIEWS OF CONDEMNED 81MM MORTAR TUBE AFTER
FIELD FIRING OF APPROX 7500 RDS

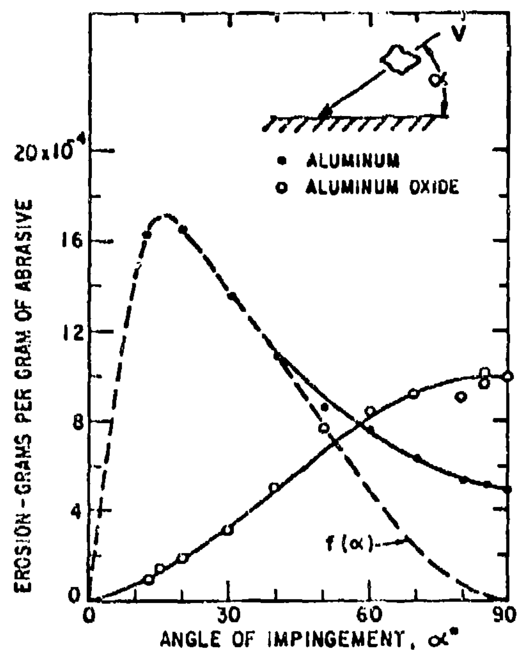


FIG. 5 WEIGHT REMOVED BY EROSION AS FUNCTION OF ANGLE OF IMPINGEMENT FOR 1100-0 ALUMINUM AND HIGH DENSITY ALUMINUM OXIDE (AFTER FINNIE REF. 3)

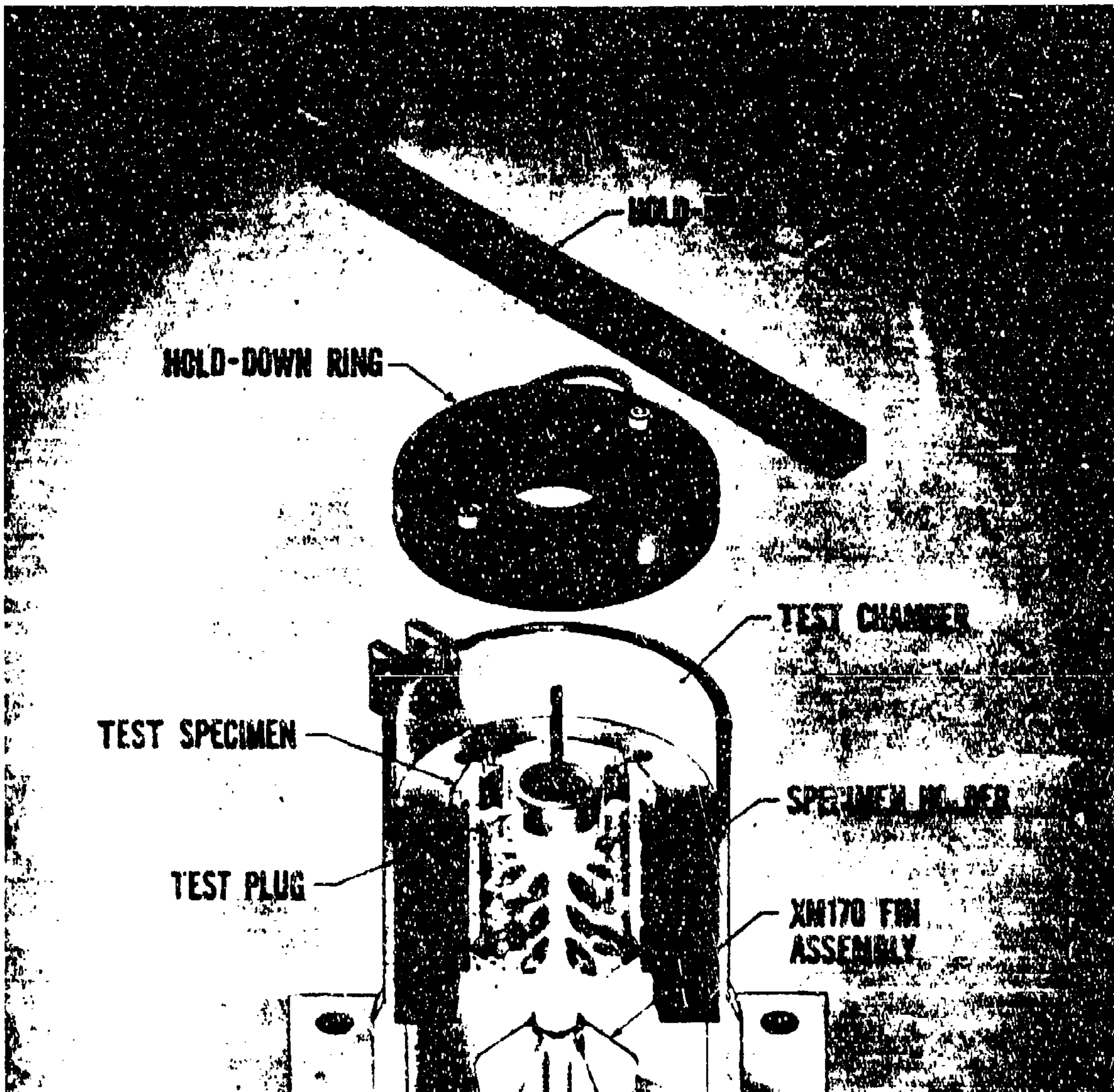


Fig. 6. 81MM Mortar Erosion Tester

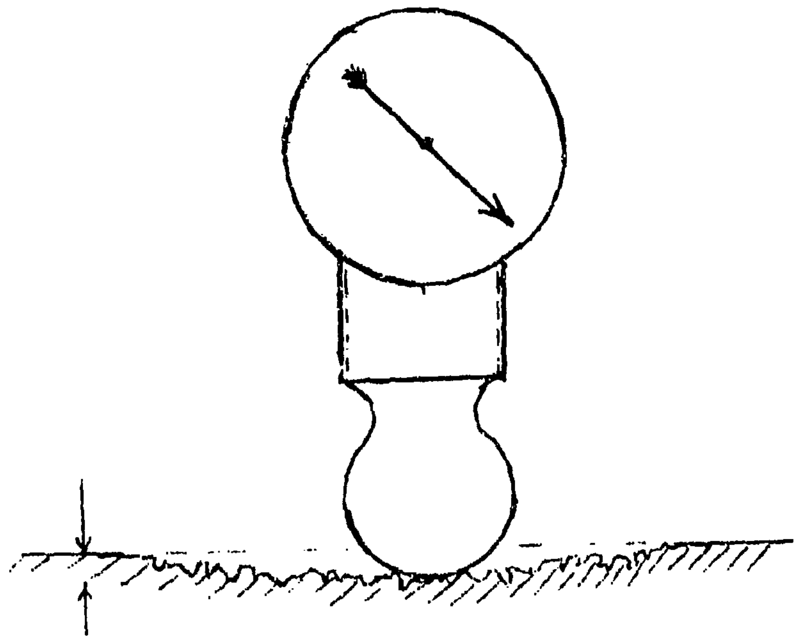


FIG 7 SURFACE WEAR MEASUREMENT - MAXIMUM DEPTH DETERMINED BY A 1/8" DIA. BALL TIP

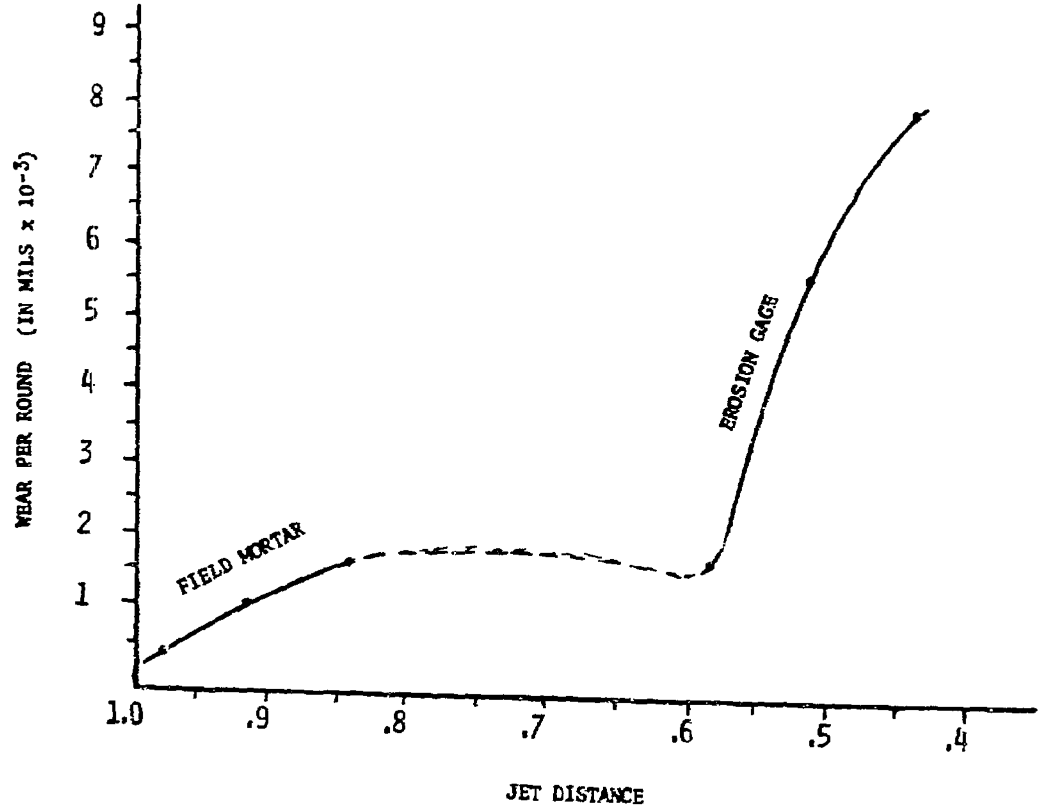
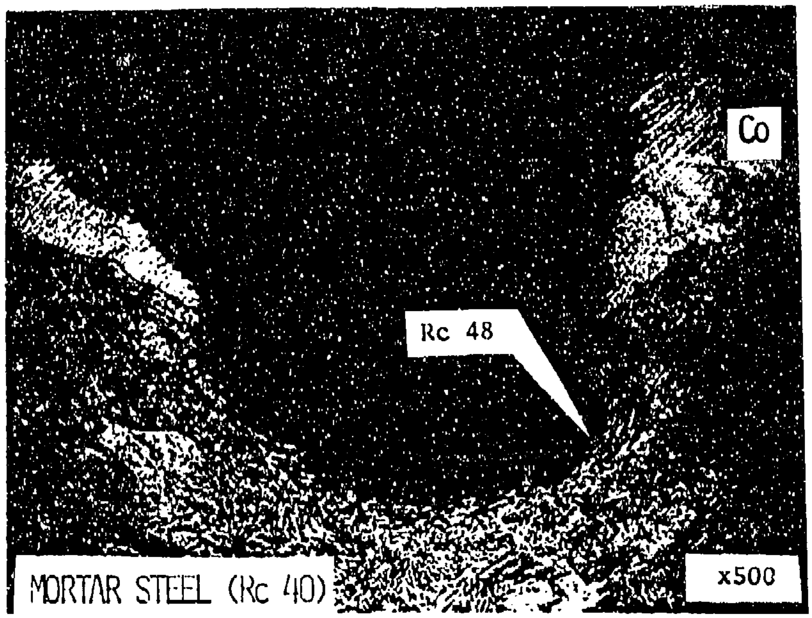


FIG 8 WEAR RATE VS JET DISTANCE FOR MORTAR STEEL
(COMBINING FIELD AND EROSION GAGE DATA)



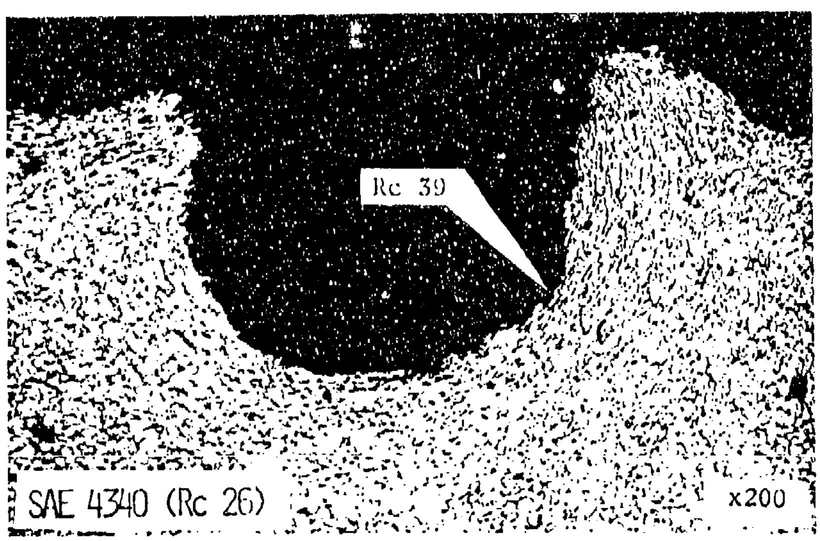


FIG 9 MICROSCOPIC CROSS-SECTIONAL VIEW OF A PIT IN 2 STEELS (NOTE FINE GRAIN STRUCTURE ALONG THE PERIPHERY OF THE PIT DUE TO COLD WORKING.)

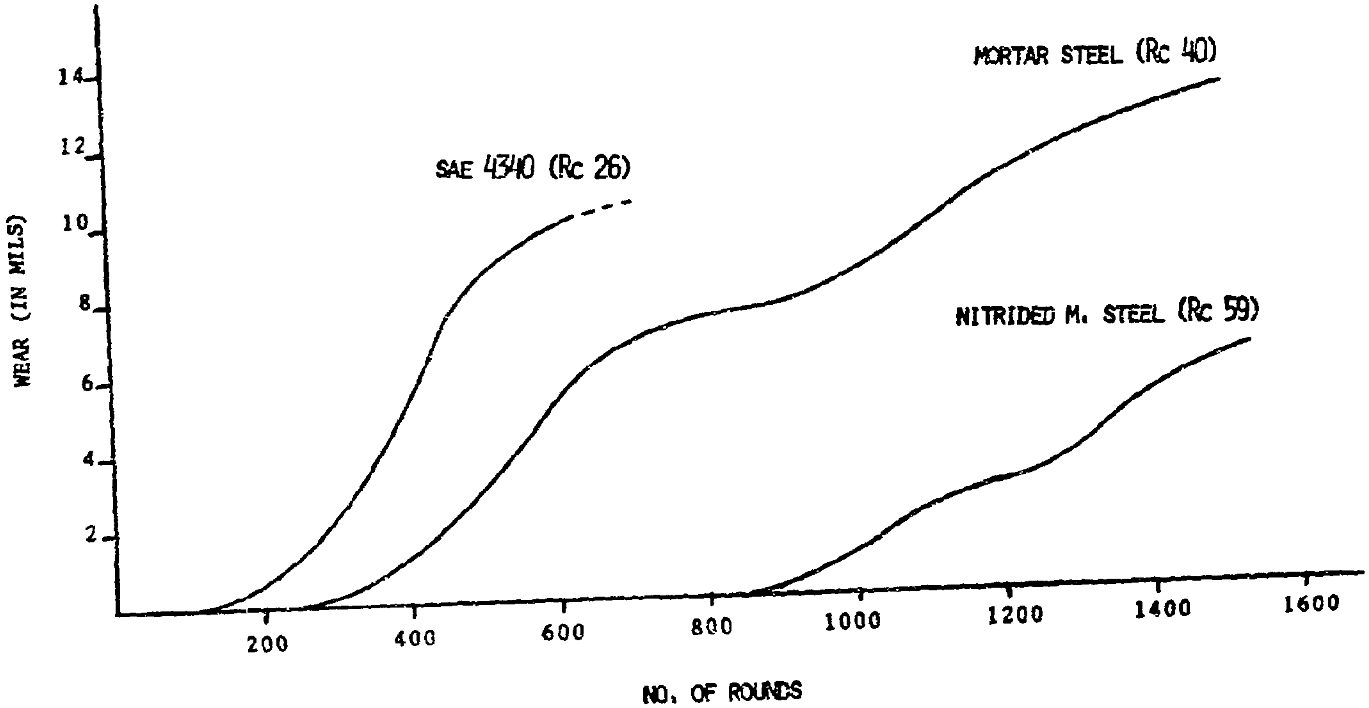


FIG 10 WEAR VS ROUND CURVES SHOWING RELATION OF STEEL HARDNESS

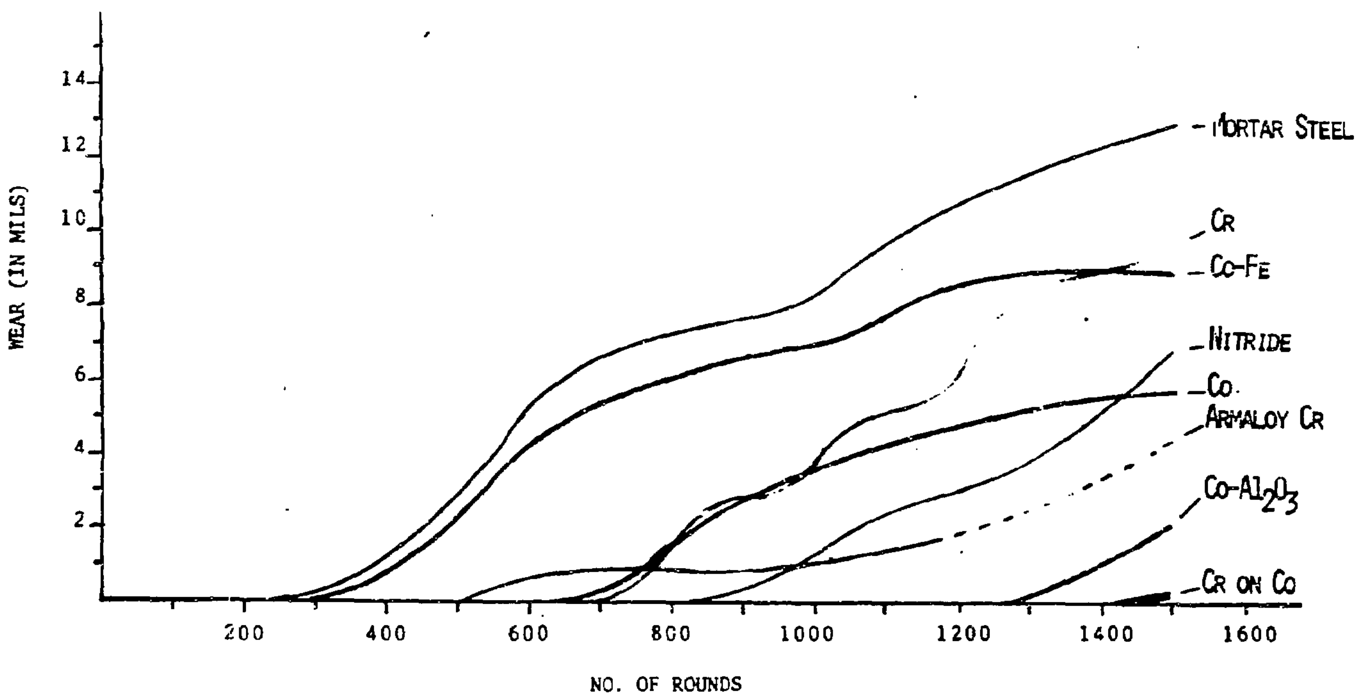


FIG 11 WEAR VS ROUND CURVES COMPARING VARIOUS COATINGS WITH MORTAR STEEL

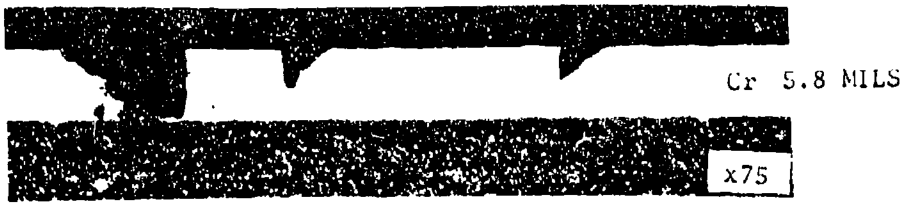


FIG 12 CROSS-SECTION OF CHROMIUM DEPOSIT ON MORTAR STEEL WITH "V" NOTCH SECTIONS REMOVED LEADING TO EARLY FAILURE

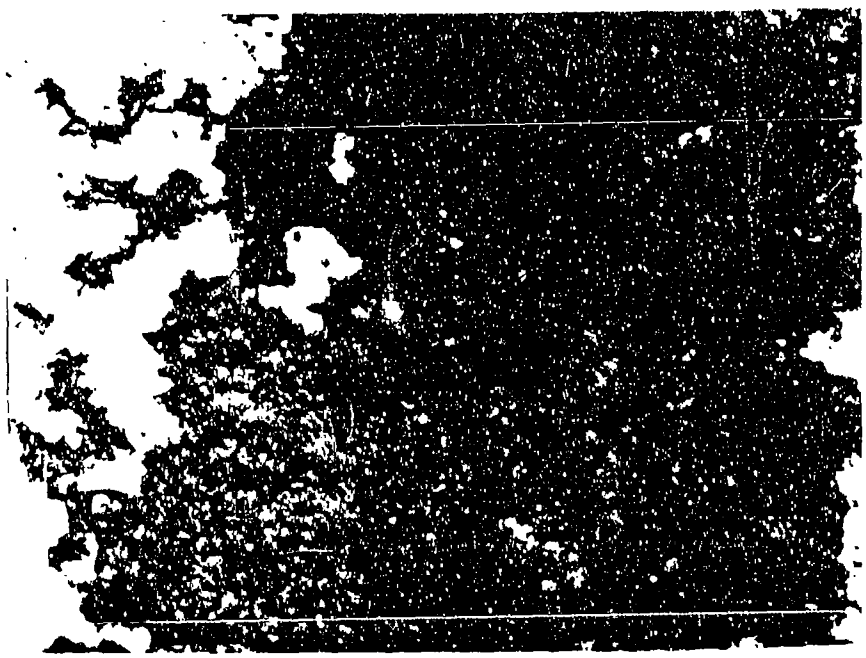


FIG 13 SECTIONAL SURFACE VIEW OF PIT EROSION IN 81MM BORE AFTER CHROMIUM HAS FLAKED AWAY (WHITE PATCHES ARE REMAINING CHROMIUM)

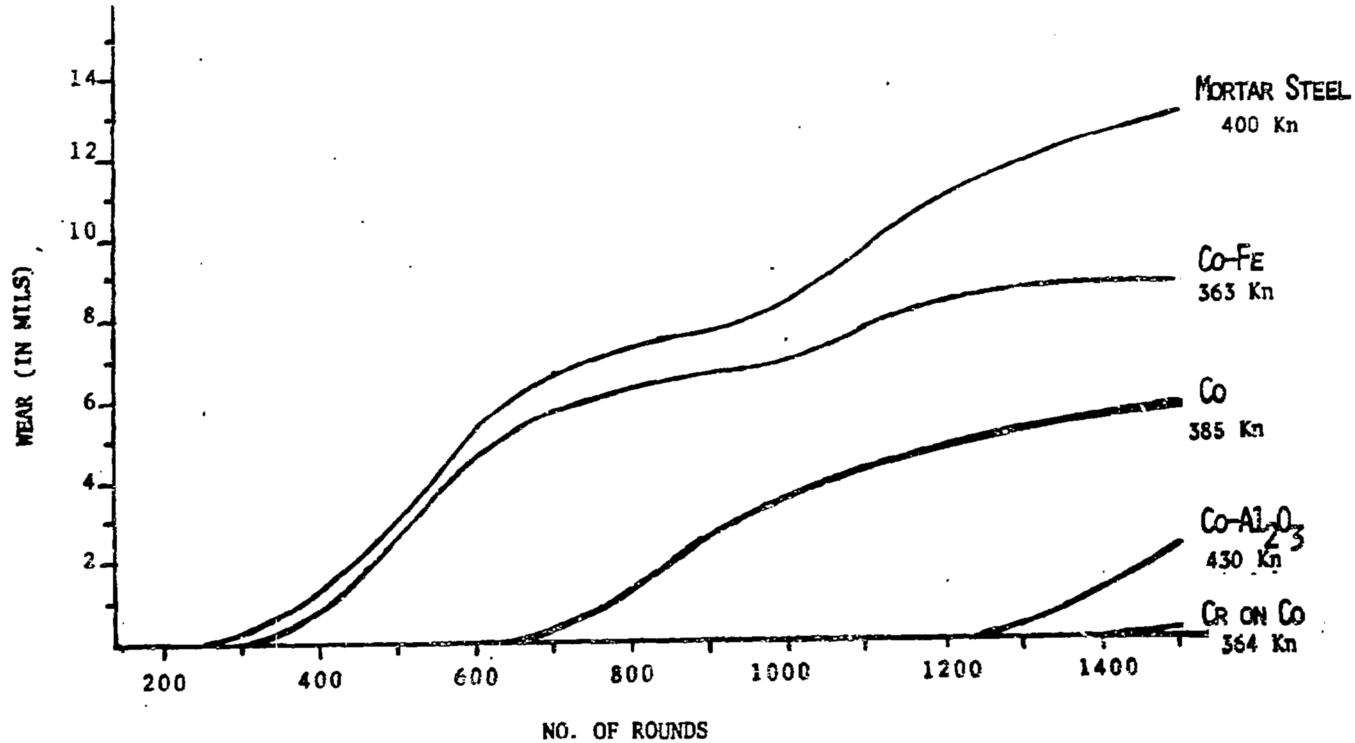


FIG 14 WEAR VS ROUND CURVES COMPARING COBALT SYSTEM COATINGS WITH MORTAR STEEL

ANALYSIS OF RUPTURE IN RAPIDFIRE BARRELS

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ABSTRACT

Small arms rapidfire barrels experience repetetive high rate loading in a corrosive environment at elevated temperatures. Fatigue cracks nucleate at the intersections of lands and grooves, propagate radially and eventually cause rupture. A consistent rupture pattern in M134 minigun barrels has been observed and explanations are sought in this paper. Reasons to believe that the thermal gradient is steepest along a preferred radial direction across the barrel wall are presented. The pressure stress combined with the thermal stress causes fatigue and because of the nonuniformity of thermal gradient fatigue crack propagation is faster along a preferred direction.

INTRODUCTION:

Small arms rapid fire barrels experience repetetive high rate loading in a corrosive environment at elevated temperatures. The burning of the propellant heats up the bore surface which, in turn, heats the barrel wall by conduction. The thermal gradient across the wall causes thermal stresses. The explosive action which propels the projectile causes pressure stresses and the engraving action on the projectile by the rifling causes shear stresses on the rifling. The burnt gases provide the corrosive environment.

The hydrostatic pressure inside the barrel tube reaches a peak when the projectile travel has reached a point about 1 1/2" from the origin of rifling (OR). Attempts to measure/calculate bore surface temperature indicate that the bore surface temperature is highest in about the same region. Due to the fact that the barrel wall is thickest in the region the thermal gradient is the steepest. While the tangential component of the thermal stresses at the bore surface is compressive, the tangential component of the pressure stress is tensile. However, the thermal stress lags behind the pressure stress. So much so, instead of compensating each other, the two stresses cycle the material at the bore surface between a pressure tensile peak and thermal compressive peak. In a rapid-fire weapon, fatigue crack develops and propagates into the barrel wall and progressively weakens the structure. Fracture occurs when the crack reaches a critical dimension.

The intersections of the sides of the lands with the grooves are stress raisers (there are eight of them in a four land barrel) and are locations for crack nucleation. The shear stress caused by the driving action of the land makes the driving side intersection of the land with the groove a relatively weaker location than that of the trailing side. Out of the eight locations, the cracks (four of these) emenating from the driving side propagate faster. Under symmetrical conditions of temperature and pressure, all four of them have an equal probability for reaching critical dimensions. The type of fatigue damage carried to supercritical stage in M134 barrels is shown in Figure 1. The damage to personnel and materiel which could be caused by fatigue crack propagation is shown in Figure 2.

OBSERVATIONS:

During the conduct of several programs on the evaluation of materials and manufacturing techniques for rapid fire weapons,[2] M134 weapon was used as a test vehicle in which barrels made of several materials were test fired. Standard M134 barrels (Cr-plated Cr-Mo-V steel) were used as slave barrels to make up compliments of six barrels for the test firing. The barrels were usually retired on the basis of standard criteria, (loss of velocity, loss of accuracy and yaw). Many of the slave barrels ruptured near the breech end before normal retirement. A superficial examination of the ruptured barrels revealed a striking fact: namely, the location of the rupture was the same on all barrels irrespective of the firing schedule. Table 1 shows the details of firing schedules. Figure 3 shows samples of the ruptured barrels and the lettering on each barrel shows the schedule and the number of rounds fired. In the figure, the barrels are laid out identically with the flat face on the right upwards and the curved surface of the locating tab on the left downwards. As can be seen, the rupture is on the same location in each barrel.

Barrels, which were retired before through-rupture took place, were sectioned transversely at 1 1/2" from OR and the sections were prepared for metallography. Figures 4 and 5 show representative features of such sections. The bore in this section is severely eroded. All traces of rifling have been wiped out. Several cracks emenating from the bore surface progress radially. One of the cracks has been unmistakably progressing at a faster rate than the others and the geometrical location of this crack is the same as that which caused rupture in barrels of Figure 3.

DISCUSSION:

Earlier accounts of the process of erosion in chrome plated Cr-Mo-V steel rapid fire barrels describe how extant cracks in the Cr-plate nucleate cracks on the substrate.[3] These cracks progress into the material, interconnect and lift material out of the barrel I-D. A circularly symmetrical I-D geometry progressively deteriorates and has the appearance shown in Figure 5. The I-D is no longer circular and is almost a square with rounded sides. Even if ideal conditions concerning uniform-

Table I. Firing Schedules (Per Barrel)

Schedule	
I	83 rounds 10 second pause 83 rounds, 10 minute cool repeated four times for a 666 round compliment then complete cool to ambient temperature.
II	125 rounds 10 second pause 125 rounds 10 minute cool repeated four times for a 1000 round compliment then complete cool to ambient temperature.
III	250 rounds 10 minute cool 250 rounds 10 minute cool repeated until 2,000 rounds have been fired then complete cool to ambient temperature.
IV	250 rounds 8 minute cool 250 rounds 8 minute cool repeated until 2,000 rounds had been fired then complete cool to ambient temperature.

ity of pressure behind the base of the projectile, identity of temperature at all points on the circumference of the I-D contour and balance of the projectile around its axis in flight could be assumed, as pointed out earlier, there are four prime locations for fast fatigue crack propagation. As erosion progresses, these four locations become increasingly more vulnerable to fatigue crack propagation. Certain characteristics of the M134 system need to be stated to understand why one particular crack out of the four leads to rupture.

M134 minigun is a gattling gun with six barrels which has a capability of firing 6000 rounds a minute. The test firings described in this paper were carried out at 4000 rounds a minute. During firing, each barrel is rotated past the loading, firing and ejection positions at the rate of 666 times a minute which is to say the barrel cluster is rotated at 666 rpm. Figure 6 shows a barrel cluster configuration. During firing the barrels are heated to high temperatures and Figure 7 is a photograph taken at the end of schedule IV. The crack which progressed to rupture travelled radially in the barrel section and outward radially in the barrel cluster.

The diameter of the six barrel cluster (Figure 6) is 2 1/2 inches. The proximity of each barrel to another is easily visualized. The outside of the barrel cluster is open to air and the radiated heat from each barrel in the inside of the cluster stays inside. Due to the high velocity of rotation of the barrel cluster convection currents will extract heat from the outside surface of the barrel. Remembering the proximity of barrels and that the outside of the cluster is exposed to air and the inside of the cluster is protected, it is easy to see that the heat extraction is preferred on the outside over that on the inside of the cluster. This fact would lead to a situation where in any barrel, heat extraction is faster along a radial direction going outward in the cluster scheme. This alone would make the thermal gradient steeper in that direction and thus the thermal stress higher even if the I-D of the barrel experiences uniform temperature all along the circle. Since the barrel cluster is rotated at high speed, instrumentation to verify this hypothesis is extremely difficult.

It is also hypothesized that because of the centrifugal forces caused by revolution, the density of the burning propellant behind the base of the projectile will be higher along a radius in the I-D of the barrel running toward the outside of the barrel cluster. This would make the temperature of the bore surface nonuniform and will be hotter toward the outside of the cluster. This would further accentuate the thermal gradient and the thermal stress in that direction. A schematic illustration of this situation is shown in Figure 8.

Thus the peak to peak cyclic stress at the bore surface is maximum in a region indicated in the barrel cluster scheme in Figure 6. Given the facts above, it is not surprising that the fatigue crack which causes rupture is the one that travels outward toward the outside of the barrel cluster.

CONCLUSION:

The predominance of rupture in a specific location in M134 barrels during service can be attributed to the fact that the thermal gradient in that direction across the barrel wall is the steepest and that the cyclic stress has the highest peak to peak value.

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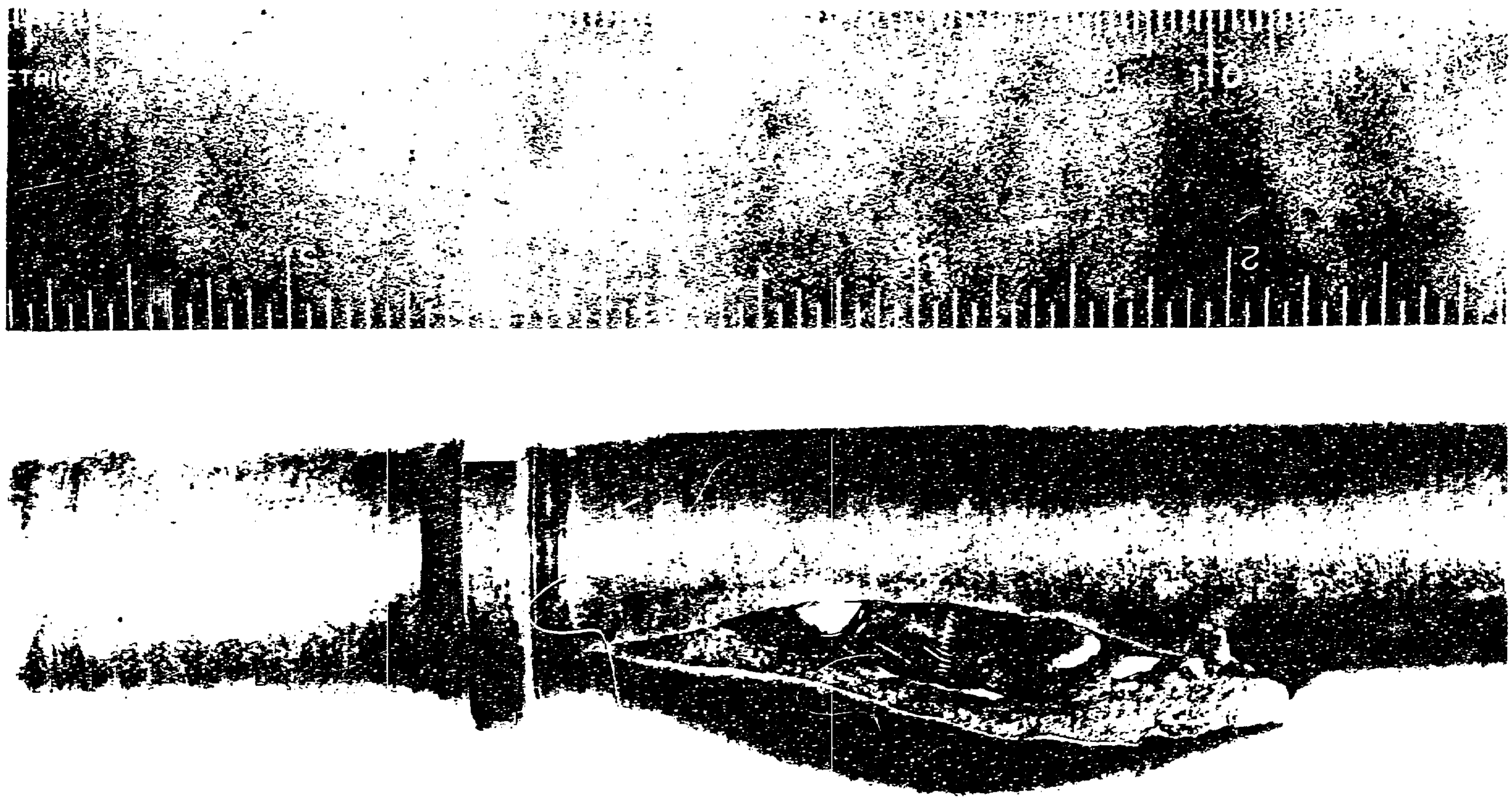

FIGURE 1. RUPTURE IN M134 BARREL

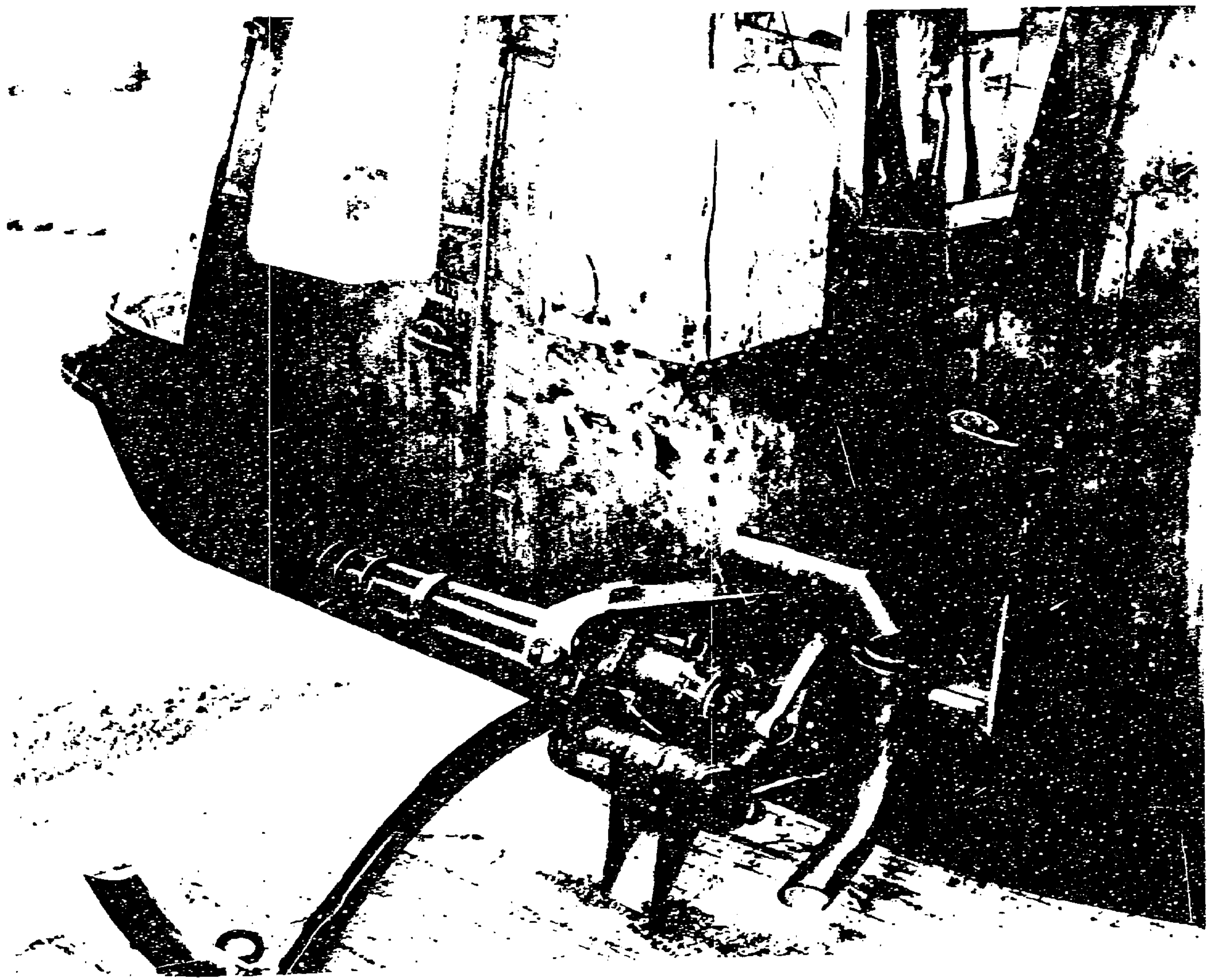

FIGURE 2. DAMAGE TO MATERIEL CAUSED BY BARREL RUPTURE

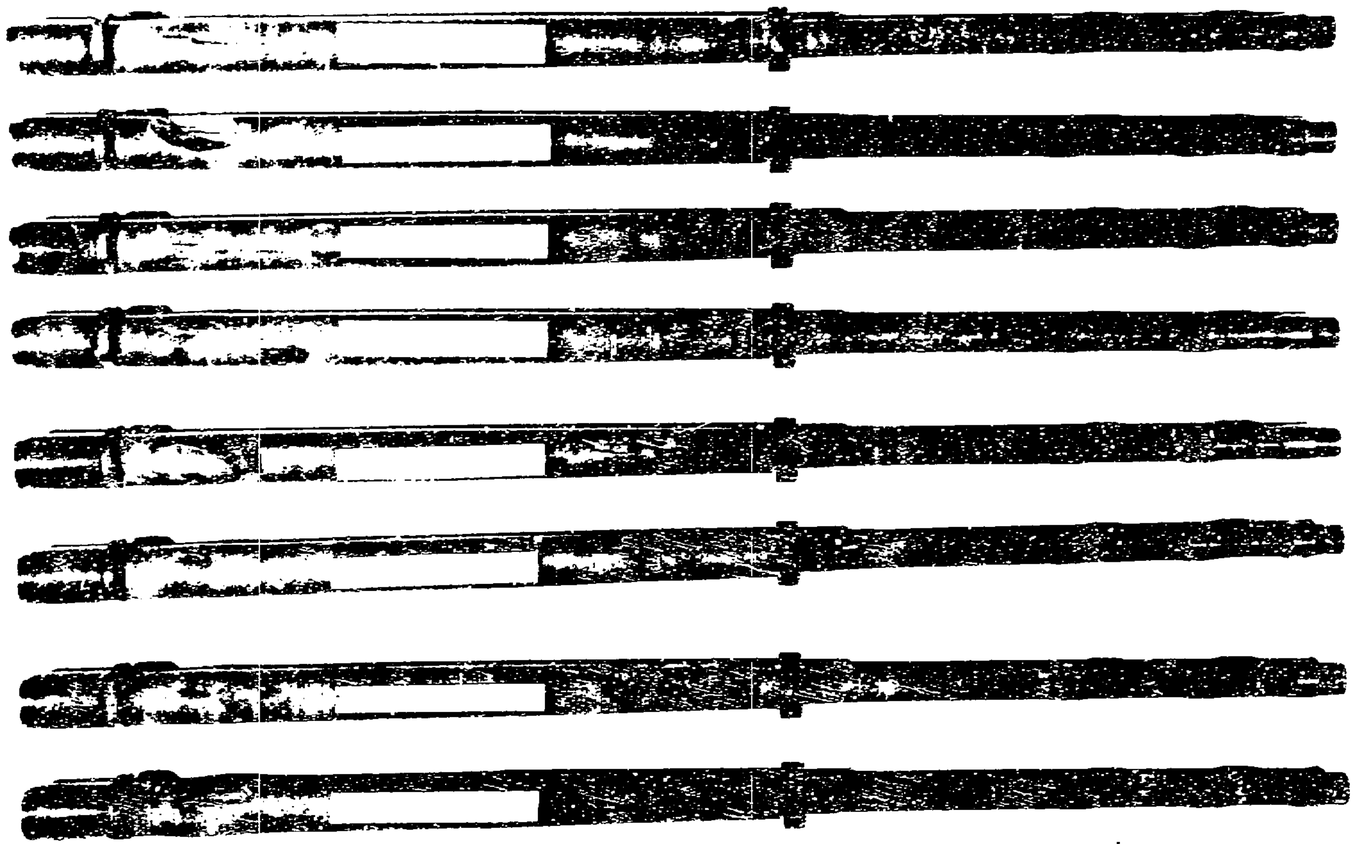

FIGURE 3. M134 BARRELS TEST FIRED AT FOUR DIFFERENT SCHEDULES. BARRELS POSITIONED IDENTICALLY TO SHOW THE LOCATION OF RUPTURE BEING THE SAME IN ALL CASES

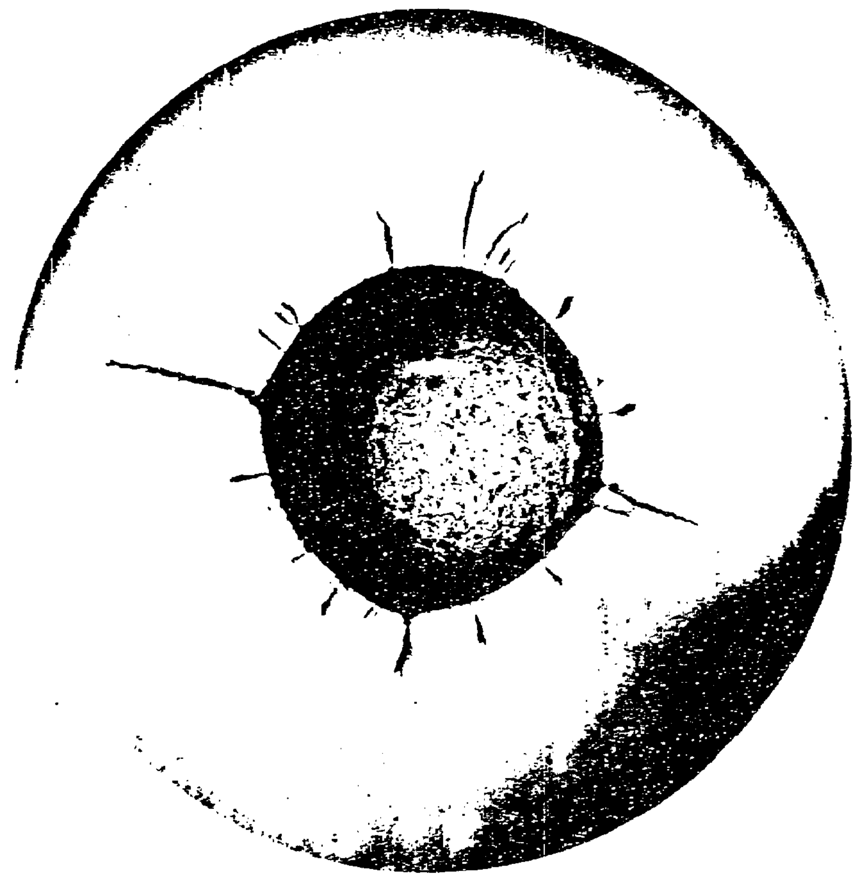

FIGURE 4. TRANSVERSE SECTION OF RETIRED BARREL AT 1 1/2 INCHES FROM 'OR'

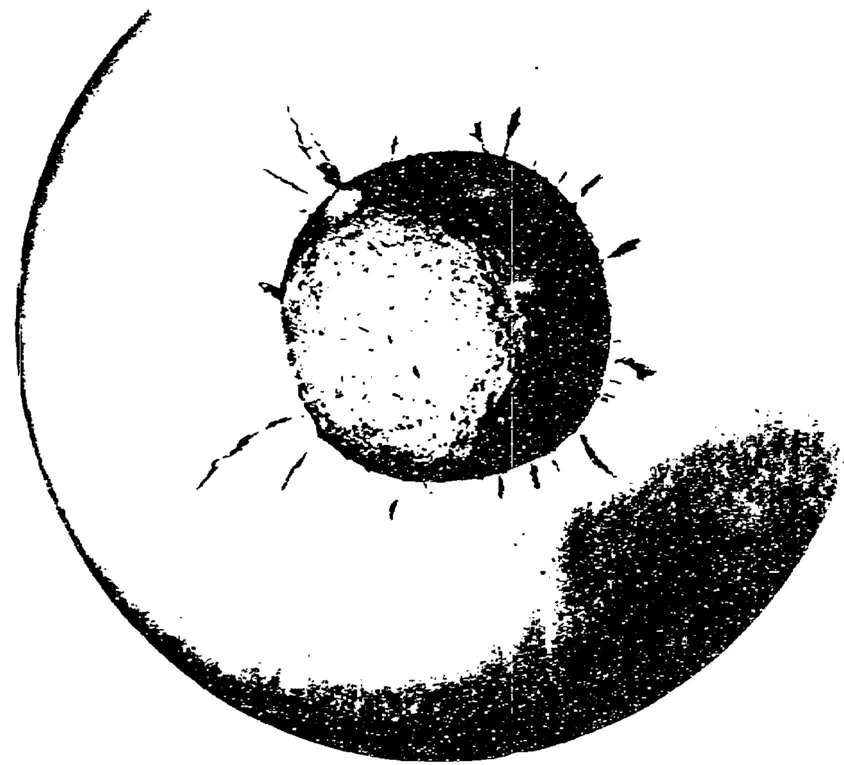

FIGURE 5. SAME AS 4, DIFFERENT BARREL

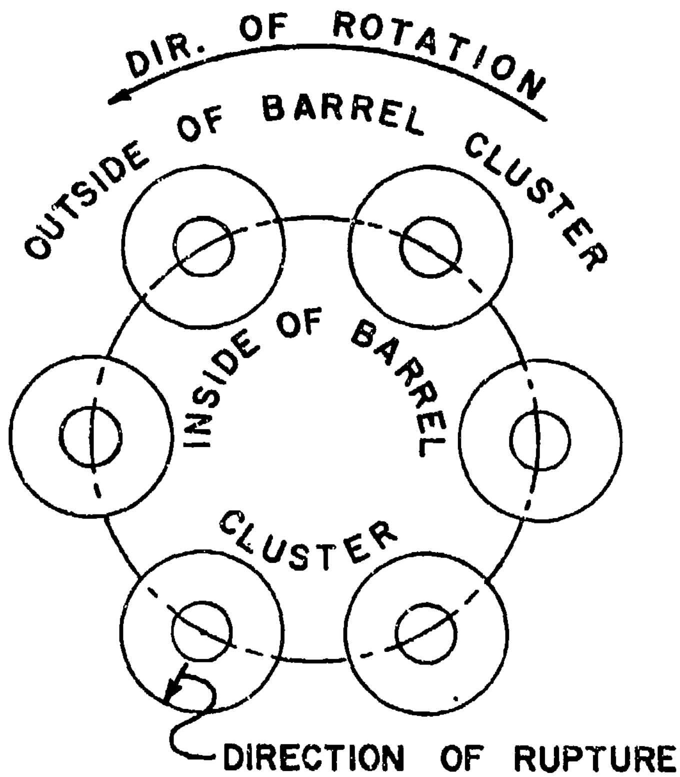

FIGURE 6. SCHEMATIC TRANSVERSE SECTION OF M134 BARREL CLUSTER

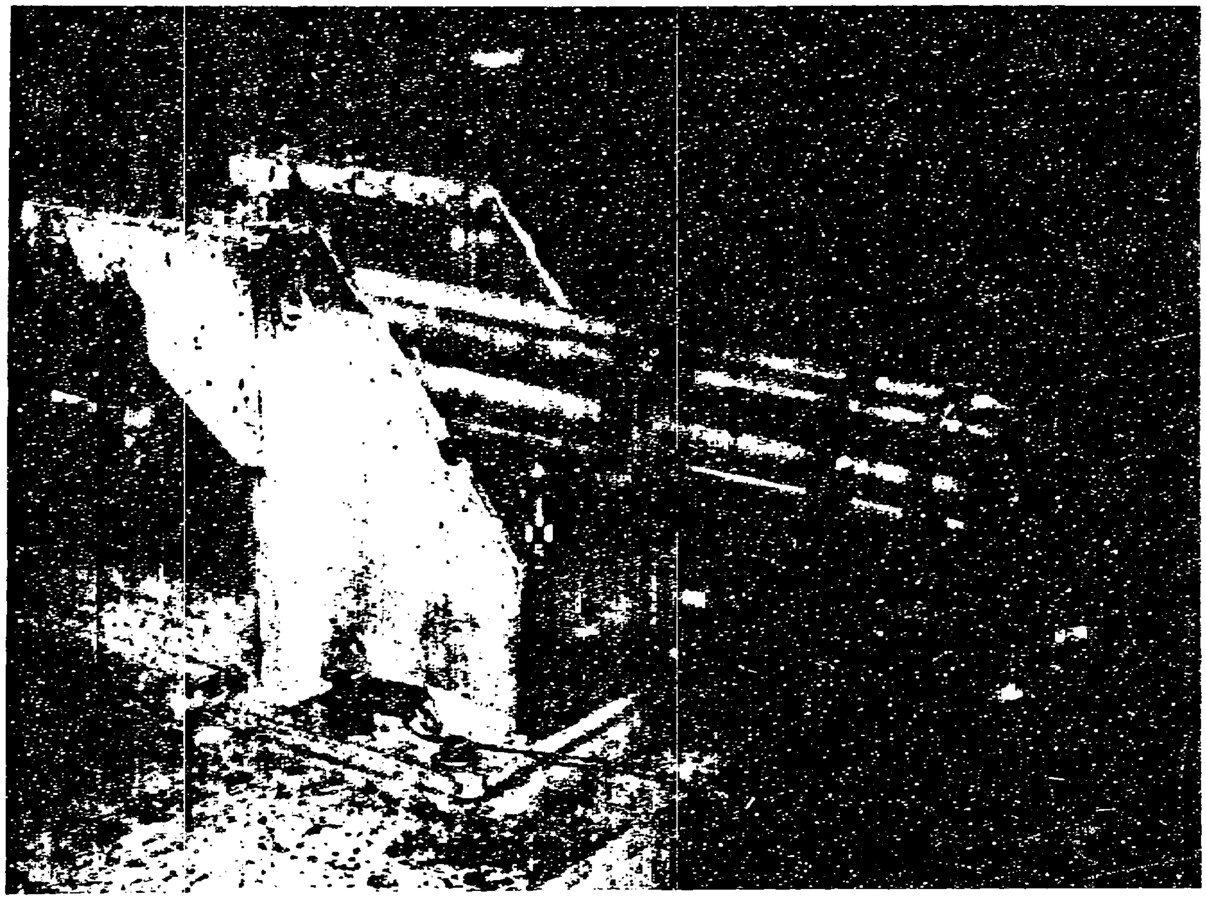

FIGURE 7. APPEARANCE OF M134 BARRELS AT THE END OF SCHEDULE IV

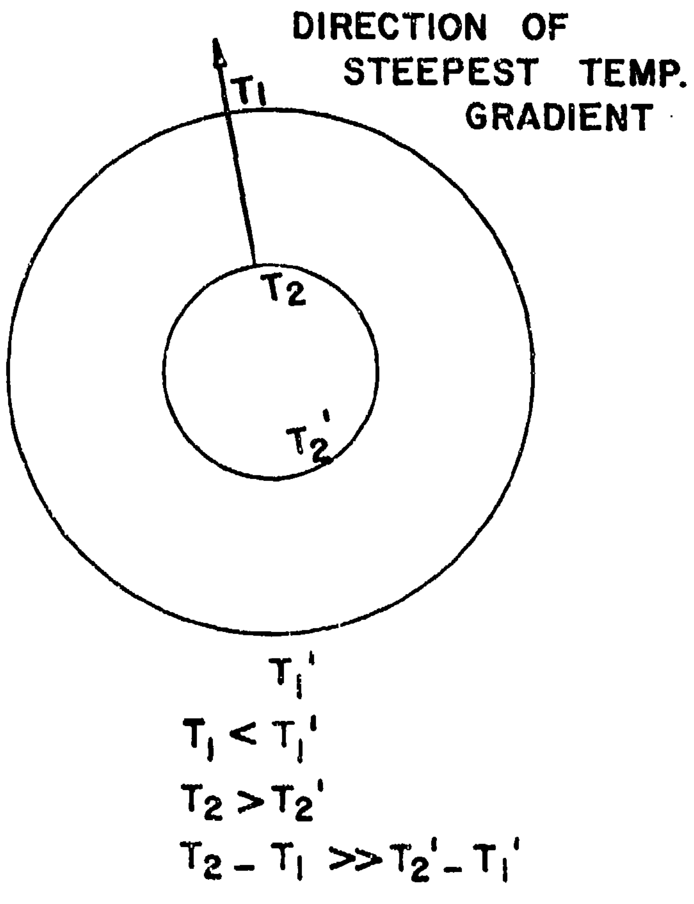

FIGURE 8. SCHEMATIC OF TRANSVERSE SECTION INDICATING TEMPERATURE GRADIENTS

GUN PROPELLANT HEAT TRANSFER AND BARREL TEMPERATURE MEASUREMENTS

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ABSTRACT

This paper will discuss aircraft cannon temperature transducer development conducted at the Interior Ballistic Laboratory of Eglin AFB. The desired goal was to develop a thermocouple which would, with microsecond response capability, provide the bore surface temperature during the course of the interior ballistic cycle. The internal transducer design and typical experimental output will be described. Correlation between measured and experimental barrel temperature profiles will be discussed.

INTRODUCTION

The purpose of this paper is to discuss the analytic and experimental determination of in-bore aircraft cannon barrel temperatures generated during the course of the interior ballistic cycle. Accordingly, the topic is divided into three sections. The first is a simplified mathematical analysis of the heat transfer process. The second is a discussion of the details of a microsecond response bore surface temperature gauge, while the final section covers the correlation of analysis to experiment for a typical spectrum of conventional and advanced propellants. This report is presented as work in progress rather than a completed effort. More work needs to be done in both boundary layer analysis and gauge development.

One of the classical problems of interior ballistics has been the quantitative determination of the amount of heat transferred from propellant combustion gases to gun barrel steel. The development of this data is of central importance from a standpoint of both understanding the interior ballistics cycle and the mechanisms of thermal gun barrel erosion.

Historically, for analytic interior ballistic purposes, a Hirschfelder (Reference 1), BETA type approach has been used to "lump" the friction and heat loss terms. This approach has been extensively employed, not as a result of generalized intellectual lethargy, but rather due to its being a reasonably accurate and intuitively attractive approximation. In any event, the description of ballistic cycle thermal phenomena by highly mathematical approaches such as those discussed in (Reference 2) have, in the past, been of questionable utility due to the lack of appropriate experimental tools to evaluate the quantitative predictions. The goal of the effort conducted during the last several years in this area at Eglin AFB has been twofold. The first of the tasks addressed was to develop a microsecond response in-bore temperature transducer to provide the necessary experimental verification of theory. The second was to couple a simplified analytic heat conduction temperature predictive algorithm to an engineering type interior ballistic program such as that described in (Reference 3). The thrust of this analysis was to explicitly compute the heat loss rather than allow it to remain implicit in the BETA term.

This report, then, will discuss these tasks sequentially. First covered will be the simplified heat transfer analysis.

Heat Transfer Analysis:

The analytic treatment of the heat loss from the propellant gases to the gun barrel steel requires a significant amount of physical properties and thermal parameter data for the gun system. If a Nusselt number heat transfer approach is desired, the amount of transport property data requirement for the involved systems appears at first to be absolutely prodigious.

For the propellant gas system the necessary data is the density, velocity, temperature, viscosity, thermal conductivity and specific heat of the gas mixture. For the gun barrel steel the thermal conductivity and specific heat is required. The actual analysis consists of three parts. That is, an interior ballistic solution to determine the free stream gas properties, and a numerical radial heat conduction solution for the barrel thermal transport. These two solutions are coupled by the Nusselt number approximate solution for the boundary layer energy transport which provides both the interface temperature and mathematical heat flow boundary condition for the steel conductivity solution.

As the thermal heat transfer program is coupled to an interior ballistic solution, such as that described in (Reference 3), many of the gas properties such as velocity, density and temperature are known. The unknown transport properties are thus the gas viscosity and conductivity. These can be computed from a free energy thermo-chemical program, (such as that described in Reference 4 by means of Leonard-Jones potential technique 5).

To determine the thermophysical properties of high strength alloy steels, normally used for gun barrels, is also not as simple as it might seem. An examination of the literature of ballistic cycle heat transfer is interesting in that there exists an enormous variability of values for the quoted thermal properties of steel. (Reference 6) provides an excellent source for thermal diffusivity and conductivity values for a large variety of high strength steels. It is important to note that for many steels these constant values are not at all constant, but are rather, very strong functions of the instantaneous steel temperatures.

Providing that appropriate care is taken with equation stability, the mathematics of heat transfer through the barrel can be quite nicely handled numerically through standard approaches such as that discussed in (Reference 7).

If a Nusselt number approximate solution is attempted, a clear conception of the physics of situation must be retained as, to a degree, some of the systems mechanistic phenomena becomes implicit with this type approach.

Formal approaches to this problem such as those discussed in (Reference 2), address the boundary layer momentum integral and usually result in sets of coupled partial differential equations which are solved by Lax Wendroff or other numerical techniques. Frequently the free stream conditions, which define the instantaneous flow behind the projectile, are addressed by means of a method of characteristic approach. As the interior ballistic program used in this case is of an engineering nature, rather than rigorously mathematical, approximate Nusselt number techniques are both fitting and a more appropriate manner to determine the convective coefficient h.

At any given point in time during the ballistic cycle, the gas properties are a function of barrel location. The central approximation made is that the gas velocity is logarithmic. That is, a zero velocity exists at the breech and the flow velocity at the base of the projectile is that of the projectile. With the flow profile as illustrated on Figure 1 and a known projectile velocity, the gas velocity V_G at all points in the barrel is uniquely determined.

(The experimental data justification for this type profile is presented in Reference 8) With the interior ballistic solution providing the static values of the gas density ρ_T and the flame temperature T_T the local values ρ_G and T_G are determined in a quasi-isentropic manner as per Reference 9. It is to be noted that the local gas density is not constant but strongly a function of velocity. The speed of sound a is defined as

$$a = (g\gamma RT/Mw)^{1/2} \quad \text{with} \quad R = 2780 \ \frac{\text{ft-lbs}}{\text{lb} \ {}^{\circ}\text{K}}$$

also the mach number is defined

$$M = \frac{V_g}{a}$$

then

$$\frac{T_G}{T_T} = \left(1 + \frac{\gamma - 1}{2} M^2\right)^{-1}$$

$$\frac{\rho_g}{\rho_t} = \left(1 + \frac{\gamma - 1}{2} M^2\right)^{\frac{-1}{\gamma-1}}$$

With the local gas velocity, density and temperature determined as above, the gas viscosity and thermal conductivity are determined by means of free energy techniques per Reference 4. Reference 10 provides the following relations.

The Reynolds number based on gun tube diameter is:

$$R_e = \frac{\rho v d}{\mu}$$

and the Prandtl number is:

$$P_R = \frac{\mu_g}{K_G} C_p$$

It has been experimentally determined that an empirical heat conduction description of flow processes can be frequently defined as some function of R_e + P_R. This correlating factor is the Nusselt number N. By definition the convective coefficient is related to the Nusselt number by

$$N \cong \frac{hD}{K}$$

with the rate of heat flow

$\dot{Q}$ defined as

$$\dot{Q} = hA\,(T_g - T_s)$$

and

$$\dot{Q} = \frac{NKA}{D} (T_g - T_s)$$

Thus the Nusselt number will provide the heat transfer flow data from the propellant gas to the gun barrel. It remains only to develop an empirical relationship for the Nusselt number from the calculated Reynolds and Prandtl numbers.

That is

$$N = f \ (R, P)$$

Extensive experimental correlations are published in the literature 10) 11) 12) in the form

$$N_P = C_1 R^b P^d$$

McAdams describes in detail in reference 12) the empirically determined nature of the N, R & P relationship. He shows that for $P > 0.1$ $b = .8$ For a typical gun propellant combustion gas system

$$P \approx .75$$

For highly turbulent fully developed smooth tube fluid flow the most widely quoted other constant values 11, 12) are

$$C_1 = 0.023$$

and

$$d = 0.4$$

To account for the flow entrance region increase in heat transfer, experimental studies 11) have indicated the necessity to include the entrance coefficient K_E factor in the Nusselt equation. This is given by the ordinate value in figure 2. The abscissa value is distance down the gun barrel in diameters.

The majority of required data is known. The value of $b = 0.8$ is well established and the value of $d = 0.4$ is relatively unimportant as with $P \approx .75$ virtually any fractional exponent will result in a final value very close to unity.

The Reynolds numbers usual for the gas conditions which exist in the gun are in excess of 5 million. The constant value of .023 is the result of smooth pipe data collected for fluids and gases flowing in basically a steady state condition. The aircraft cannon system is significantly different. It is rifled and thus macroscopically rough rather than smooth. The duration of the total ballistic cycle is typically less than 5 milliseconds and at no time approaches anything like steady state. Thus there exists no real basis to anticipate an identically equivalent premultiplication term. In fact, utilizing the Nusselt approach the best correlation with experimental data was found with the constant value $C_2 = 0.05$. Then finally for the case of a gun the Nusselt correlation becomes

$$N_G = K_e \, C_2 \, R^{.8} \, P^{.4}$$

This then provides the necessary data required to solve for the heat flow to the gun barrel. The heat transfer problem into the barrel is described below and is approached by standard numeric techniques. The interface between the solid and gas systems is provided by the gun barrel surface temperature T_S.

A somewhat subtle but significant conceptual point to remember is that the convective coefficient approach used here computes the thermal heat transfer through the boundary layer. This $\dot{Q}$ is then related to the heat transferred to the gun barrel on a conservation of energy basis. The important point is that at the barrel wall the boundary conditions require that the gas temperature equals the steel temperature and no thermal discontinuities exist. This assumption may not be completely valid early in the ballistic cycle.

HEAT TRANSFER THROUGH THE GUN BARREL

The numerical solution to this problem is through finite difference techniques. The approach used here closely follows that of reference 7). The basic time variant heat conduction equation in radial co-ordinates is

$$\frac{\partial \Theta}{\partial t} = \alpha \left(\frac{\partial^2 \Theta}{\partial \gamma^2} + \frac{1}{\gamma} \frac{\partial \Theta}{\partial \gamma} \right)$$

This analysis starts with the Taylor Series Approximations

$$\mu\,(x + h) = \mu(x) + h \frac{du}{dx} + \frac{h^2}{2!} \frac{d^2\,\mu}{dx^2} + \frac{h^3 d^3\,\mu}{3!\,dx^3} + \ldots$$

$$\mu\,(x - h) = \mu(x) - h \frac{du}{dx} + \frac{h^2}{2!} \frac{d^2\,\mu}{dx^2} - \frac{h^3 d^2 \mu}{3!\,dx^3} + \ldots$$

then combining 1 & 2 and dropping higher order terms

$$\left.\frac{du\,(x)}{dx}\right|_x = \frac{\mu\,(x + h) - \mu(x - h)}{2h} + 0(h)^2$$

or alternatively the second derivative may be obtained by:

$$\left.\frac{d^2\,\mu(x)}{dx^2}\right|_x = \frac{\mu\,(x - h) + \mu\,(x + h) - 2\,\mu\,(x)}{h^2} + 0(h^2)$$

The higher order terms can be ignored without the generation of numerical instabilities in equations of the form of those equations.

above, provided that the distance step and time step are selected such that

$$\frac{\alpha \, \Delta t}{(\Delta x)^2} < 1/2$$

The α term is the thermal diffusivity of the gun barrel steel and has the value of

$$\alpha = \frac{k}{C_p \rho}$$

with the independent properties being the thermal conductivity, specific heat and density of the gun barrel steel.

The numeric solution within the barrel is considered as two cases. The first condition applies to all internal points in the barrel which have conductive thermal input and output. (While the second is the special case which considers the surface layer that has convective heat input and conductive heat outflow). It is assumed all conductive heat flow is radial. This is valid as the radial thermal gradients are orders of magnitude larger than those in a longitudinal direction.

The gas flow behind the projectile is postulated to have a logarithmic profile as per the dots on Fig 1). This is in contrast to the commonly accepted linear gradient illustrated by the dashed line. The non dimensional velocity of the projectile during the ballistic cycle is defined by the solid line and labeled "Historic Plot". As stated above, Reference 8) discusses in-bore gas gradients in detail.

HEAT CONDUCTION IN GUN BARREL STEEL

$$\frac{\partial \Theta}{\partial t} = \alpha \left(\frac{\partial^2 \Theta}{\partial \gamma^2} + \frac{1}{\gamma} \frac{\partial \Theta}{\partial \gamma} \right) \tag{1}$$

Let $\Theta = \Theta m,n$ with m being a space interval reference and n a time interval reference

$$\frac{\partial \Theta}{\partial \gamma} \cong \frac{\Theta m + 1,\ n - \Theta m,\ n}{\Delta \gamma} \tag{2}$$

in the space interval m to M + 1

Also

$$\frac{\partial \Theta}{\partial \gamma} \cong \frac{\Theta m,\ n - \Theta m-1,\ n}{\Delta \gamma} \tag{3}$$

in the space interval m-1 to m

And

$$\frac{\partial \Theta}{\partial \gamma} \cong \frac{\Theta m + 1,n - \Theta m -1,n}{2\Delta \gamma} \tag{4}$$

in the space interval M-1 to m+1

Then

$$\frac{\partial^2 \Theta}{\partial \gamma^2} \cong \frac{1}{\Delta \gamma} \left(\left[\frac{\Theta m + 1,\ N - \Theta m,n}{\Delta \gamma} \right] - \left[\frac{\Theta m,n - \Theta m-1,n}{\Delta \gamma} \right] \right) \tag{5}$$

Also

$$\frac{\partial \Theta}{\partial t} \cong \frac{\Theta m,n + 1 - \Theta m,n}{\Delta t} \tag{6}$$

Then equation (1) becomes

$$\frac{\alpha}{\Delta \gamma} \left(\frac{\Theta m + 1,n - 2\ \Theta\ m,\ n + \Theta m-1,\ n}{\Delta \gamma} + \frac{\Theta m + 1,n - \Theta m-1,\ n}{2\gamma} \right)$$

$$= \frac{\Theta m,\ n + 1 - \Theta m,\ n}{\Delta t} \tag{7}$$

or with $M = \frac{(\Delta\gamma)^2}{\alpha\,\Delta t}$ (8)

$$\Theta_{m,\,n+1} = \frac{(\Theta_{m+1,\,n}) + (-2\,\Theta_{m,\,n}) + (\Theta_{m-1,\,n})}{M}$$

$$+ \frac{\alpha\Delta t}{2\gamma\Delta\gamma}\left(\Theta_{m+1,\,n} - \Theta_{m-1,\,n}\right) + \Theta_{m,n} \tag{9}$$

As $\gamma \gg \Delta\gamma$ the first term will be of primary importance.

In accordance with the discussion provided in reference (7) the value of M must be equal to 2 or greater to avoid oscillatory instability. For the special case of the first station at the barrel interface we assume again $\gamma \gg \Delta\gamma$

Also Q to barrel $= hA\,(\Theta_F - \Theta_{1,N})$ (10)

As $\frac{\partial\Theta}{\partial\gamma} = \frac{-Q}{KA} = \frac{-h}{K}\,(\Theta_F - \Theta_{1,N})$ (11)

Then for the barrel surface

$$\frac{\partial^2\Theta}{\partial\gamma^2} \simeq \frac{1}{\Delta\gamma}\,\frac{\Theta_{m+1,\,n} - \Theta_{m,\,n}}{\Delta\gamma} + \frac{h}{K}\,(\Theta_{F,N} - \Theta_{1,N}) \tag{12}$$

and

$$\frac{\partial\Theta}{\partial t} = \frac{\Theta_{1,\,n+1} - \Theta_{1,\,n}}{\Delta t} \tag{13}$$

then

$$\Theta_{1,\,n+1} = \Theta_{1,\,n} + \frac{\Delta t\,\alpha}{\Delta\gamma}\;\frac{\Theta_{m+1,\,n} - \Theta_{1,\,n}}{\Delta\gamma} + \frac{h}{K}\,(\Theta_{F,N} - \Theta_{1,N})$$

$$+ \frac{\Delta t\,\alpha\Delta\gamma}{\Delta\gamma\gamma}\;\frac{\Theta_{m+1,\,n} - \Theta_{1,\,n}}{\Delta\gamma} \tag{14}$$

The problem now remains to generate a value of the convective co-efficient h in terms of the properties of the propellant gas system. To this end the definitions of the Prandtl, Reynolds and Nusselt numbers are:

$$\text{PRANDTL \#} = \frac{C_p \mu}{K} = P_r$$

$$\text{REYNOLDS \#} = \frac{VD\rho}{\mu} = Re$$

$$\text{NUSSELT \#} = \frac{hD}{K_F} = N\mu$$

The values of C_p, μ and K for the propellant gas can be obtained by means of a Leonard-Jones potential technique appended to a free energy minimization thermochemical program run on the particular formulation for the gun propellant used. This approach is discussed in Reference (4).

As mentioned above, the values for the propellant gas velocity V and density come from interior ballistic computations conducted for the gun firing parameters as used. These techniques are covered in Reference (3). It then remains only to formulate an expression for the Nusselt number as some function of the Prandtl and Reynolds numbers.

We then have all the relationships which are required to approach a simplified heat conduction solution to a typical gun interior ballistic problem.

TRANSDUCER DEVELOPMENT

The requirements for an interior ballistic temperature measuring system are varied and severe. The temperature range capability must be from ambient to approximately 1700°K. The response time must be on the order of microseconds and the pressure forces which the gauge has to withstand are in the range of 50,000 psi.

Thus, the requirements on the transducer are not only stringent but to a degree they are seemingly contradictory. To withstand high pressures on a repeated basis, the thermocouple housing must be relatively massive and strongly designed. However, to provide a very fast signal response, which is necessary for the adequate thermal definition of a ballistic cycle that has a duration of only 3 to 4 milliseconds, a very small thermal mass thermocouple junction is mandatory.

The basic design of the thermocouple used to simultaneously satisfy these requirements is shown by Fig 3 with the operational device illustrated by Fig 4.

This thermocouple design has been undergoing a joint development effort between NANMAC Corp and the AFATL Interior Ballistic Laboratory during the course of the last four years. The primary problems have been the design of the junction itself and the ability of the gauge to withstand repeated exposures to gun pressures.

The sensor assembly of the thermocouple is formed by two wires flattened to a thickness of 0.001 inches. The wires (Pt and Pt - 10% Rh or Tungsten and Tungsten - 26% Rhenium) are insulated from each other and the thermocouple case by phlogopite mica sheets of 0.0002 inches thickness. This "sandwich" of wire and mica is inbedded in a split pin and pressed into a matched tapered hole of the thermocouple case. The ability to withstand the applied pressure during operation depends on the matching of the "sandwich" to the tapered hole. Fig 5 shows an example of a thermocouple which malfunctioned during its first exposure to gun pressure. Although the junction assembly was swaged into the tapered hole with a force equivalent to 50,000 psi the thermocouple did not withstand 30,000 psi before the junction "sandwich" was pushed into the thermocouple case. The major requirement is therefore that the insertion forces be exactly balanced on each side of the "sandwich" support structure to insure the achievement of an absolute parallel gap. This is done by precise matching of the sensor assembly into the tapered hole of the thermocouple case. Newly designed thermocouples with parallel gaps were swaged into the steel body by a static force equivalent to 70,000 psi. Although these thermocouples withstood a dynamic pressure up to 50,000 psi, the majority malfunctioned during firings as a result of an electrical short or embedded junctions inside the thermocouple body. Frequently a thermocouple junction subsequent to the firing of several shots at the 30,000 psi level would fail due to an electrical short inside the thermocouple. To eliminate this type malfunction, it was proposed to fill the space inside the thermocouple with a flexible insulator. This was done to avoid internal junction gas penetration and increase the rigidity of the transducer assembly. It was expected that this type of thermocouple would withstand at least 10 shots at pressures in excess of 50,000 psi. The thermal junction of the thermocouple is formed by grinding across the sensing tip. This action flows micron size metal particles across the compressed mica insulation, thereby insuring the formation of the required thermocouple junctions. To remake the junction in used thermocouples, it was necessary to polish the junction with successively coarser grades of abrasive cloth until the electrical resistance dropped to approximately 2 ohms or less. At that point a thermal junction is made with a response time of about 50 μ sec. To maintain the rapid time response, it was necessary to keep the sensor surface clean and abrade the junction after each shot. A later model thermocouple, has had the junctions offset and overlapped to decrease the "sandwich" junction thickness from .005 inches to .0035 inches. Another design approach has been to make the junction across the steel face of the thermocouple rather than the thermocouple ribbons. This will allow the thickness of the "sandwich" to decrease to .015 inches, which may reduce the possibility of gas flowing around the mica insulation and into the sheath area causing an embedded junction or internal shorting.

The status of the transducer development and design is such that adequate time response and accurate results are now available. Gun firing life times of the transducer are still unacceptable at exposures to 50,000 psi in-bore pressures. The basic problem appears to be the formation of embedded junctions due to high pressure deformation of the housing. Future work will concentrate on improving the physical strength and thus lifetime of the transducer. In general, of the two available thermocouple materials, platinum and tungsten, the tungsten provides the best results.

RESULTS

It was stated in the introduction that both the mathematical model of heat transfer and the transducer design need further refinement. The thermocouple currently provides accurate data but lacks durability. The model of heat transfer is oversimplified at the point of initial exposure of unheated barrel surface. This can be seen in Figures 6, 7, and 8 which illustrate the correlation of the analysis with the thermocouple output for different types of propellant. These figures show typical results for nitramine, triple base and single base propellant respectively. It is seen that the experimental and theoretical chamber pressure versus time correlations are very good in all cases. Additionally, the predicted peak single shot bore surface temperature for the nitramine and triple base propellants is reasonably good, however, the experimental temperature rises and falls more rapidly than the analytic predictions. This temperature is plotted at the point of maximum heat transfer, about 3.5 inches from the start of rifling. Figure 9 illustrates the boundary layer model utilized. The central and unavoidable postulate implicit in a Nusselt type approach is that the temperature of the inner boundary layer gas next to wall is that of the wall. This assumption is good for pipe heat flow but less adequate for the highly transient phenomena of gun ballistics. Methods of modifying this model deficiency without the introduction of an unacceptable array of arbitrary parameters will be investigated in the future. The temperature measured in the case of the single base propellant is everywhere higher than the theoretical predictions and will have to be further investigated.

It is interesting to note that the theoretical total ballistic heat loss predicted is very close to the level established by the Hirschfelder Beta approach. In conclusion, it is reasonable to state that a final solution to the determination of real time bore surface temperature during the ballistic cycle is not yet at hand. However, the experimental and mathematical tools to address the problem are well on their way to development.

PROPELLANT PROPERTIES

	Single Base	Triple Base	Nitramine	Units
F_p	338,000	355,000	375,000	$\frac{\text{ft-lbf}}{\text{lbm}}$
γ	1.246	1.252	1.266	
M_w	23.4	22.2	19.6	
T_f	2873	3010	2700	^{o}K
C_p	.44	.45	.486	$\frac{\text{cal}}{\text{gm-}^{o}\text{K}}$
μ	.00070	.00074	.00066	$\frac{\text{gm}}{\text{cm-sec}}$
κ_{gas}	.00034	.00034	.00038	$\frac{\text{cal}}{\text{cm-sec-}^{o}\text{K}}$

GUN PROPERTIES
GUN STEEL

κ	.138	$\frac{\text{cal}}{\text{cm-sec-}^{o}\text{K}}$
α	.1	$\frac{\text{cm}^2}{\text{sec}}$
Gradient depth for prog	.002	cm
ρ	8.89	$\frac{\text{gm}}{\text{cm}^3}$
C_p	.156	$\frac{\text{cal}}{\text{gm-}^{o}\text{K}}$

TABLE I

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COMPARISON OF VARIOUS VELOCITY ALGORITHMS

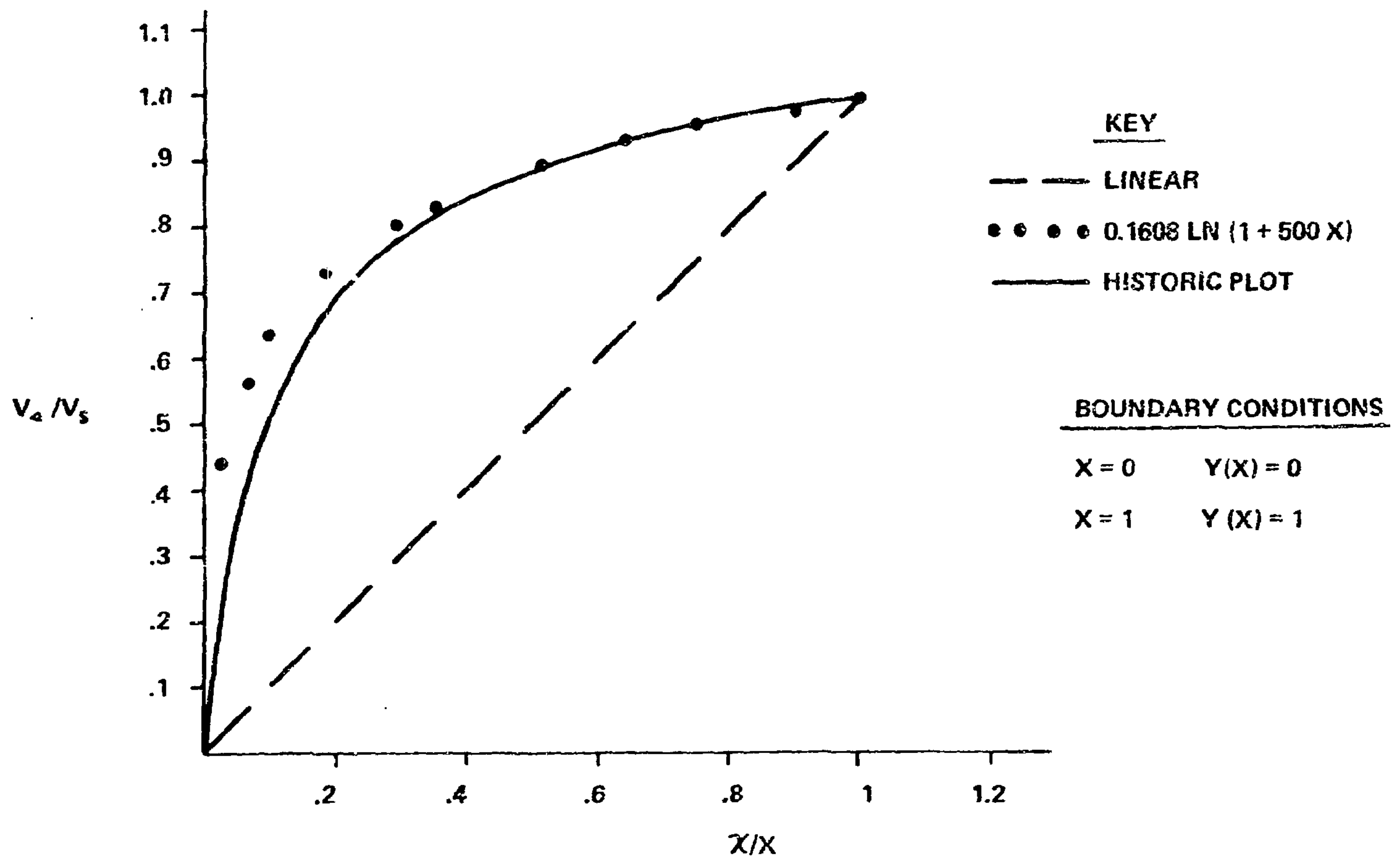


Figure 1.

VELOCITY GRADIENT

ENTRANCE FACTOR

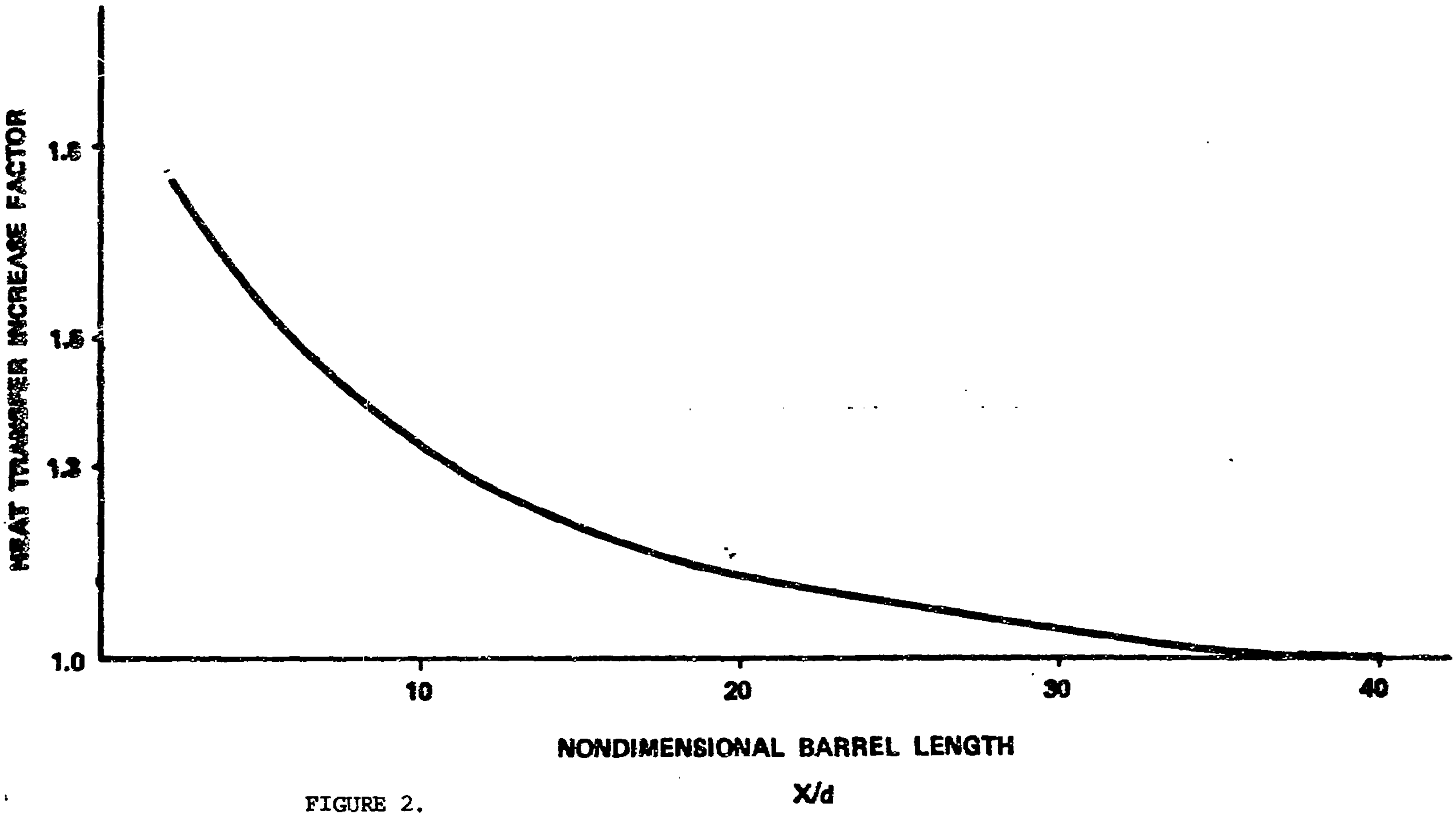


FIGURE 2.

THERMOCOUPLE SCHEMATIC

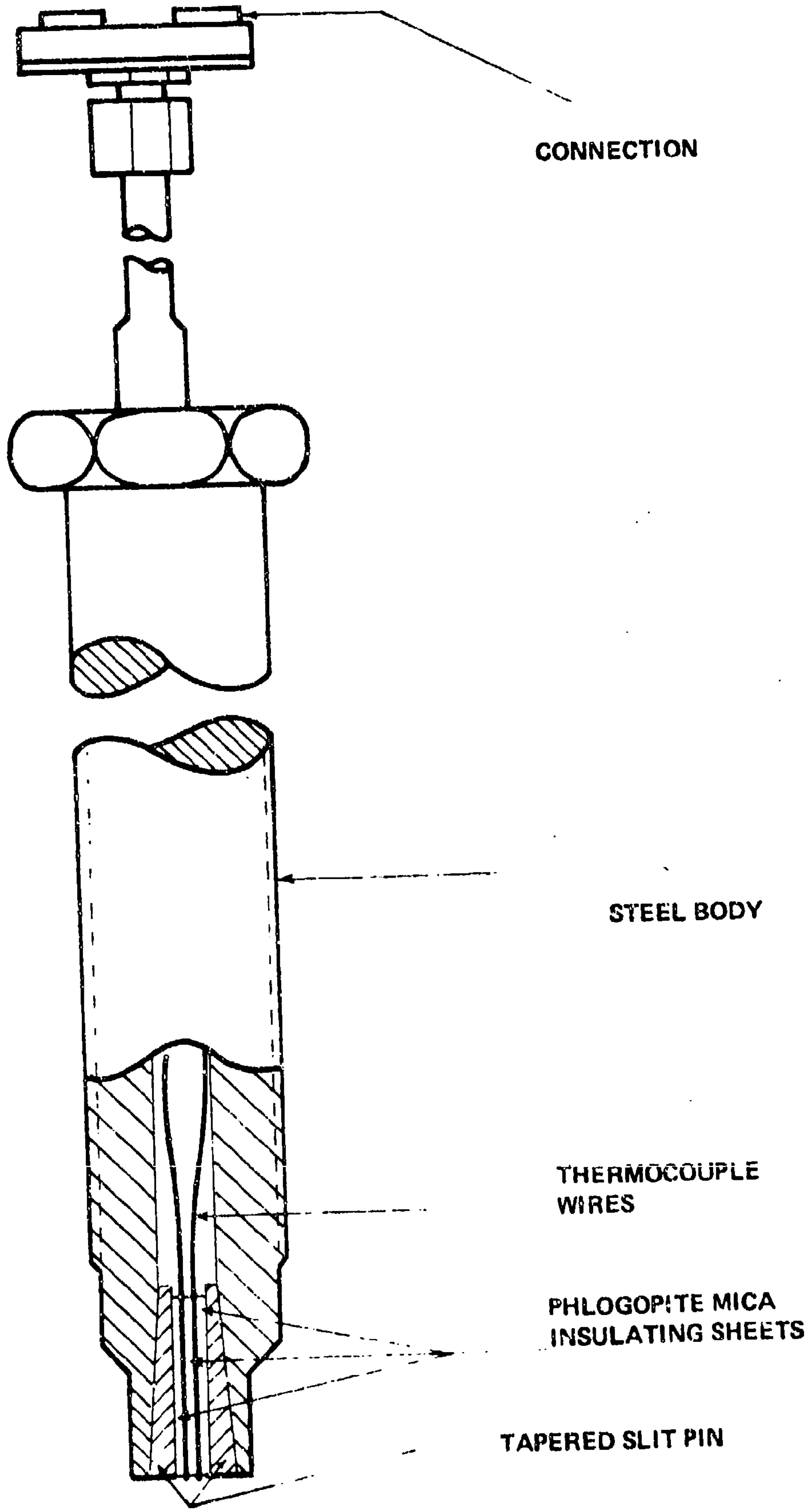


FIGURE 3.

LATEST THERMOCOUPLE DESIGN

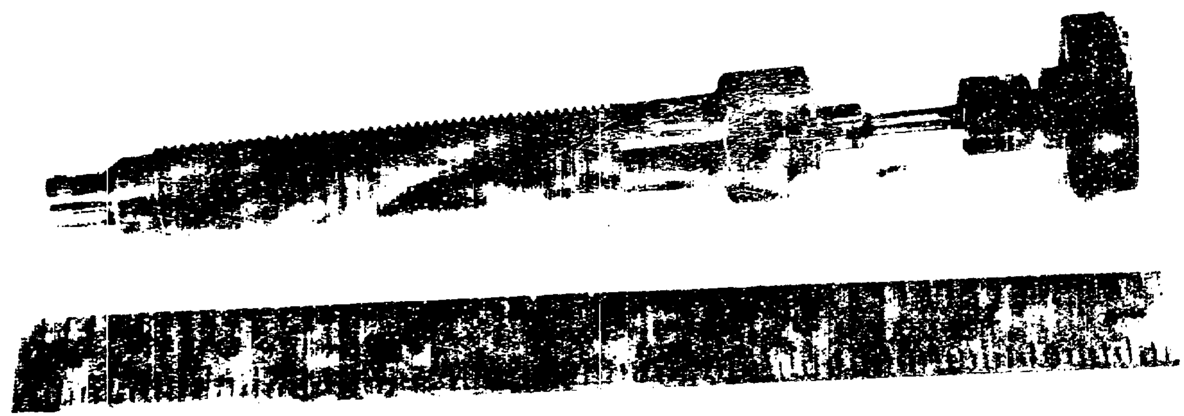
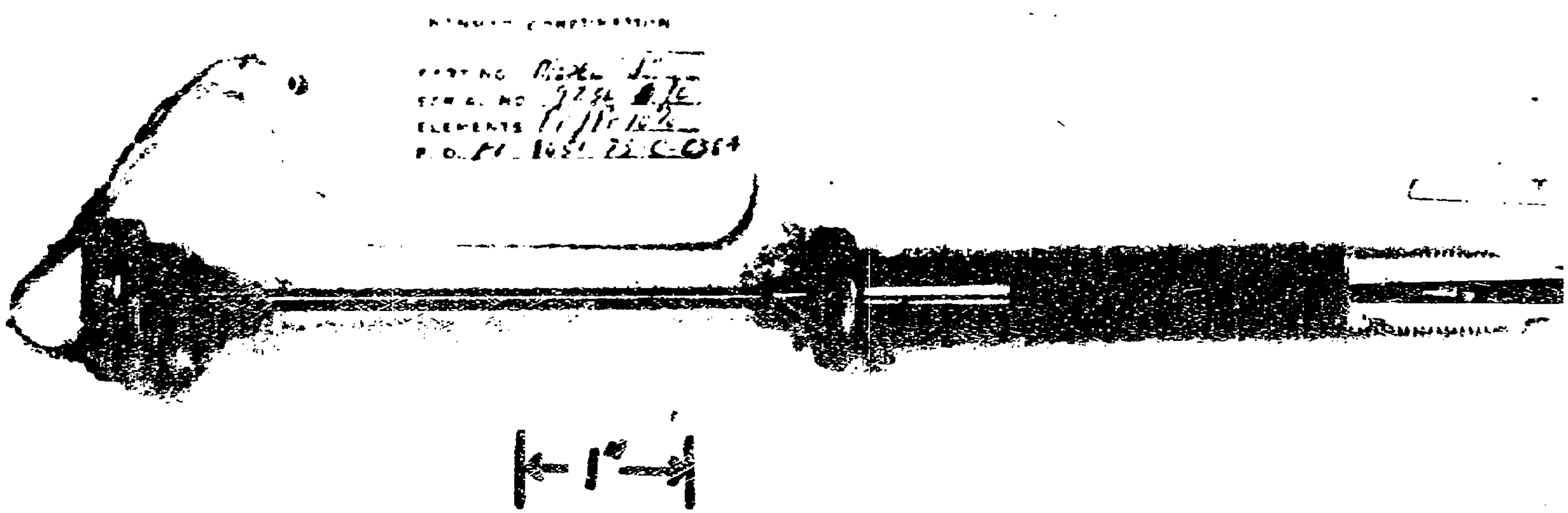


FIGURE 4.



EARLY THERMOCOUPLE - SENTINEL

FIGURE 5.

NITRAMINE PROPELLANT

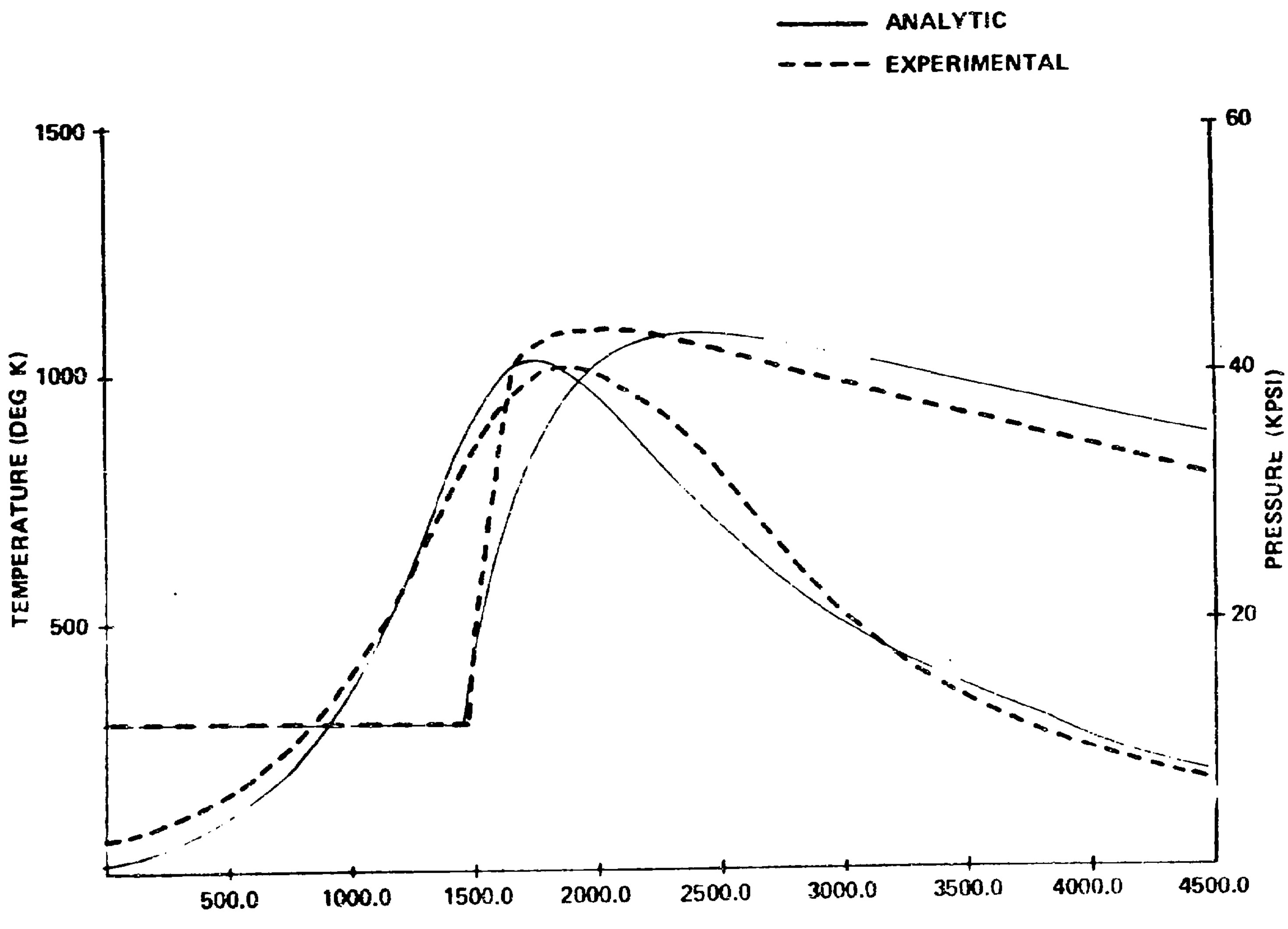


FIGURE 6.

TRIPLE BASE PROPELLANT

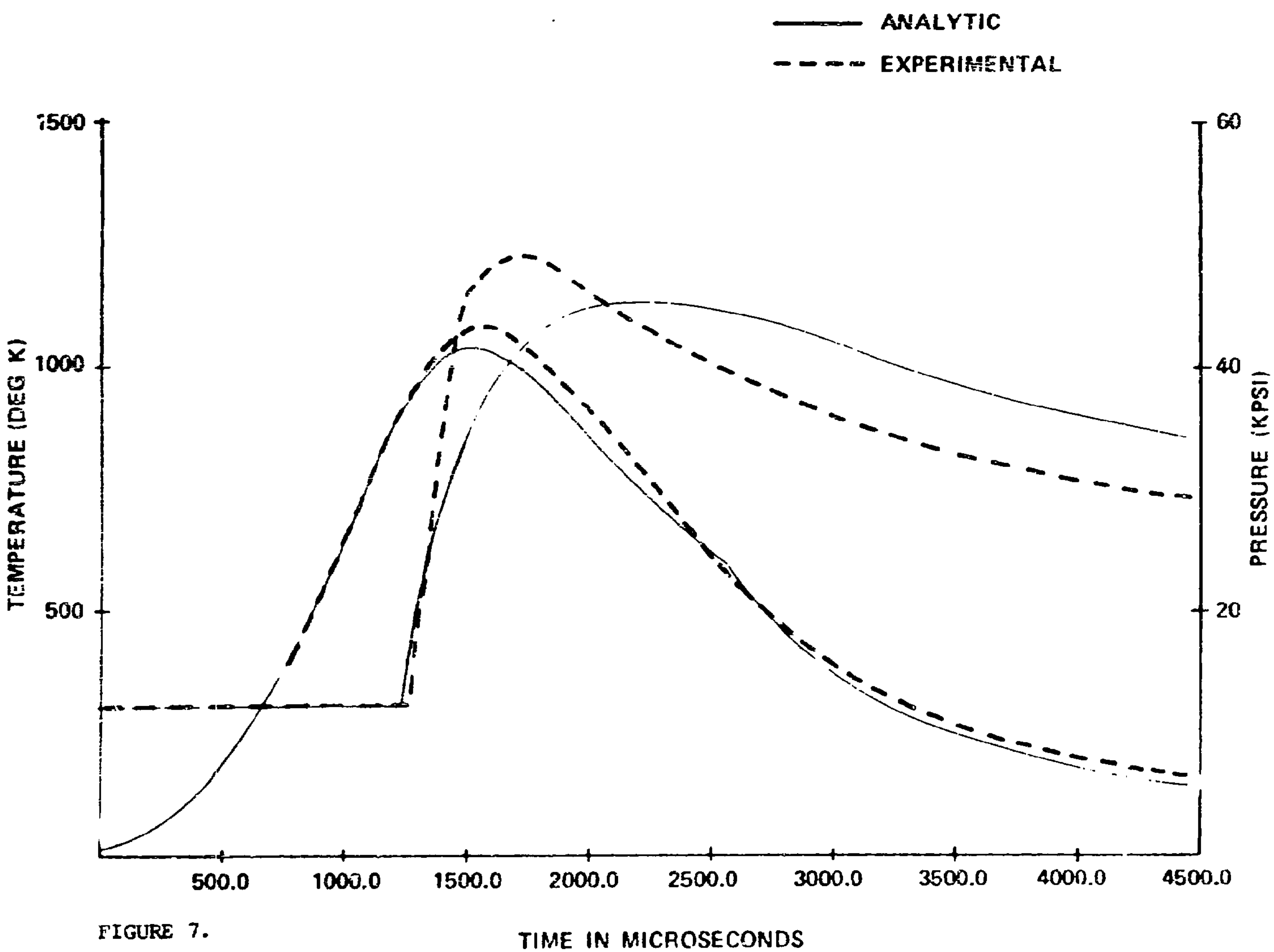


FIGURE 7.

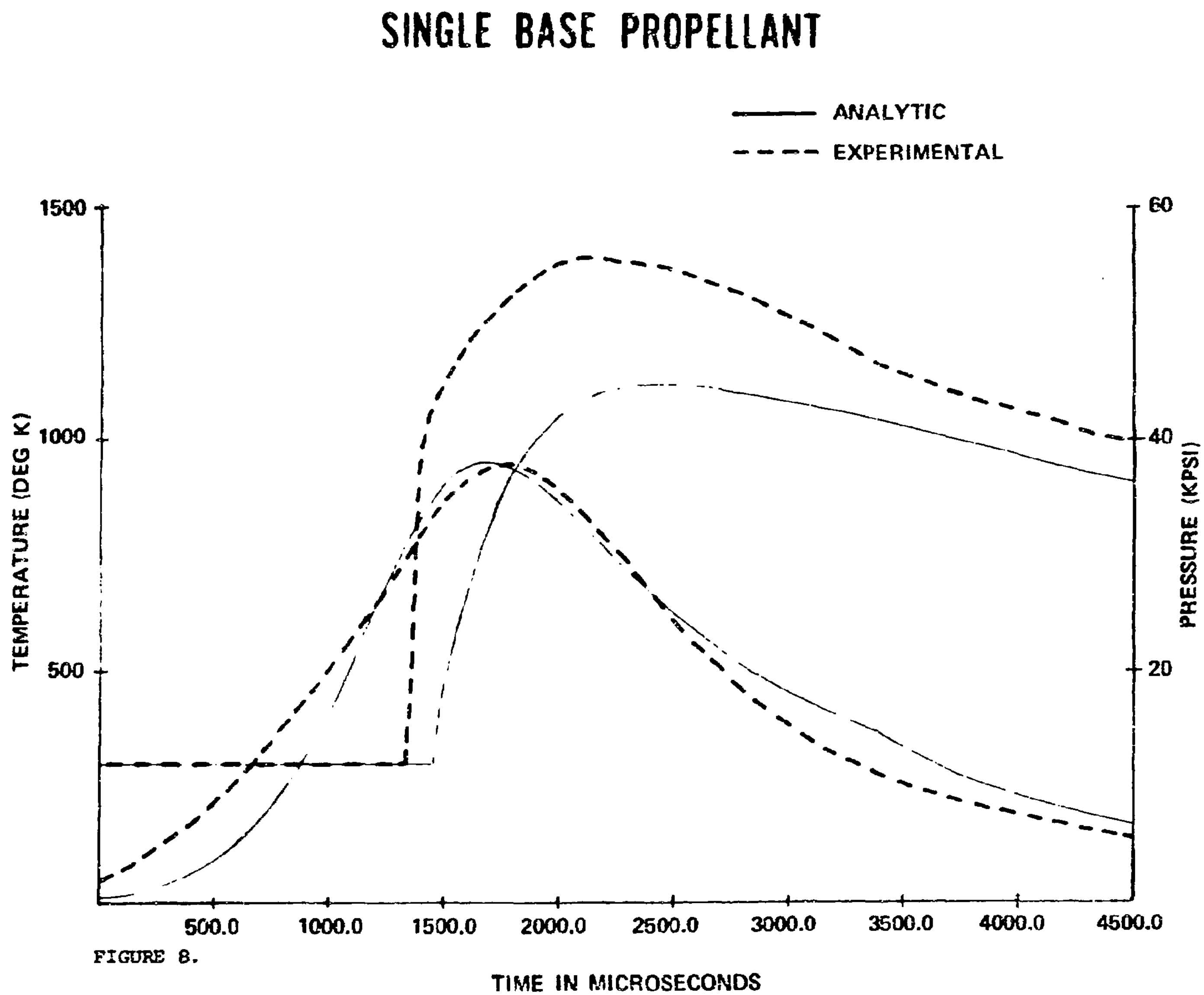

SINGLE BASE PROPELLANT
ANALYTIC
EXPERIMENTAL
1500
1000
500
TEMPERATURE (DEG K)
60
40
20
PRESSURE (KPSI)
500.0
1000.0
1500.0
2000.0
2500.0
3000.0
3500.0
4000.0
4500.0
TIME IN MICROSECONDS

FIGURE 8.

BOUNDARY LAYER MODEL

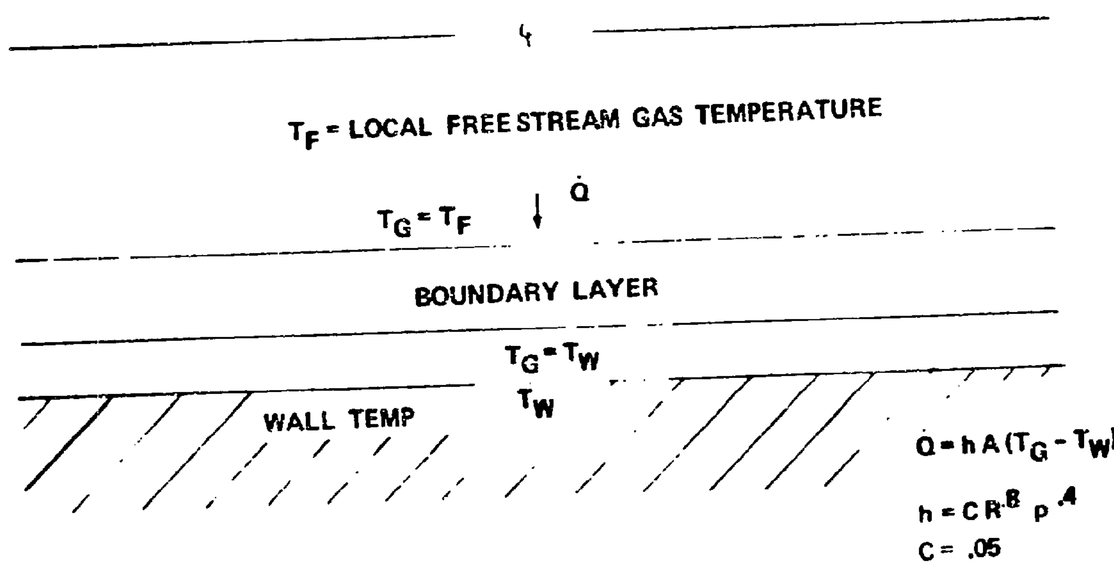


FIGURE 9.

THE SHOCK TUBE GUN

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ABSTRACT

Full scale tube instrumentation and field test evaluations although of great value in final performance testing of improved charges, should be preceded by a more efficient and cost effective approach to optimization of propelling charges. Realizing the need for a test facility applicable to the study of propellant/tube interactions, Calspan, through internal funding has designed, fabricated and tested a unique variable gun system, the operation of which is based upon shock tube principles (Shock Tube Gun). This paper describes briefly the design and makeup of the Calspan Shock Tube Gun and presents the results of a very preliminary exploratory study of the influence of oxygen on tube erosion.

INTRODUCTION

In order to determine the causes of gun tube erosion and to be able to assign at least qualitative values to these causes, it is necessary to conduct variation studies of those parameters involved in the firing process. Studies of this type consist in part of varying each parameter independently of the others and observing the effect on erosion. This procedure is difficult to follow in the actual gun system because independent variation is either too costly or not possible and, therefore, insufficient variations can be made resulting in limited data and unclear correlations. Hence, studies involving variation of parameters are best conducted in a laboratory environment where experiments are performed using a device that allows control over the desired independent parameter variations. The Shock Tube Gun is one such device. Briefly, this device generates a high pressure, high temperature test gas by a polytropic compression process. As shown in Figure 1, the facility consists of a driver chamber, a driven tube, a flying piston, a gas collection chamber and an instrumented gun tube containing a projectile. Compression of the test gas (the counterpart to the propellant gas in an actual gun) within the driven tube is accomplished by motion of the flying piston contained within this tube. Motion of the piston is the result of force applied by the pressure of the driver gas. As the piston approaches the collection chamber, the test gas pressure and temperature rapidly increase in a time history representative of

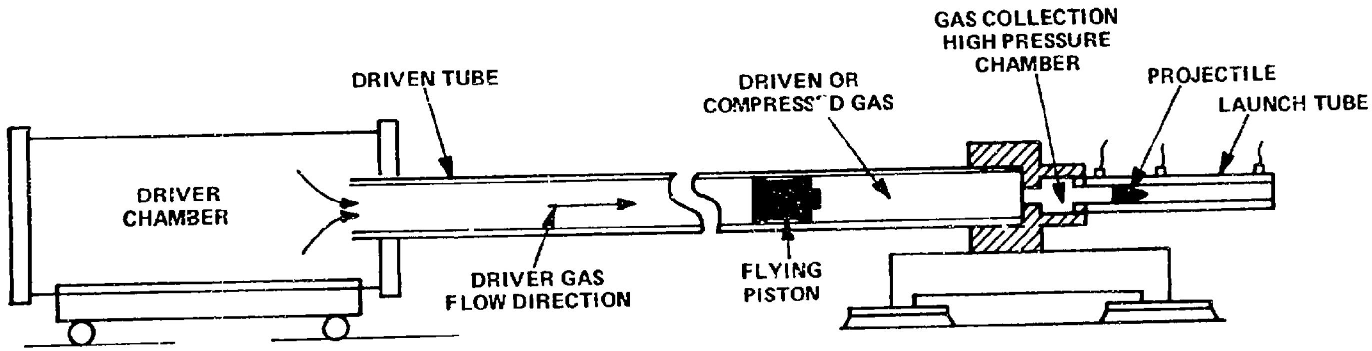


Figure 1 SHOCK TUBE GUN SCHEMATIC

actual gun firings. A projectile contained at the start of the gun tube is acted upon by this collected gas. Suitable means are provided in the facility to allow regulation of shot start pressure on the projectile. Once released, the projectile is accelerated along the tube in a ballistic cycle dependent upon selected input factors. Data regarding pressure, velocity, heating, and erosion are collected through measurements in the instrumented test barrel.

With suitable variation in parameters, factors affecting erosion such as pressure history, propellant gas velocity, gas temperature, gas composition, tube composition, and propellant additives may be investigated. The compression ratio, driven gas composition and its initial conditions essentially govern the peak temperature and pressure; piston motion, influenced by its mass and projectile movement effectively govern the pressure pulse duration. Variation of piston mass, initial conditions, and compression ratio then permit independent change in peak temperature and pressure as well as time. Obviously, the effect of change of driven gas composition may be tested under controlled interior ballistic conditions. This is a most powerful experimental mode of operation of the facility. Other variable items can be readily explored in specific tests, such as rotating band design and the relationship between obturation and erosion.

SHOCK TUBE GUN DESIGN AND CONSTRUCTION

In the Calspan design efforts, design requirements were selected in an attempt to adequately represent ballistic conditions within the eight-inch howitzer. As taken from Reference 1, Figure 2 illustrates typical interior ballistics for this weapon at Zone 9. Table I further lists some ballistics and heating measurements for firing conditions from Zone 3 to Zone 9. Design goals were then to permit testing at peak chamber pressures up to 40,000 psi and at projectile velocities up to 2500 ft/sec. Table II lists the structural makeup of the Shock Tube Gun required to satisfy this goal. These values were established through the use of a preliminary mathematical model of piston action based upon adiabatic compression. Through exercise of the model, approximate size requirements were established with final selection of sizes dictated by available engineering materials The photographs of Figure 3 illustrate the resulting Shock Tube Gun assembly as well as its individual components. As shown, the projectile launch components consist of the driven tube, chamber and 30mm smooth bore barrel. These are supported on a carriage which is free to move on tracks in the direction of piston motion. The tracks allow movement of the carriage during the extreme impulse loads imposed by the unbalanced chamber pressure during test gas compression, thus maintaining integrity of the supporting base structure. In early firings conducted without installation of the tracks,

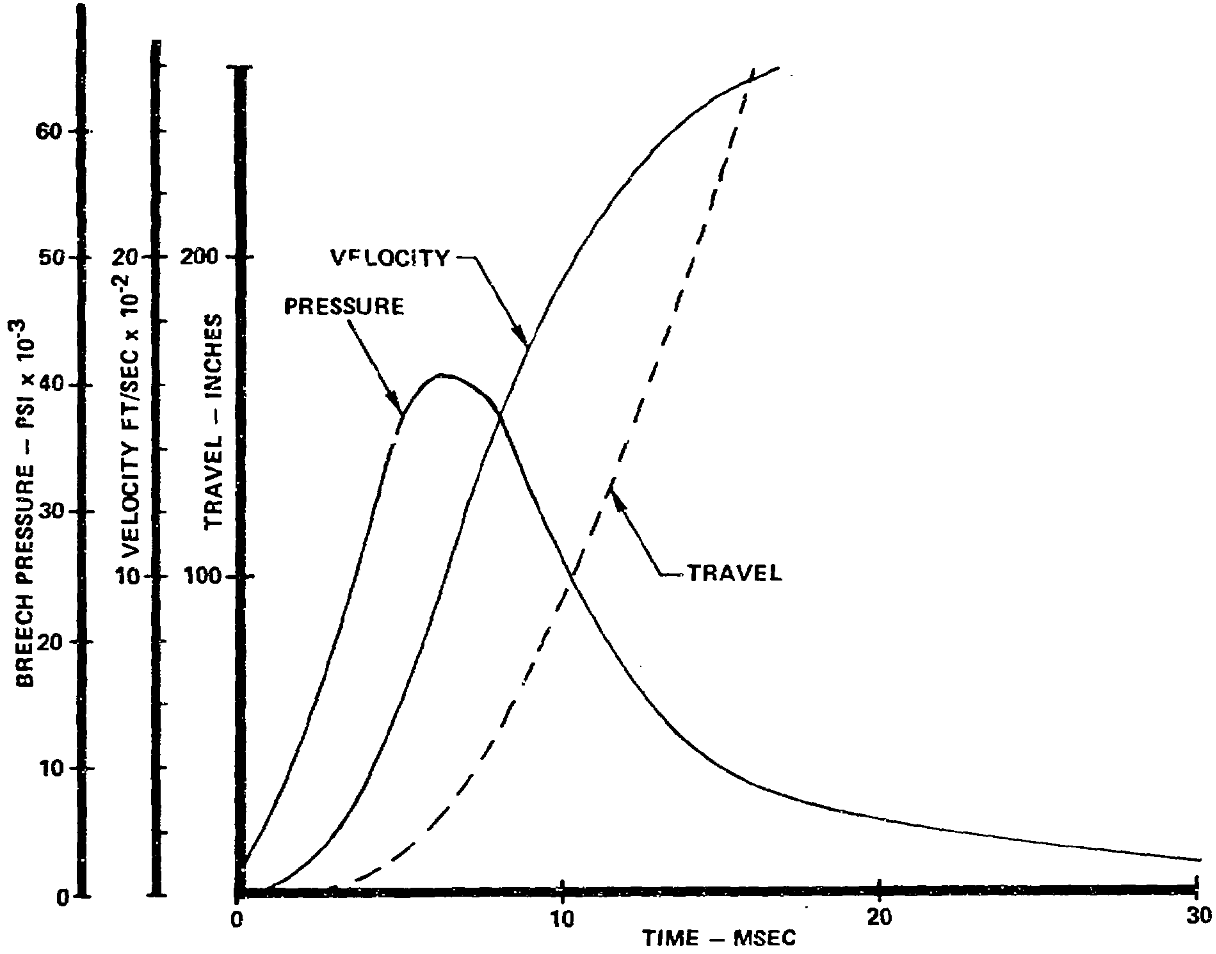


Figure 2 INTERIOR BALLISTICS OF EIGHT INCH HOWITZER XM188 ZONE 9

Table I

REDUCED FIELD TEST DATA

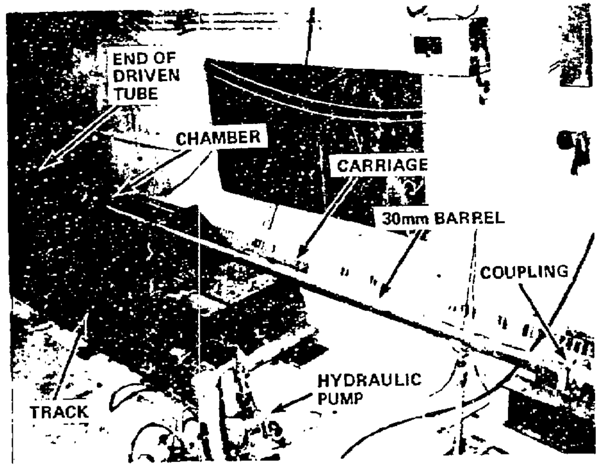
TEST NO.	CHARGE ZONE	PRESSURE psi	VELOCITY ft/sec	HEAT INPUT AT GIVEN DISTANCE - FROM MUZZLE - Btu/ft^2				COMMENT
				273.5	213.5	144	8	
1	3	12,600	1,011	–	–	–	–	CALIBRATION
2	7	31,500	1,980	–	–	–	–	CALIBRATION
3	3	12,000	1,010	62	36	32	9	LOW CHARGE
4	5	14,300	1,474	87	66	58	15	MED. CHARGE
5	7	30,100	1,980	102	84	75	21	HIGH CHARGE
6	9	41,300	2,583	160	147	126	53	NO TiO_2
7	9	41,300	2,585	160	146	117	48	NO TiO_2
8	9	41,800	2,592	149	144	–	46	NO TiO_2
9	9	41,000	2,585	153	141	114	47	NO TiO_2
10	9	40,700	2,589	157	146	118	55	NO TiO_2
11	9	41,700	2,592	158	140	118	48	NO TiO_2
12	9	41,800	2,598	159	140	111	47	NO TiO_2
13	9	41,800	2,595	155	143	115	47	NO TiO_2
14	9	41,500	2,547	142	127	103	43	TiO_2
15	9	40,550	2,551	139	127	102	42	TiO_2
16	9	41,000	2,559	130	124	100	42	TiO_2
17	9	41,000	2,559	127	125	98	43	TiO_2
18	9	40,100	2,510	125	120	97	44	TiO_2
19	9	40,000	2,568	123	119	100	43	TiO_2
20	9	41,000	2,586	134	135	109	47	NO TiO_2

TABLE II

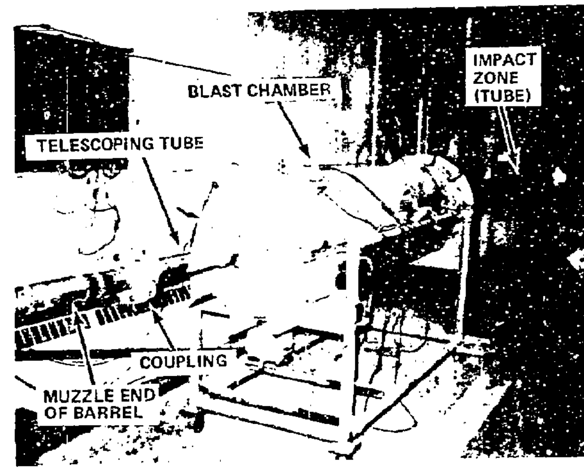
SHOCK TUBE GUN CHARACTERISTICS

Configuration Data:

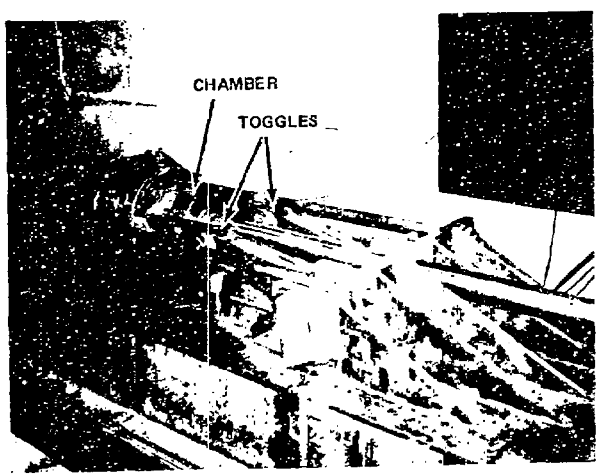
Driven Tube I.D.	7.5 in.
Driven Tube length	80.83 ft (970 in.)
Piston Area	44.179 sq. in.
Piston Weight	Up to 150 lb.
Projectile Diameter	1.181/30 in./mm
Projectile Area	1.095 sq. in.
Projectile Weight	Up to 2 lb.
Driver Volume	54,000 cu. in.
Chamber Volume	130.8 cu. in.
Pressure - at release of projectile	Variable
Tube Length	167 in.



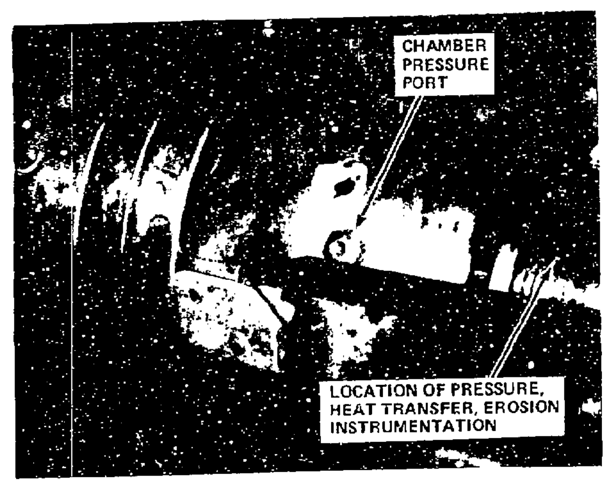
VIEW OF PROJECTILE LAUNCH COMPONENTS



VIEW OF PROJECTILE CAPTURE COMPONENTS



VIEW OF TOGGLE ARRANGEMENT



VIEW OF INSTRUMENTED AREA OF TUBE

Figure 3 SHOCK TUBE GUN (DOWNSTREAM END) splitting tensile failure of the supporting concrete structure was evidenced, thus suggesting the need for a mount having less rigidity. Following installation of the floating mount system, which requires use of a pneumatic brake on the driven tube for safety, test firings produced no further damage to the concrete.

The projectile capture components consist of a telescoping tube coupled to the barrel, a blast chamber, and a sand filled tube to decelerate the projectile (impact zone). The blast chamber, the purpose of which is to reduce the noise and pressure levels at projectile exit of the tube, also contains an internal provision for measurement of projectile velocity using velocity screens. The telescoping tube allows motion of the carriage independent of the blast chamber.

Also shown in Figure 3 are views of the chamber and toggle restraint system needed to contain the high chamber pressures and associated axial loads. Chamber pressures are sensed using piezoelectric pressure transducers. The entrance region of the launch tube can accommodate pressure, heat flux, and erosion sensing devices. The launch tube itself is a 30mm smooth bore barrel, 15 feet long.

Figures 4 and 5 illustrate two essential components of the facility. Figure 4 shows a view of the brake area at the upstream end of the driven tube. This brake limits the maximum permissible axial load on the driven tube during the rapid piston deceleration period. Without it, loads could exceed the axial strength capability of the tube. In essence, load is limited by slippage in the brake at a preselected load below the failure strength of the tube. The brake is air actuated and, thus may be adjusted according to the amount of slippage desired up to the strength limit of the tube. The brake is presently used at only about one-half capacity without excessive slippage. Hence, much greater maximum chamber pressures than presently produced can be accommodated.

A view of the piston which is used to compress test gas in the driven tube is shown in Figure 5. It is made from 4340 steel and weighs 140 lb. including the release catch at its rear end. As shown, gas seal is obtained using "T" rings at the front and rear of the piston. Three plastic wear rings are used to prevent metal-to-metal contact between piston and tube. A buffer projection on the face of the piston and a complementary close fitting port at the end of the driver tube entering the chamber preclude direct impact of the piston with the end of the driven tube in the event that driven gas is exhausted too rapidly as by a failure in the chamber. The buffer projection is necessitated by the presence of the chamber volume at the end of the driven tube which can permit piston contact at the driven tube end with insufficient chamber pressure development.

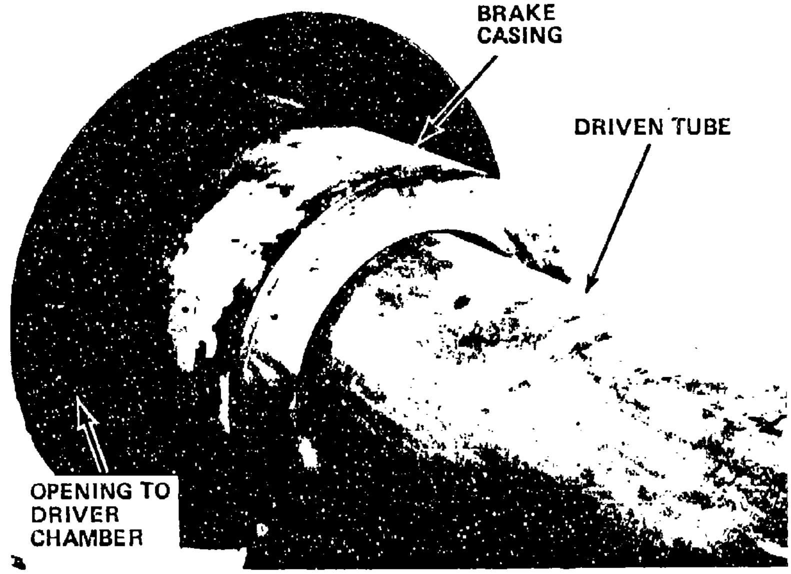


Figure 4 VIEW OF BRAKE AREA (UPSTREAM END OF DRIVEN TUBE)

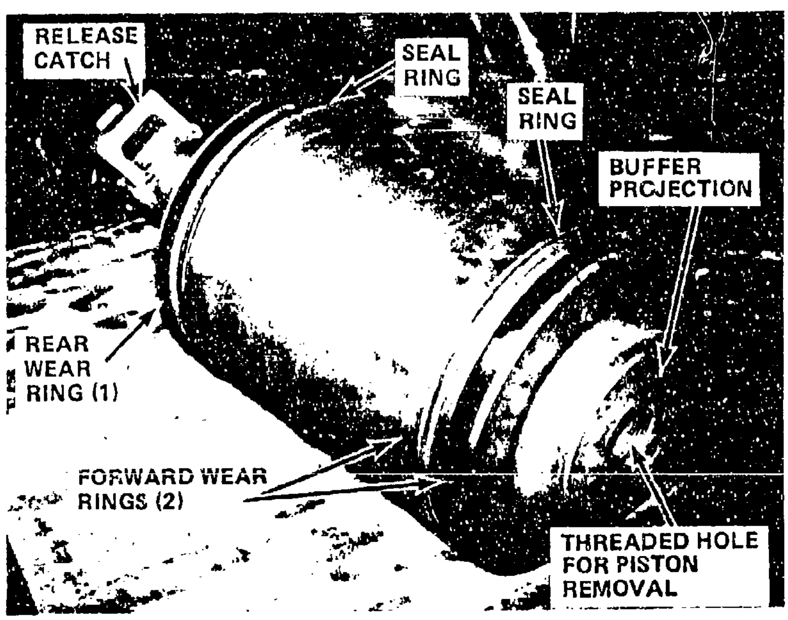


Figure 5 VIEW OF PISTON

Metal-to-metal impact if allowed to take place could cause damage to the piston and/or tube face. The buffer prevents this occurrence.

SHOCK TUBE GUN TESTING

Initial investigations were conducted for the purpose of determining Shock Tube Gun performance compared with design predictions and to provide a basis upon which an operational computer code could be formulated for use in establishing required gun settings for any desired ballistic result. These investigations were performed through Calspan funding as a part of the facility development. The computer code generated and validated through these early tests in which piston position, chamber pressure and projectile velocity were measured was brought to a state of development sufficient to permit a demonstration to be made of its versatility as a research tool. As an example of the adequacy of the computer code, Figure 6 shows a comparison of predicted and measured chamber pressure histories at a condition approximately representing Zone 7 in the eight-inch howitzer. It is clear that chamber pressure is more than adequately predicted by the computer both with regard to magnitudes of pressure and time. Using the computer code thus developed, shock tube input factors such as driver pressure, piston weight, driven gas type and pressure, and projectile weight could be chosen to result in preselected test conditions of pressure, temperature, gas velocity, heating time and gas activity at the entrance to the launch tube. These represent parameters which are important in tube heating and erosion. Figure 7 illustrates computed STG characteristics for nitrogen as a test gas. One may note that appropriate conditions of pressure, velocity, and time can be produced with this gas. Characteristics for other gases can be computed in similar fashion.

As a demonstration of its ability to act as a research tool for the study of gun tube erosion, a study of limited scope was conducted in an effort to show effects of change in test gas on erosion. Here, among other possible candidate selections, it was deemed of considerable interest to illustrate the influence of oxygen on erosion. This exploratory demonstration test was chosen because it was felt that rather significant differences in erosion would be found in a limited test program within the proven test range of the facility. Hence, driven gas effects using air and a mix by weight of 47 percent argon to 53 percent CO_2 at various pressures were determined. Air, of course, represents a severe oxidizer while that of argon-CO_2 a very mild oxidizer. Test data were derived regarding pressure, temperature, projectile velocity, heating and erosion.

Pressure and projectile velocity were measured as indicated above. Gas temperature was derived through computer results. Heating at the entrance to the tube was determined through analysis of a measured in-wall temperature as discussed in Reference 2. Erosion was measured and surface changes were determined through use

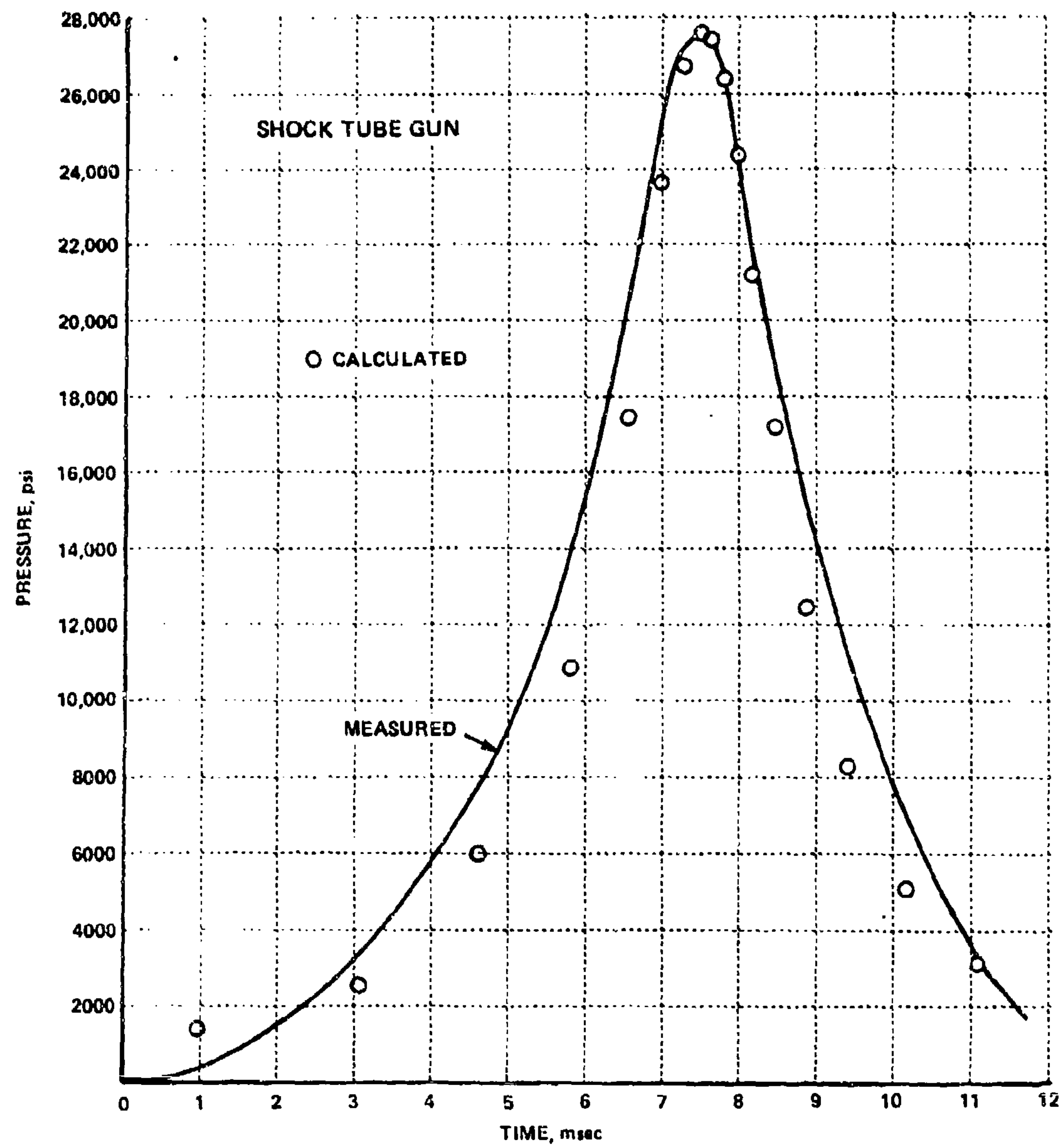


Figure 6 COMPARISON OF CALCULATED AND MEASURED CHAMBER PRESSURES – AIR

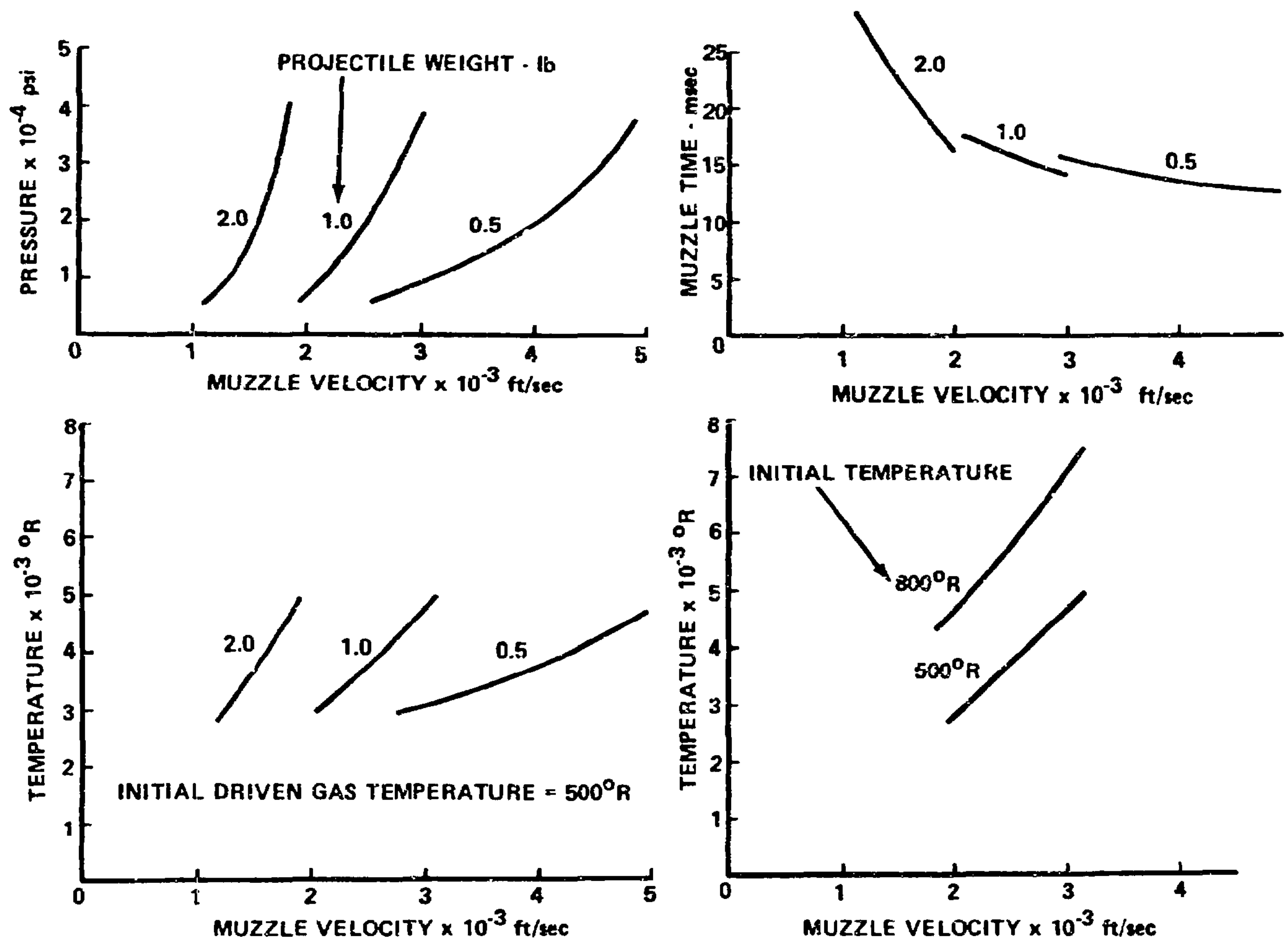


Figure 7 COMPUTED STG CHARACTERISTICS FOR NITROGEN

of removable erosion ring inserts placed at the entrance to the launch tube. These erosion ring inserts of 30mm bore size were fabricated of 4130 steel and 2024 aluminum. Ring thickness was greater than the depth of penetration of heat in the firing time. A typical erosion ring before test is shown in Figure 8. Each ring was made to contain three erosion indexing scribe marks at 120° separation on the surface to be exposed to the gas flow. Each scribe mark as well as initial surface condition was characterized by SEM photography prior to test. Sample weight and bore diameter were also recorded. Erosion experienced by the ring sensor in any test would be shown by change in any or all of these indicators.

The test procedure employed exposed selected test rings to conditions of increasing severity with continuous monitoring of data. It was expected that testing of each gas-ring combination would be terminated when excessive erosion was experienced within the proven test range of the facility. Accordingly, the 4130 steel ring inserts were initially tested with air at increasing chamber pressure. Test data derived are as shown in Table III. Clearly, the indicated oxidation and erosion is shown to be influenced greatly by gas stream conditions and associated bore heat input. With air as the driven gas, it appears necessary to exceed a heat input threshold of about 50 Btu/ft^2 to induce surface oxidation of sufficient magnitude to result in measurable surface loss of steel. At this condition, oxidation and erosion was confined to several very local areas on the ring. Evidence suggested it to have been triggered by impact from particles in the test stream which removed or lowered the resistance of the everpresent protective surface oxide on the steel. At increasing heating conditions, the number of erosion sites and their extent was found to increase rapidly with apparently less dependence on particulate impact, but still enhanced by local surface perturbations such as those imperfections resulting from the indexing scribe marks. Figure 9 illustrates the local oxidation and erosion exhibited by the 4130 ring of Test No. 7 in the vicinity of an indexing scribe mark. It is evident that outside the area of induced chemical activity, little effect of firing is indicated. Within the oxidized region, severe local melting and erosion is noted. Although oxidation potential of the gas stream in these tests using air is much greater than that available with most gun propellants, where some surface activity exists, effect of local perturbations such as surface cracking is expected to have similar influence on erosion patterns.

Figures 10 and 11 further illustrate the oxidation and erosion of the 4130 steel ring of test 7. Figure 10 shows the typical extent of erosion experienced by the ring in one shot. Figure 11 shows a magnified view of the surface "wash" at one eroded area of the ring. Oxidation at each eroded area appears to have been induced or triggered at sites where the resistance of the protective oxide is locally

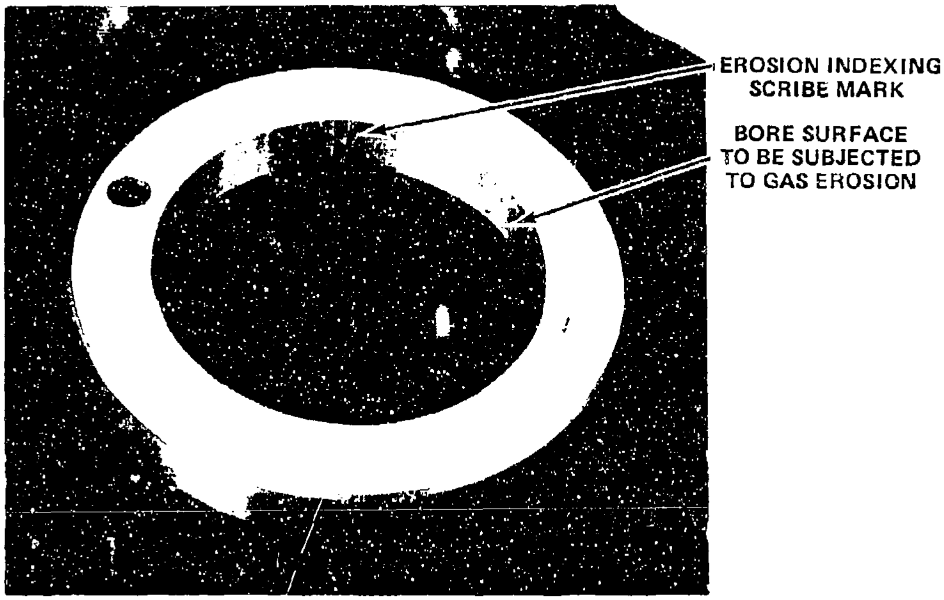


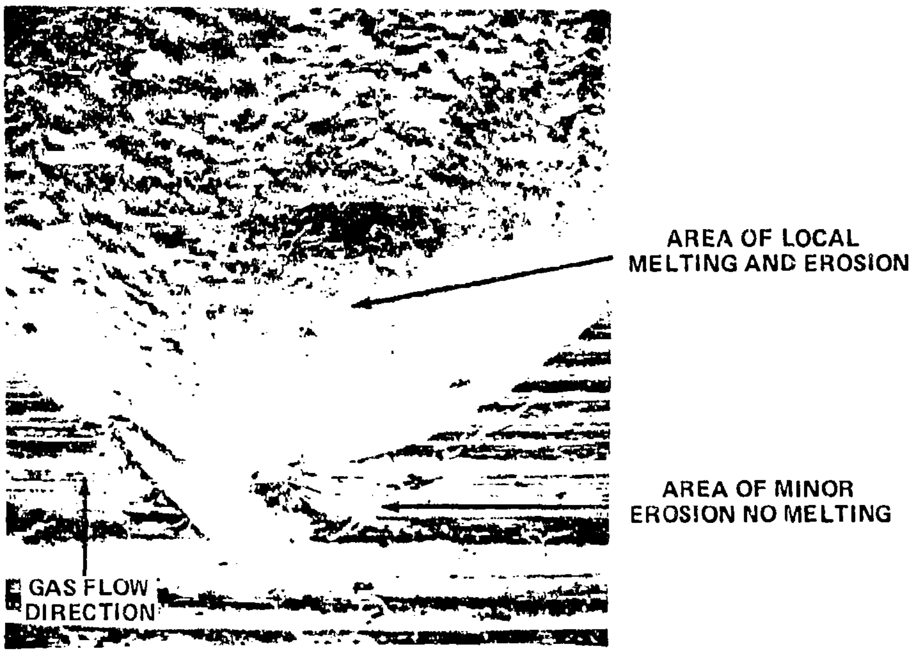
Figure 8 TYPICAL EROSION RING BEFORE TEST

Table III

SHOCK TUBE GUN EROSION TEST DATA SUMMARY

TEST	DRIVER PRESSURE psi	PROJECTILE WEIGHT lbs	DRIVEN GAS	PEAK PRESSURE psi	PEAK TEMPERATURE °F	VELOCITY ft/sec	HEAT INPUT Btu/ft^2	COMMENT
1	300	0.89	AIR	8,000	2250	1605	45	NO MEASUREABLE EROSION OF 4130 STEEL
2	350	0.91	AIR	12,300	2575	2010	52	SOME MEASUREABLE LOSS, AVERAGE ≈ 9 MICRO IN.
3	400	0.91	AIR	18,500	2910	2360	70	LOCAL EROSION ON 4130, AVE. LOSS = 15 MICRO IN.
4	425	0.9	AIR	23,000	3110	2530	71	LOCAL EROSION INCREASED, AVE. LOSS = 30 MICRO IN.
5	450	0.9	AIR	26,400	3246	2700	87	SEVERE LOCAL OXIDATION AND EROSION, LOSS = 1000 MICRO IN.
6	450	2.33	AIR	27,600	3280	1375	66	SOME EROSION LOSS = 13 MICRO IN.
7	450	0.89	AIR	26,300	3240	2720	90	NEW STEEL RING SEVERE LOCAL OXIDATION AND EROSION, LOSS = 272 MICRO IN.
8	450	0.89	ARGON-CO_2	20,000	3700	2200	70	NO MEASUREABLE EROSION ON 4130 STEEL
9	400	1.04	ARGON-CO_2	14,400	3330	2130	58	UNIFORM MATERIAL LOSS ON ALUMINUM, LOSS ≈ 450 MICRO IN.
10	425	1.16	ARGON-CO_2	17,500	3550	2150	63	UNIFORM MATERIAL LOSS ON ALUMINUM, LOSS ≈ 500 MICRO IN.

INDEXING SCRIBE MARKS
BEFORE TEST (100X)



AREA OF SCRIBE
MARKS AFTER ONE
SHOT (100X)

Figure 9 ILLUSTRATION OF EROSION INDUCED BY SURFACE PERTURBATION 4130 RING

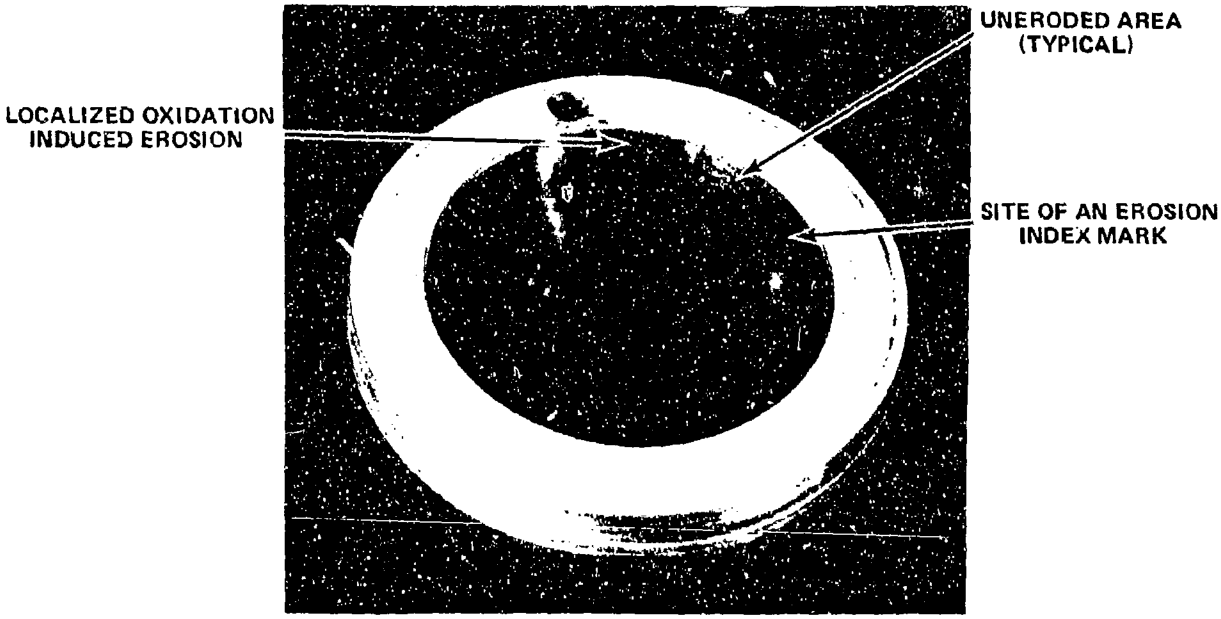
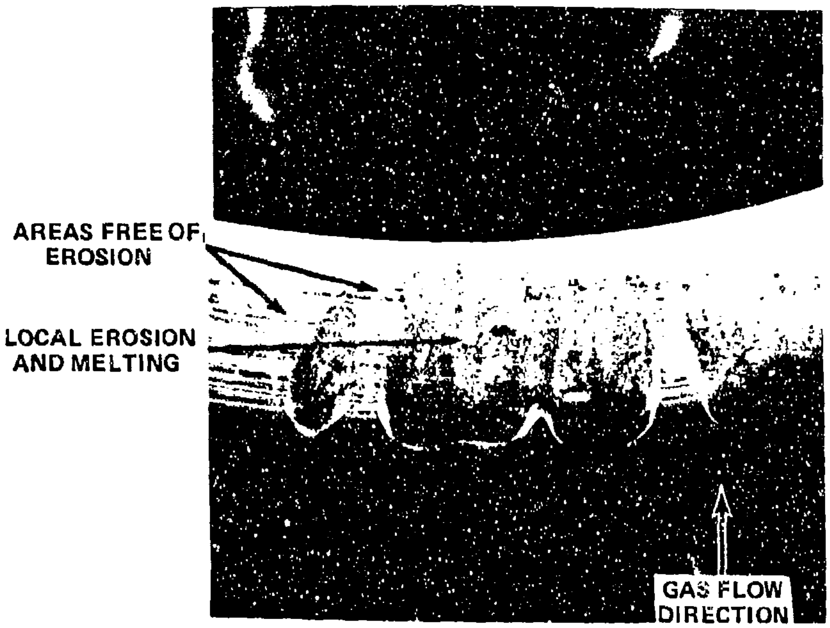
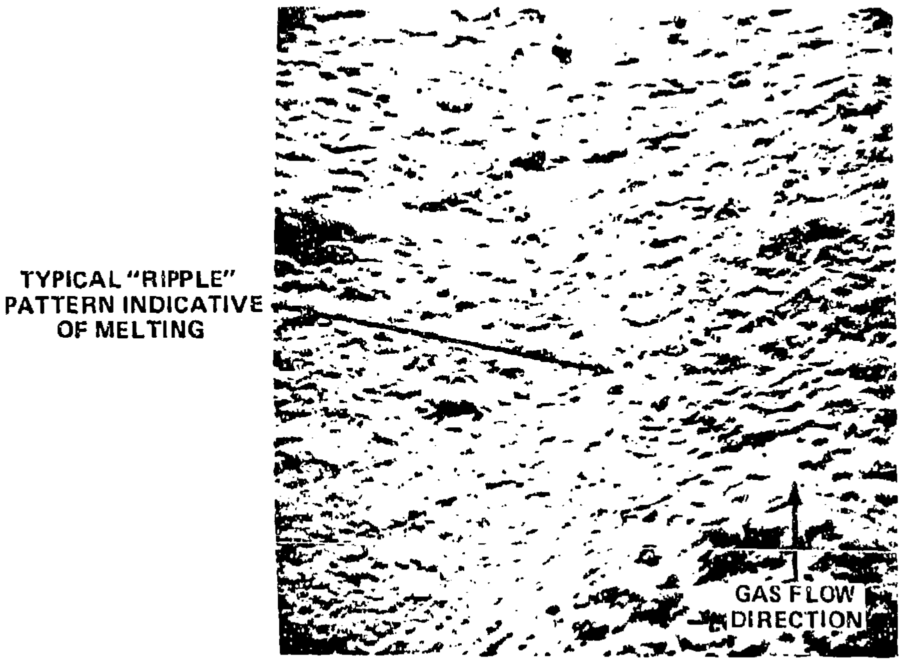


Figure 10 VIEW OF 4130 STEEL EROSION RING AFTER TEST USING AIR



VIEW OF RING AT 10X



VIEW OF ERODED AREA AT 100X

Figure 11 SURFACE CONDITION OF 4130 RING AFTER ONE SHOT USING AIR overcome. The extent of oxidation and melting increases in the downstream direction following this initial breakdown, probably the result of increased downstream heating due to oxidation combustion of the steel in the boundary layer. This is much the same as an oxy-acetylene torch cutting operation. In the eroded area, Figure 11 shows the typical ripple pattern indicative of melting and solidification. Under the conditions of test, the results of which are typified by Figures 9 through 11, an average material loss at the exposed ring surface of about 0.0003 inches in a single shot is indicated. Test results of Table III suggest the effect of multiple shots to be increasing nonlinearly as more erosion sites are generated. Based on this, one can readily conclude that oxidation potential of propellant systems must be maintained much below that corresponding to those of the limited study, if reasonable tube service life is to be maintained.

In continuation of the test plan, a steel test ring was subjected to conditions of erosion using the argon-CO_2 mix. Here, with reduction of oxidation potential, no erosion or change in the 4130 steel sensor was found. In this test, the surface heat input was measured at 70 Btu/ft^2 which is about 40 percent greater than that of the earlier air test in which measurable oxidation and erosion was first noted. Inasmuch as all significant factors (pressure, temperature, velocity and heating) exclusive of free oxygen content were greater in the test utilizing the argon-CO_2 mix, one concludes that a lowering of oxidation potential can result in superior materials erosion performance.

After reviewing the ballistic data derived in the above argon-CO_2 test and through comparisons with computer estimates, it became clear that increase of ballistic conditions for this gas mix could only be obtained with some risk of seal failure at the driven tube end. Predictions indicated the piston to be entering the buffer region at high speed, thus producing excessive pressure in the buffer volume. In this test, in fact, some gas leakage was noted as was a partial failure of the piston seal rings. Although known modifications of the facility can be readily made to overcome this limitation, time and funding constraints forced a termination of the argon-CO_2 tests on steel at the 20,000 psi level. As a demonstration that oxidation effects can, however, be obtained with the argon-CO_2 gas mixture, testing was continued using aluminum as a test ring. Because this material is more sensitive to oxidation, it was felt that it would show effects of reduced oxygen potential within the proven test range of the facility.

Consequently, erosion tests of aluminum rings were conducted at pressures of 14,400 and 17,500 psi, using the argon-CO_2 mix. Data are as given in Table III for entries 9 and 10. As opposed to the rather localized erosion noted for the steel test rings, oxidation, cracking, and erosion of the aluminum rings was found to be much more uniform over the surface of the ring. This is illustrated in Figure 12 for the test at 17,500 psi. Average material loss in this test was about 0.0005 in. As shown, there is evidence of oxidation and cracking at all areas of the ring with somewhat greater concentration at the entrance region. There is evidence of melting indicated by surface "wash" and rippling. This is further shown in Figure 13 at higher magnification in the area of a surface scribe mark. Surface oxide is clearly present as are axial surface cracks. Although original machining marks are still visible, there is apparent loss of material indicated by both rounding of the machining marks and loss of scribe mark depth.

Certainly, the importance of oxygen in the erosion process is unmistakable where sufficient activity is generated to overcome the materials natural protective surface oxide barrier. The Shock Tube Gun with minor future modification provides an effective means by which this determination can be made with relationship to recognized gun conditions. Its versatility as a laboratory research tool for investigation and mitigation of heating and erosion has been shown both by its demonstrated simulation of eight-inch howitzer ballistic and heating conditions near Zone 7 and through the brief exploratory investigation of chemical effects on erosion.

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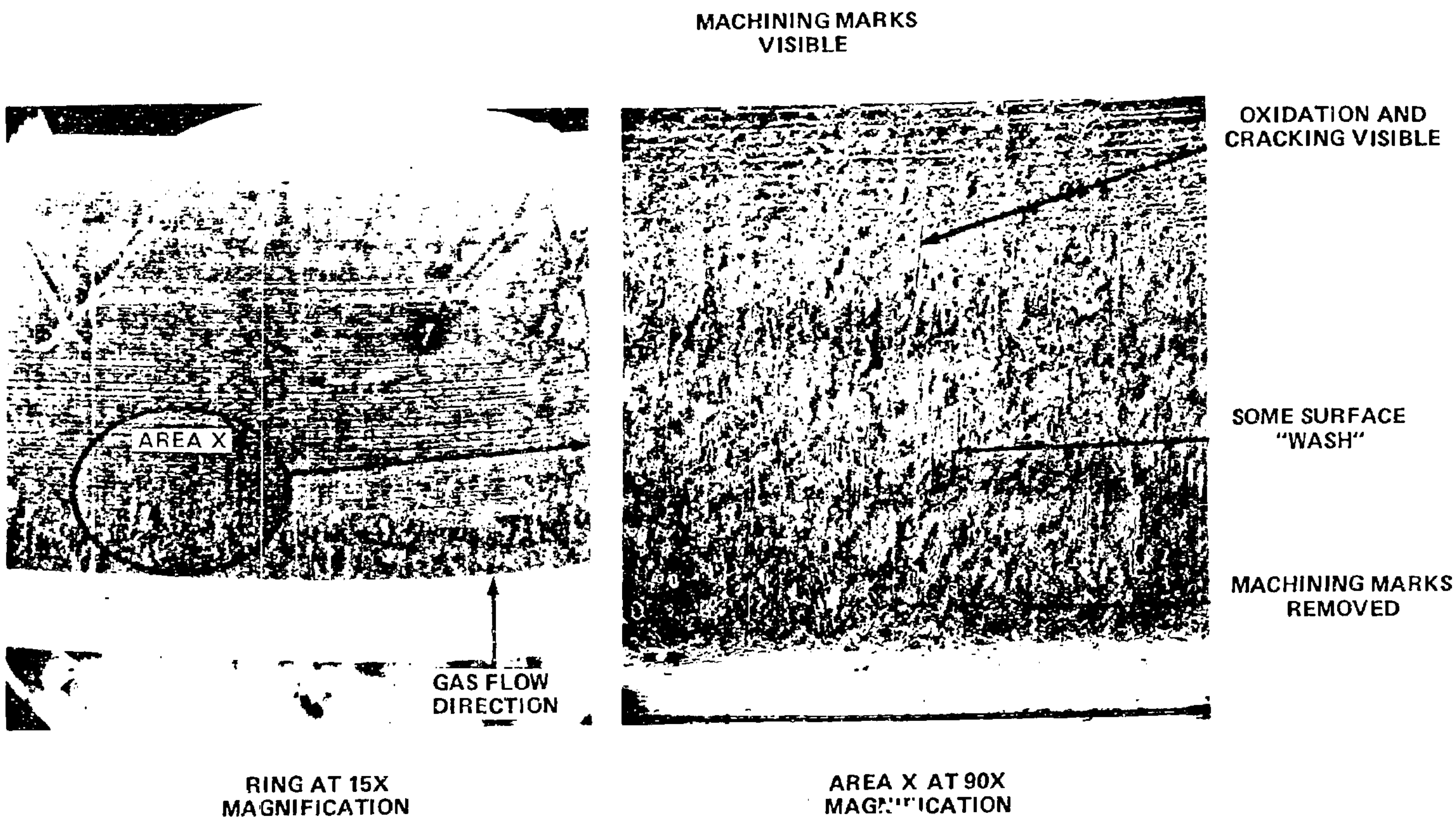
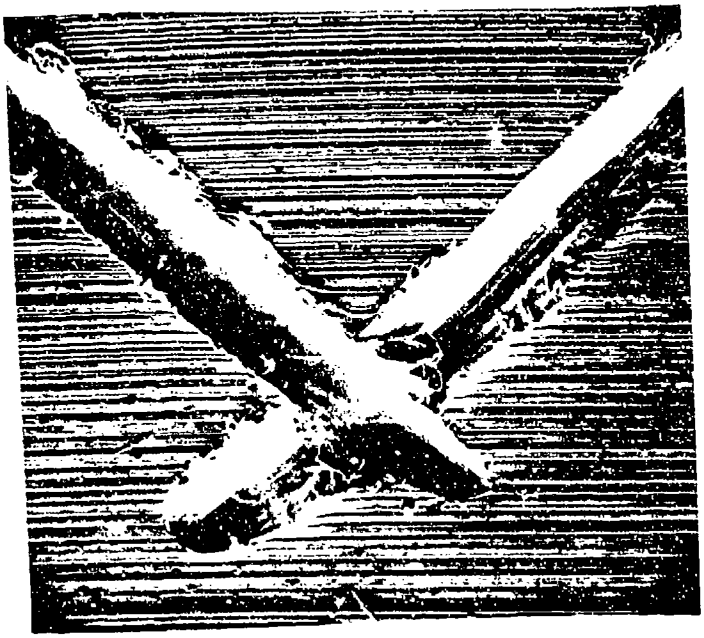
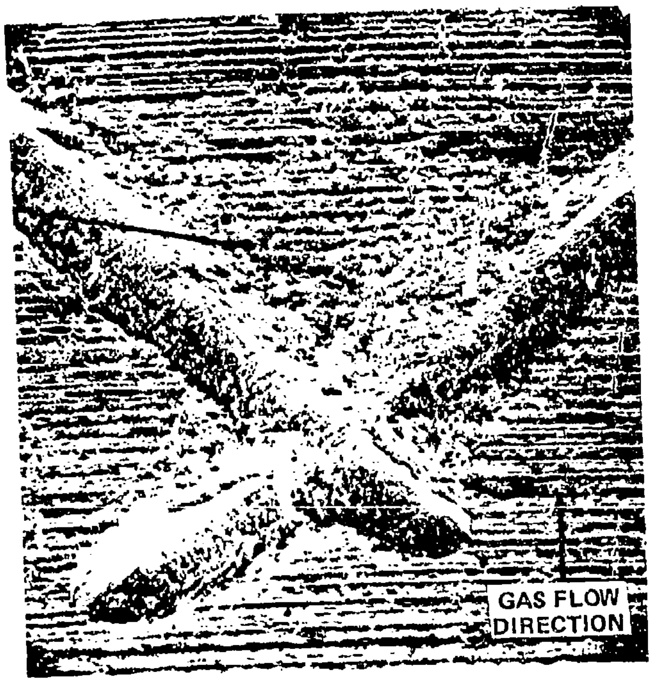


Figure 12 ALUMINUM RING AFTER ONE SHOT USING ARGON-CARBON DIOXIDE MIX



AREA OF SCRIBE MARKS
BEFORE TEST (100X)

SOME OXIDATION AND
MELTING NOTED



SAME AREA AFTER
TEST (100X)

Figure 13 SUR- ACE CONDITION OF ALUMINUM RING AFTER ONE SHOT WITH ARGON-CARBON DIOXIDE MIX

RADIOACTIVE TRACERS IN EROSION WEAR MEASUREMENTS

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Ballistic Research Laboratory

I. INTRODUCTION.

An important parameter in the evaluation of gun tube-propellant systems is the erosive wear of the bore surface. Until recently, the measurement of surface wear could only be accomplished by a micrometer technique (star-gauging) with an inherent accuracy of 25 microns. Since the amount of material removed with a single shot, or a small number of shots, is usually much less than 25 microns, large amounts of munitions have to be expended at sometimes great cost in time and money in order to determine the erosivity of any given gun barrel-propellant-additive-rotating band configuration by this technique. In addition, the accuracy of the star-gauging method can be affected by the presence of foreign matter on the bore surface. This paper describes a very precise measurement of bore surface wear which utilizes a radioisotope technique.

The basis of the radioisotope technique is the transformation of stable materials into radioactive isotopes by nuclear reactions produced by energetic positive ion beams. The characteristic radiations emitted by these radioisotopes provide a means to monitor changes in the characteristics of the activated area of the surface. Activation of a small region of a surface to a prescribed depth allows a measurement of wear from that region by monitoring the decrease in the intensity of radiation due to the erosion of surface materials. The advantages of this technique are that wear can be measured to submicron accuracy, in situ and without bore surface cleaning.

II. EXPERIMENT.

A. Radioisotope Technique.

When ^{56}Fe is bombarded with a beam of energetic protons, a nuclear reaction can take place in which some of the ^{56}Fe is transformed into the radioactive isotope ^{56}Co. The nuclear reaction is endothermic and requires a proton energy in excess of 5.45 MeV. A beam of monoenergetic protons will lose energy as it penetrates a steel surface due to collisions with electrons. Any proton may react with an ^{56}Fe nucleus provided its energy is still above 5.45 MeV level, and thus a surface region may be activated to a prescribed depth which is determined by the incident beam energy. The uncertainty in the activated depth depends on the energy uncertainty of the incident

†NRC Research Associate

protons, the accuracy of the cross section for the $^{56}Fe(p,n)^{56}Co$ reaction and the flatness of the surface area that is activated. A ^{56}Co concentration of less than one part per million is sufficient for wear measurements and is easily produced. Neither this amount of ^{56}Co nor the amount of hydrogen produced by the proton beam is sufficient to have a significant effect on the characteristics of the steel.

The $^{56}Fe(p,n)^{56}Co$ reaction is well suited for erosion wear measurements. The nucleus of ^{56}Co decays via electron capture or positron emission to ^{56}Fe with a half-life of 77.3 days. Subsequent de-excitation of the ^{56}Fe produces a number of characteristic gamma rays, the most prominent of which have energies of .847 and 1.238 MeV. Gamma rays of this energy will easily penetrate through the surface layer and can be detected by Ge(Li) or NaI detectors. For a typical Ge(Li) spectrum, see Figure 1. The 77.3 day half-life is long enough to permit activation, wear, and wear measurements to take place over a period of months, and yet the half-life is short enough to provide sufficient activity at the part per million concentration levels. In addition, the (p,n) reaction cross section falls rapidly near the 5.45 MeV threshold providing a relatively sharp boundary between the activated and non-activated regions.

In order to relate activity measurements to surface wear, the relative ^{56}Co concentration must be known as a function of depth. This depth profile was determined by irradiating a stack of thin iron foils (instead of a solid piece of steel) with the proton beam. The specific activity of each foil and its thickness was then measured from which the relative concentration of ^{56}Co as a function of depth into the foil stack was determined. Using this relative concentration information, the total gamma-ray activity as a function of the depth of the foil stack is obtained. The resulting function directly relates the gamma-ray activity change to removal of iron layers from the stack and is given by the wear calibration curve of Figure 2. Since the layered foils generate a surface which is exactly identical to a single thick iron surface, the wear calibration curve can be used with a solid steel surface which has been appropriately activated. The result of the foil experiment indicates that surface wear can be measured with an absolute error of less than 0.25 μm. For a more detailed description of the radioisotope technique see references 1 and 2.

B. 20 mm Pressure Barrel.

An unplated 20 mm pressure barrel was prepared for activation by machining three holes into its wall; one at the origin of rifling, one in the middle and one at the muzzle end. The holes were positioned so that a proton beam could enter through each hole and strike a land on the opposite side of the barrel, activating a spot of about 1.5 mm diameter to a depth of approximately 25 μm. (See Figure 3.) The activation was performed at the Tandem Van de Graaff Laboratory of the University of Pennsylvania. The barrel was then mounted in an indoor firing range at BRL. The three holes were pressure sealed by ball bearings held in place by bolt and clamp arrangements as shown in Figure 3.

C. Activity Measurement.

The gamma-ray activity was measured with a 7.5 cm x 7.5 cm NaI scintillation detector whose output pulses were recorded by a multichannel analyzer. Since the attenuation of gamma-rays from the ^{56}Co decay through the roughly 20 mm steel wall of the barrel does not affect the precision of the wear measurement, the detector was mounted outside the barrel. The reproducibility of the detector position from measurement to measurement, a critical aspect of this technique, was accomplished by a self positioning detector holder. From 5 to 10 cm of lead shielding was placed around the NaI scintillator to reduce both room background and cross-talk between the three active spots. The activity at each of the three positions was measured before any shots were fired to establish a baseline for subsequent measurements. The geometry of the barrel-detector system is also shown in Figure 3.

III. RESULTS.

Erosion wear measurements utilizing the radioisotope technique were made on a 20 mm pressure barrel as well as on 37 mm nozzles. The nozzle work is presented at this conf· ·nce by Richard Ward et al. This report will deal exclusively with the 20 mm barrel work.

The first activity measurement was made after a single M55A2 round had been fired. No measurable loss in activity was detected at any of the three activated spots. Next, a 10 round burst was fired at 2 rounds per minute and 0.5 μm of wear was measured at the origin of rifling. No measurable loss was recorded at the two other activated spots. Subsequently, the firing program consisted of 10 round bursts fired at 2 rounds per minute and one 50 round burst at the same rate. Activity measurements were made after each of these ten or fifty round bursts and the results are shown in Figure 4 for the spot at the origin of rifling. Little or no wear was recorded at either of the other activated spots.

A discussion of the results displayed in Figure 4 is in order. Whenever two points are plotted at the same round number, it indicates two independent activity measurements, usually separated by at least

16 hours. The differences between any pair of points is indicative of the instrumental uncertainties in the measurements and amount to 0.1 μm or less. The following feature of the results should be noted:

1. From round 31 through 171, the data points lie on a straight line indicating a constant wear rate per round of 0.023 μm/round (9×10^{-7} in/round).

2. Rounds 132 through 151 had nylon rotating bands but showed no difference in wear from the standard copper bands used for all other rounds in the experiment. Thus in the slow fire mode, it appears that nylon rotating bands do not reduce erosion wear as reported for the fast fire mode[3]. The implications of these data to the Army's programs to reduce erosion in large caliber guns by use of non-metallic bands are very significant. At the very least, more tests are needed both in slow and fast fire modes with large and small caliber guns before large scale development is started.

3. Rounds 172 through 181 were standard M55A2 rounds but the projectile tips were coated with TiO_2 wax. It appears that the TiO_2 wax coating abraded the surface causing greater wear. The next ten shots (182 to 191) were standard rounds and also showed a higher wear rate. It is hypothesized that the abrasive TiO_2 wax "cleaned" the bore surface and that the subsequent rounds were eroding a "clean" surface rather than one which contained an altered layer of oxides, nitrides or whatever. Since the first thirty rounds also show higher wear rates, it might be speculated that the original bore surface had been clean, or at least was significantly different than the surface layer left behind by the firing of the M55A2 rounds. This hypothesis also implies that the condition of the surface layers is a very important factor in determining erosion rates. More definitive experiments are necessary to test this hypothesis.

IV. CONCLUSION.

In conclusion, it has been shown that the erosion wear can be measured in a gun barrel with a precision of ± 0.1 μm compared to ± 25 μm precision inherent in star-gauging measurements. The measurements can be performed in-situ, without cleaning the bore surface, and in a relatively short time. The usefulness of this technique has been demonstrated by the measurement of wear due to projectiles with plastic bands and TiO_2 wax coatings. Plans have been initiated to use this technique to measure wear in large caliber guns. This requires the development of radioactive plugs to be inserted into the bore surface. It is expected that this method will permit more efficient and less expensive evaluation of gun tube erosion wear characteristics.

ACKNOWLEDGEMENT

We wish to thank Mr. T. Brousseau for support during the firing stages of the experiment. Dr. G.M. Thomson contributed extensively to the design of the gun barrel and accelerator beam tube interface hardware. The activation of the barrel would not have been possible without the generous assistance of Mr. D. Freil and Mr. C. Adams at the Tandem Laboratory of the University of Pennsylvania.

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1. Stephen E. Caldwell and Andrus Niiler, "The Measurement of Wear From Steel Using the Radioisotope ^{56}Co", BRL Report No. 1923, September 1976.

2. Andrus Niiler and Stephen E. Caldwell, "The $^{56}Fe(p,n)Co^{56}$ Reaction in Steel Wear Measurement", NIM 138 (1976), 179.

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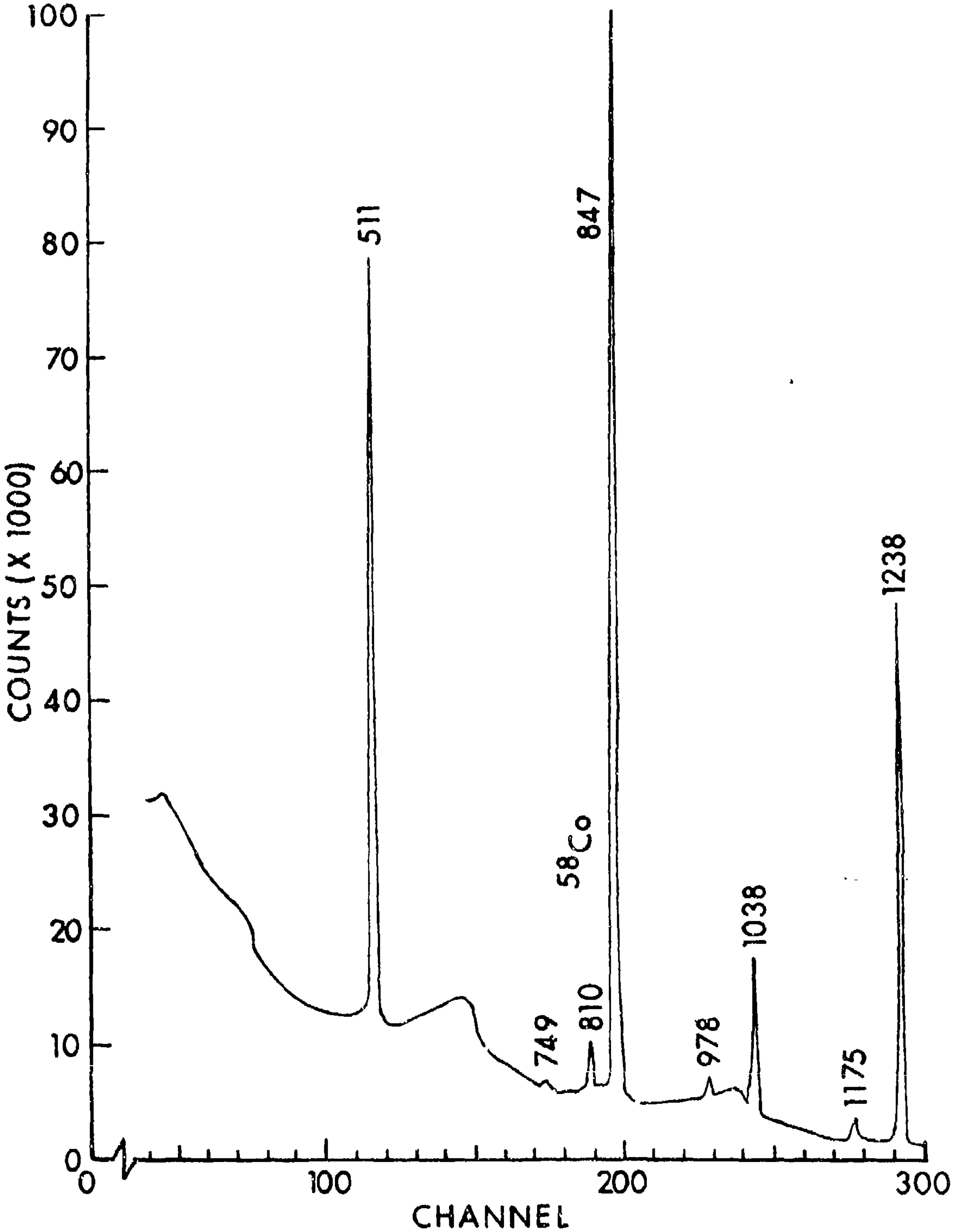

Figure 1. Spectrum of gamma-rays from an activated steel surface. All but the .810 MeV peak are due to ^{56}Co decay.

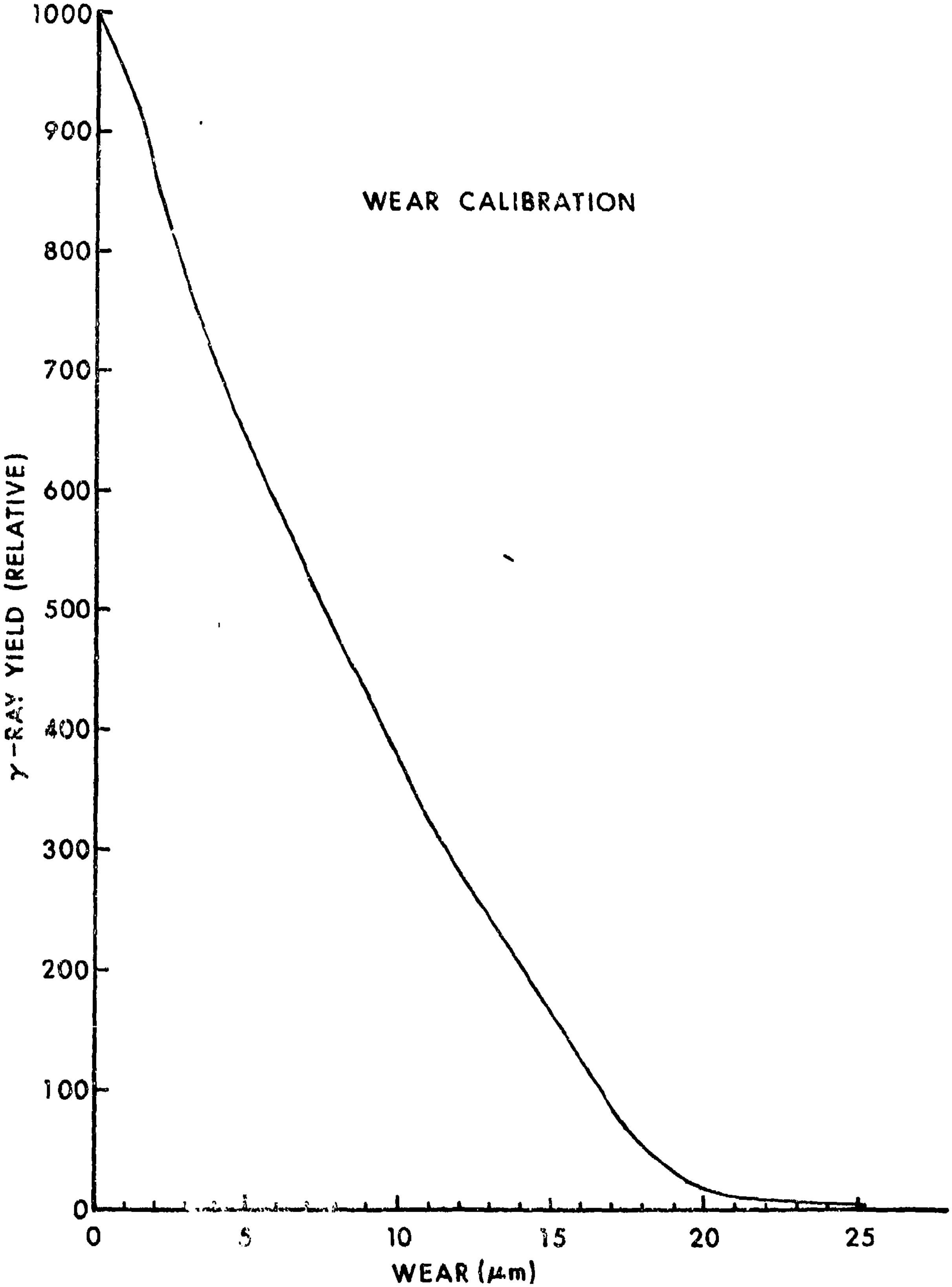

Figure 2. Calibration curve displaying the gamma-ray yield as a function of the activated layer depth. (From Reference 1)

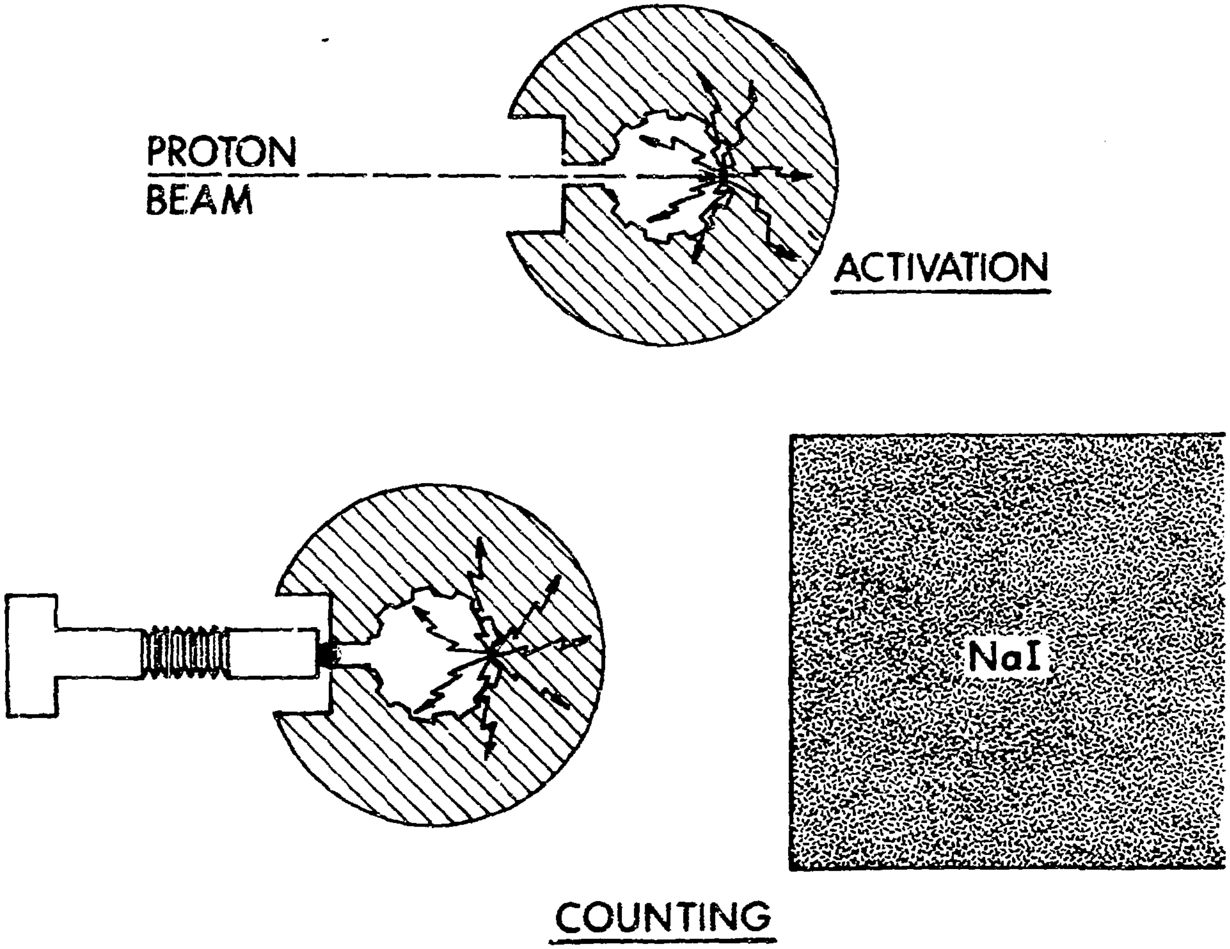

Figure 3 (Top) Schematic of the experimental arrangement for activation of a land in a 20 mm barrel.

(Bottom) Geometry of barrel-detection system with ball bearing pressure seal.

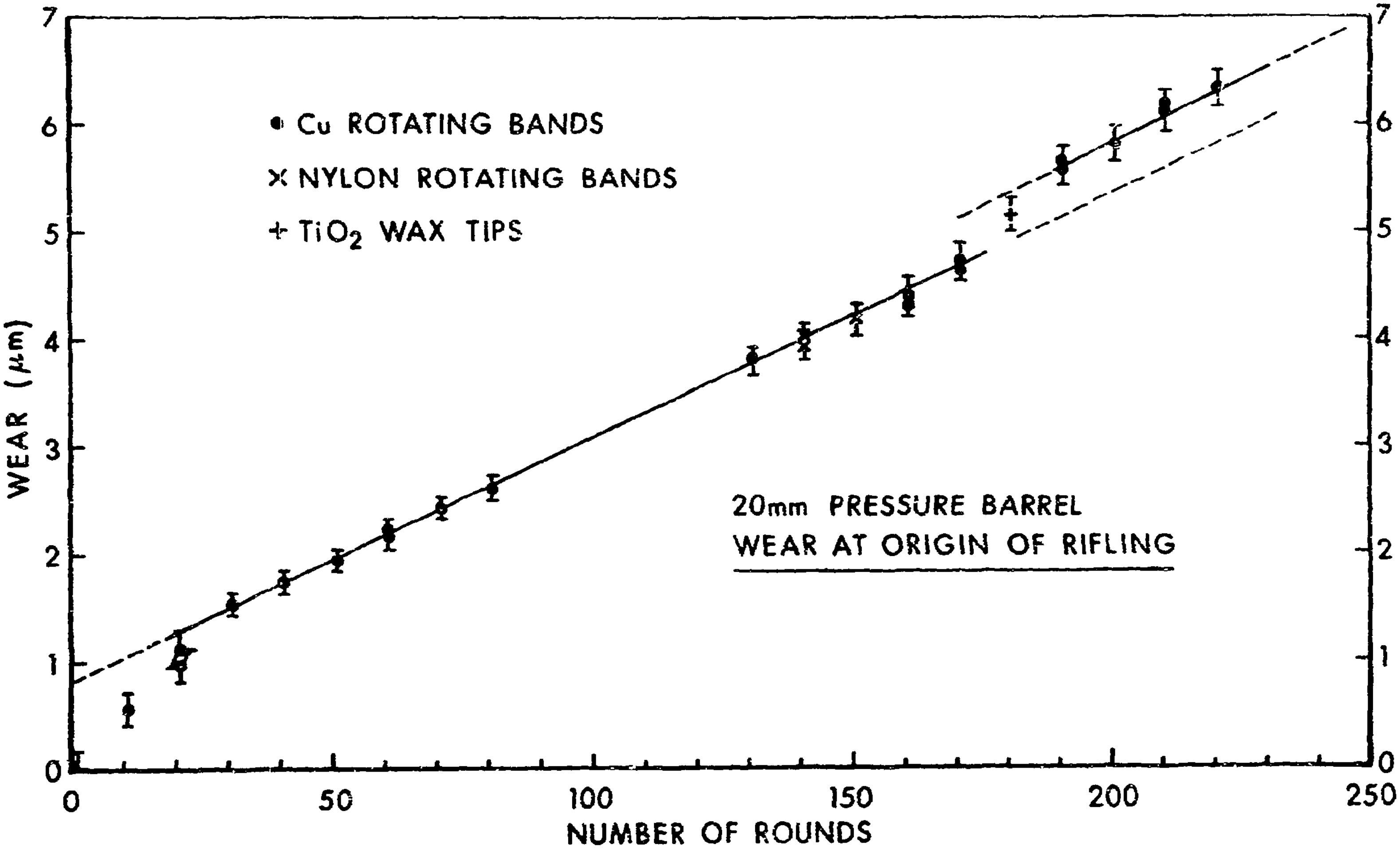

Figure 4. Surface material removed at the origin of rifling as a function of the number of rounds fired. The errors shown are absolute errors; relative errors are smaller.

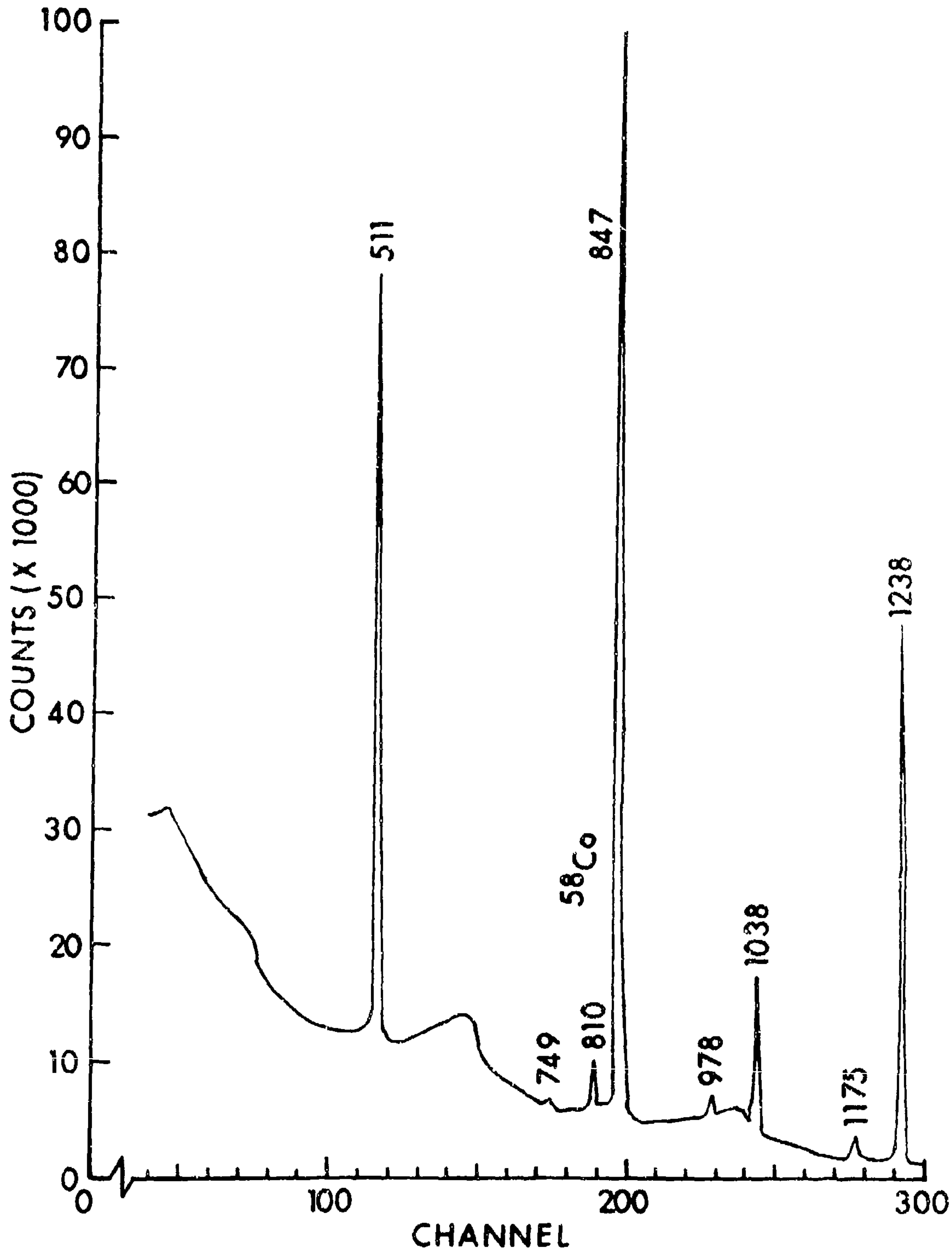

Figure 1. Spectrum of gamma-rays from an activated steel surface. All but the .810 MeV peak are due to ^{56}Co decay. II-301

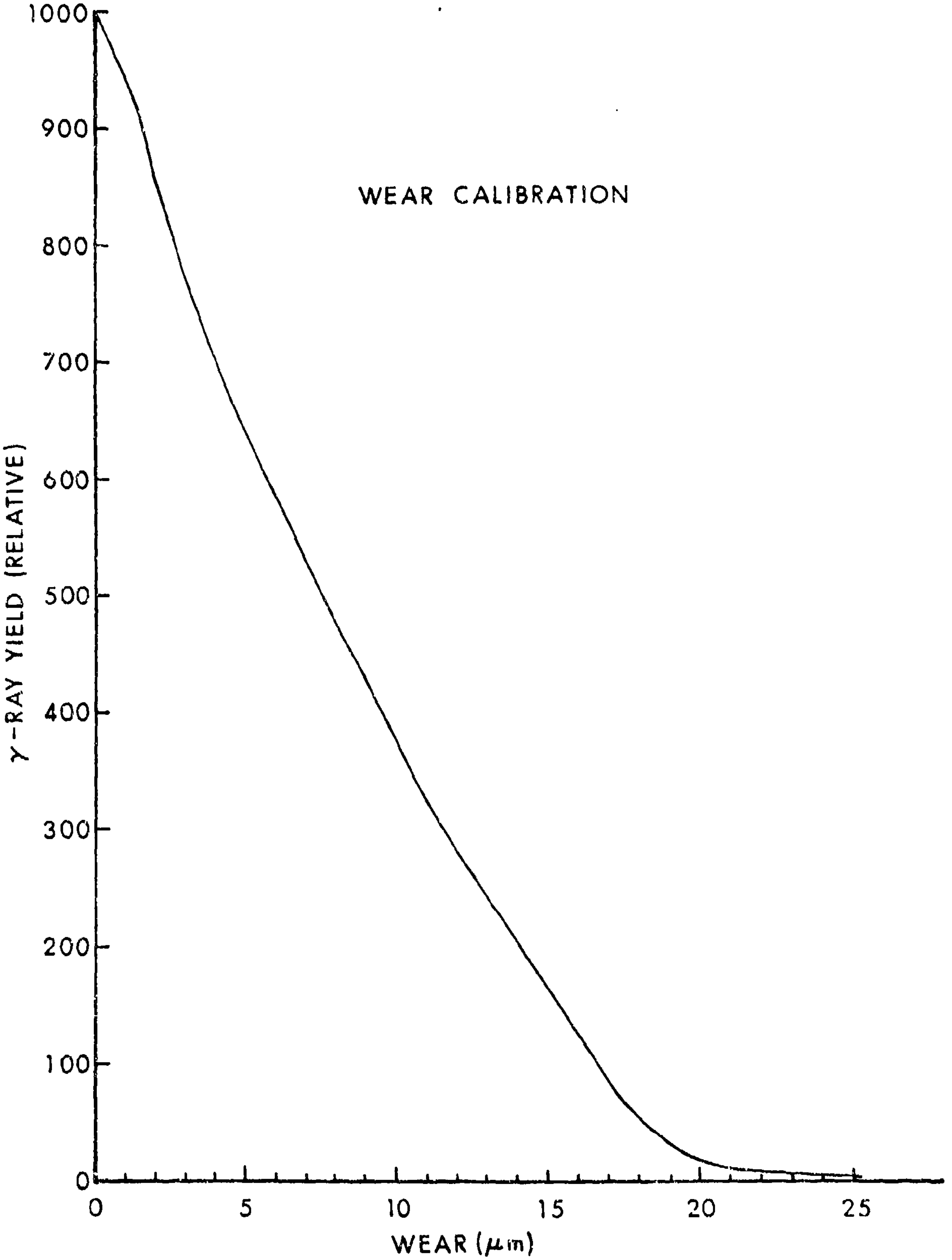

Figure 2. Calibration curve displaying the gamma-ray yield as a function of the activated layer depth. (From Reference 1) 11-302

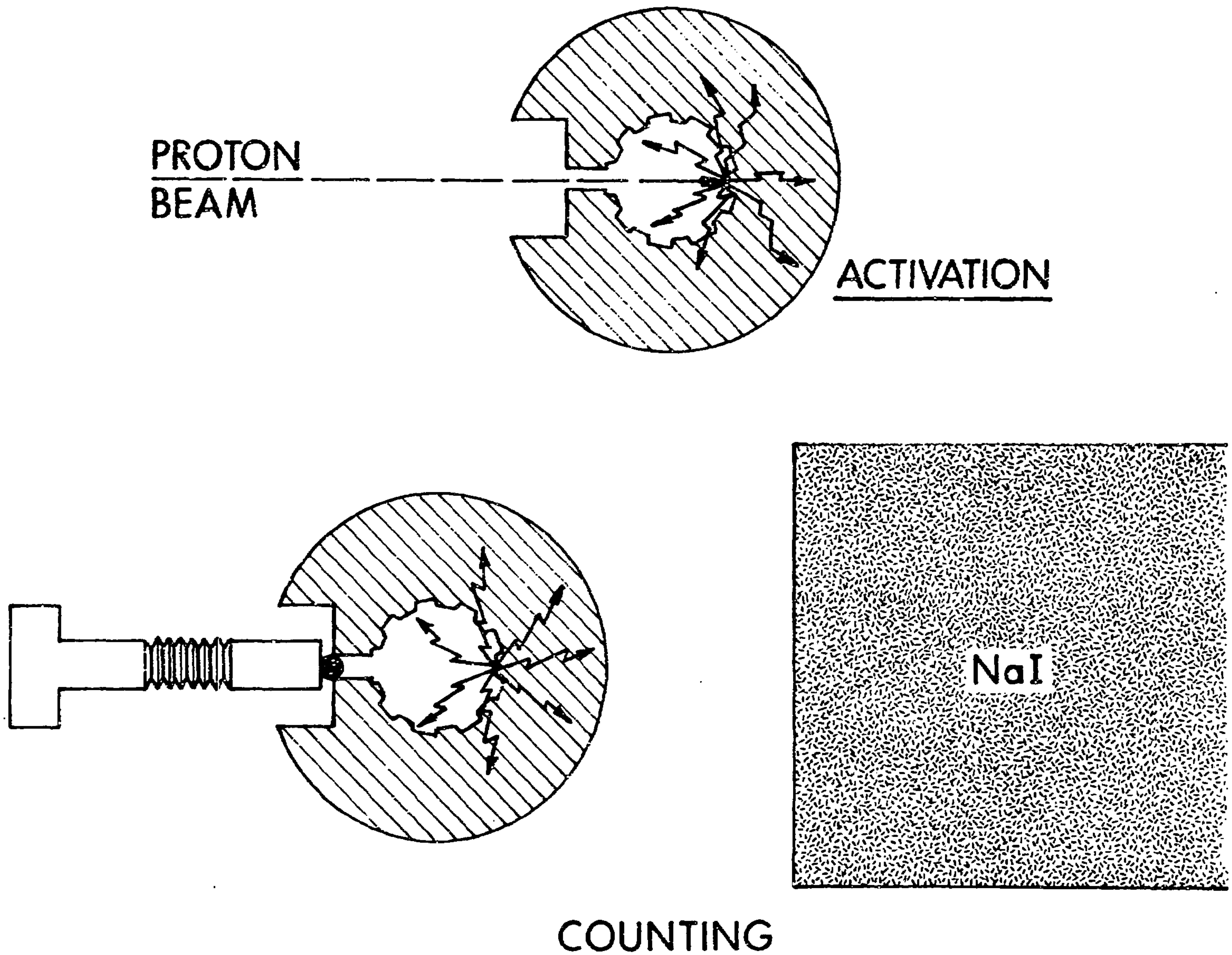

Figure 3. (Top) Schematic of the experimental arrangement for activation of a land in a 20 mm barrel.

(Bottom) Geometry of barrel-detection system with ball bearing pressure seal.

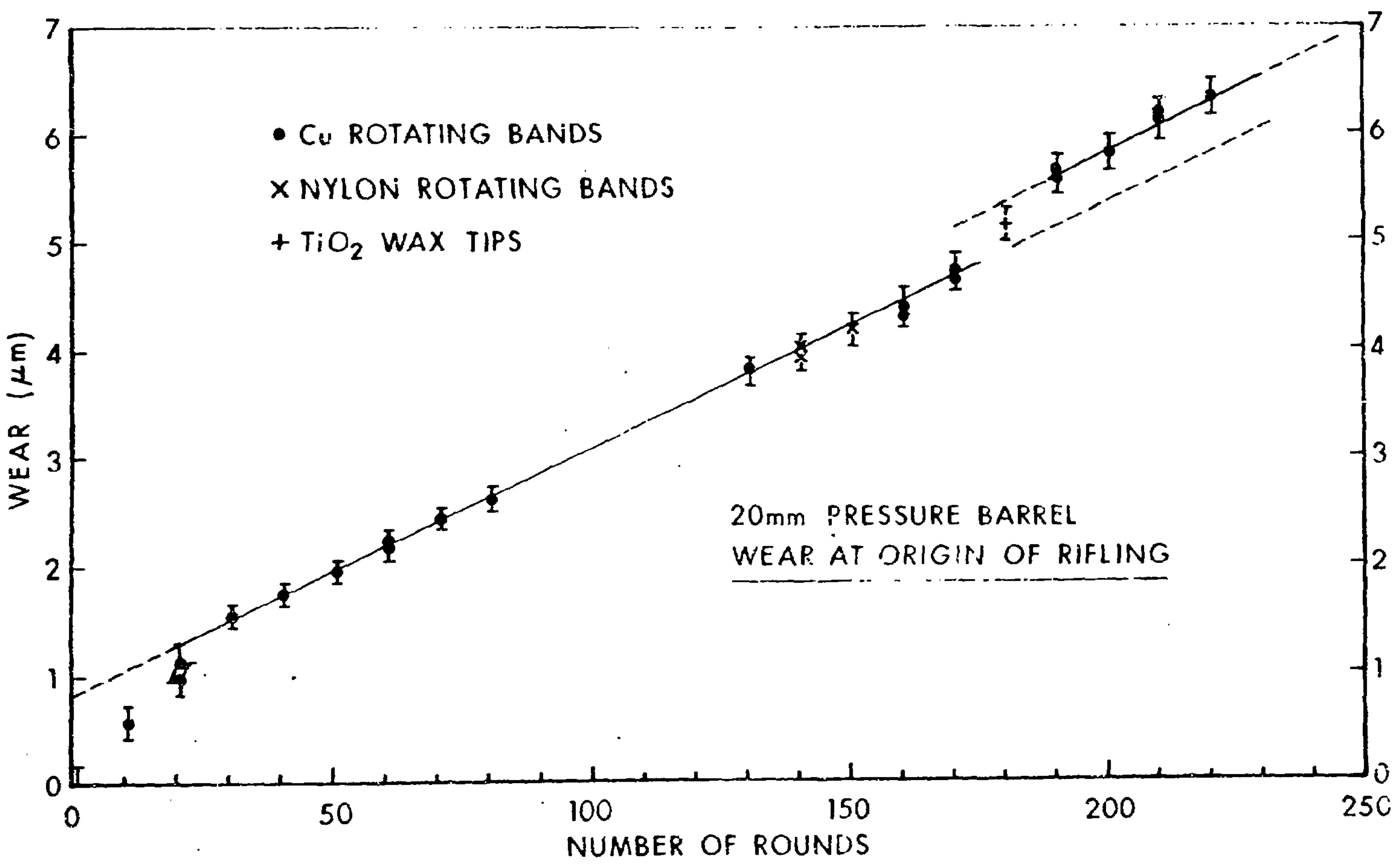

Figure 4. Surface material removed at the origin of rifling as a function of the number of rounds fired. The errors shown are *absolute* errors; relative errors are smaller.

"END OF LIFE CRITERIA

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ABSTRACT

A survey was made of the erosion "end of life" criteria which are applicable to Navy 5"/54 gun barrels, firing service propelling charges, and projectiles. The controlling element of the erosion "end of life" criteria for the 5"/54 gun system was the dud rate allowable for the variable time fuze and not loss of velocity or range performance. The influence of the projectile components design (rotating band/gun barrel interfaces, ballistic dispersion and components, shock hardening of fuze, etc.) upon erosion "end of life" criteria will be examined.

BACKGROUND

In the course of the development of the EX 400 VT-1R Fuze/MARK 42 Rear Fitting Safety Device (RFSD) for 5"/54 ammunition it was discovered that this combination was exhibiting a very high dud rate (up to 100%) when fired from extremely worn 5"/54 gun barrels. Examination of the particular gun barrels in which the high dud rates had occurred revealed that the 5" MARK 41/64/65 projectiles were seating either at the origin of bore or in the wear cone, i.e., projectile seating distance of 99 to 114 cm (39-45 inches)* from the breechface vice 89 cm (35 inches) typical of new guns. Instrumented projectiles fired from similarly worn guns revealed that extreme axial shock loadings were present, reference (a). These shocks result from the engraving of the projectile's rotating band while the projectile is traveling at about 30.48 m/sec (100 ft/sec). As can be seen in Figure 1, taken from reference (a), the engraving of the rotating band results in a projectile velocity change of about 9.14 m/sec (30 ft/sec) in 100 microseconds. Laboratory tests simulating this shock revealed that the MARK 42 Rear Fitting Safety Device (RFSD) (and all MARK 18 RFSD's) was being defeated. This failure mode was confirmed by parachute recover tests. Similar high shocks are known to damage and fail other projectile and fuze components.

It was noted that the MARK 18 type RFSD is also used with all MARK 73 VT-RF fuzes and that a similar dud problem should be expected in the fleet. Fleet firing experience, however, was contrary to this expectation. The fleet was exhibiting a reliability of at least .91 at a 95% confidence level based on 1750 observed rounds, reference (b). This discrepancy between fleet experience and laboratory testing data was traced to the simple fact that since the introduction of NACO propellant into general use, fleet 5"/54 gun barrels do not become worn to the same extent of the barrels used in fuze acceptance and development testing which were worn by firing the D305 (PYRO) propellant charges. Figure 2 is a histogram of the wear state (bore enlargement at the origin) of all fleet returned 5"/54 gun barrels from 1970 to 1972. The level at which the high fuze dud rate begins corresponds to a bore enlargement of at least 3.5 mm (0.140 inches).

These experiences resulted in a detailed review of the "end of life" criteria, for 5"/54 Naval Gun. The critical elements of "erosion end of life" were examined and new criteria for 5"/54 guns were established as reported by reference (c).

*Report written in soft metric. Original work and data taken in English system.

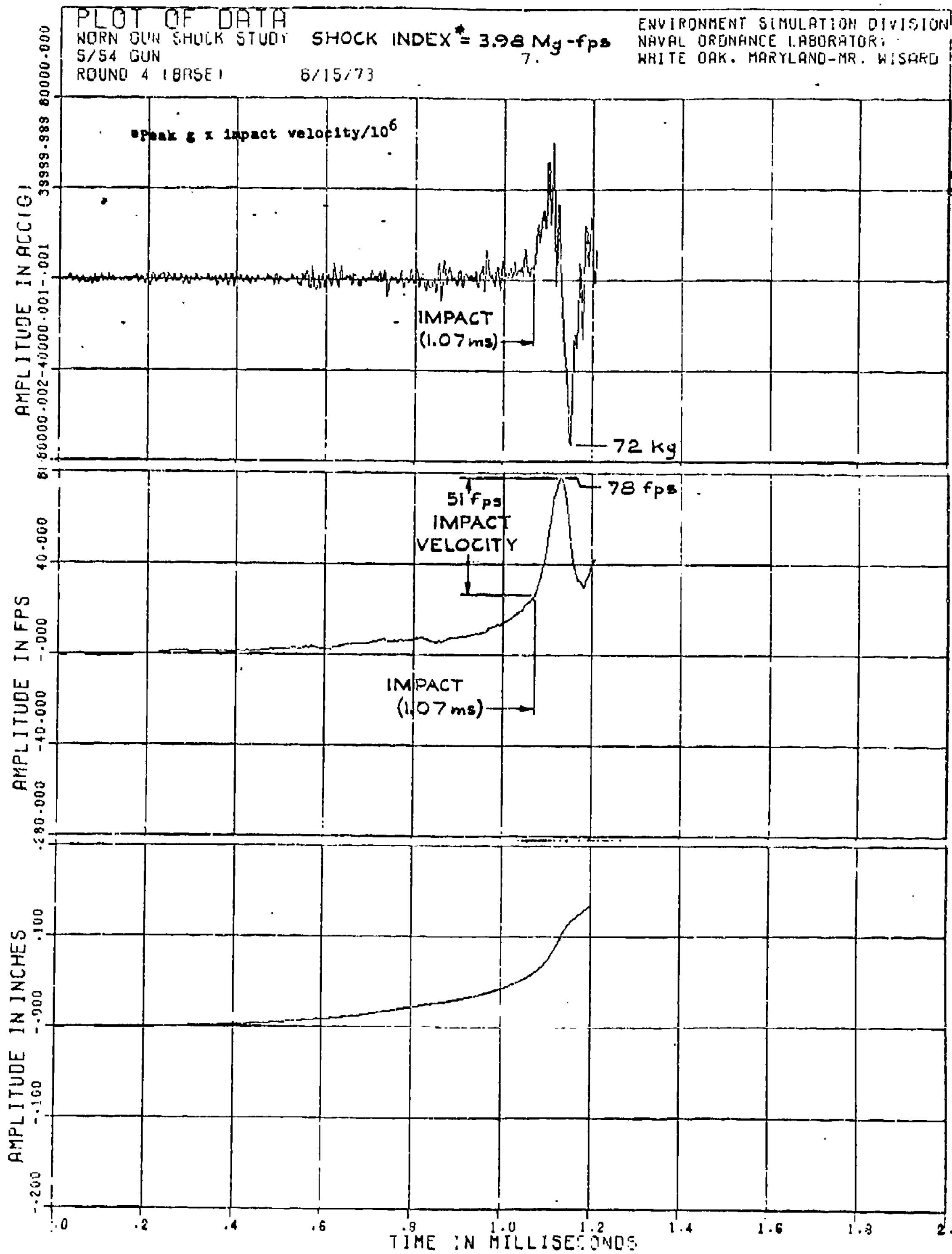

PLOT OF DATA
WORN GUN SHOCK STUDY
5/54 GUN
ROUND 4 (BASE)
6/15/73
SHOCK INDEX* = 3.98 Mg-fps
ENVIRONMENT SIMULATION DIVISION
NAVAL ORDNANCE LABORATORY
WHITE OAK, MARYLAND-MR. WISARD
*Peak g x impact velocity/10^6
AMPLITUDE IN ACC(G)
IMPACT (1.07ms)
72 kg
78 fps
51 fps IMPACT VELOCITY
AMPLITUDE IN FPS
IMPACT (1.07ms)
AMPLITUDE IN INCHES
TIME IN MILLISECONDS

Figure I

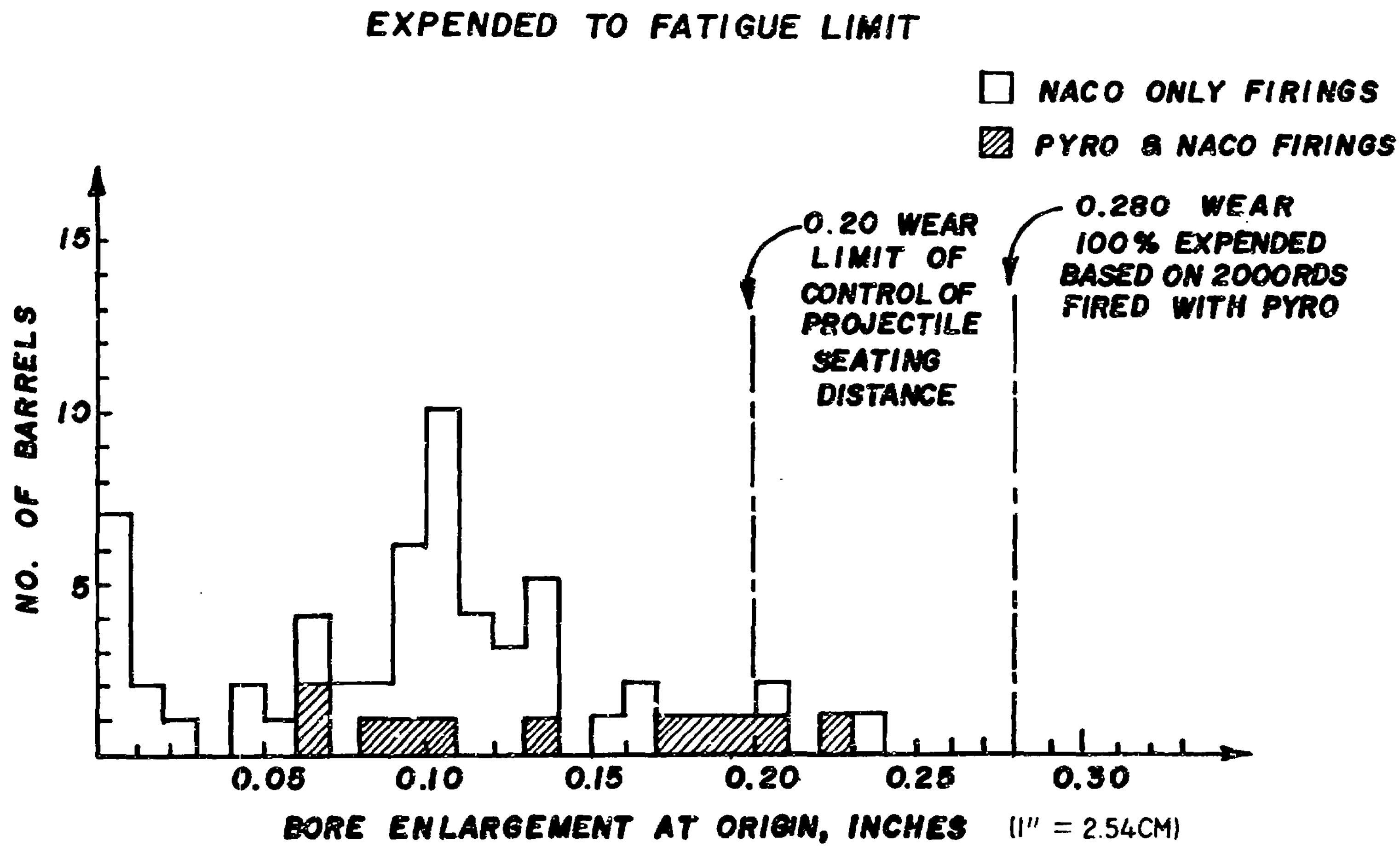


FIGURE 2

WEAR OF 5"/54 GUN BARRELS

This investigation revealed that the projectile rotating band design and its interface with the eroded gun barrel surface control the erosion end of life (e.g., the band designs controls the number of rounds that can be fired from a given gun barrel while maintaining an "acceptable ballistic performance" by permitting either small or large amounts of material to be removed from the gun bore surface.

The design history and consideration of various factors in rotating band/gun chamber design for Naval guns is presented in Appendix A. Appendix A material has been selected from a 1937 paper by Carl F. Jeansen titled "Notes on Gun Chambers, Projectile Bands and Accuracy Life of U. S. Naval Guns." Design of metal rotating bands for Navy projectiles has remained essentially unchanged since that time.

INTRODUCTION

Two different sets of 5"/54 "end of life" criteria are currently applied to 5"/54 gun barrels. These sets, hereafter denoted "fatigue criteria" and "erosion criteria" respectively, are completely different in content and intent. The "fatigue criteria" is a safety restriction intended to prevent the structural failure of the gun barrel in service. As such, it is completely independent of the "erosion criteria" except in the sense that the erosion life must be sufficiently great so as to make a barrel fatigue failure possible. The "erosion criteria", in contrast, represents a minimum standard of ballistic system operational performance. The specific criteria under which a particular gun barrel is condemned depends upon which situation arises first. A gun barrel must be condemned if it represents an unacceptable safety hazard or if it degrades the ballistic system performance to the point that a real operational incapability exists. For example, it is a common occurrence for 5"/54 gun barrels firing primarily NACO service charges to reach the "fatigue criteria" while stibl exhibiting "new" gun ballistic performance under the "erosion criteria."

This document is intended to address only the "erosion criteria." The "erosion criteria" consists of three elements as recognized in reference (d):

1. Reduction in muzzle velocity

2. Fuze performance

3. Range dispersion

The governing element is that element which occurs first. In some cases, a product improvement may change the governing element.

At the beginning of the 5"/54 MARK 42 gun mount evaluation in 1951, the following elements of the gun barrel "erosion criteria" were established by reference (e):

1. The maximum allowable reduction in muzzle velocity due to erosion was to be 108.23 m/sec (355 ft/sec).

2. The maximum allowable degradation of VT fuze performance was to be an operability level of 30% normal (70% dud rate).

3. The exhibition of "excessive" range dispersion ("excessive" was unspecified but usually taken as $\frac{D}{\bar{R}} \leq .007$, where D = absolute mean dispersion and $\bar{R}$ = Average Range).

The fact that such fuze and velocity loss performance levels are not responsive to the present operational environment should be obvious, especially in view of single and dual gun installation on present ships. (In Anti Ship Missile Defense (ASMD), an average engagement will consist of four rounds/mount; a 70% dud rate means to expect 3 duds.)

As a result of the formal barrel life test conditions and of the low fuze performance requirements, the element of the "erosion criteria" which controlled the condemnation of 5"/54 rapid fire gun barrels was the reduction in muzzle velocity.

A detailed discussion of the nature of erosion in which considerable attention has been directed toward the mechanism of erosion, can be found in Appendix A of reference (c). Reference to this report may be necessary so that an adequate background can be available for the discussion of the manifestations of erosion which affect the performance of the ballistic system. Figure 3 is a plot of bore enlargement versus position for a worn gun as obtained from reference (c). Now consider the projectile-gun barrel interface. Figure 4 shows a schematic of the rotating band gun interfacing of a 5" MARK 41 projectile seated in a new gun. Figure 5 shows a similar schematic of the same projectile in a worn gun. Figure 5 shows the interface in a badly worn gun. Note that the projectile seating distance has changed considerably. Also note that in both worn gun cases, the distance between the point of first contact and full rise of the rifling is considerable, i.e., the projectile will obtain a significant velocity prior to engraving the rotating band. Note also that in a worn gun, the projectile can become cocked in the bore.

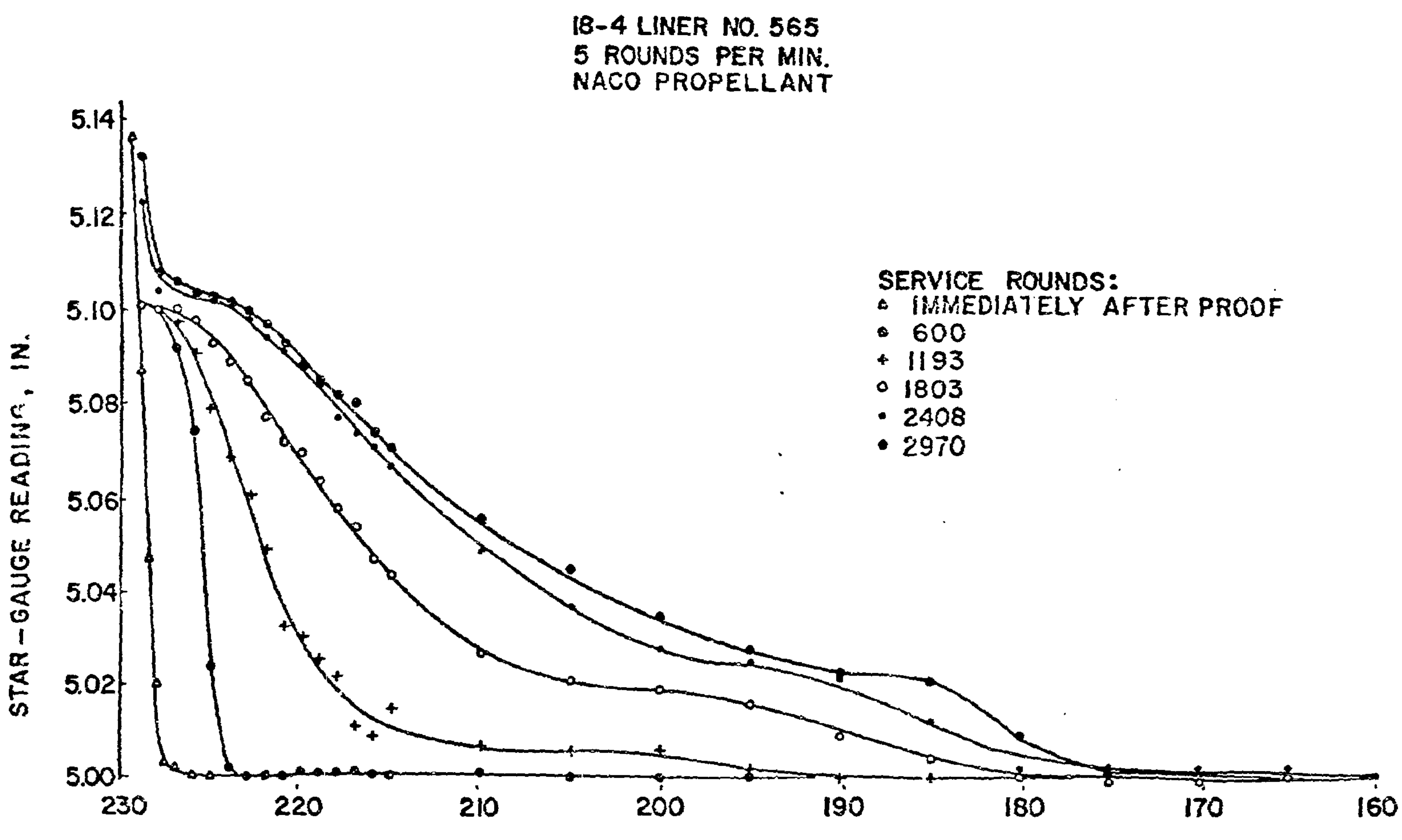
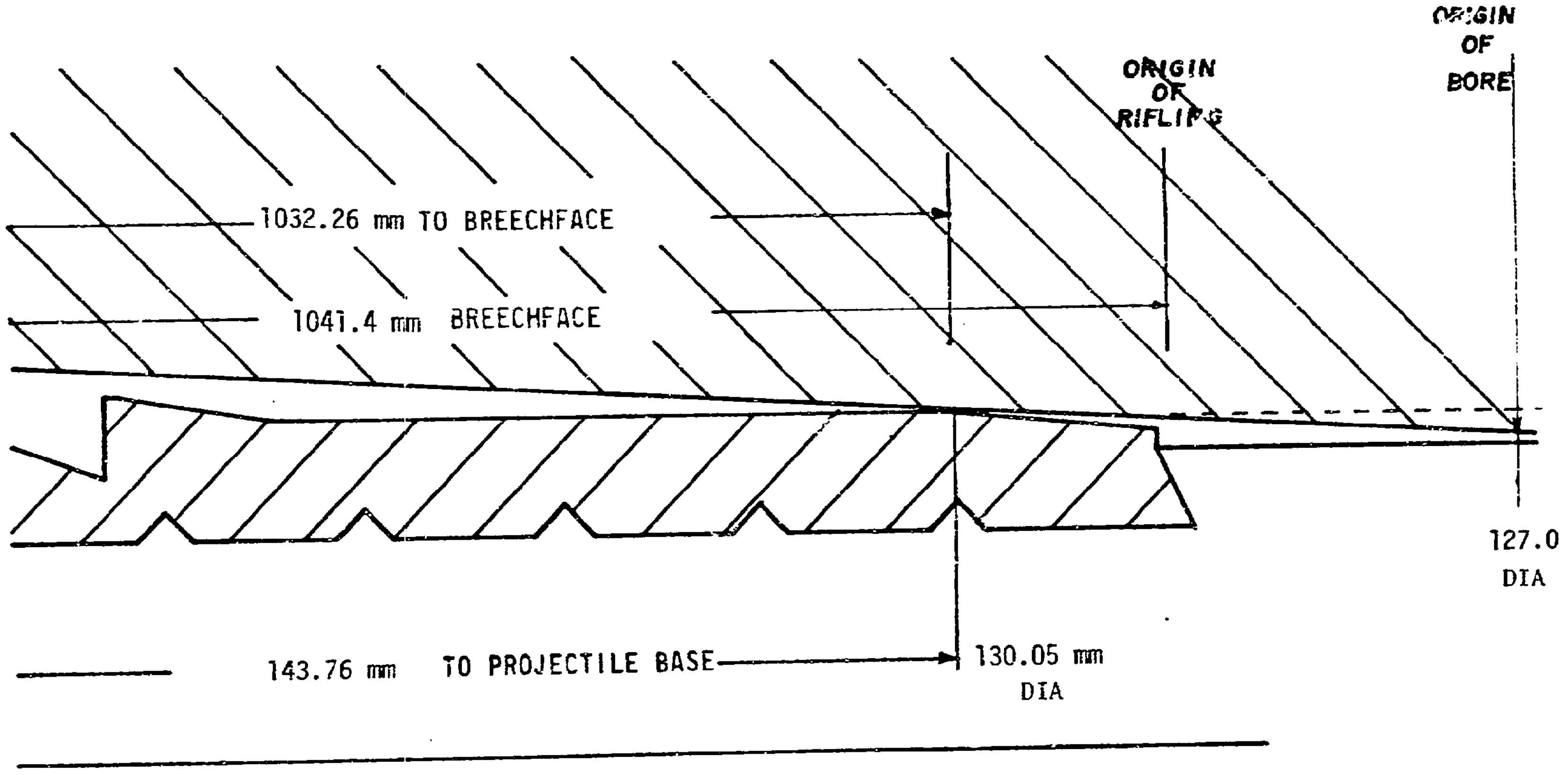


FIGURE 3

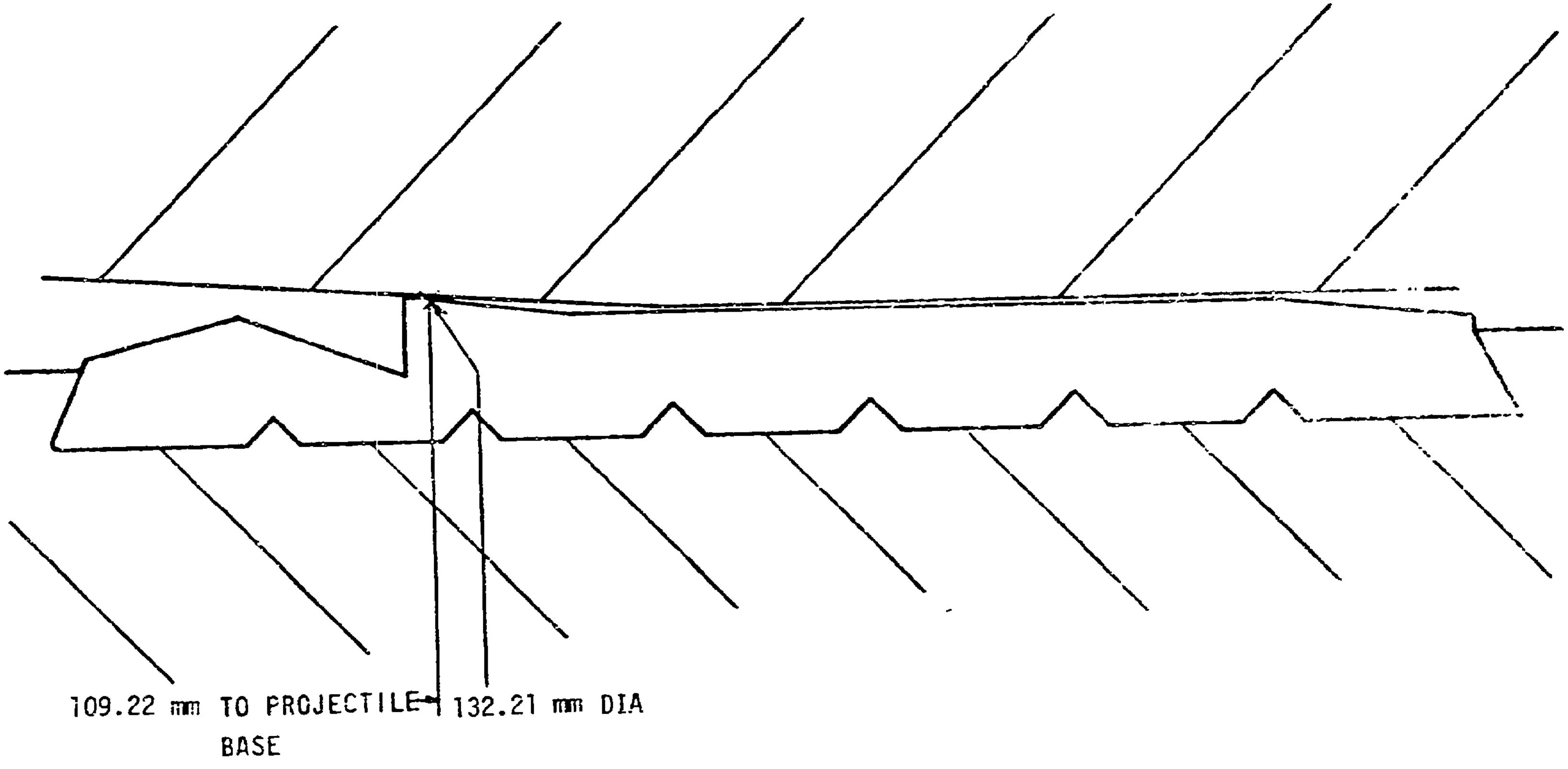
BORE ENLARGEMENT vs. LOCATION FOR WORN GUN



5"/54 MK 41 PROJECTILE SEATED IN NEW GUN

FIGURE 4

II-312



5"/54 PROJECTILE SEATING ON COMPRESSION CONE OF WORN GUN

FIGURE 5

II-313

The effect of each of these manifestations on the "erosion criteria" will be discussed later.

From a review of Figures 3 through 6 and the Appendix A, one can easily understand how the rotating band interface with the gun bore determines the projectile seating location. Projectile seating distance (location) from the breech face controls many of the key ballistic parameters as discussed below.

The sources of the data presented herein are formal gun barrel life tests, velocity checks on fleet returned gun barrels, gun mount reliability tests, fuze acceptance tests, and fuze development tests. Table 1 contains a summary of the major data sources.

GUN BARRELS

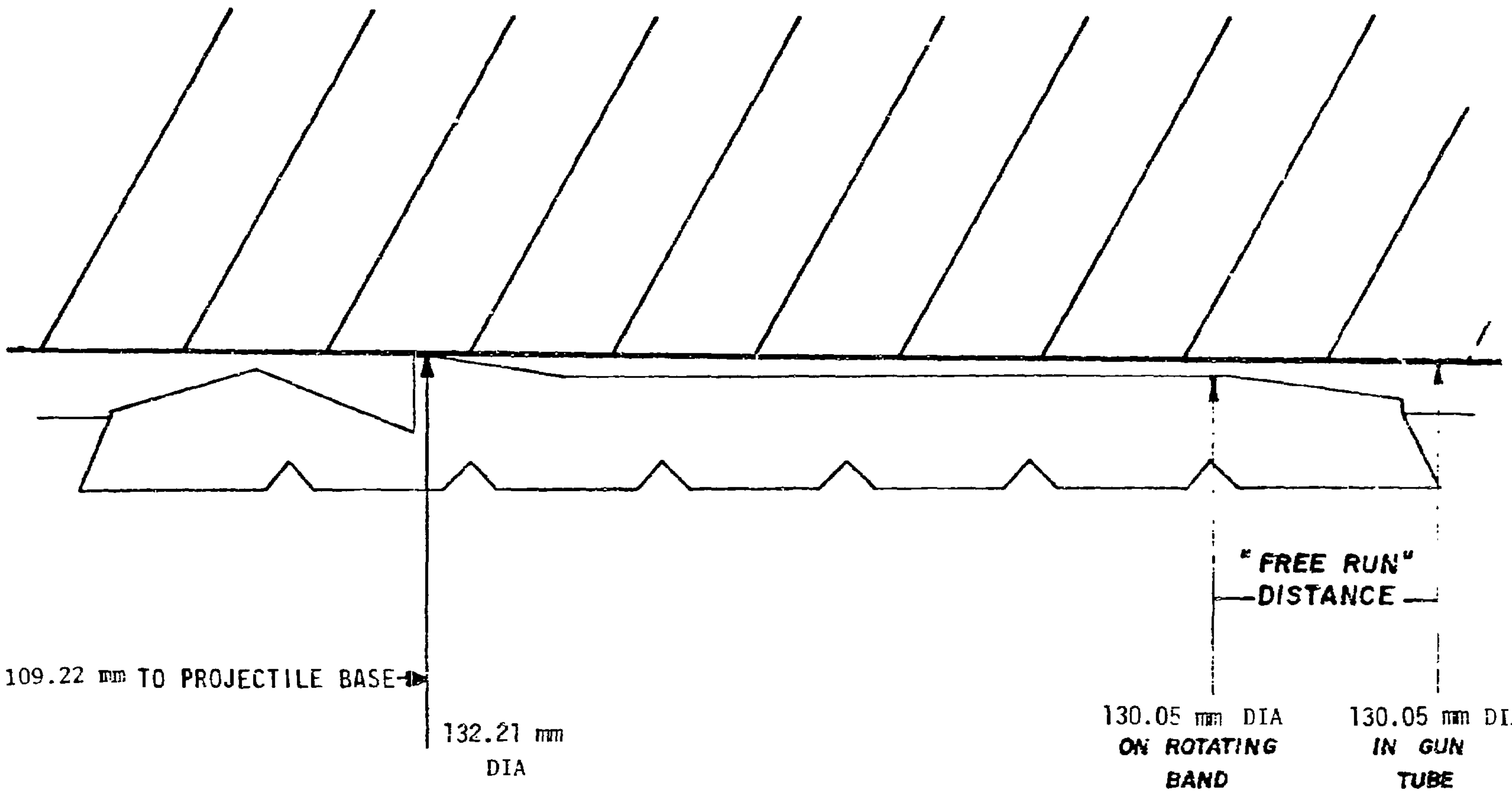
It has become customary in the preparation of Velocity Loss Tables to treat each different gun barrel designation as a separate design requiring separate calibration tables. Thus, in OP 1182, reference (f), velocity calibration tables are included for the 5"/54 MARK 16 MOD 0, 5"/54 MARK 18 MOD 0, and 5"/54 MARK 18 MOD 1 gun barrels firing SPD (PYRO) propellant and for the 5"/54 MARK 18 MOD 1, 5"/54 MARK 18 MOD 3 and 5"/54 MARK 19 MOD 0 gun barrels firing SPCF (NACO) propellant. Calibration tables for the 5"/54 MARK 18 MOD 4, 5"/54 MARK 18 MOD 5 and 5"/54 MARK 19 MOD 1 gun barrels have not been issued because of limited data. The addition of the 5"/54 MARK 19 MOD 2 gun barrel and developmental rounds makes the task of developing calibration tables in the customary manner both arduous and expensive (16 and eventually 24 combinations of barrels and service rounds). Indeed, it is evident that only the use of velocity checks on fleet returned gun barrels makes the process at all possible, the formal barrel life test procedure being so expensive as to be prohibitive.

As detailed in reference (c), the nature of erosion damage and similarity barrel designs enabled the combining of the wear of velocity loss date for the 5"/54 gun barrels. Thus, the only two barrel "designs" which need be considered in 5"/54 erosion data are the following:

Type A - 5"/54 MARK 16 MOD 0 gun barrel

Type B - 5"/54 MARK 18 MOD 0, MARK 18 MOD 1, MARK 18 MOD 3, MARK 18 MOD 4, MARK 18 MOD 5, MARK 19 MOD 0, MARK 19 MOD 1 and MARK 19 MOD 2 gun barrels.

It should be noted that each propellant type must be considered



5"/54 MARK 41 PROJECTILE SEATED ON EROSION CONE IN VERY WORN GUN

FIGURE 6

TABLE I

DATA SOURCES

Ref.	Gun (MK-MOD-S/N)	Propellant	Firing Schedule: Rate (SPM)*	Burst Length (Rds)	Cooling Period (min.)	Duration (rds)
NPG 994	18-0, 16064	PYRO	40	20	.33	100
NPG 1337	18-0, 16058	PYRO	40	20	.33	100
NPG 1501	TYPE J, 15232	PYRO	40	20	.33	100
NWL:WWI-2: BZJ:dmr, 8300 of 9/24/68	18-1, 16345	PYRO	40	Variable (Cold Gun)		40 max.
NPG 1532	18-0, 16182	NACO	40	20	.33	100
NWL Draft Rpt., 7/70	19-0, 7	PYRO	15	20	5.00	100
NWL ltr EPM:DWC:jmv 11/22/72	18-3, 490	NACO	5	600	-	600
	18-3, 492	NACO	31	40	5-15	600
	18-4, 564	NACO	5	600	-	600
	18-4, 565	NACO	31	40	5-15	600
NWL ltr TOE:TEL:smm 8300 of 2/6/73	19-0, 528	NACO	5	600	-	600

*Shots per minute

separately in each service propelling charge configuration. This means that now, instead of 16 combinations, we need deal with four, of which only two (full service NACO propellant and Type B barrels) represent 99% of the fleet requirement. The effects of reduced charges and rate of fire will be discussed later.

REDUCTION IN MUZZLE VELOCITY

The principal element in the "erosion criteria" for 5"/54 gun barrels has been "velocity loss" or the manifestation of erosion which leads to a reduction in muzzle velocity. The operational impact of velocity loss is principally a substantial reduction in maximum range capability. A velocity loss of 108 m/sec (355 ft/sec) is a reduction in maximum range of the 5"/54 gun system of about 3486.2 meters (3800 yards).

For any conventional ballistic system, the muzzle velocity depends upon the following:

1. The expansion ratio - the ratio of total gun volume to chamber volume

2. The charge to projectile weight ratio

3. The thermochemical characteristics of the propellant

4. Bore resistance

5. Ignition action

We fix items 2, 3 and 4 for a specific service gun and complete round. This means, in essence, that the propellant granulation, thermochemistry and the projectile weights and propellant energy are held constant. As the gun wears, however, the expansion ratio changes due to the fact that the projectile seats farther down bore as described earlier. This effect results in an increase in chamber volume,a decrease in projectile travel, and a decrease in expansion ratio, all of which results in a muzzle velocity (and peak pressure) reduction. According to reference (d) and as observed in reference (g), this change in initial projectile seating position is the principal cause of velocity loss. Bore resistance and ignition variation explain most of round-to-round muzzle velocity variation, reference (h), but have only slight effect upon the average velocity level.

The "acceptable" muzzle velocity reduction level of 77.7 m/sec (255 ft/sec) for 5"/54 rapid fire guns established by reference (e) must be considered in the context in which it was established. The

5"/54 MARK 42 gun mount was originally designed as an anti-aircraft weapon. The surface-to-surface maximum range capability of the MARK 47 MOD 3 computer was only 18,440 meters (20,100 yards) as compared to the 23,853 meters (26,000 yards) maximum range capability of a new 5"/54 gun. The range of muzzle velocity input for this computer was ±76.2 m/sec (±250 ft/sec)or a total of 152.4 m/sec (500 ft/sec), reference (h), which is much greater than 108 m/sec (355 ft/sec) allowed for velocity loss. In its primary anti-aircraft role, the allowable velocity loss hardly affected the solution so long as the mean muzzle velocity was known. Thus, muzzle velocity calibration capability was important for both surface and anti-air combat roles.

Four muzzle velocity calibration techniques have been authorized for use on 5"/54 gun barrels at various times. The fourth technique (projectile seating distance gauge) is presently being implemented. Three techniques are based upon predicting the muzzle velocity performance of a gun barrel firing a particular complete round by means of measuring some critical dimension in the worn gun tube[1].

The four methods of muzzle velocity calibration using erosion parameters are the following:

1. Muzzle velocity correlation with number rounds fired

2. Muzzle velocity correlation with the bore enlargement on the lands at the origin of bore

3. Muzzle velocity correlation with a Bore Erosion Gauge Reading

4. Muzzle velocity correlation with Projectile Seating Distance

[1]At this point, it should be noted that the actual measurement of muzzle velocity by doppler radar velocimeter is well developed as an instrument technique. The velocimeter can be used in an appropriate digital filter to significantly reduce velocity bias errors such as result from charge assessment, bore erosion, and other ammunition lot anomalies. Its use to isolate wear measurement via the velocity loss technique is not feasible because of the other variables (for example, charge assessment) in its total output.

As discussed in detail in reference (c), each of the first three methods have significant disadvantages and/or errors which are greatly increased by such operational aspects as reduced charges (firing at less than full muzzle velocity) rate and duration of fire, different firing history of calibration and service gun barrels and charges in propellant type and peak pressure (see Figures 7 and 8).

It was established that at least two erosion parameters existed which may be effectively used to establish muzzle velocity calibrations (see Figures 9 and 10) in worn guns. These erosion parameters are the bore enlargement at origin of bore and the projectile seating distance. They are really indicators as measures of a common interior gun ballistic parameter-chamber volume. The accuracy of the calibration based on these parameters is such that a probable error in velocity estimation of 3.65 m/sec (12 ft/sec) may be expected. The calibration curve using either parameter will be independent of the previous firing history of the gun barrel being calibrated. Both calibrations will, however, be sensitive to the quickness of the propellant of indices other than the Master Index, i.e., if the muzzle velocity versus wear parameter curve of the Master and test propellant lots are not identical. This deviation along with powder assessment errors will appear when the test lot is issued to the fleet. An example of this effect is shown in Figure 11. "Velocity quickness" can be compensated for by a table of differentials such as was issued for the 5"/38 SPD indices; however, generation of such data is very expensive and for propellant of some nominal impetus is probably not necessary.

FUZE PERFORMANCE

The originally specified standard of VT-fuze performance was 30% normal functioning, i.e., 70% dud rate when fired from the 5"/54 MARK 18 gun barrels in the 5"/54 MARK 42 gun mount. According to reference (e), the average level of VT-fuze performance achieved over the life of the gun barrel in the early barrel life tests was 51% normal with extreme values ranging between 15% and 95% normal. Such a VT-fuze performance standard is, of course, unacceptable in the present operational environment.

The probable application of VT-fuze rounds under present conditions is in the anti-shipping missile defense role (ASMD). The typical engagement time in this role is 30 seconds, with one weapons station engaged. The maximum number of 5"/54 rounds fired in the ASMD engagement will vary according to gun mount design, but will not exceed 15 for the MARK 42 gun mount or eight for the MARK 45 gun mount. With such limited number of projectiles being fired,

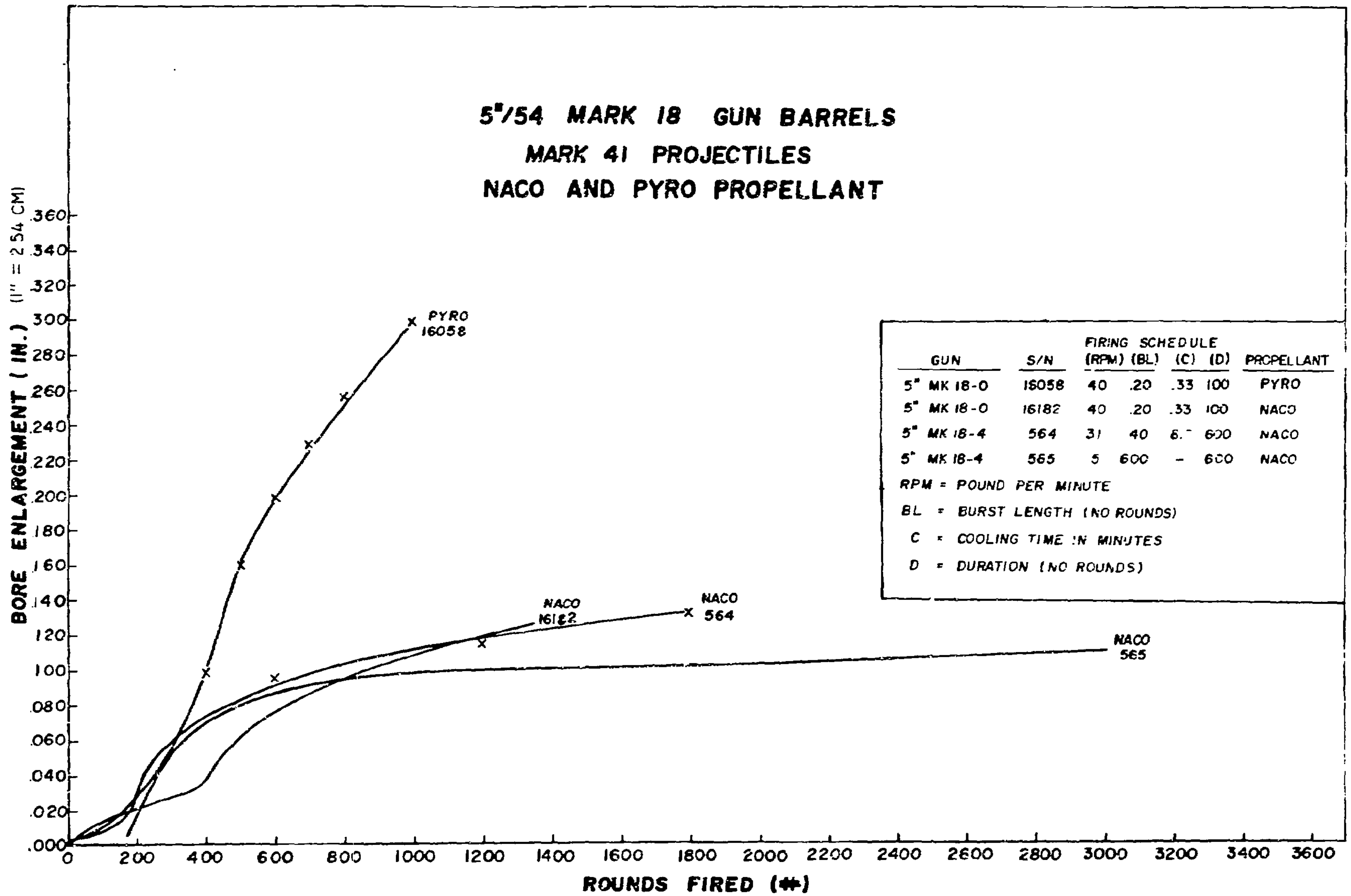


FIGURE 7

ORIGIN OF BORE ENLARGEMENT vs. ROUNDS FIRED

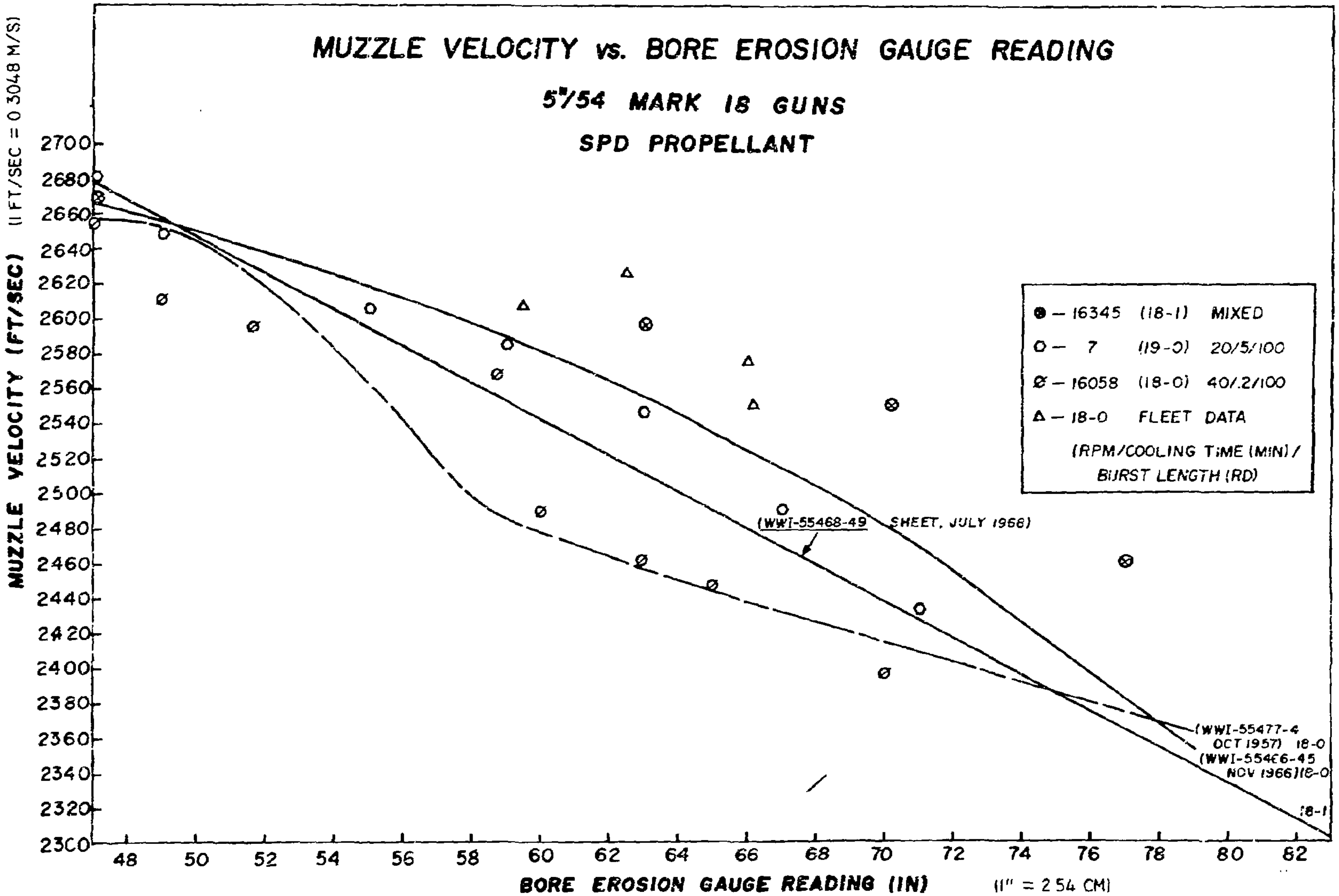


FIGURE 8

MUZZLE VELOCITY vs. BORE EROSION GAUGE READING

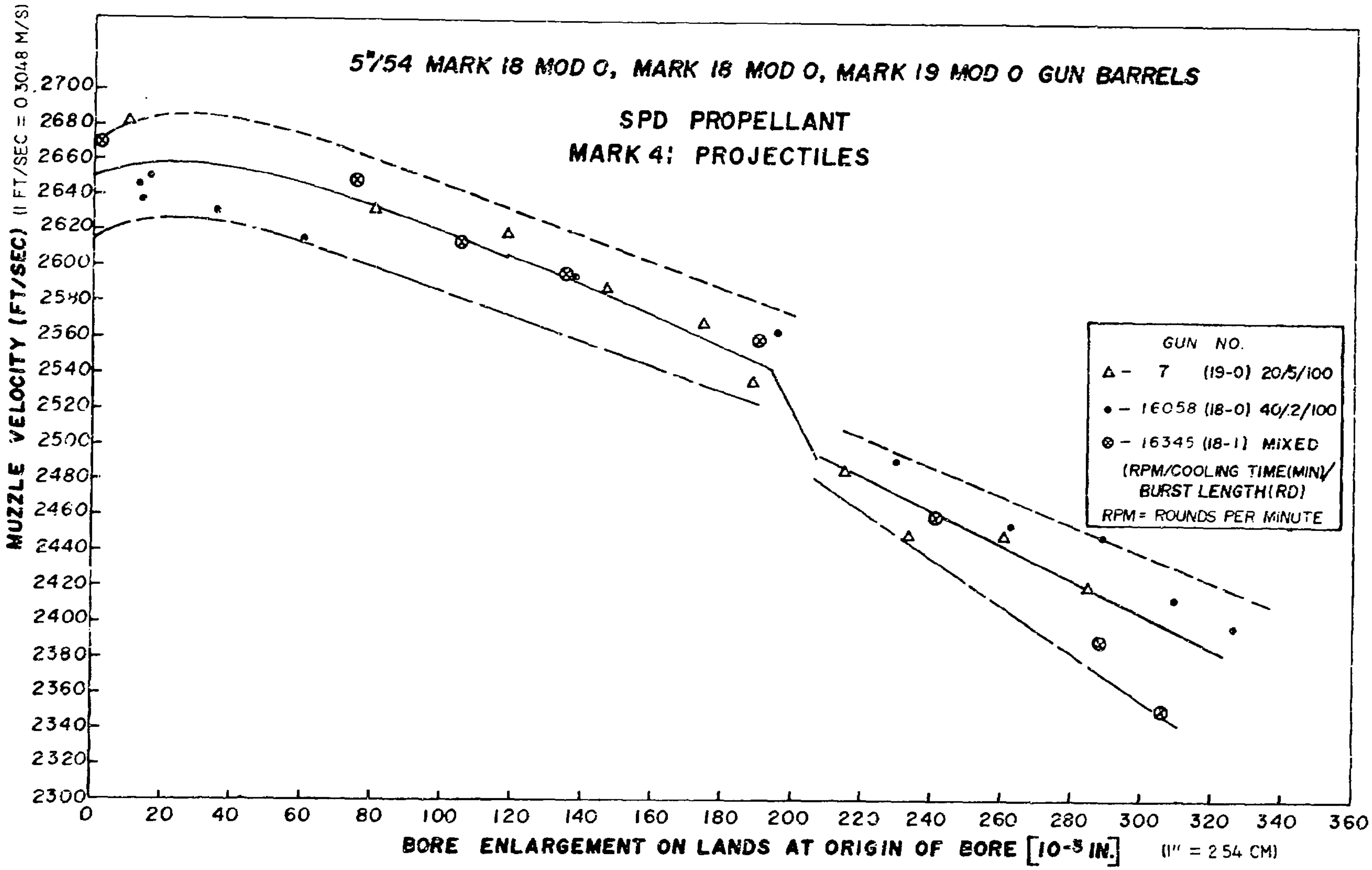


FIGURE 9

MUZZLE VELOCITY vs. BORE ENLARGEMENT

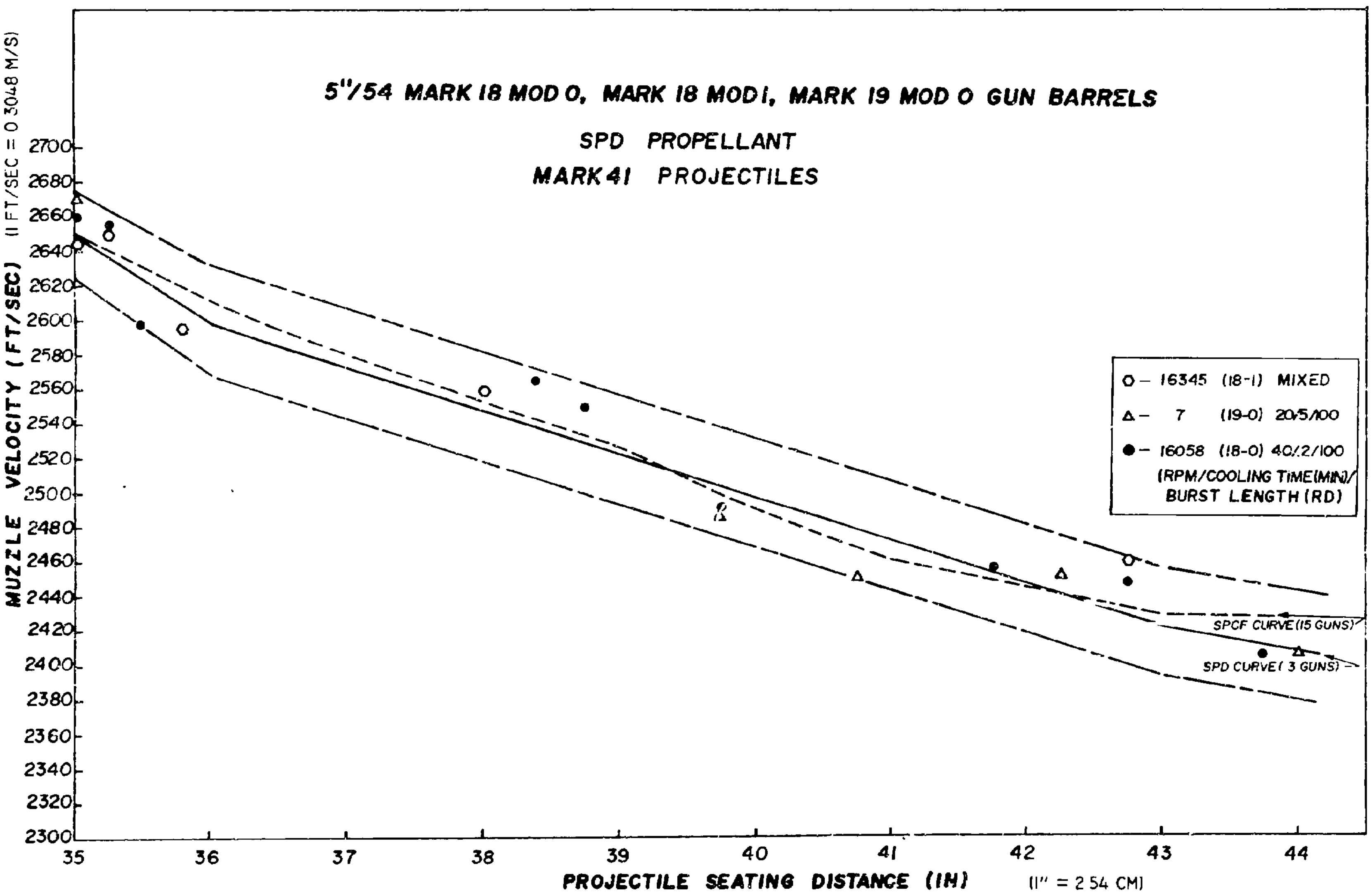


FIGURE 10

MUZZLE VELOCITY vs. PROJECTILE SEATING DISTANCE

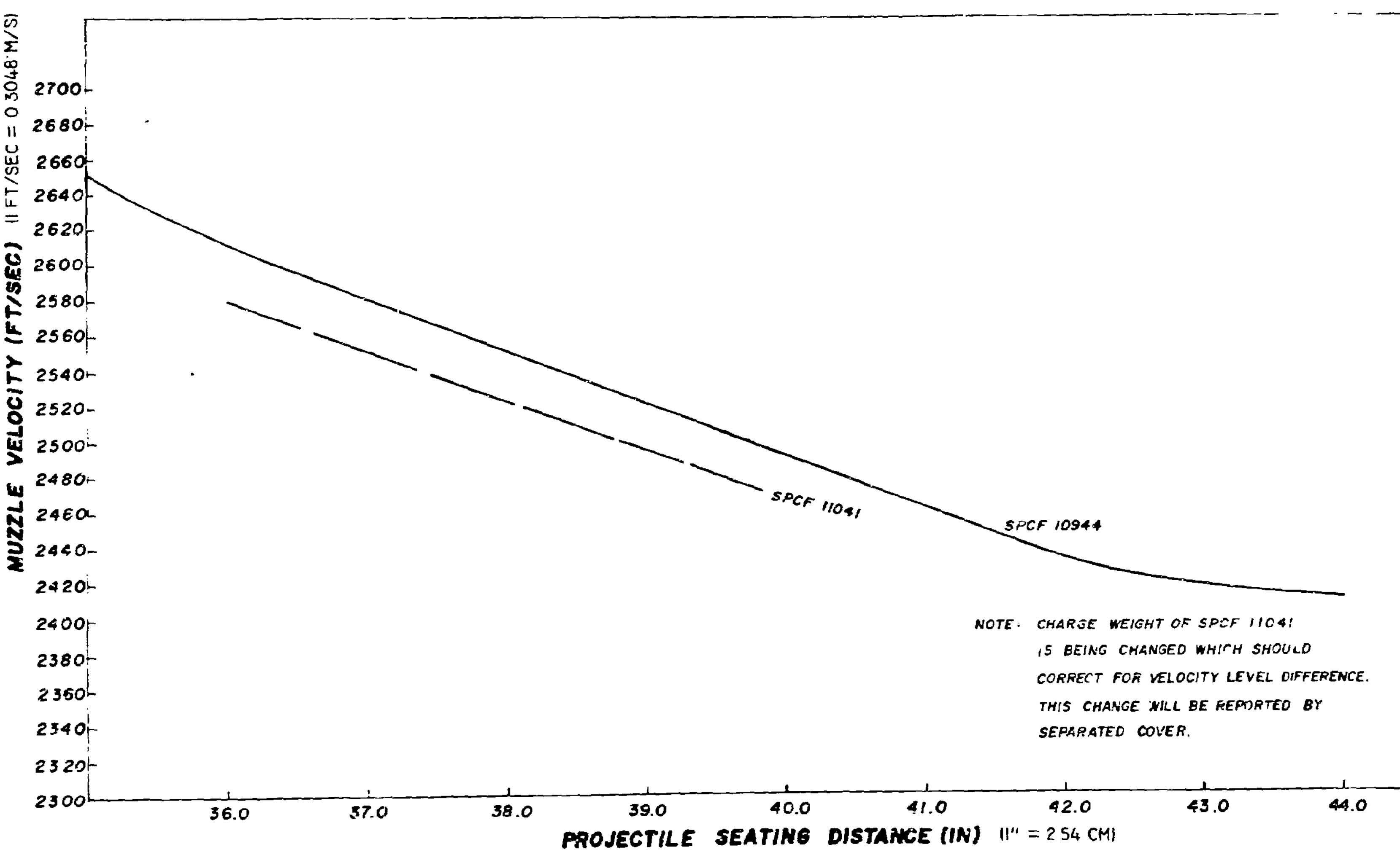


FIGURE 11

ILLUSTRATION OF SMALL (VELOCITY QUICKNESS) EFFECT BETWEEN BETWEEN TWO MASTER POWDER INDEXES IN WORN GUNS the VT-fuze reliability must be very high (at least 85%) if there is to be any semblance of an effective defense. The realization that dud rates as high as 85% could be expected from all fuzes using the MARK 18/MARK 42 RFSD under certain barrel erosion conditions was, therefore, alarming.

The mechanism by which the MARK 18/MARK 42 RFSD is defeated was discussed in some detail in the Background section of this paper. It was apparent that, while the dud rate encountered was directly related to the wear state of the gun barrel, the precise relationship was none too obvious. Figure 12 is a plot using development and acceptance test data of Percent Duds versus Bore Enlargement on the lands at the origin of bore. As can be seen, the extreme boundary of the dud rate envelope remained at less than 10% up to a bore enlargement of 3.04 mm (0.120 inches). The average dud rate exceeded 15% at a bore enlargement of 3.55 mm (0.140 inches). It should be noted that this average dud rate occurred in a gun barrel (5"/54 MARK 18 MOD 1, S/N 16838) which was worn with PYRO propellant in cold gun firing and that this condition is not detectibly different from a worst case NACO fleet fired profile. It should also be noted that as control of projectile seating distance is lost at a bore enlargement of 4.82 mm (0.190 inches) to 5.08 mm (0.200 inches), i.e., the projectile seats in the erosion slope, the percent dud rate varies between 0% and 100% in startling fashion. Obviously, the bore enlargement, while serving as a reasonable screening parameter, is not adequate to predict the dud rate.

As detailed in reference (c), an analysis of variance was conducted on 34 gun wear conditions represented by the six-barrel and 924-round sample. Twenty-six variable distances to various diameters in the erosion cone and average slopes between these diameters were considered. The objective of the analysis was to determine which variables were significant contributors to the dud problem. If any of the variables were significant, then it could be concluded that the details of the engraving process were controlling the dud rate as already indicated by experimental shock measurement.

The analysis of variance resulted in the identification of three variables which account for 71% of the dud rate variation and have obvious physical significance as explained in reference (c) as follows:

"The largest contributor is the distance between the 5.200" and 5.120" diameter in the gun barrel. This parameter represents the "free run" distance and is an indicator of the projectile velocity obtained just prior to the beginning of the engraving process. The significance of the two diameters can be seen from Figure 18*, the

*Figure 13 of this paper.

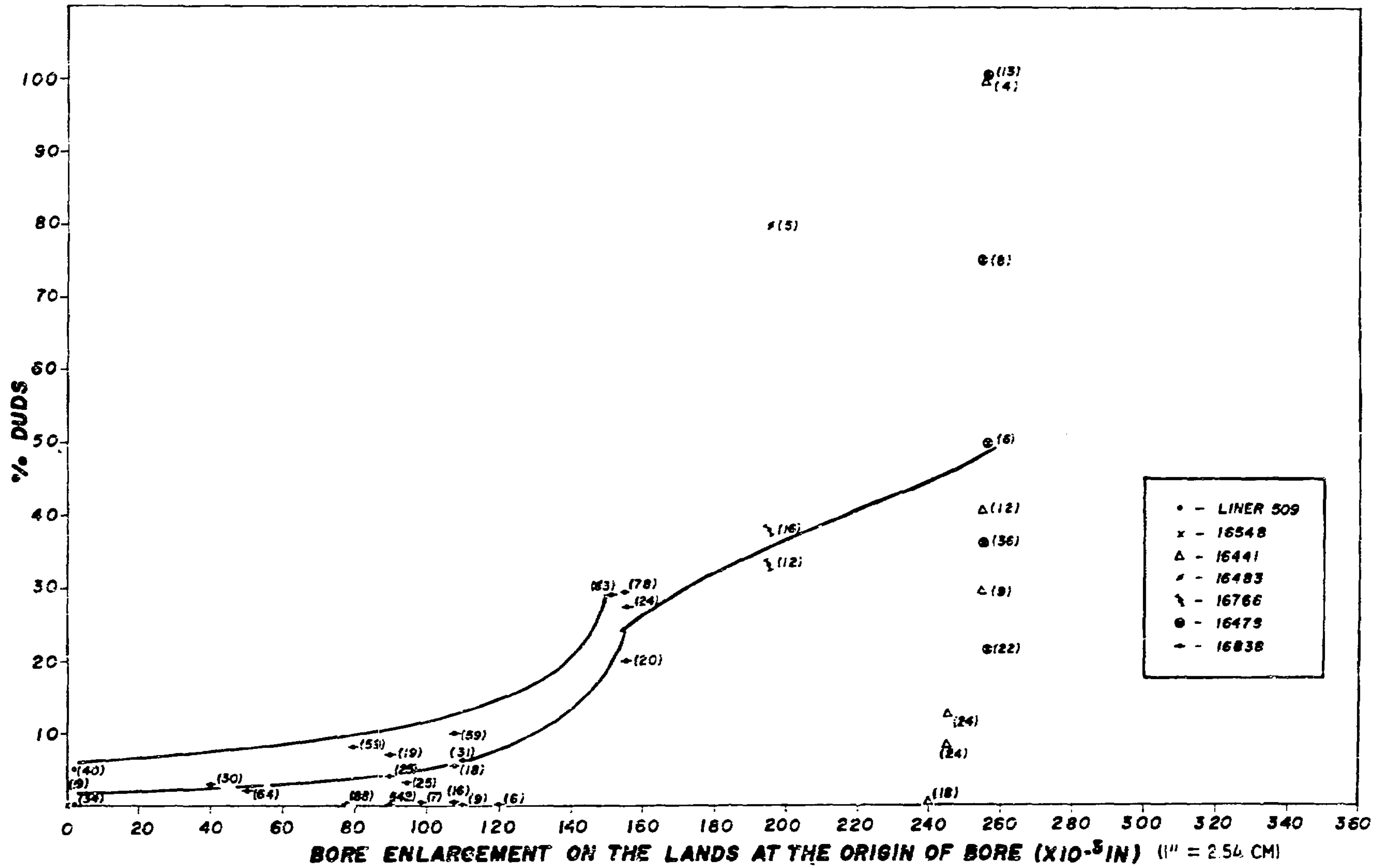
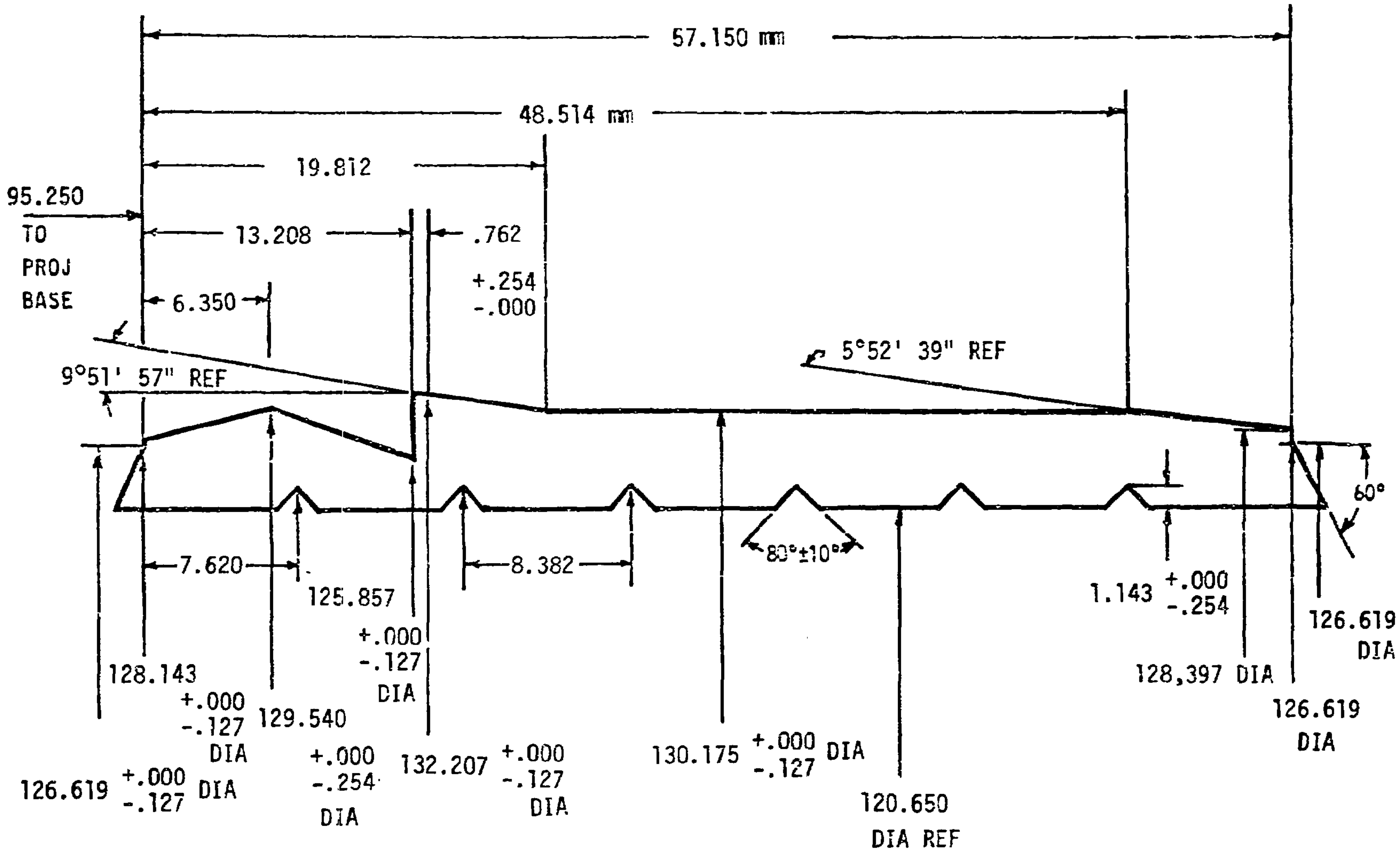


FIGURE 12

5"/54 VT FUZE PERFORMANCES (WITH MARK 18 RFSD) vs. BORE ENLARGEMENT



BAND CONFIGURATION 5"/54 PROJECTILE

FIGURE 13

rotating band configuration. Initially, the projectile is seated on the high lip (5.200" diameter). On firing, this lip squeezes down under relatively little force. The maximum diameter of the bulk of the band is about 5.120". Thus, when the projectile rotating band reaches this diameter in the gun barrel, a shock occurs. The "free run" distance accounts for 39% of the variation in dud rate.

The second most significant variable is the average slope between the 5.120" diameter and the 5.100" diameter, which accounts for an additional 18.3 percent of the variation in dud rate. This variable physically represents the rate of plastic flow of the band material prior to the cutting of the rifling indentation in the band. Note that the maximum depth of the rifling grooves is 0.100" diametrally and that in a worn gun, this depth is achieved in a gradual slope. The swagging of the rotating band from 5.120" diameter to 5.100" diameter absorbs a considerable amount of energy and contributes to the duration of the shock loading.

The final major contributor is the distance between the 5.200" diameter and the 5.050" diameter; i.e., the distance to the point at which 50 percent engraving is achieved together with the "free run" velocity during engraving. This variable explains 14.9 percent of the variation in dud rate."

Table 2 contains the prediction model for dud rate in nine variables. Using this model, an estimate of the dud rate within ±13% of the MARK 18/MARK 42 RFSD may be made based on five barrel measurements. This model accounts for 86% of the variation in MARK 18/MARK 42 RFSD dud rate.

RANGE DISPERSION

The final element in the "erosion criteria" is range dispersion. No Navy standard of acceptable range dispersion level has been established although a mean absolute dispersion level is used in projectile body acceptance testing, as given below:

$$\frac{D}{\overline{R}} = \sum_{1=i}^{n} \frac{Ri - \overline{R}}{n\overline{R}} \leq .007$$

where: Ri = the range of the observation
$\overline{R}$ = the average range
n = the number of observations
D = absolute mean dispersion

TABLE 2*

DUD RATE PREDICTION MODEL FOR 5"/54 GUN BARRELS

$$\%N = -150.6 - 36.7\,[S(200) - S(120)] + 12.66\,[S(200) - S(100)] - 6.404\,[S(200) - S(50)]$$

$$+5.44\,[S(200) - S(0)] - 1773.4\left[\frac{.200 - .120}{S(200) - S(120)}\right] + 2402.7\left[\frac{.200 - .100}{S(200) - S(100)}\right]$$

$$-139.8\left[\frac{.120 - .100}{S(120) - S(100)}\right] - 194.9\left[\frac{.100 - .050}{S(100) - S(50)}\right] + 2173.3\left[\frac{.050 - .000}{S(50) - S(0)}\right]$$

The standard error of the estimate = 19.37%

Where: S = distance from breechface to first $\frac{5 + (\quad)\text{" diameter}}{1000}$

N = % Duds

*Table VI from reference (c)

The Army uses a standard of

$$\frac{\text{Range PE}}{\bar{R}} \leq .0027$$

where: PE = probable error
$\bar{R}$ = average range

A typical set of U. S. Army data can be seen in Figure 14 from reference (i). Note that the range probable error is equal to .6745 Range Standard Deviation.

Two elements enter into the range standard deviation as sources of variation attributable to the erosion state of the gun barrel:

1. Random variation in muzzle velocity due to random variation in projectile seating distance.

2. Random variation in initial projectile yaw angle due to in-bore projectile dynamics. This action, if present, produces muzzle erosion and projectile body engraving.

Figures 15, 16 and 17 (from reference (c)) are plots of range standard deviation versus range for three different levels in muzzle velocity standard deviation at constant muzzle velocity for 5"/54 MARK 41/MARK 61/MARK 64/MARK 65 projectiles assuming a 0% standard deviation in ballistic coefficient and a 2.4 minute standard deviation in initial launch angle. The muzzle velocity levels chosen were 807.4 m/sec, 762.2 m/sec and 701.2 m/sec (2650 ft/sec, 2500 ft/sec and 2300 ft/sec). The range standard deviation at each muzzle velocity level is approximately constant at each level of muzzle velocity standard deviation. Thus, as can be seen, the muzzle velocity standard deviation is the dominant factor in the range standard deviation. Any increase in the muzzle velocity standard deviation due to gun erosion conditions will result in increased range standard deviation.

The second contributor, increased angle of initial yaw, resulting in increased range dispersion and occasional short rounds, seems to be absent in the 5"/54 gun system. The contribution seems to arise from improper centering of the projectile in the gun bore, and it is to this cause that the increase in range probable error with erosion seen in U. S. Army guns is attributed. Apparently, the excellent design work in the development of Naval projectiles and the use of the high lipped nonfringing rotating band, introduced in 1928, reference (g) has prevented this problem area.

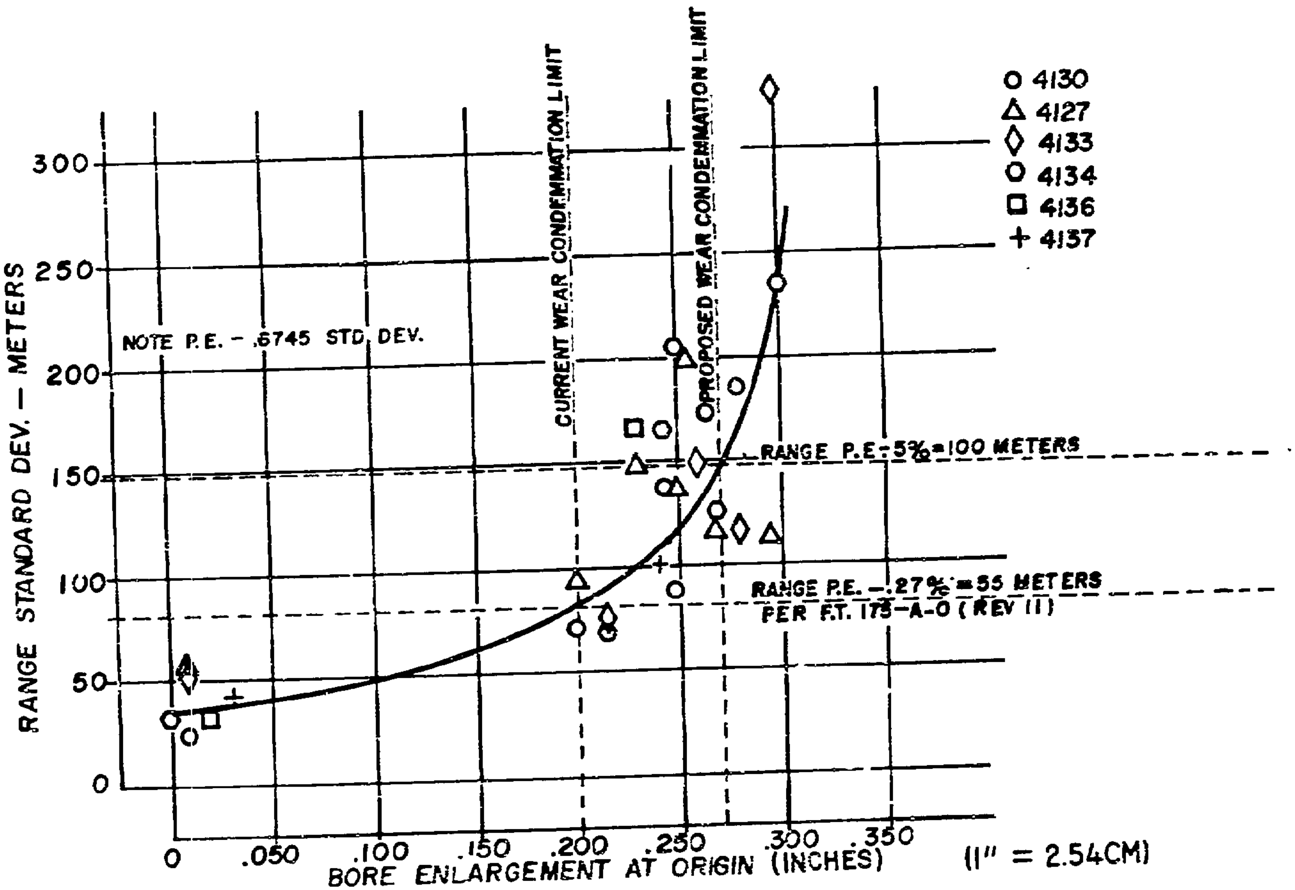


FIGURE 14

175mm AUTOFRETTAGED GUN TUBE M113E1 PRECISION AT 20,000 METERS

FROM WATERVLIET ARSENAL FEB. 1969

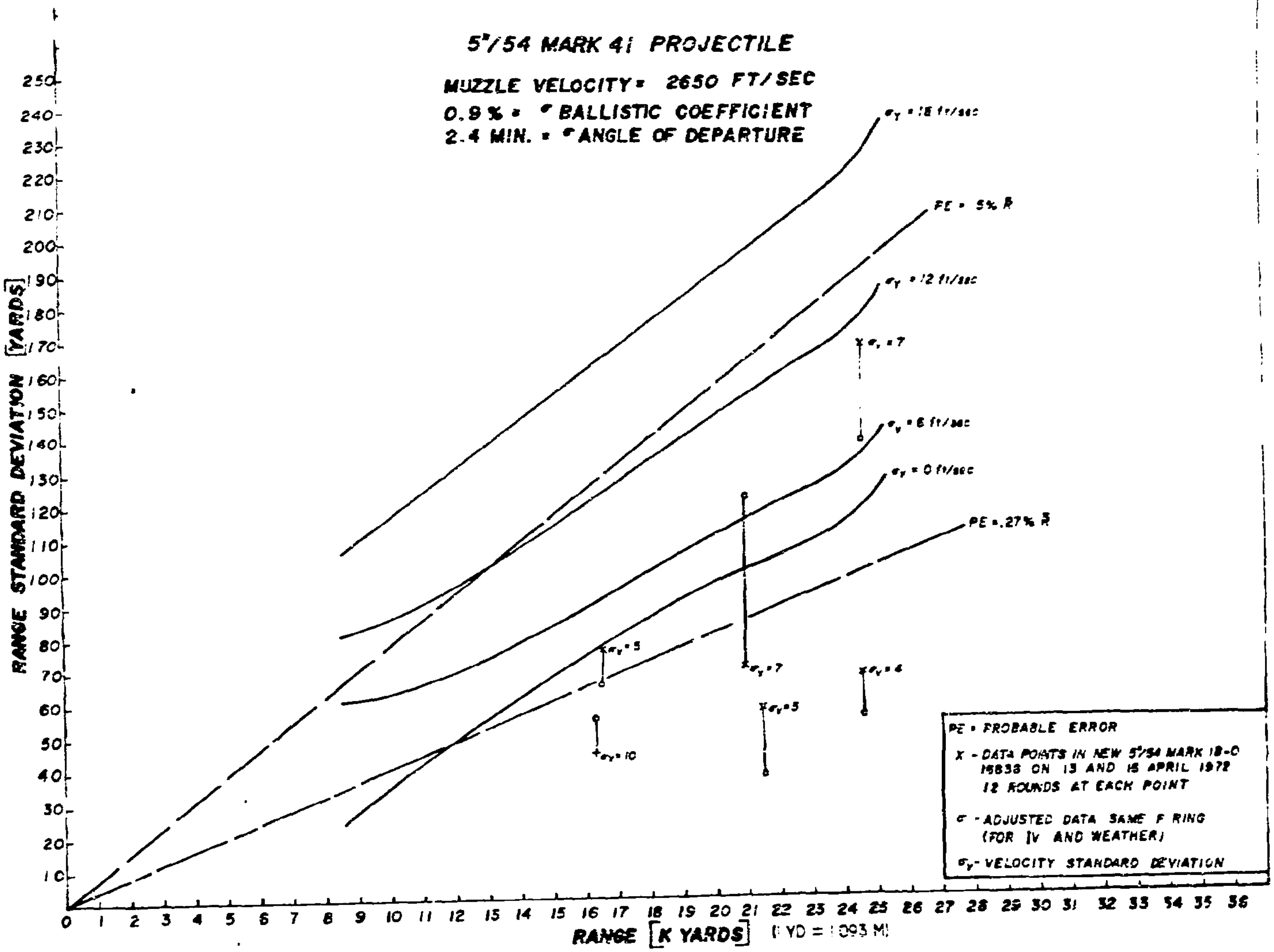


FIGURE 15

RANGE STANDARD DEVIATION vs. RANGE

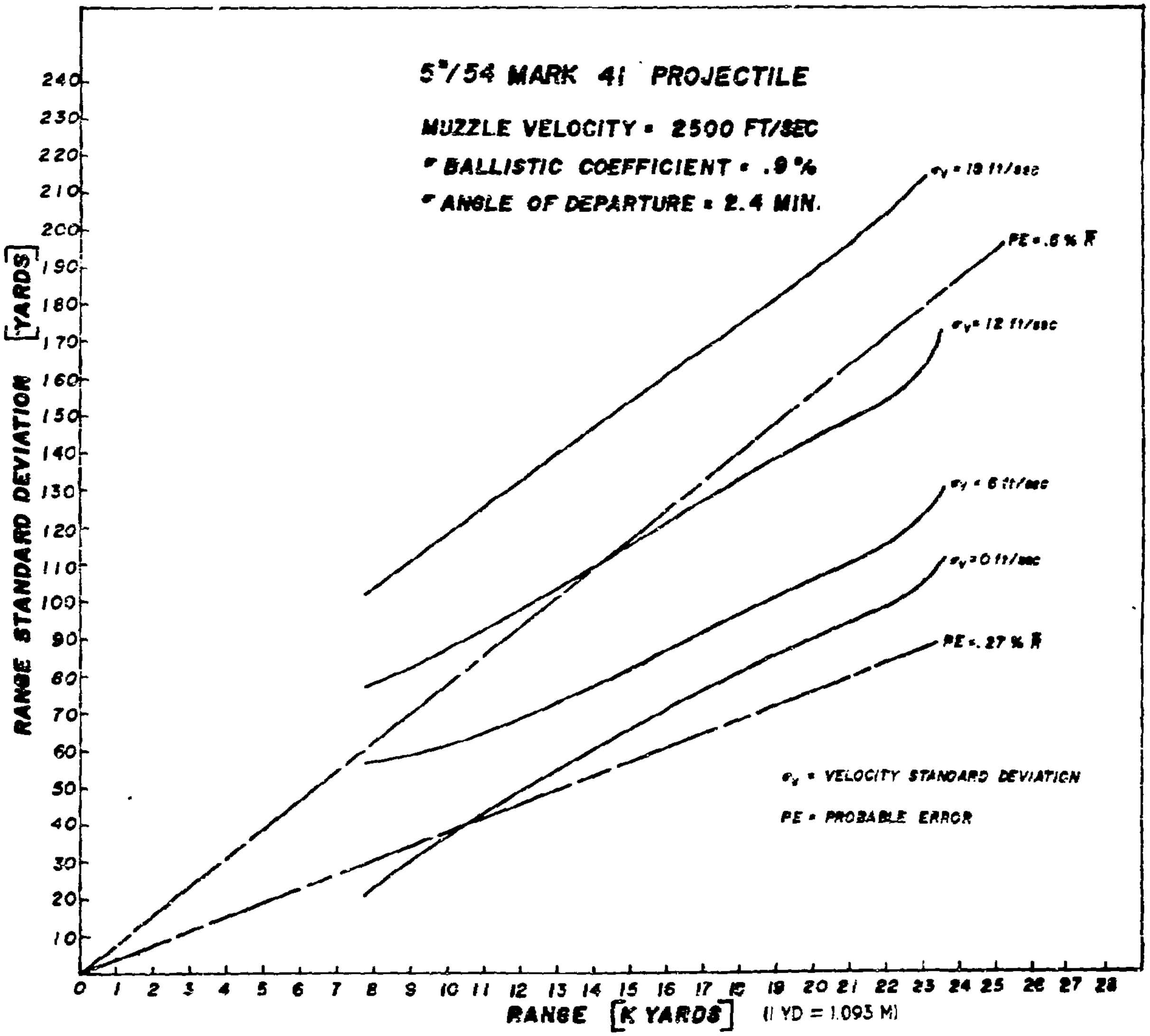


FIGURE 16

RANGE STANDARD DEVIATION vs. RANGE

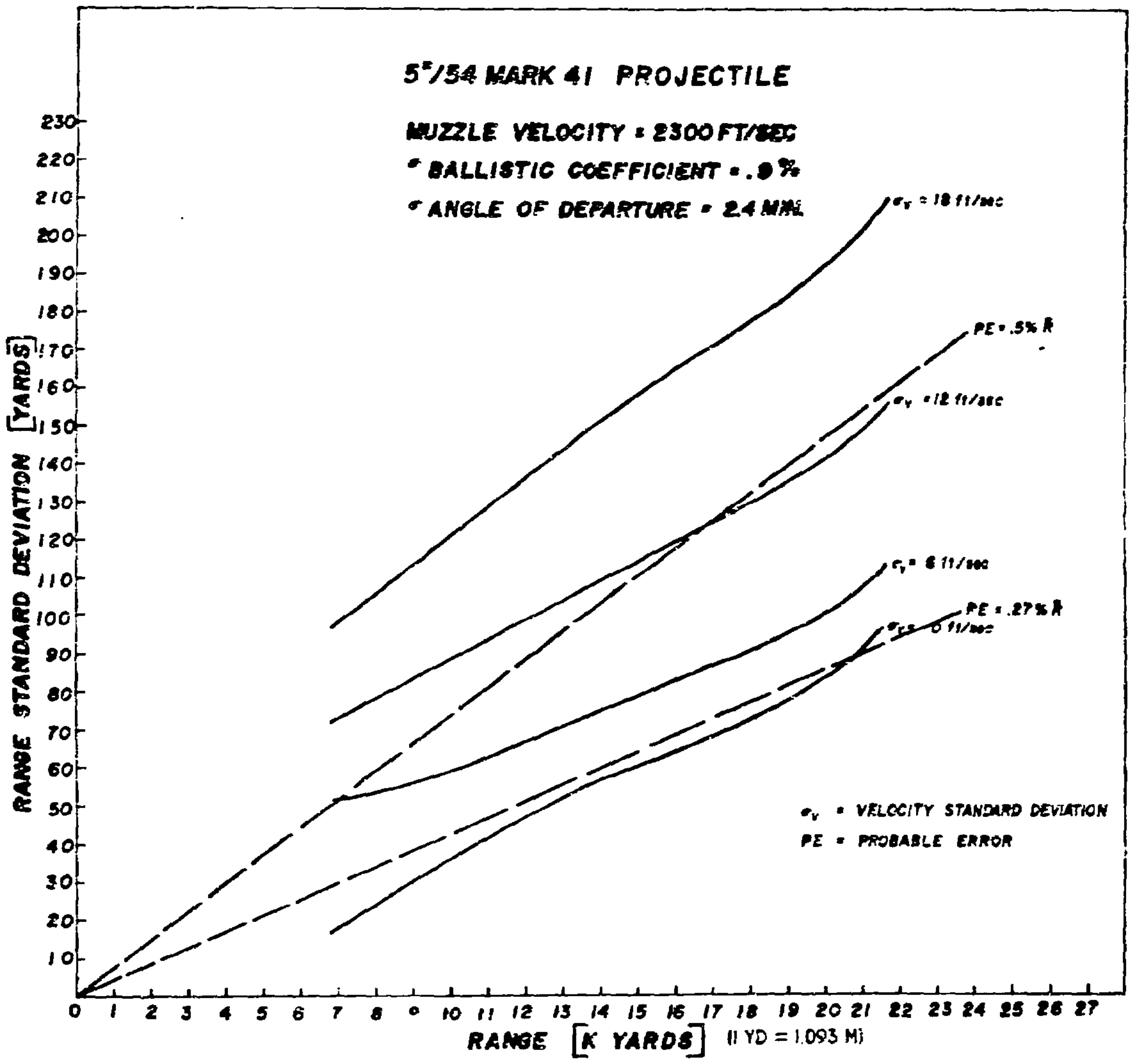


FIGURE 17

RANGE STANDARD DEVIATION vs. RANGE

The only effect expected to produce an increased range standard deviation in 5"/54 service ammunition is the variation in projectile seating distance due to projectile seating on a shallow erosion slope (OBE* > 5.08 mm (.200")). This variation in projectile seating distance has been observed in PYRO worn guns to be as large as 76.2 mm (3") and to average 25.4 mm (1"). This situation introduces a random variation in velocity, having standard deviation as large as 2.1 m/sec (7 ft/sec) and averaging 0.61 m/sec (2 ft/sec) provided that the rotating band high lip tolerance is normally distributed. Table 3 contains the effect on velocity standard deviation of this situation.

If the rotating band high lip tolerance is not normally distributed, but rather slowly varies through a projectile lot from the minimum level to the maximum level, then for the limited number of rounds taken from a single lot to be used for an "erosion check" on a gun barrel, no effect will be noted. There is some evidence that this is what has happened in many NWL firings.

On the other hand,, if by some accident, two sets of projectiles, one at the upper and one at the lower limit of the high lip band tolerance, were fired from the same worn gun (OBE > 5.08 mm (.200)) in a fleet firing, then two overlapping normal distributions would result. The average difference between means would be 4.5 m/sec (15 ft/sec) and the maximum difference between means would be 13.7 m/sec (45 ft/sec). Table 4 contains the effect of such a situation on velocity standard deviation.

SUMMARY AND CONCLUSIONS

General - For the purposes of establishing "erosion criteria", the 5"/54 MARK 18 MOD 0, MARK 18 MOD 1, MARK 18 MOD 3, MARK 18 MOD 4, MARK 18 MOD 5, MARK 19 MOD 0 and MARK 19 MOD 1 gun barrels may be safely considered a single design. The curves of muzzle velocity versus projectile seating distance and muzzle velocity versus bore enlargement are identical for each gun barrel firing the same complete round. The following results are summarized by the three elements of the "erosion criteria."

Reduction in Muzzle Velocity. The operational impact of velocity is principally a substantial reduction in maximum range capability. As presently allowed, a velocity loss of 108 m/sec (355 ft/sec) is a reduction in maximum range on the 5"/54 gun systems of about 3486.2 meters (3800 yards).

---

*Origin Bore Enlargement

Table 3

VELOCITY STANDARD DEVIATION
EFFECT OF RANDOM SEATING AND POWDER

σ_{vp}	σ_{vs} m/sec (ft/sec) 0.61 (2)	2.1 (7)
	σ_v m/sec (ft/sec)	
1.8 (6)	1.92 (6.3)	2.80 (9.2)
3.6 (12)	3.72 (12.2)	4.24 (13.9)
5.4 (18)	5.52 (18.1)	5.88 (19.3)

σ_{vp} = standard deviation in velocity due to powder

σ_{vs} = standard deviation in velocity due to projectile seating

σ_v = standard deviation in velocity

Table 4

VELOCITY STANDARD DEVIATION EFFECT
OF EXTREME DIFFERENCES IN SEATING AND POWDER

σ_{vp}	ΔV m/sec (ft/sec)	
	4.6 (15)	13.7 (45)
	σ_V m/sec (ft/sec)	
1.8 (6)	2.59 (8.5)	[illegible].59 (8.5)
3.6 (12)	5.18 (17.0)	5.18 (17.0)
5.4 (18)	7.80 (25.6)	7.80 (25.6)

σ_V = standard deviation in velocity

σ_{vp} = standard deviation due to powder

ΔV = difference in velocity mean due to extreme seating

The best muzzle velocity calibration technique based upon erosion measurement is the correlation based upon projectile seating distance which is controlled by projectile rotating band design.

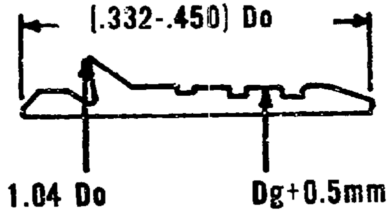
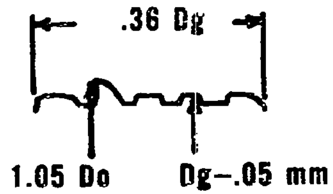
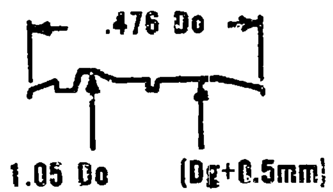
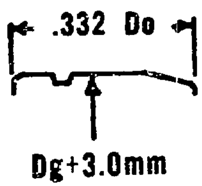
The percent of allowable bore enlargement for various rotating band designs is shown in Figure 18 for typical Navy and Army gun systems.

VT Fuze Performance. The dud rate experienced in developmental and acceptance testing of VT fuzes equipped with MARK 18/MARK 42 RFSD is definitely the result of shock loading generated during the engraving process in worn gun barrels. This dud rate is sufficiently severe to constitute a new "erosion criteria" for 5"/54 MARK 18 and MARK 19 gun barrels. Dud rates of other fuzes are not known; however, laboratory data indicate that all fuzes are shock sensitive. However, a recent product improvement program for the RFSD has demonstrated acceptable performance when fired in guns more severely worn than those of this study.

A nine-variable linear algebraic model based upon five barrel erosion measurements allows the prediction of the MARK 18/MARK 42 RFSD dud rate. This model explains 86% of the dud rate variation and has a probable error of 13%. Three variables in this model dominate, explaining 72% of the dud rate. Each variable has a significant physical interpretation.

Range Dispersion. For the 5"/54 service round, range dispersion is dominated by the muzzle velocity variation. The only situation in which erosion state influences this variation is for an extremely worn gun (bore enlargement greater than 5.08 mm (0.200") when the projectile is seating on the erosion slope and the rotating band high lip diameter varies.

No evidence can be found in the 5"/54 system for an increase in range standard deviation due to the effects of either in-bore ballotting affecting the initial launch angle, or the projectile not obtaining sufficient spin for stability or stripping of the rotating band. Rotating band fringing had been eliminated by design by undercutting the space behind the band lip.

SHAPE	GUN SYSTEM	% ALLOWABLE BORE ENLARGEMENT
	NAVY METALLIC BAND	4–6
	175mm M 437	3
	120mm M-73	5
	155 mm M101 (GUN)	2

Dg=DIA GROOVES
Do=BORE DIAMETER

CONDEMNATION CRITERIA FOR ROTATING BAND DESIGNS

FIGURE18

REFERENCES

(a) NOL ltr 702:VFC:dlt 8010 Ser 6529 of 6 November 1973

(b) NWL/Concord Confidential ltr Ser 0196 of 13 November 1973

(c) NWL TR-3152 of August 1974

(d) NAVORDSYSCOM OP 1429 of 16 March 1954

(e) BUORD Confidential ltr Re5a-FBW:cmj NORD 10377 of 4 June 1951

(f) NAVORDSYSCOM OP 1182 of 24 March 1958, Change 6 of

(g) NWL TR-3081 of March 1974

(h) The Influence of Ignition Phenomena in Gun Propelling Charges upon Muzzle Velocity and Ammunition Safety, M. C. Shamblen, et al. Proceedings of the International Symposium on Gun Propellants, 15-19 October 1973

(i) The Wear-Accuracy Life of the 175mm M113E1 Gun Tube, Watervliet Arsenal Interim Report of March 1969

(j) Jeansen, C. F., Notes on Gun Chambers, Projectile Bands, and Accuracy Life of U. S. Naval Guns of February 1937

CANNON WEAR
SINGLE SHOT TESTING METHOD

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US Army Armament Research & Development Command
Large Caliber Weapon Systems Lab., Energetics Application Branch

Gun wear presents a major logistic problem and historically efforts to improve cannon life have been thawarted. In the main, in order to establish the effectiveness of any approach it was necessary that extensive, expensive, and time consuming gun tests be conducted. A gross technical penalty resulted that limited the evaluation of new design concepts. This in turn stagnated studies to establish the mechanism of cannon wear.

Until recently, the high performance tank weapons were the main target regarding cannon wear studies and the wear problem for tank systems are well in hand. Artillery systems historically did not present cannon wear problems. Artillery weapons were characteristically low performance systems for which low energy cool propellants could be used. Such systems had cannon lives in excess of 10,000 rounds. On the other hand for tank weapons a cannon life of 1000 rounds is considered excellent.

Advanced artillery weapon systems of recent vintage no longer are blessed by the desired extensive cannon life. The newer artillery systems are often referred to as "Gun Howitzers" and have the following characteristics:

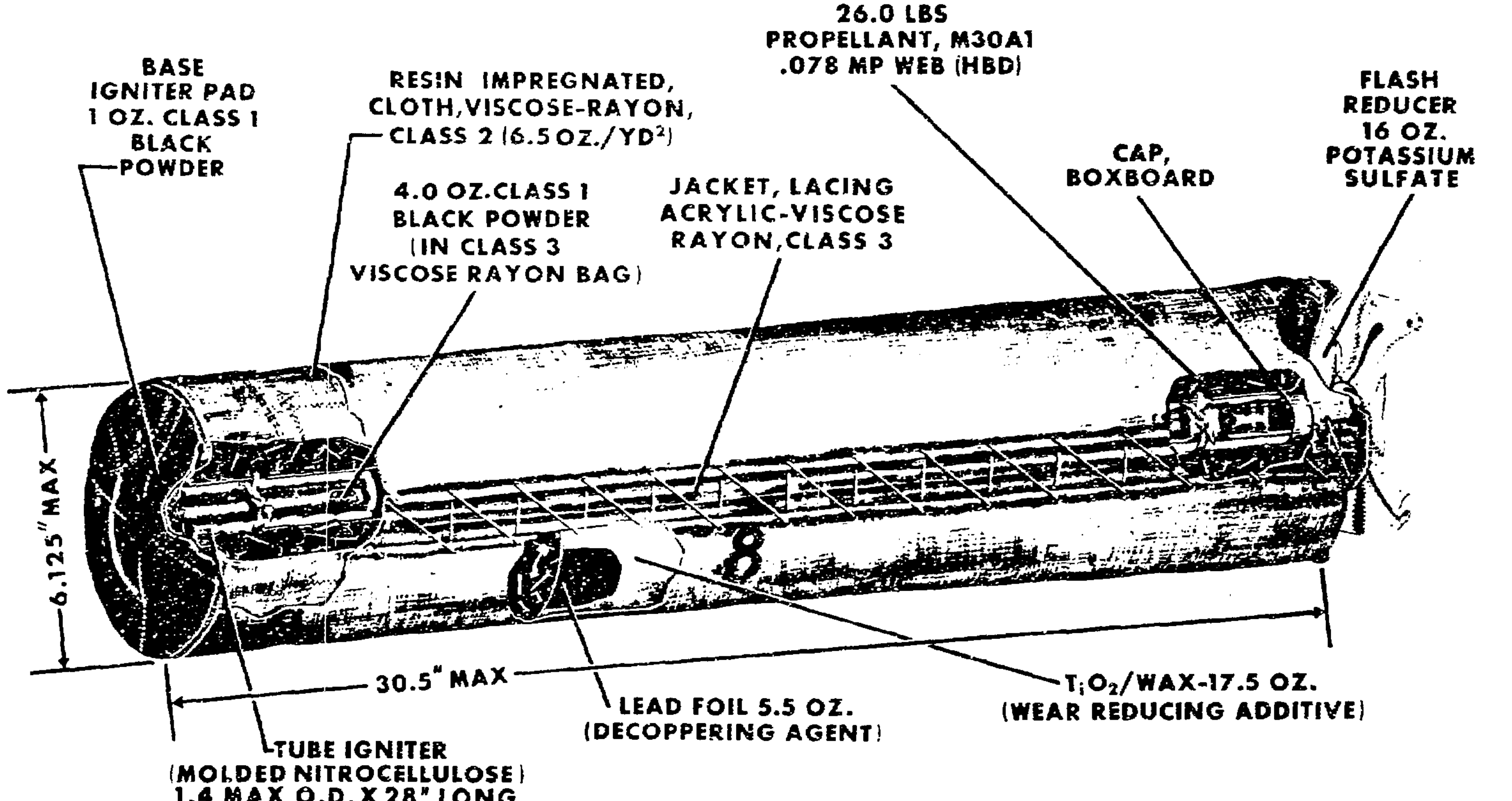
a. The weapons are more like guns.

b. The performance levels are high and require high energy propellant having high flame temperatures.

c. The cannon wear rates are very high.

The cannon life of the various fielded artillery weapons can be seen in Table I. In each case, with the exception of the M119 charge, wear reducing additives are required to achieve even modest cannon life. Although the cooler M6 propellant can be expected to provide greater tube life (even without additive), it is apparent that in the 175MM, M107 Weapon firing the M86 charge (M6 Propellant with additive) induces a relatively high wear rate. Another anomoly is noted with regard to the M203 (Z8) and XM201E2 (Z7) for the 155MM Howitzer, XM198. With the exception of the ignition train they are similar, in that, each charge contains M30A1 propellant and each has titanium dioxide/wax wear reducing additive (see figures 1 and 2). The

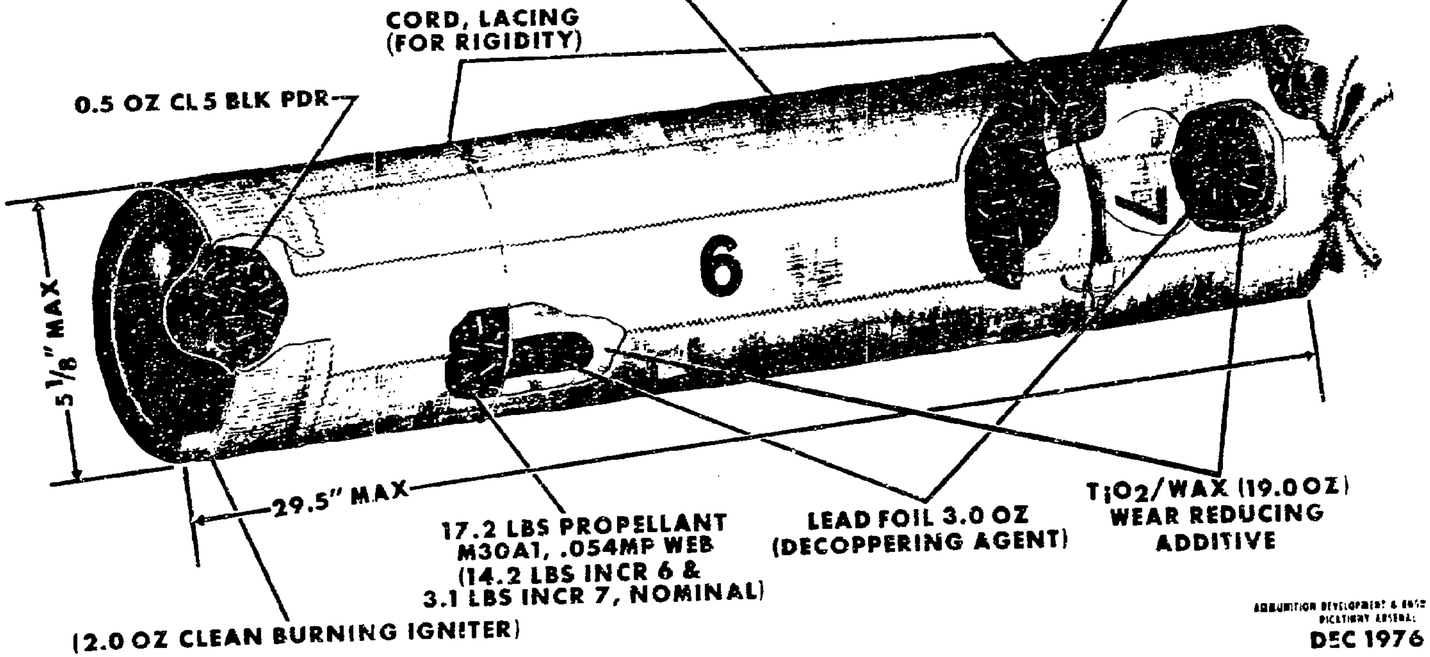
CHARGE, PROPELLING, 155MM, M203
(FINAL DESIGN, XM203E2)



AMMUNITION DEVELOPMENT & ENGR DIRECTORATE
PICATINNY ARSENAL
OCT 1976

CHARGE, PROPELLING, 155MM, XM201E2 (WHITE BAG)

FINAL ED DESIGN



AMMUNITION DEVELOPMENT & ENG DIRECTORATE
PICATINNY ARSENAL
DEC 1976

comparative tube life of these charges is contrary to expected performance, in that the higher performing M203 induces a lower wear rate than the XM201E2 charge. In addition, the XM119E4 and the XM201E2 also with the exception of the ignition train are essentially the same design and provide the same ballistic performance; and yet, the tube life with the XM201E2 charge is approximately 55% of that achieved with the XM119E4 charge. The comparative wear induced by the M188 and M188E1 charges for the 8" M110E2 Howitzer, on the other hand, follows expected performance.

In summary there is no clear cut definition of the factors which induce tube wear and many are the postulations.

As evidence from the data just presented the following questions are posed:

a. Why is tube life higher with the M203 than for the XM201E2 charge?

b. Why was wear greater with the XM201E2 than the XM119E4?

c. What design modifications are required to achieve decreased tube wear with the XM201E2 charge?

In order to answer these questions rapidly and inexpensively an analysis method based upon a single shot technique was needed. This need was recognized several years ago and Calspan under contract to Picatinny Arsenal developed a method which use wear sensors in the form of probes. (Figure 3).

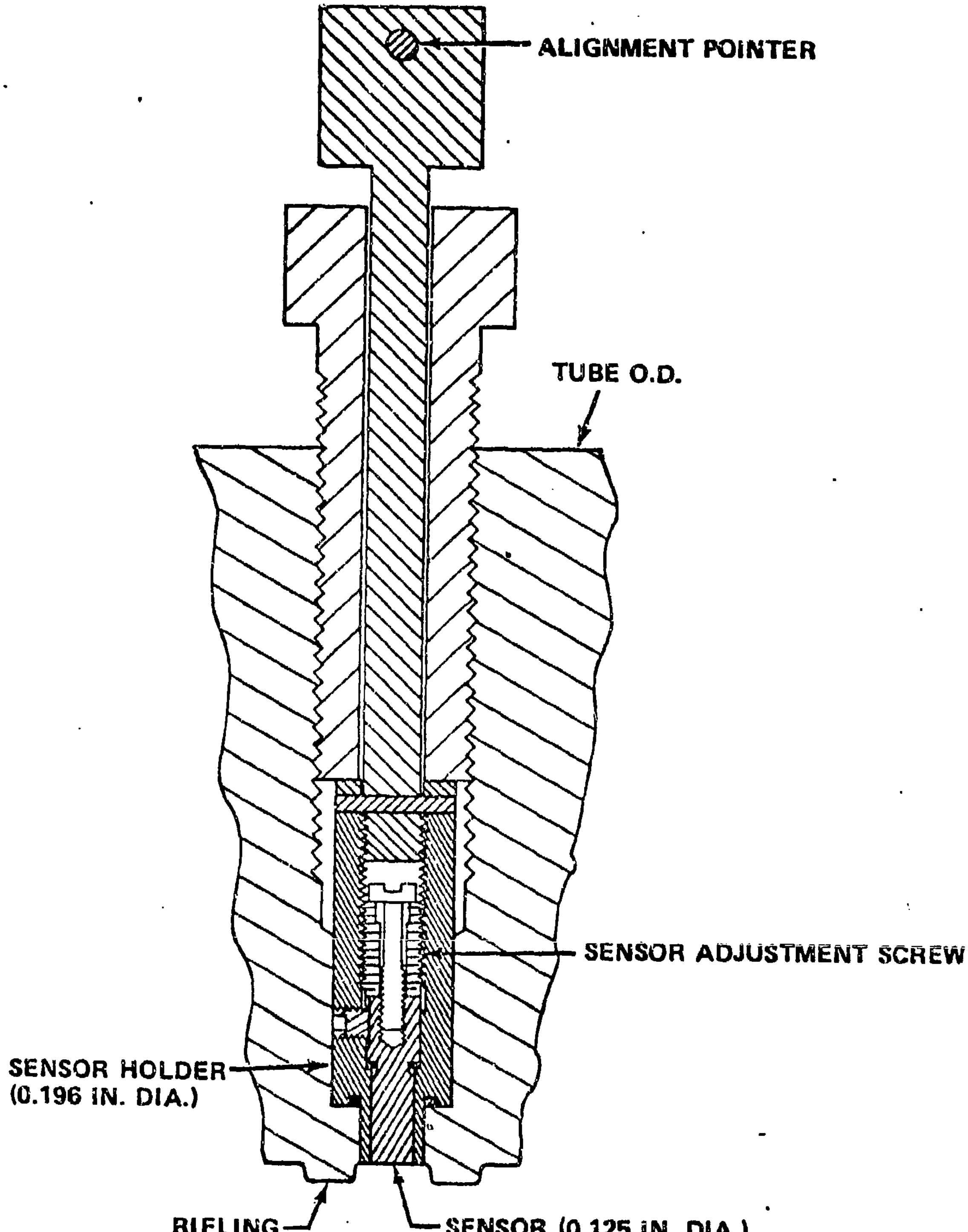
The wear sensors have a highly polished surface on one end containing calibrated indentations called Knoops. The sensor is assembled into the gun wall and the polished surface is precisely oriented to be coincident to the inner surface of the tube (see figure 4).

Figure 5 is a view of the surface of a typical wear sensor before and after firing five shots. In this case the charge used was the M201E2. A notable change in the definition of the knoops is observed after firing.

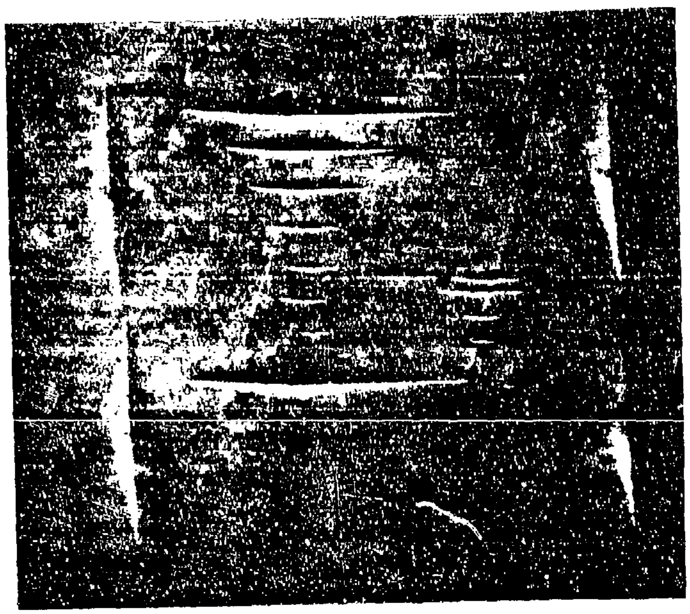
Although it is not possible to correlate the data generated with wear sensors to actual gun life, it is viewed as an excellent quantitative method for determining the relative performance of charge design iterations. This method was applied to the XM201 charge using the performance of the M119 charge as the reference.

FIGURE 3

WEAR SENSOR POSITIONED IN CANNON WALL

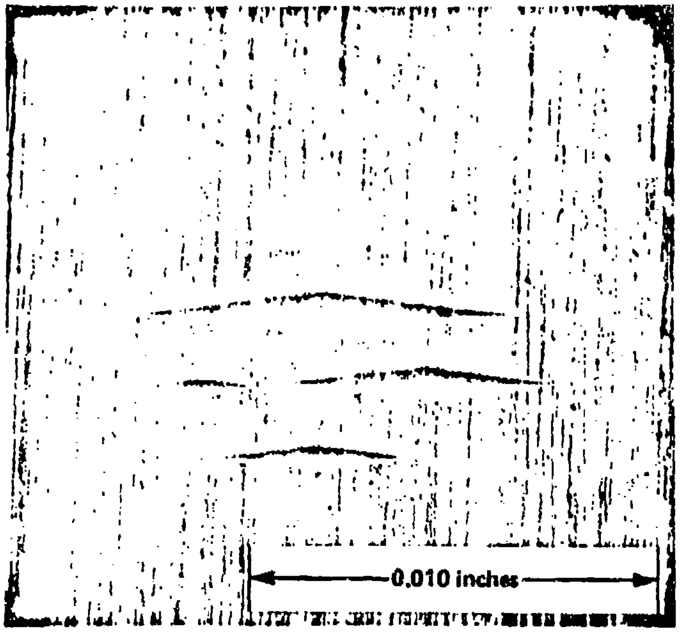


40X

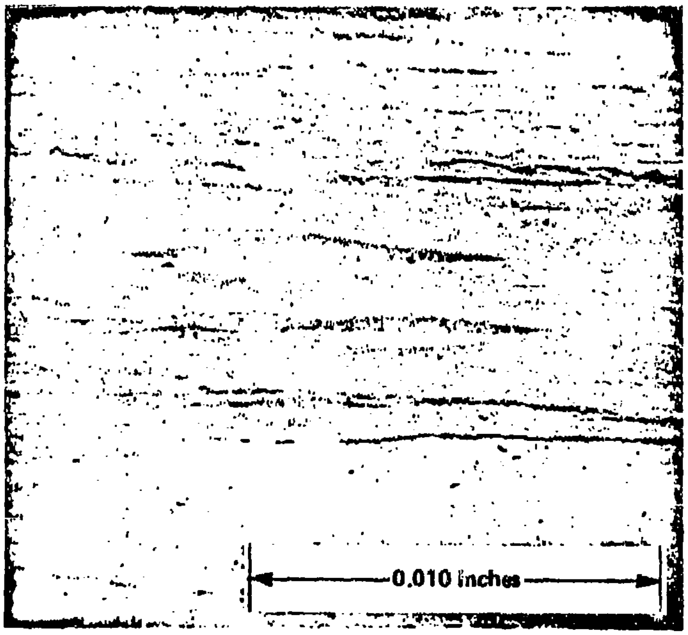


400X

Figure 4 TWO MAGNIFICATIONS OF EROSION SENSOR SHOWING KNOOP ARRANGEMENT



STEEL BEFORE FIRING



STEEL AFTER 5 SHOTS
(LOSS 30-40 MICROINCH)

ORIGIN EROSION, STD XM201E2
(SERIES 26) FIGURE 5

The design iterations included variations in:

a. Ignition trains.

b. Propellant types.

c. Type, amount geometry and orientation of the wear reducing additive.

In all, 34 iterations of charges were evaluated (Table 2).

Table 3 singles out the relative wear of standard and experimental charges used in the 155MM Howitzer XM198. While Table 4 addresses results obtained with the various XM201E2 modifications. Discussion on the results of wear at a location which is coincident with the origin of rifling, follows:

(1) The rating of the various standard and some experimental charges using the wear sensors correlate quite well with the performance of these charges in extensive gun firings. The M4A2 and M119 exhibited the least sensor wear (less than 5 microns) and the respective tube life with these charges are 25,000 and over 5,000 rounds. On the other hand, the XM203E2 and XM201E2 which exhibited higher sensor wear (5-10 and 30-40 microns respectively) for 5 round groups also when fired in conventional cannon exhibited cannon lives of 1800 and 1200 rounds respectively.

(2) Tests conducted with the various XM201E2 charge modifications were rated against the performance of the M119 charge using the single shot wear sensor technique. The sample size for each modification was 5 charges. Table 4 lists 19 iterations in the order of least wear.

It is apparent when comparing these iterations with the M119 charge that none did as well except for the one which used silicone ablative. This design option is one which could not be readily adopted because of constraints on time and dollars which would be required to engineer a practical method for achieving a configuration that could be efficiently handled in field use.

The results indicate other charge design modifications which were appreciably better than the XM201E2 charge (5-10 microinches compared to 30-40 microinches) by a factor of 4 to 6. The analyses indicated, as expected, that the use of cooler propellants (M15, $T_v = 2590^{\circ}K$ and M6 $T_v = 2500^{\circ}K$), offers a means of improving wear. These options also would require an extensive engineering design cycle which would require a large dollar commitment.

TABLE I

ARTILLERY WEAPON SYSTEMS

CANNON LIFE

STANDARD & DEVELOPMENTAL

WEAPON	PROPELLANT	ADDITIVE	TUBE LIFE
175MM, M107	M6	X	1200 ROUNDS
M86A2 CHARGE			
8" M110E2			
M188 (Z-8)	M30A1	X	3000 ROUNDS
M188E1 (Z-9)	M30A1	X	1500 ROUNDS
155MM M109/M198			
M119	M6	NONE	5000+ ROUNDS
XM119E4	M30A1	X	2100 ROUNDS
M203 (Z-8)	M30A1	X	1800 ROUNDS
XM201E2 (Z-7)	M30A1	X	1200 ROUNDS

TABLE 2

XM201E2 CHARGE ITERATIONS

SERIES	SAMPLE SIZE	DATE FIRED 1976	CHARGE	ADDITIVE MOD.	IGNITION	PROPELLANT		PROJ.	REMARKS
						TYPE	WT (lbs)		
0	44	3/2-11	M4A2 STD.	NO ADDITIVE	BASE/3.5 oz CBI	M1	13.3	M107	CLEANING ROUNDS
1	3	3/2	XM201E2	CURRENT DESIGN	BASE/2.5 oz CBI	M30A1	17.2	M107	
2	3	3/2	XM201E2	NO ADDITIVE	BASE/2.5 oz CBI	M30A1	17.2	M107	
3	3	3/2	XM201E2	NO ADDITIVE	BASE/2.5 oz CBI	M15	20.0	M107	LEAD FOIL ONLY
4	3	3/2	XM201E2	NO ADDITIVE	BASE/2.5 oz CBI	M6	20.5	M107	LEAD FOIL ONLY
5	3	3/2	M119	NO ADDITIVE	CENTER CORE 2.0 oz CBI 2.5 oz. BENITE	M6	20.5	M107	
8	3	3/3	XM119E4	SINGLE-PIECE ADDITIVE-LINER	CENTER CORE 1.0 oz CBI 2.0 oz BENITE	M30A1	17.5	M107	
9	3	3/3	XM119E4	NO ADDITIVE	CENTER CORE 1.0 oz CBI 2.0 oz BENITE	M30A1	17.5	M107	
13	6	3/9	XM201E2	FLAPS	BASE/2.5 oz CBI	M30A1	17.2	M107	
14	6	3/8	XM201E2	ADDITIVE JACKET PLUS CURRENT ADDITIVE	BASE/2.5 oz CBI	M30A1	17.2	M107	
16	6	3/9	XM201E2	DOUBLE THICKNESS	BASE/2.5 oz CBI	M30A1	17.2	M107	
17	6	3/8	XM201E2	LINER POSITIONED IN CHAMBER	BASE/2.5 oz CBI	M30A1	17.2	M107	
18	3	3/8	XM201E2	POLYURETHANE LINER	BASE/2.5 oz CBI	M30A1	17.2	M107	TiO_2 LINER REMOVED
19	6	3/8	XM201E2	.75# SILICONE ABLATOR	BASE/2.5 oz CBI	M30A1	17.2	M107	TiO_2 NOT REMOVED
20	6	3/9	XM201E2	GREATER CONCENTRATION TiO_2	BASE/2.5 oz CBI	M30A1	17.2	M107	
22	8	3/3	XM203E2	CURRENT 17.5 oz ONE-PIECE	CENTER CORE 1.0 oz BP 4.0 oz BP	M30A1	26.1	M549	
23	3	3/1	XM208	17.0 oz ONE-PIECE	BASE/6.0 oz BP	M30A1 STICK	28.8	M549	CURRENT DESIGN
24	2	3/11	XM208	NO ADDITIVE	BASE/6.0 oz BP	M30A1 STICK	28.8	M549	
25	6	3/11	UK	POLYURETHANE LINER & DISC	BASE/2.1 oz CBI 0.5 oz BP	CORDITE STICK	27.1	M549	CURRENT UK DESIGN
26	5	3/3	XM201E2	CURRENT DESIGN	BASE/2.5 oz CBI	M30A1	17.2	M107	CONTROL ROUNDS
27	5	3/8	XM201E2	CURRENT DESIGN	BASE/3.5 oz CBI	M30A1	17.2	M107	CONTROL ROUNDS
28	5	3/10	XM201E2	CURRENT DESIGN	BASE/3.0 oz BP	M30A1	17.2	M107	
29	5	3/10	XM201E2	CURRENT DESIGN	BASE (DUAL) 0.5 oz BP 2.5 oz CBI	M30A1	17.2	M107	
30	5	3/10	XM201E2	NO ADDITIVE	BASE (DUAL) 0.5 oz BP 2.5 oz CBI	M15	19.3	M107	REDUCED CHG. FROM SERIES 3
31	5	3/10	XM201E2	NO ADDITIVE	BASE (DUAL) 0.5 oz BP 2.5 oz CBI	M6	20.0	M107	REDUCED CHG. FROM SERIES 4
32	5	3/11	XM201E2	DOUBLE THICKNESS	BASE-3.0 oz BP	M30A1	17.2	M107	SAME ADDITIVE AS SERIES 16
33	5	3/11	XM201E2	DOUBLE THICKNESS	BASE (DUAL) 0.5 oz BP 2.5 oz CBI	M30A1	17.2	M107	SAME ADDITIVE AS SERIES 16
34	3	3/10	XM201E2	CURRENT DESIGN	BASE 3.0 oz CBI	M30A1	17.2	M107	CONTROL ROUNDS

TABLE 3

STANDARD CHARGES
RELATIVE EROSION PERFORMANCE

SERIES	CHARGE TYPE	EROSION MICRO INCHES
0	STD M4A2	<5
5*	STD M119	<5
8*	STD XM119E4	5-10
26, 27, 34	STD XM201E2	30-40
9*	XM119E4 NO LINER	<10
25	UK CHARGE POLY LINER	-
22	STD XM203E2	5-10
23*	STD XM208	80-120
24*	XM208 NO LINER	150-200

*LESS THAN 5 SHOTS; BEST EXTRAPOLATION OF EQUIVALENT 5 SHOT EROSION

TABLE 4

XM201E2 MODIFICATIONS
RELATIVE EROSION PERFORMANCE (1)

SERIES NO.	MODIFICATION	EROSION MICRO INCHES
19	SILICONE ABLATOR	0-5
3*	M15 PROP., NO LINER	> 5
30	M15 PROP. + SPOT BASEPAD	COPPERED
31	M6 PROP. + SPOT BASEPAD	5-10
29	SPOT BASEPAD	5-10
33	SPOT BASEPAD, DOUBLE LINER	5-10
15	DOUBLE LINER	5-10
28	BLK. PWD. BASEPAD	5-10
32	BLK PWD. BASEPAD, DOUBLE LINER	5-10
4	M6 PROP. NO LINER (2)	> 15
13	LINER FLAPS	15-20
14	JACKET LINER	30-40
17	LINER IN CHAMBER	30
27	STD.	30-40
26	STD.	30-40
34*	STD.	-
18*	POLYURETHANE LINER	30-40
20	55% TiO_2 LINER	40
2*	NO LINER	50

(1) Sample Size 5 Shots
(2) *Less than 5 Shots; Best Extrapolation of Equivalent 5 Shot Erosion

The most readily achieved design modifications are represented by series No. 29 and 33. In series No. 29, the addition of a small supplementary charge of black powder in the base ignition pad reduced the wear (in a five round group) from 30-40 microinches (series 27 and 26) for the XM201E2 to 5-10 microinches (series 29). A similar improvement was achieved with a further modification where the quantity of additive was doubled. These results were quite enlightening since they indicated that the effectiveness of the additive was highly influenced by the ignition process.

In selecting a design iteration of the XM201E2 for further full scale engineering evaluation, a charge with redundent features was selected, that is, series 30.

This charge configuration in full scale gun tests resulted in a significant reduction in cannon wear rate, in that:

(1) The indicated cannon life with the XM201E1 charge was increased from 1200 rounds to over, 3000 rounds (estimated life 3500 rounds).

(2) The modified XM201E2 charge (series 30) would approach the 5000 life level of the M119 charge but would not achieve the performance of the latter.

Determination of wear by the single shot technique as a diagnostic method for determining the relative merit of charge designs against a known reference has been established. It is highly recommended as a development tool in the assessment of the wear potential of charge.

The work report in this paper was a joint effort of Large Caliber Weapon Systems Lab and BRL of ARRADCOM and the CALSPAN Corp (under government contract DAAA-21-73-C-0709).

A more detailed presentation of this work is contained in CALSPAN TECHNICAL REPORT NO. VL5337-D-1.

ESCA STUDY OF EROSION OF METALS BY GUN PROPELLANTS

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Abstract

A comparative study of erosion in Gun Steel #4340, Stainless Steel #303, MetGlas #2826A and gold has been conducted by applying the technique of Electron Spectroscopy for Chemical Analysis (ESCA). ESCA examines the debris left on the reacted surfaces and indicates what changes in the various atoms of the metal surface have taken place, even to the level of a monomolecular layer. The metals were exposed to burning gases of HMX and nitrocellulose. The corrosive action continues for days after the exposure of the metals to the burning propellants. HMX is found to be more reactive. The magnitude of the reaction of the metals studied followed the order in which the metals have been listed above. Steel #4340 has been found to be most reactive. The fact that MetGlas, having amorphous glassy structure, reacted least indicates that the microstructure of the metal plays an important role. Of the various atoms on the surfaces, Ni and Fe readily changed to oxides, nitrides and carbonyls. The chromium atoms on Stainless Steel and MetGlas showed maximum resistance to corrosion.

Introduction

The problem of gun barrel erosion is essentially as old as the gun barrel itself, but in relation to other problems, it has surfaced at various times with different degrees of importance. Also the problem is very often specific to a given gun system. The recent push for higher muzzle velocity, accuracy, and range has generated demands for more powerful propellants which are associated with greater erosion, so that the lifetime of the gun tube is limited more by erosion than by fatigue. Also the demand for low vulnerability propellants has drawn attention towards nitramine based formulations and although these yield lower flame temperature yet are alleged to cause greater erosion. The attempted solution to the problem spreads over various degrees of sophistications ranging from ad hoc additives like waxes (Ref 1), polyurethanes (Ref 2), silicones, sand, (SiO_2), Talc (Ref 3), TiO_2 (Ref 4,5,6) etc. on the one hand, to the complete reorientation of the problem by using plastic rotating bands and electroplating of the gun tubes with chromium (Ref 7) metal. Somewhere in between there are pet theories of gun barrel erosion, which have given rise to the various approaches and ad hoc solutions. It might be noticed that in various gun systems all the aspects of performance are stretched to the highest optimization, so that change of one parameter can lead to many adverse effects. So far only very limited efforts have been made to scientifically understand the problem and to finding technological solutions. It is estimated that in badly behaving guns microns of material are removed from the inner surface of the gun tube, during each firing. Probably the erosion consists of two overlapping steps, one involving the reaction of the gun surface with the hot and chemically reactive gases of the propellants, the other involving the removal of the loosened material by friction of the gas and the projectile.

The present paper focuses on the first problem. It addresses the following questions:

1. Do the decomposition products of the propellants undergo chemical reaction with the gun tube, at the temperatures and pressures generated in the guns?

2. What distinct roles in erosion are played by the various ingredients like nitrocellulose, nitroglycerine, nitroguanidine, HMX and RDX, etc.?

3. Do decomposition products like nitroso derivatives, nitramine ring polymers and NO_2 type molecular debris play an important role in the reaction?

4. What are the final relative ratios of the products of reaction e.g. oxides, nitrides and carbonyls of the metals?

5. What role is played by the microstructure of the steel?

6. Do the different component atoms of the steel on the surface have varying participations in the reaction?

So far, in this paper, the results of a preliminary study using Electron Spectroscopy for Chemical Analysis (ESCA) are being reported. It is planned that the ESCA study will be supplemented by Auger Electron Spectroscopy (AES) and Scanning Electron Microscopy (SEM) studies. Also it is planned that the ion peeling technique and depth profile studies will be exploited for quantitative erosion measurements. Once the relative role in erosion of various components of the propellants is understood, such a study may provide some guidance in the formulations of suitable propellant mixtures.

Also a better understanding of the erosion problem may lead to a new solution of the problem.

Experimental

The ESCA spectra were recorded with a Varian IEE-15 (Ref 8) photoelectron spectrometer using Mg anode, giving characteristic K-line at 1253.6 eV. The analyser was run at 100 volts yielding a resolving power of 1.5 eV. The spectrometer was run at 10^{-6} mm of Hg. pressure. The spectra were calibrated internally using the carbon 1s line at 285.0 eV. arising from the contamination hydrocarbon line found on all samples. In the preliminary study reported here, the metal cylinders machined from gun Steel #4340, Stainless Steel #303, ribbons of Allied Chemical MetGlas #2826A and from pure gold were used to provide a broad comparative basis. These specimen were exposed to burning propellants in calorimetric bombs where estimated temperatures of about 2500 K and pressures in the range of 10,000 psi were generated. The specimen surfaces were studied with ESCA technique before and after burning.

Results

The ESCA study has revealed the following facts.

1. Of the metals studied gun steel #4340 exhibited maximum reaction of the surface. Stainless steel showed lesser reaction. MetGlas #2826A exhibited least reaction, if the ribbons were in contact with the walls of the calorimeter. Gold hardly showed any reaction.

2. The corrosion process continued for days after the metals had been exposed to the burning gases.

3. The 1s N spectra of the debris left on the specimen showed the presence of the decomposed products of the two propellants used. On washing the specimen with acetone the unreacted debris could be removed but the reacted part remained chemi-absorbed. HMX left its ring polymer with most of the NO_2 limbs missing, on the other hand nitrocellulose left its nitroso products. Thus there is evidence for different reactions taking place in the two cases.

4. The 1s spectra of C, N and O on the specimen also revealed that carbonyls, nitrides and oxides of the metals are produced.

5. The spectra of Fe, Ni, Cr, P and B showed that after burning the atoms of Fe and Ni were no longer seen in metallic state within the sampling depth of the technique. The chromium atoms on

stainless steel and on MetGlas still partially showed up in metallic state. The reacted chromium atoms showed up in two different oxidative states. The P and B atoms on MetGlas showed partial oxidation.

Discussion

Taking into consideration the two facts that the sampling depth of ESCA is 15 A (10^{-7} inches) and that the chromium atoms still show metallic nature after firing, it can be concluded that the erosion of chromium atoms is less than 10^{-7} inches deep. In other words, if any other complication did not crop up, chromium surface could be expected to give a life of 10 million firings. Thus a push in the direction of using chromium as a protective coating on the inside of the gun tube has good prospects of success.

The fact that MetGlas showed most resistance to erosion leads to the belief that the microstructure of steel does play a very significant part in the erosion process.

The above mentioned results also show that the ESCA technique looks very promising for investigating the chemical aspects of gun barrel erosion problem in details. Combining this with Auger Electron Spectroscopy and Scanning Electron Microscopy is likely to yield useful results. Also by collecting the exhaust gases and debris from the gun firings and examining them with these techniques, can identify the products removed from the surface and thrown outside the gun.

Acknowledgments

The author is thankful to Dr. R.F. Walker, Director of the laboratory, for suggesting that ESCA technique should be applied to the study of the gun barrel erosion problem. The author is also thankful to Mr. C. Lenchitz and Mr. B. Haywood for their help in firing specimen in the calorimeters. Thanks are also due to Allied Chemicals, Morristown for their free gift of a MetGlas sample.

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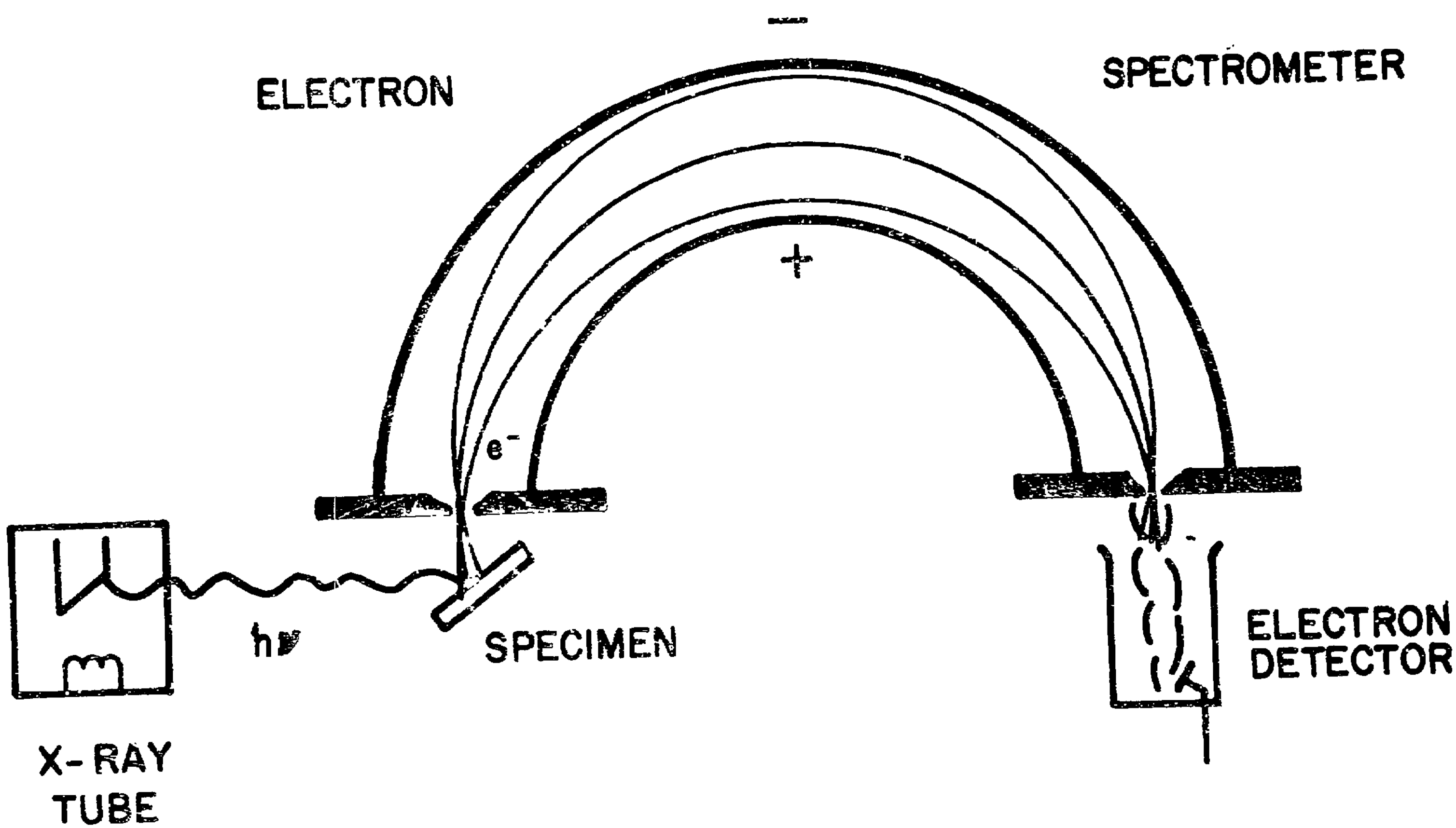
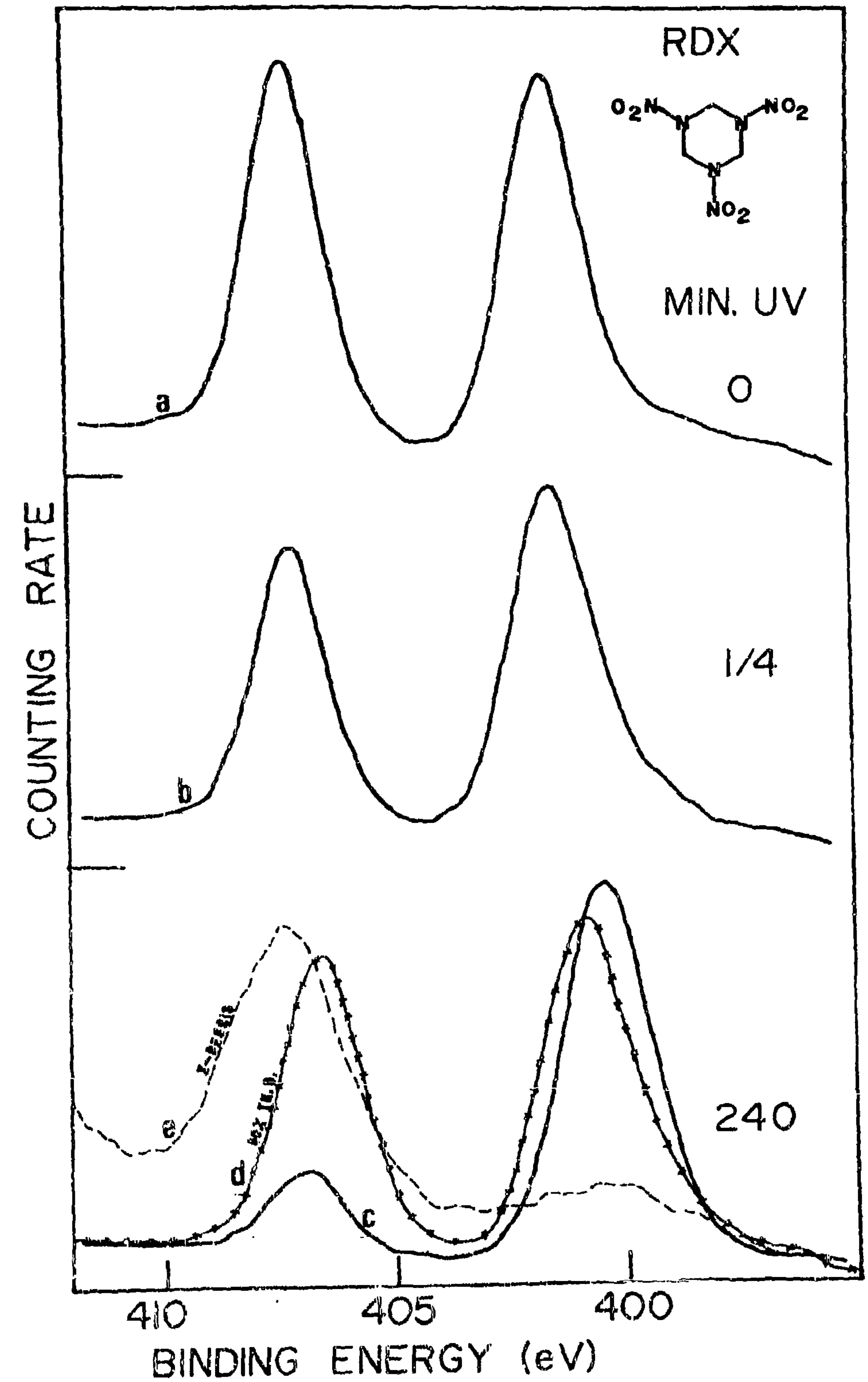
Fig. 1. Schematic representation of an ESCA instrument in which x-rays of known energy are made to shine on a specimen and the kinetic energy of the photoelectrons is measured in a magnetic or electrostatic analyser. Einstein's photoelectric equation is used to measure the binding energy of all the accessible occupied electronic levels. The core level spectra identify elements and show oxidative states.



X-RAY PHOTOELECTRON SPECTROMETER

Fig. 2. The $1s_{1/2}$ N spectrum of RDX (a) showing two peaks due to the nitrogen in the ring and NO_2 position (left peak), (b) in RDX photolysed for 15 seconds, showing decrease of the left peak due to breakaway of NO_2 limbs, (c) photolysed RDX for 4 hours, (d) in RDX 50% thermally decomposed, (e) in RDX decomposed by detonation.


RDX
O_2N
NO_2
NO_2
N
MIN. UV
0
1/4
240
a
b
c
d
e
COUNTING RATE
410
405
400
BINDING ENERGY (eV)

SESSION III

MECHANISMS

Chairman: Dr. J. R. Ward

Ballistic Research Laboratory
Aberdeen Proving Ground, Md.

CALCULATION OF INTERIOR HEAT TRANSFER IN SMALL DIAMETER GUN BARRELS USING SOLUTIONS OF THE COMPRESSIBLE BOUNDARY LAYER BEHIND A MOVING PROJECTILE#

by

Michael J. Adams* and Herman Krier**

ABSTRACT

From an idealized model for the gas dynamic variations in the inviscid core behind a projectile, the wall temperature of a small diameter gun tube is examined by solving the compressible boundary layer equations. The current model incorporates an unsteady, compressible and turbulent boundary layer analysis along the inner gun tube wall coupled to a consideration of radial heat conduction through the gun tube wall. The boundary layer analysis follows the formulation proposed by Bartlett, Kendall, and Anderson (1972) and is solved by an extension of the computational scheme of Cebeci, Smith, and Wang (1968). Radial heat conduction through the gun tube wall is solved using an energy balance across the inner tube wall along with an integral approximation of Goodman (1964). Some preliminary results are presented.

INTRODUCTION

Every internal ballistic device is designed in correspondence with a pressure-travel curve which will allow the attainment of a desired muzzle velocity. The peak pressure on this characteristic pressure-travel curve ultimately affects the total performance of the system. Many factors inherent to both the propelling charge and the gun system act to inhibit the pressure generation such that the desired performance in the system cannot be maintained (1). The most severe factor in this respect is the erosion of the gun tube wall of which heating plays an important role. For this reason, this study will deal with the extent to which the heating of the gun tube wall (after total combustion of the propelling charge) affects gun tube erosion.

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When dealing with internal flow in a gun system, one can consider the gas flow behind the moving projectile as consisting of an inviscid core coupled to a growing boundary layer at the tube wall. The assumption of the existence of boundary layer flow implicitly demands that the maximum boundary layer height represents only a very small fraction of the tube radius. This criteria is generally assured for systems with large tube diameters but must be carefully re-evaluated for systems incorporating small tube diameters as will be investigated in this study.

The approach taken by this investigation will be to solve, at a specified instant in time, the unsteady, turbulent, compressible boundary layer development behind a moving projectile in a gun tube. The inviscid core solution which supplies the boundary conditions at the boundary layer edge is assumed to be known either from a previous experimental or numerical investigation. The solution to the boundary layer development will provide the temperature history of the propelling gas at the tube wall. This solution will be used in conjunction with a one-dimensional, axisymmetric heat analysis to calculate the radial heat conduction in the gun wall. Longitudinal heat conduction in the gun tube wall will be of secondary importance due to the presence of much steeper temperature gradients in the radial direction and therefore is neglected. Knowledge of the thermal history at various locations along the gun tube wall will be applied towards representing a transient heat transfer coefficient.

Past Work

Before continuing with the specifics of the present analysis it is perhaps advisable to comment on the past efforts made towards modeling heat transfer in a gun system. Generally these efforts can be grouped as being either experimental, numerical (i.e. differential), or analytical (i.e. integral) investigations. A good summary of many of the studies is given by Dahm and Anderson (2) and is not repeated here. However, two more recent investigations, which generally characterize the types of analysis applied towards this particular heat transfer problem will be reviewed briefly due to their relevance to this investigation.

The work at Bartlett, Anderson, and Kendall (3) represents perhaps the first thorough attempt to model, using a differential approach, the time-dependent boundary layer development for an interior ballistic flow situation. Although providing for a unique treatment of the boundary layer problem for interior tube flow, the overall analysis is incomplete with regard to the heat transfer problem since results are presented only for an idealized inviscid core solution coupled to a restriction of a non-varying wall temperature. No attempt was made to solve for radial heat conduction through the gun tube wall.

Shelton, Saha, and Bergles (4) employ a one-dimensional free stream model coupled to a wall convective heat transfer and barrel conduction heat transfer model. An integral approach at the inner tube wall is expressed through the use of a local friction coefficient obtained in part by experimental means. Through an appropriate definition of a shape factor as well as the momentum and displacement thicknesses, the integral momentum equation is reduced to a partial differential equation in $\theta(x, t)$, the momentum thickness. This differential equation is solved numerically. A Colburn analogy is used to relate momentum transfer to convective heat transfer by specifying the heat transfer coefficient as a function of the local friction coefficient and the Prandtl number. This assumption allows for the solution to radial heat conduction through the tun tube wall.

Though a less precise method than when using a differential approach, the integral method has merit in terms of overall versatility. However, the use of an integral approach generally requires both a reasonable formulation of the local friction coefficient as well as an a priori' judgement as to the functional dependence of the heat transfer coefficient on the system parameters. In uncoupling the momentum and energy considerations by employing a Colburn analogy, one essentially pre-supposes this functional dependence and hence implicitly define the physics of the flow. While experiments have been performed to obtain reasonable skin friction data for interior pipe flow (5), an experimental determination of the proper form for the heat transfer coefficient still remains imprecise. However, if a proper heat transfer coefficient can be obtained through using a computational approach, the overall utility of an integral approach would be greatly advanced.

The Modeling Approach

A theoretical representation for the complete hydrodynamic flow in a ballistic device has yet to be attained. Hence the system on which the analysis to follow is based reflects this dilemma. For this initial investigation, the gun system will be idealized as an enclosed, small diameter tube in which one wall is accelerating outward due to the influence of a high pressure, high temperature gas. Further restrictions which are imposed are as follows;

1) The gun bore represents a smooth surface; rifling is neglected. Surface roughness is heuristically treated through the turbulent diffusivity model. Possible rotational effects due to the presence of a spinning projectile are not considered.

2) The unsteady, compressible, turbulent boundary layer flow is taken as axisymmetric. The inviscid core is viewed as being one dimensional and previously defined by either experimental or computational means.

3) All combustion of the propelling charge has been completed before any significant projectile motion occurs. The propelling gas is assumed to be homogeneous and governed by the Noble-Abel equation of state.

4) The gas velocity in the inviscid core decreases linearly from the projectile base to the breech (i.e. Lagrange Approximation). This assumption then determines the pressure and density variations in the free stream behind the projectile.

Pictorially, the system is that shown in Figure 1.

Co-ordinate System

Since we are dealing with an idealized system encompassing cylindrical geometry, one might initially consider employing a cylindrical co-ordinate system with the center of the tube representing r (or y) = 0. Indeed, the analysis of Ref. (4) proceeds in this manner . However, a closer look at the boundary layer flow assumption reveals that a more convenient, almost necessary, co-ordinate system would be one situated at the tube wall (3).

We are concerned with studying the heat transfer (and skin friction) at the gun tube wall. By working with a co-ordinate system situated at the wall, one possesses, through appropriate co-ordinate transformations, the capability of imposing an effective infinity location in the free stream (i.e. tube center core) in the boundary layer problem. The analysis of boundary layer flow with a co-ordinate system situated at the centerline of the tube would impose finite locations on all boundary conditions besides necessitating working through the inviscid core during all computations.

Wall Curvature

Since this investigation will deal with a small diameter tube (.221 in), attention must be directed towards the incorporation of curvature in the co-ordinate system. At this point, although assuming the boundary layer to be thin, it would be entirely premature to speculate that wall curvature will play no role in the boundary layer development. Since we desire to place the co-ordinate system at the inner tube wall, a proper accounting of curvature can be made by using the concept of the metric coefficient.

Metric coefficients deal with a measure of length in a given coordinate system (6). Their use in external boundary layer flows is well documented (7, 8). For an axisymmetric flow, the metric coefficient represents the local radius of curvature, say r (s, y). For a cylindrical internal flow, the metric would possess the following properties, namely

$$\frac{\partial r}{\partial s} = 0 \qquad \text{and} \qquad \frac{\partial r}{\partial y} < 0 \tag{1}$$

Therefore, a proper representation of the metric coefficient with regard to the geometry of the present system becomes,

$$r = r_o - y \tag{2}$$

where r_o = inner tube radias
y = stream normal distance from the inner wall

The Boundary Layer Model

The employment of the metric coefficient to the present problem lends further simplification to the overall analysis since the equations governing the boundary layer flow are now cast into a planar form. The two-dimensional governing equations along with the appropriate boundary conditions for our problem are as follows;

Continuity

$$\frac{\partial \rho r}{\partial t} + \frac{\partial \rho u r}{\partial x} + \frac{\partial \rho v r}{\partial y} = 0 \tag{3}$$

Momentum

$$\rho \frac{\partial u}{\partial t} + \rho u \frac{\partial u}{\partial x} + \langle \rho v \rangle \frac{\partial u}{\partial y} = -\frac{\partial P}{\partial x} + \frac{1}{r}\frac{\partial}{\partial y} r\left[\mu \frac{\partial u}{\partial y} - \rho \langle u'v' \rangle\right] \tag{4}$$

Energy

$$\rho \frac{\partial H}{\partial t} + \rho u \frac{\partial H}{\partial x} + \langle \rho v \rangle \frac{\partial H}{\partial y} = \frac{\partial P}{\partial t} + \frac{1}{r}\frac{\partial}{\partial y} r\left[\frac{\mu}{P_r}\frac{\partial H}{\partial y} - \rho\langle v'H' \rangle + \mu\left(1 - \frac{1}{P_r}\right) u \frac{\partial u}{\partial y}\right] \tag{5}$$

Boundary Conditions

$$u(x,t) = 0 \quad \text{at} \quad y = 0 \qquad \text{(No slip)} \tag{6a}$$

$$v(x,t) = 0 \quad \text{at} \quad y = 0 \qquad \text{(No Mass Transfer)} \tag{6b}$$

$$\lim_{y \to \infty} u(x,t) = u_E(x,t) \qquad \text{(Inviscid Core Soln.)} \tag{6c}$$

$$H(x,t) = h_w(x,t) \quad \text{at } y=0 \quad \text{(from wall temp.)} \tag{6d}$$

$$\lim_{y\to\infty} H(x,t) = H_e(x,t) = h_e(x,t) + \tfrac{1}{2}u_e^2 \tag{6e}$$

The boundary layer equations are rewritten using the co-ordinate transformations of Ref. (3), namely

Axial

$$\xi = \ln\left(\frac{x}{L}\right) \tag{7a}$$

Stream Normal

$$\eta = \frac{1}{C_1}\int_0^y \frac{\rho}{\rho_e}\,d\bar{y} \tag{7b}$$

where

$$C_1 = (L-L_0)(2\xi)^{1/2}\left(\frac{r_0}{r}\right)$$

$L = L(t)$ = projectile displacement

$\rho_e = \rho_e(t)$ = gas density (inviscid core)

The co-ordinate transformations provide for non-dimensional equations as well as simplified boundary conditions. Particular note should be taken that the axial transformation provides for a streamwise marching from the projectile base towards the breech, but never totally to the breech. Hence possible singularities which might arise at the breech can be avoided. The singularity at the juncture of the projectile base and the inner tube wall is avoided by starting computation at a "small" distance from the projectile base.

The continuity and momentum equations can be combined by an appropriate choice of the stream functions (3), namely

$$f - f_w = \frac{1}{C_1}\int_0^y \frac{\rho u}{\rho_e u_e}\,d\bar{y} \tag{8}$$

where

$$u_e = u_p\frac{x}{L}\;, \quad f' = \frac{u}{u_e}\;, \quad f_w = \frac{-1}{C_1}\int_L^x \frac{\rho_w v_w}{\rho_e u_e}\,dx$$

With these definitions and the chosen co-ordinate transformations, the governing equations are quite similar to those derived by Bartlett, et al, namely, Momentum

$$\left[C(1+\varepsilon^{+})\left(\frac{r}{r_o}\right)^2 \frac{L}{L-L_o}\frac{f''}{Re_L}\right]' - \left(f - \eta\frac{\dot{L}}{u_p}\right)f'' - 2\xi\left[f'^2 + f'\frac{\dot{L}}{u_p}\left(\frac{L}{u_p}\frac{du_p}{dL} - 1\right) - ff''\right]$$

$$= -\frac{1}{\rho u_e^2}\frac{dP}{d\xi} + 2\xi\left[\frac{df'}{d\xi}\left(\frac{\dot{L}}{u_p} - f'\right) + f''\frac{df}{d\xi}\right] + (2\xi)\frac{\dot{L}}{u_p}\left[L\frac{df'}{dL} - (\eta f'')\frac{L_o}{L-L_o}\right] \quad (9)$$

Energy

$$\frac{1}{Re_L}\left(\left(\frac{r}{r_o}\right)^2 C\left[\left(1+\varepsilon^{+}\frac{Pr}{Pr_t}\right)\frac{G'}{Pr} + \frac{u_e^2}{Pr\, He}\left\{(Pr-1) + \varepsilon^{+}\frac{Pr}{Pr_t}(Pr_t - 1)\right\} f'f''\right]\right)'$$

$$= 2\xi\frac{(L-L_o)}{L}\left[\frac{\dot{L}}{u_p}\left\{\frac{dG}{d\xi} - \eta\left(\frac{L_o}{L-L_o} + \frac{1}{2\xi}\right)G' + L\frac{dG}{dL}\right\} - \left\{f\left(\frac{1}{2\xi} - 1\right) + \frac{df}{d\xi}\right\}G'\right.$$

$$\left. - f'\frac{dG}{d\xi}\right] - 2\xi\frac{(L-L_o)}{L}\left\{\frac{\dot{L}}{\rho u_p}\left(L\frac{dP}{dL} + \frac{dP}{d\xi}\right)\right\} \quad (10)$$

where

$(\ )'$ denotes $\dfrac{d(\ )}{d\eta}$

$C = \dfrac{\rho\mu}{\rho_e\mu_e}$, $Re_L = \dfrac{\rho u_p (L-L_o)}{\mu_e}$, $Pr = \dfrac{C_p\mu}{K}$

$G = \dfrac{H}{He}$, $H = h + \frac{1}{2}u^2$, $\varepsilon^{+} = \dfrac{\rho\varepsilon}{\mu}$

$\dot{L} = u_p$ ($\neq u_p$ for "leaky piston")

Boundary Conditions

$$f'(0,L) = 0 \quad \text{at} \quad \eta = 0 \quad (11a)$$

$$f(0,L) = 0 \quad \text{at} \quad \eta = 0 \quad (11b)$$

$$f'(\xi,L) = 1 \quad \text{at} \quad \eta = \eta_\infty \quad (11c)$$

$$G(0,L) = \frac{h(0,L)}{He(\xi,L)} \quad \text{at} \quad \eta = 0 \quad (11d)$$

$$G(\xi,L) = 1 \quad \text{at} \quad \eta = \eta_e \quad (11e)$$

At this point in the analysis, several comments with regard to the non-dimensional governing equations are in order.

1) The turbulence model, as implied by equations (9) and (10) employs the standard mixing length formulation. The choice of this model was made due to its simplicity as well as its successful incorporation in past boundary layer models (3,7). A more complex and perhaps more precise turbulence representation could be used, but in view of the other simplifying assumptions now employed, the added complexity was deemed unnecessary at this time.

2) The present model for the gun system, following the treatment in Ref. (3), also provides for the analysis of the "leaky piston" problem. However, this consideration will not be explored in this investigation.

3) Considerable simplification is achieved with the assumption of a Lagrange velocity distribution in the gas core behind the moving projectile. As pointed out in Ref. (2), the Lagrange approximation appears to be quite poor during the early stages of projectile motion. This fact is unfortunate as one is generally most concerned with wall heating during this period when the gas temperature gradients are the most severe. However, the employment of the Lagrange approximation should not be viewed as a permanent restriction. The present model, as being developed, could be coupled to a more complex inviscid core analysis. In such a case, a greater complexity would be encountered in the formulation of the non-dimensional governing equations.

Thus far only the boundary layer model has been discussed. This model essentially represents the type of formulation proposed in Ref. (3). At this stage, however, a useful extension can be provided to the model by incorporating a radial heat conduction analysis through the gun tube wall. Unlike the approach used in Ref. (4), this investigation will use computed boundary layer results to provide the appropriate information needed in solving the radial heat conduction problem.

<u>Radial Heat Conduction Model</u>

The heat equation for an angular region, neglecting axial heat conduction, can be expressed as

$$\frac{\partial T}{\partial t} = \alpha \frac{1}{r} \frac{\partial}{\partial r}\left(r \frac{\partial T}{\partial r} \right) \tag{12}$$

where α = thermal diffusivity

The boundary conditions required for the solution of equation (12), in regards to radial heat conduction through a gun tube wall, are obtained in the following manner.

At a given projectile displacement, the boundary layer model is employed to obtain the temperature gradient in the gas at various locations along the inner surface of the tube wall. One now imposes the energy balance condition, at the inner wall namely,

$$-k_g \frac{dT_g}{dr} = -k_w \frac{dT_w}{dr} \tag{13}$$

This assumption defines an "inner" boundary condition on the heat conduction problem. The "outer" boundary condition is taken as

$$-k_w \frac{dT_w}{dr}\bigg|_{r=r_0+\delta_T} = 0 \tag{14}$$

where δ_T represents a maximum thermal penetration distance into the gun tube wall beyond which no further heating occurs. It is assumed that for single shot results, the distance will be significantly less than the gun tube wall.

Equations (12) - (14) define a radial heat conduction model. The solution of this system of equations can be accomplished by several means. This investigation will employ the integral approach of Goodman (9) as extended by Lardner and Pohle (10) to cylindrical geometries. Following the analytical procedure outlined in Ref. (10), one can obtain a time dependent expression for the surface temperature, Tw, as

$$\frac{T_w k_w}{A r_0} = \frac{1}{2} \ln\left(3.32 \frac{\alpha t}{r_0^2}\right) \tag{15}$$

where $T_w = T(r = r_0)$

k_w = thermal conductivity of the wall

A = heat flux into the wall $\left(= -k_g \frac{dT_g}{dr}(r=r_0)\right)$

t = time

The overall solution procedure can now be fully visualized, namely,

1) At a given instant of time (i.e. a given projectile

displacement), the boundary layer development behind the projectile is solved according to the previously described boundary layer model.

2) A successful solution of the boundary layer flow provides the necessary information on the heat flux into the tube wall at various fixed locations along the inner surface of the tube wall.

3) Knowing the projectile displacement and the axial velocity variation in the gas, one can calculate the "soak period" at each axial station as occured during the projectile motion.

4) The wall temperatures prior to the projectile displacement can be updated by using equation (15) in conjunction with the appropriate "soak" period.

5) The updated wall temperature variation is then used as an initial guess for the next boundary layer solution for the next projectile displacement. A linear variation in temperature to some ambient condition at the projectile base is assumed over all newly uncovered axial stations.

A pictorial representation of this process is shown in Figure 2.

It becomes immediately obvious that one must assume an initial axial temperature variation along the inner wall during the first projectile displacement. This assumption is needed for complete specification of the boundary conditions for the initial boundary layer analysis. By imposing the assumption of an impulsive start from some initial position L_o, the initial wall temperature distribution along the inner tube surface can be assumed to be at some ambient condition, say a constant, independent of x.

The Boundary Layer Solution

A computational strategy for solving the boundary layer problem will now be reviewed. The present investigation proposes to use a modified version of the computer model of Cebeci, Smith, and Wang (7). The computer model developed in Ref. (7) was designed to analyze a steady, compressible, turbulent exterior boundary layer by using a standard streamwise marching procedure. For the unsteady analysis pursued here, temporal variations will be incorporated through numerical differencing of the unsteady partial derivatives. This numerical differencing is similar to that employed by Cebeci, *et al*., in representing partial derivatives with respect to axial variations. Specific details of the computational strategy can be found in Ref. (7).

Essentially the Cebeci et al., computer model for boundary layer flow discretizes the flow domain by using a finite set of grid points which incorporate a geometric spacing in the stream normal direction. This spacing allows for good resolution near the surface over which the boundary layer is developing, hence optimizing grid utilization. All partial derivatives are rewritten according to a five point numerical difference scheme which, along with proper co-ordinate transformations, allow the governing set of partial differential equations to be written as a set of ordinary differential equations. A Lagrange interpolation formula is used to represent higher order derivatives in terms of a five point computational cell. Therefore, each grid point becomes represented by a zero order algebraic equation incorporating four neighboring points. All nonlinear coefficients are implicitly defined by an initial guess coupled to an iterative updating procedure. The resulting set of algebraic equations, as represented by the matrix equation $A\underline{x} = b$, is solved simultaneously by Choleski matrix method. The solutions thus calculated are then employed in the Lagrange interpolation formulas to obtain all needed higher order derivatives.

A synopsis of the computational strategy with regard to the problem under current consideration is given by the flow chart in Fig. 3.

Concluding Remarks

To date, only partial boundary layer solutions have successfully been achieved using the previously discussed model. These results are presented in Figs. (4) - (5). The present numerical formulation has shown some tendencies towards oscillation in the computed solutions of the energy equation as the computation proceeds towards the breech. Though the reason for this stability problem is as yet unknown, a suspicion is that the initial value of the unsteady pressure gradient, an unknown quantity for the current analysis, is not properly defined at each axial station along the gun bore. The current analysis is not coupled to an inviscid core computer model and therefore is not simultaneously supplied with this information during computation as was the boundary layer model in Ref.(3). Hence, for the current analysis, the initial unsteady pressure variation along the gun tube (i.e. from the projectile base to breech) must be supplied according to speculation as to its structure and magnitude. It is hoped that this restriction will be overcome shortly and current stability problems in the solution of the energy equation at later stations will be resolved.

What has been presented is an outline for solving the heat transfer problem through a gun tube wall. The current analysis incorporates a simplified boundary layer model which follows the initial work of Bartlett, et al. (3). The boundary layer model is extended by considering radial heat conduction through the gun tube wall as well as possible curvature effects from the tube wall. The computational strategy for solving the boundary layer problem represents an extension of the steady-state analysis of Cebeci, et al. (7). Preliminary results are presented with a more complete evaluation of the current model to be presented at a later time.

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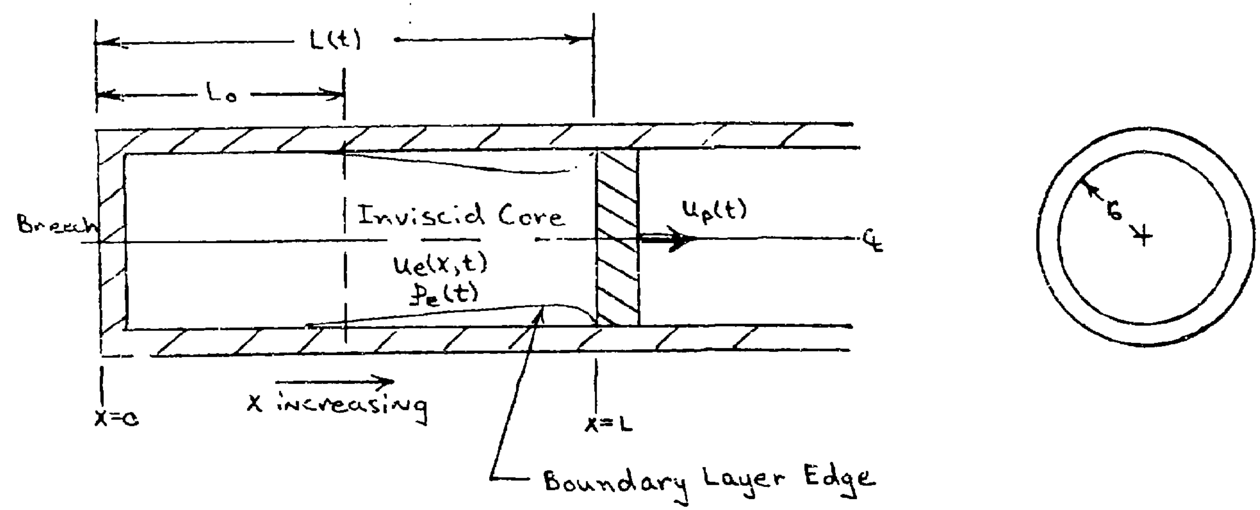
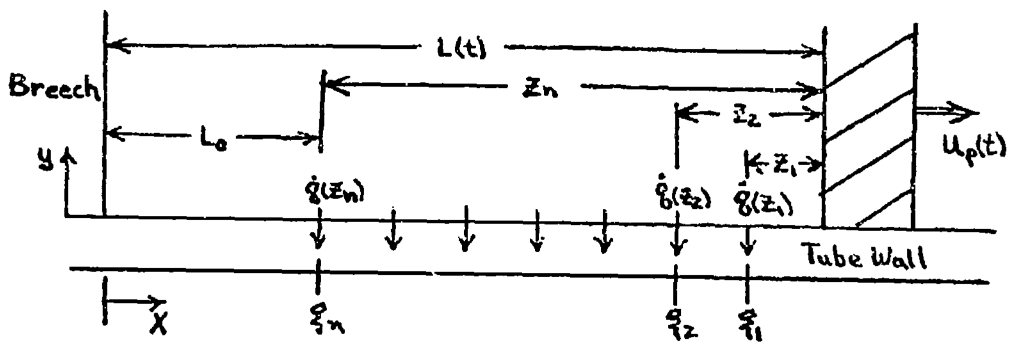


$u_e(x,t) = u_p \frac{x}{L}$ (Lagrange Approximation)

L_o = Initial Projectile Position

Figure 1 Idealized Gun System



Radial Heat Flux
(from Boundary Layer Soln.)

$$\dot{q}(z_1) = -K_g \frac{dT_g}{dr}(z_1, 0)$$

$$\dot{q}(z_2) = -K_g \frac{dT_g}{dr}(z_2, 0)$$

$$\vdots$$

$$\dot{q}(z_n) = -K_g \frac{dT_g}{dr}(z_n, 0)$$

Soak Period Per Station
(from Inviscid Core Soln.)

$$t_1 = z_1 / U_p(t)$$

$$t_2 = z_2 / U_p(t)$$

$$\vdots$$

$$t_n = z_n / U_p(t)$$

Inner Wall Temperature
(from Integral Approximation)

$$\frac{T_w(z_i) K_w}{\dot{q}(z_i)\, r_c} = \frac{1}{2} \ln\left(3.32 \frac{\alpha t_i}{r_o^2}\right) \qquad i = 1, 2, \cdots n$$

Figure 2 Update Procedure on Inner Wall Temperatures

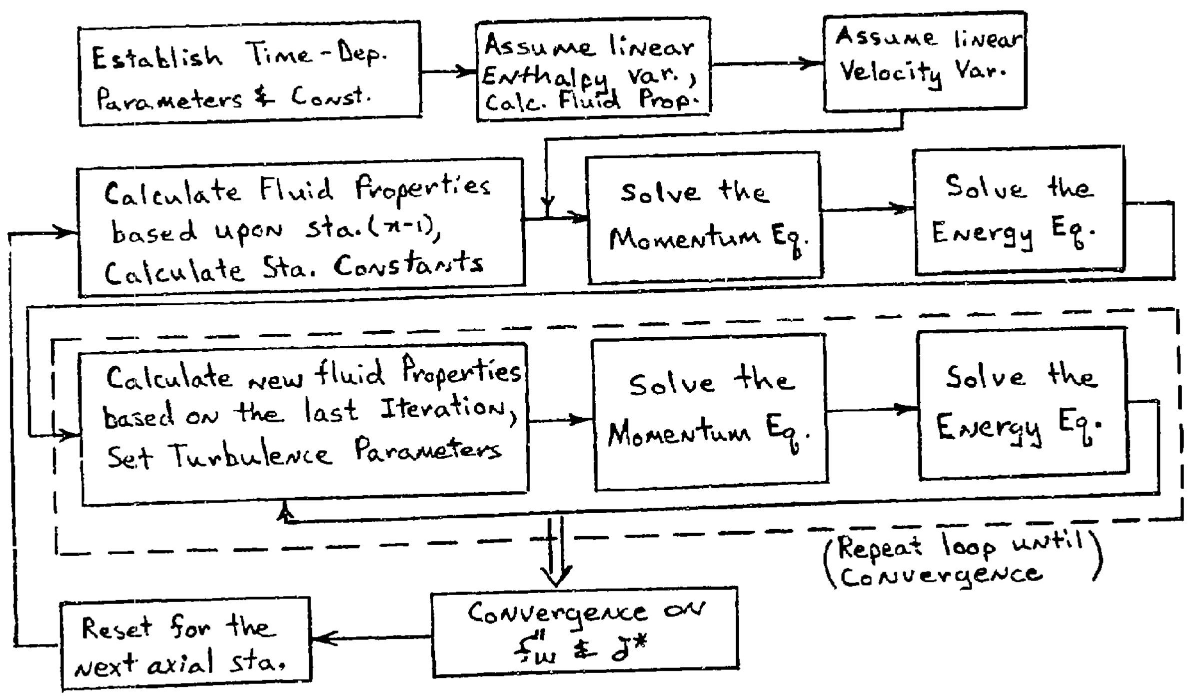
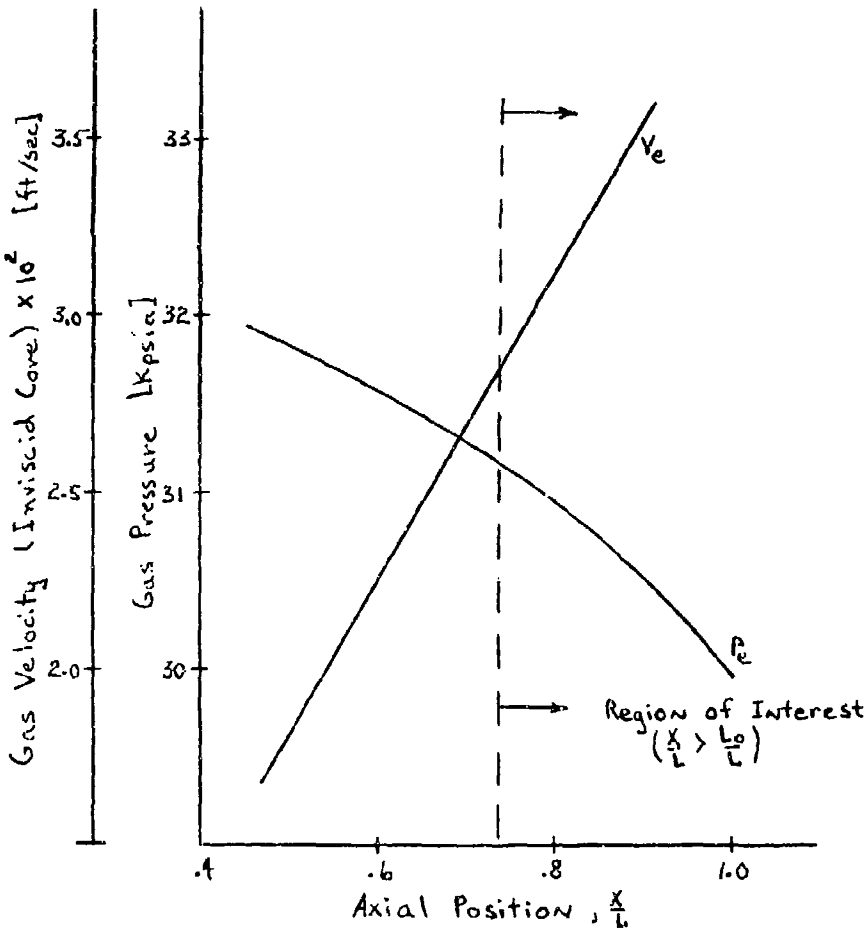


Figure 3 Computational Strategy

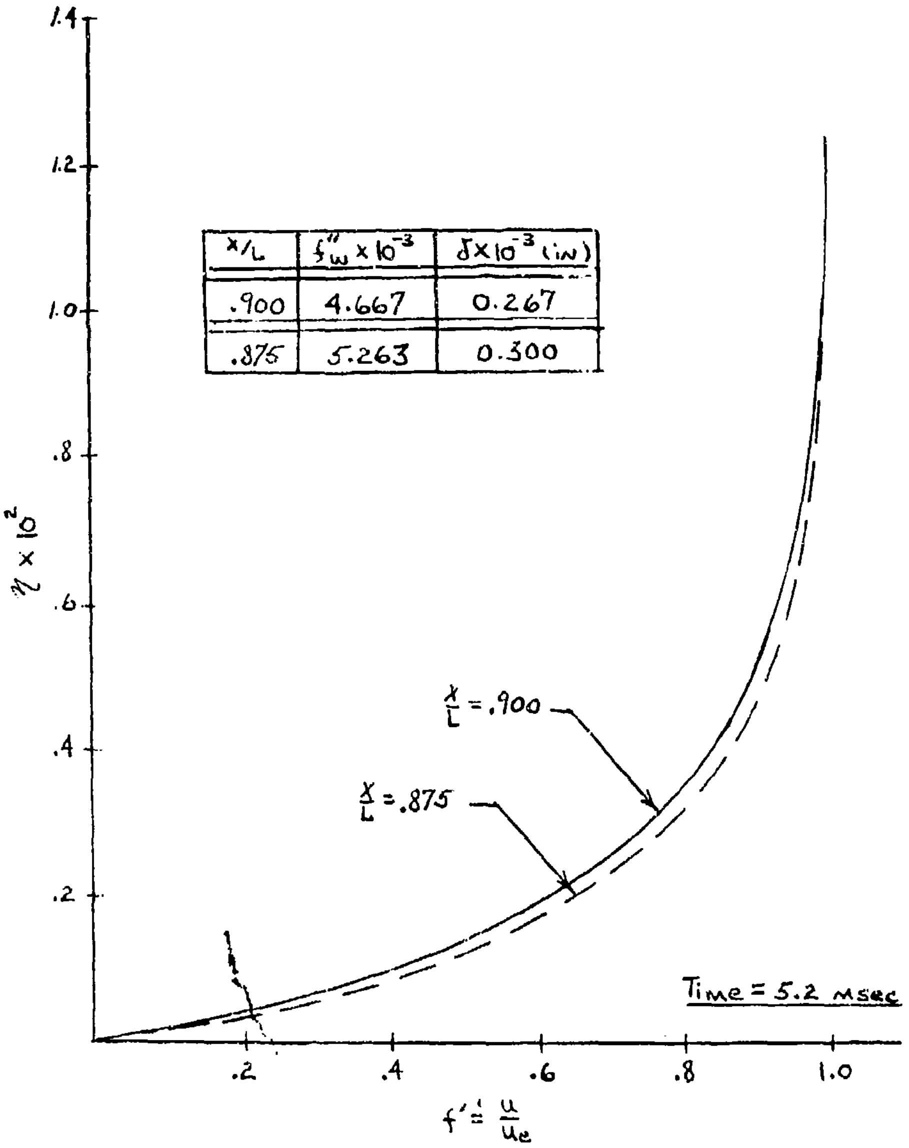


Case Data

L_o = 1.5 in
L(t) = 2.0 in

Base Pressure = 30 Kpsia
$U_p(t)$ = 400 ft/sec

Figure 4 Axial Pressure-Velocity Variation In The Inviscid Core At t = 5.2 msec



x/L	$f''_w \times 10^{-3}$	$\delta \times 10^{-3}$ (in)
.900	4.667	0.267
.875	5.263	0.300

Figure 5 Velocity Profiles At Successive Axial Stations

SIMULATION OF GUN BARREL HEATING AND ASSOCIATED AMMUNITION THERMAL RESPONSE

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ABSTRACT

The achievements of this study were as follows: (1) the development of an analytical capability for describing the transient temperature distribution in a 5"/54 gun barrel during any firing schedule and the resulting thermal response of a propelling charge when rammed at the end of firing, (2) the determination of some of the factors which control a charge assembly cook-off and (3) an evaluation of a method for reducing the likelihood of a propelling charge cook-off within the gun barrel. Specific results were determined for the standard 5"/54 MK 64 projectile when fired with both NACO service propelling charge assemblies and NACO service charge assemblies which had been modified by the inclusion of 0.75 lbm of high viscosity silicone oil, within a container in the case closure plug. For rates of fire between 1 and 10 rnd/min, a determination was made, for both charge assemblies, of the number of rounds which would have had to have been fired in a MK 18 MOD 3 gun barrel in order to make the thermal reaction occur at the end of a 10 min. time interval measured from the onset of ramming. Based on the limited analytical predictions, a comparison of the two 10 min. cook-off curves indicates that the smear coolant additive should be effective in reducing the probability of a charge assembly cook-off in a 5"/54 gun.

INTRODUCTION

The significant amount of energy conducted into the chamber wall of a 5"/54 gun barrel during long firing schedules presents a danger of an uncontrolled thermal reaction, or cook-off, in a rammed NACO charge assembly that is not immediately fired. Cook-offs such as the one experienced on board USS TURNER JOY on 7 September 1972 can occur after a misfire, a gun malfunction, or a cease fire and may result in serious injury to personnel and significant destruction of materiel.

With the aid of temperature data measured at the Naval Surface Weapons Center, Dahlgren Laboratory (NSWC/DL), an analytic simulation was formulated of (i) gun barrel heat-up during and after a firing sequence, and (ii) the resulting thermal response of a rammed propelling charge assembly. First, the single-shot heat input to the bore surface was determined for the two charge assemblies considered -- NACO service charge assemblies with and without smear coolant in the case closure plug. The appropriate thermophysical properties of the gun steel and the outside thermal boundary condition were determined by the technique of non-linear parameter estimation in which the primary inputs were selected temperature histories of the gun barrel taken after the cessation of firing.

Then for a given rate of fire, the number of rounds required to put a chambered charge assembly in a 10 min. cook-off situation was calculated using a closed-form, two-dimensional, cylindrical heat transfer solution and a finite-difference heat transfer solution. The cook-off calculations were repeated for both charge assemblies and several rates of fire from 0.5 to 10.0 rounds/min..

GUN BARREL THERMAL RESPONSE

Analysis of Single-Shot Data. Basic to the entire study was a determination of the heat input to the bore surface caused by a single NACO shot; it has to be representative of all of the reliable experimental data available. The data available for both two-piece and monoblock MK 18 5"/54 barrels with NACO propellant included two tests conducted at NSWC/DL on 21 October 1971, one test at NSWC/DL on 01 December 1971 which has been reported in reference [1], and a 29 October 1974 NSWC/DL test. Bore surface temperature data were measured at several axial locations in the barrel during each test, thus providing the information required for the determination of the local bore surface heat input as a function of axial position. Because the effective duration of the heat pulse associated with the combusting propellant gases is so short, in most cases ending at shot ejection, the penetration of the energy during this time is limited to the first millimeter below the surface of the bore. As a result, the single-shot temperature history during this period is insensitive to the influence of any factor outside this region such as boundary conditions on the outside barrel surface or the construction of the barrel. The insensitivity permitted use of all available 5"/54 NACO service charge data for the determination of the representative local temperature histories, regardless of the particular conditions of a given test.

The total heat input to the bore surface is a strong function of the distance from the muzzle and a very weak function of circumferential angle. Hence, the heat flux was assumed to be uniform at any given axial location in the gun barrel. During each of the tests, data was taken at locations along the entire length of the barrel; Figure 1 indicates the axial position of the relevant instrumentation in the barrel, cartridge case, and projectile.

The temperatures were measured using Hackemann type thermocouples developed to measure bore surface temperatures in a gun barrel as described in reference [1]. Inspection of the bore face of the thermocouple under 70X magnification indicated that an extremely thin but very good conductive path existed between the two thermocouple ribbons and the gun steel thermocouple body. According to reference [2], this insured that the indicated temperature is that of the bore surface and not the combusting gases or an intermediate temperature. Upon completion of a given test, those thermocouples that were still functioning were found to have retained their original high quality junctions.

A nonlinear parameter estimation of the local total heat input to the bore was made by minimizing, over a specific time interval, the degree of discrepancy between the measured surface temperature history and that analytically determined by a one-dimensional semi-infinite, rectilinear solution. The time interval utilized in the optimization process extended over the first 10 to 20 ms that a given location on the bore was exposed to the propellant combustion gases. The reason that so small an interval was required is that for most locations along the length of the gun barrel over 90 percent of the heat is transferred to the bore in the first 10 ms. It was assumed that the instantaneous bore surface heat flux can be described by a time varying exponential function with an initial magnitude of A and a decay rate of c. Close to the muzzle, the exponential assumption for heat flux is somewhat in error because it cannot entirely simulate that time dependence of that heat transfer to the bore which occurs after shot ejection. In the interval from 0 to 10 ms, the diffusion depth of the energy pulse on an isolated single shot was small in comparison to the radius of curvature of the bore of the 5 in. gun. This permitted the use of the rectilinear heat transfer solution to estimate the total heat input. For small caliber guns the curvature effects become significant, and a cylindrical solution would have been necessary. It was also assumed that the thermophysical properties of the barrel were invariant with respect to temperature and could be evaluated at an average value of 900°K. The properties given in References [3], [4], and [5] were taken as representative of gun steel at an average bore temperature of 900°K.

With all other parameters fixed, the degree of disparity between the calculated and measured temperatures was entirely a function of A and c. The total heat input, Q, resulting from an exponential heat pulse is the quotient A/c as seen from the following integration:

$$Q = \int_0^\infty A e^{-ct} dt = A/c \qquad (1)$$

Thus, there are infinite combinations of A and c values which will yield the same total heat input. However, each A and c pair will yield a different bore surface temperature history as illustrated in Figure 2. Before the optimization process could begin for a given bore surface location, it was necessary to combine all the experimental data and to establish a single, representative temperature history for that locality. Temperature indications attributed to blow-by past the rotating band and the passage of the rotating band over the thermocouple junction have been identified in references [1] and [6] but were not considered part of the temperature history for purposes of total energy determination.

A nonlinear parameter estimation of the local A and c values was performed with the aid of data taken at Stations B, C, D, F, H, and I using the approach outlined in Section IVA of reference [7]. The total heat input as a function of axial barrel location is graphically shown in Figure 3. In this figure, the solid curve should be interpreted as yielding the nominal or baseline values for a standard, uncooled NACO round. Note in Figure 3 the break in the curve of heat input which is located at the cartridge case mouth 602 cm from the muzzle. This discontinuity occurs because little heat can be transferred through the cartridge case wall to the barrel surface during the small time interval wherein the case is in the chamber.

Determination of the total heat input for rounds with coolant, cooled

NACO, was accomplished by using the bore temperature history of a shot where a dynamic steady state coolant deposit thickness exists. Shown as a dashed line in Figure 3 is the curve considered most representative of the total heat input along the barrel length when firing cooled NACO. The significant reduction in total heat input caused by the use of the smear coolant is readily apparent.

The Inside Boundary Conditions Extended to Multiple Rounds. A two-dimensional, closed-form cylindrical heat transfer solution as detailed in reference [7] was modified to analyze the thermal response of the gun barrel to multiple round firing sequences. The original solution had assumed that the inside boundary condition was a series of identical exponential energy pulses, that the decay rate of the local energy pulse was the same at any location on the bore surface, and that the thermophysical properties of the gun steel were invariant. The first assumption was made less stringent and a new mathematical relation was derived to allow the energy pulse magnitude to vary between shots.

The modified two-dimensional solution required the input of the magnitude of the local energy pulse as a function of axial position and the use of a single energy pulse decay rate. By utilizing the heat inputs determined in the previous section, it was a simple matter to compute a local nominal pulse magnitude A' by multiplying the single-shot total heat inputs by a nominal decay rate c'. For the purpose of predicting residual bore surface temperatures, the choice of the nominal decay rate was essentially arbitrary because the bore temperatures after several seconds were insensitive to the shape of the energy pulse; this was verified by the fact that the same residual bore surface temperatures were predicted by the solution whether the decay rate was 222 or 350 s^{-1}. Hence, the nominal decay rate was chosen to be 222 s^{-1}.

The heat flux to the bore is dependent upon the bore temperature and the combustion gas temperature at the edge of the thermal boundary layer. Conventionally, this heat flux is written with the use of a convection coefficient as the correlating parameter.

$$\dot{q}_n'' = h_{bs}(t)[T_{bg}(t) - T_{bs}(t)]. \qquad (2)$$

In this equation only the residual bore surface temperature, $T_{bs}(t)$, should be expected to vary significantly from round to round. This fact motivated the particular choice used in handling the instantaneous heat flux in the modified closed-form solution. For multiple rounds, the instantaneous heat flux during the n^{th} round was updated by weighting it with a function based upon the temperature differences across the thermal boundary layer. In computing the weighting function, the temperature of the bore during the n^{th} shot was taken to be the residual bore temperature of the (n-1) shot at that location. The temperature of the combustion gases at the edge of the boundary layer was represented by the time averaged, space-mean gas temperature, T_{bg}, computed from a Lagrangian interior ballistics model similar to that discussed in reference [8]. The inside boundary condition for multiple rounds as used in the closed form solution was

$$\dot{q}''_n = \left(\frac{T_{bg} - \left[T_r(z)\right]_{n-1}}{T_{bg} - T_i} \right) A'(z)e^{-c't}$$

where $\dot{q}''_n(t)$ was the instantaneous heat flux for the n^{th} round and T_i the initial temperature of the barrel.

<u>The Outside Boundary Condition and Gun Steel Thermophysical Properties</u>. The outside boundary condition and gun steel thermophysical properties for that portion of the barrel between 575 and 617 cm from the muzzle were determined by nonlinear parameter estimation. The temperature histories used in this analysis were all taken from reference [9]; this information had originally been acquired from a 7 December 1972 NSWC/DL test. In this experiment, an instrumented projectile and cartridge case were used to determine the barrel temperature history after the cessation of firing. As shown in Figure 1, a thermocouple was attached to the inner cartridge case surface at Location 1, the front of the propellant bed, while at Location 2 a thermocouple was attached to the outer surface of the rotating band. Data from these thermocouples was used in the nonlinear parameter estimation process with the knowledge that the i· ·licated temperatures were probably lower than the actual local bore tempe· ·res.

The closed-form, two-dimensional heat transfer code was used to predict the thermal response of the gun barrel both during firing and after the cessation of firing. The nonlinear parameter estimation process involved reducing the discrepancy between analytically predicted temperatures relevant to a time period after the cease fire and those barrel temperatures measured by the instrumented projectile and cartridge case. For a given axial barrel location, the degree of disparity between the measured barrel temperatures and those predicted by the mathematical model was a function of only two parameters. These were the overall coefficient of heat transfer applicable to the outside of the barrel, U, and the temperature representative of the average thermal state of the entire gun barrel, T_R. Specification of this temperature fixed the values of the gun steel thermal conductivity and thermal diffusivity. Note that reducing the degree of temperature disparity to only two parameters is possible because of the following factors: (1) the single-shot heat input to the bore as a function of barrel axial location is a known quantity for the uncooled NACO charge, and (2) a reasonable attenuation process for extending this boundary condition to multiple rounds has been developed.

The boundary conditions and geometry of the modeled gun barrel are indicated in Figure 4. The solution was started from an initial condition of constant temperature throughout the gun barrel. As shown in Figure 4, the outside diameter of the modeled barrel was considered invariant at a value (i.e., 32.28 cm) equal to that actually present over most of the first 282 cm forward from the breech face. In the actual gun barrel, the outside diameter is 18.67 cm at the muzzle and 30.21 cm at a point 404 cm from the muzzle. This constraint on barrel external geometry will have little effect on predicted bore surface temperatures between 575 cm and 617 cm from the muzzle. As shown in Figure 4, the inside diameter of the modeled barrel was considered to be constant over the entire barrel length. The specific value of the inside diameter was, however, a function of the axial location under investigation.

In addition to the requirement that the barrel inside and outside diameters be considered as constant, it was necessary to mathematically treat the barrel as if it had been constructed from one homogeneous piece of steel even though the available multiple round barrel temperature data was taken from two-piece MK 18 MOD 3 barrels. The effect of this assumption is realized in the specific values, for a given axial location, of the overall coefficient of heat transfer U and gun steel thermophysical properties which satisfy the minimization of disparity between measured and predicted barrel temperatures. The assumption that the ends of the barrel are insulated should be very good since the heat lost from these surfaces is very small in comparison to that lost from the outside barrel surface.

The single-shot heat input to the bore as a function of barrel axial location was previously discussed for the service NACO charge assembly with and without smear coolant. This information is applicable to all MK 18 and MK 19 gun barrels. The parameters optimized in this section are particularized to MK 18 MOD 3 gun barrels which comprise the majority of the Navy's inventory for the MK 42 gun mount. However, the general procedures outlined may be used to determine the outside overall coefficient of heat transfer and gun steel thermophysical properties for any gun barrel.

The parameter estimation process was begun by selecting a range of reasonable values or search intervals for U and T_R, the representative barrel temperature at which the thermophysical properties were to be evaluated. The value of U was allowed to vary from 0.00038 to 0.00680 $cal/cm^2 s \cdot °K$ while T_R was allowed to vary from 373°K to 773°K. The measured temperature data to be utilized in the estimation process was taken from the 7 December 1972 test conducted at NSWC/DL. In this test, the instrumented projectile and cartridge case were inserted in the barrel after 200 rounds had been fired in 70 min.. Inside surface barrel temperatures were recorded at Stations 1 and 2 for 60 min. after the last shot. For any given T_R, the variation of U over its search interval corresponded to highly disparate, predicted temperature histories; curves (a) and (b) in Figure 5 clearly support this statement. However, the extremes of the search interval for T_R with an arbitrary U corresponded to a much smaller variation in predicted temperatures as shown by curves (c) and (d) in Figure 5. By choosing an initial U mid-range in the search interval (0.0015 $cal/cm^2 \cdot s \cdot °K$) and varying T_R over its interval, an attempt was made to determine a T_R such that the predicted bore surface temperature history at 586 cm asymptotically approached the temperatures measured by the thermocouple affixed to the rotating band at Station 2. All values of T_R within the search interval resulted in an under-prediction of the bore temperature at Station 2. Therefore, another value of U was chosen which was lower than the first estimate; had temperatures been over-predicted a higher U would have been chosen. This iteration was continued until values of T_R and U were determined such that the predicted barrel temperatures asymptotically approached those measured by the thermocouple at the interface between the barrel and the rotating band.

The values of U and T_R along with the associated gun steel thermophysical properties which satisfied the optimization process at 586 cm from the muzzle are presented in Table I. The corresponding predicted bore temperature and the measured bore temperature are shown in Figure 6 by curves (a) and (b), respectively. To test the generality of the optimized U and T_R values relevant to Station 2, they were used to predict the bore temperature at the same axial barrel location for the 1 September 1972 USS TURNER JOY test. In this test, 285 rounds were fired in 160 min; the specific firing history is shown in Figure 7. The predicted bore temperature did asymptotically approach the measured temperature at station 2 for the shipboard test.

Table I. Boundary Conditions and Thermophysical Properties of Gun Steel for Multiple Round Firing Schedules in MK 18 MOD 3 Barrels

	586 cm (Bore Surface)	617 cm (Chamber Surface)
U ($cal/cm^2 \cdot s \cdot °K$)	0.00038	0.00038
k ($cal/cm \cdot s \cdot °K$)	0.0832	0.0923
ρ (gm/cm^3)	7.85	7.85
c_p ($cal/gm \cdot °K$)	0.1616	0.1362
α (cm^2/s)	0.0656	0.0863
T_R (°K)	773.0	473.0

The U determined for station 2, 586 cm, was assumed to apply to the external barrel surface to station 1, 617 cm. This is a good assumption since no significant changes occur in the geometry of the barrel and slide or in the interface conditions between these items over the region between 586 and 617 cm from the muzzle. Just as was done for station 2, the estimation process for determining U and T_R for station 1 relied upon temperature data from the 7 December 1972 test conducted at NSWC/DL. However, since U was already specified it was necessary only to vary T_R within its search interval in order to make the predicted chamber wall temperature asymptotically approach the temperature measured by the thermocouple on the cartridge case wall. Presented in Table I are the values U, T_R, and associated gun steel thermophysical properties which satisfied the optimization process at 617 cm from the muzzle. The corresponding predicted chamber wall temperature and the measured chamber wall temperature are shown in Figure 6 by curves (c) and (d), respectively. A check on the generality of the estimated U and T_R values relevant to Station 1 was made by using them to predict the chamber wall response at Station 1 for the two USS TURNER JOY firing sequences shown in Figure 7. The predicted chamber wall temperatures did closely match those measured on the two shipboard tests.

The parameters given in Table I were estimated on the basis of data from a relatively short test; however, when they were used to predict barrel temperatures at identical axial locations for tests having much longer and more irregular firing schedules, the results were in close agreement with actual measurements. This agreement not only helps to validate the generality of Table I, but also helps to justify the assumptions made in the development of the two-dimensional heat transfer solution. The accuracy of the predicted results for the two shipboard tests demonstrates the specific capability of predicting, for any firing schedule, the thermal response of a MK 18 MOD 3 barrel inside surface at two specific axial stations, 586 cm and 617 cm from the muzzle.

CHARGE ASSEMBLY RESPONSE

A charge assembly cook-off was most likely to occur at the front of the propellant bed, 617 cm from the muzzle, because the chamber wall at that axial position was hotter than anywhere else along the length of the bed. The grains closest to the cartridge case wall presented the greatest cook-off danger due to their proximity to the hot chamber wall. Once a reaction occurred at that point, it would immediately spread through the remainder of the charge. Therefore all of the charge assembly cook-off analyses were conducted at 617 cm from the muzzle.

To predict the thermal response of the charge, it was first necessary to specify the thermal boundary condition at the outside of the cartridge case wall. Assuming perfect thermal contact between the case and chamber wall, the case outside wall temperature history was the same as that of the chamber wall. By using a one-dimensional, explicit, finite-difference approximation cast in radial coordinates and similar to that described in reference [10], it was possible to predict the transient thermal response of a local region of the propellant bed. A thermally induced, exothermic, runaway decomposition or cook-off was said to occur when the temperature of a node point in the propellant increased by two or more orders of magnitude within several milliseconds. The decomposition of the propellant in the cartridge case was assumed to be governed by a zero-order Arrhenius rate equation.

Some of the thermophysical properties of NACO propellant were not well defined in the literature because of the inherent difficulty in measuring them. The difficulty in determining the thermal conductivity of a propellant arises because the propellant not only conducts heat but also generates its own internal energy due to decomposition. Internal energy generation also affects the measurement of the heat of fusion. Since these two properties were unavailable for NACO, a representative value for each was deduced from data for propellants of similar composition as specified in reference [11] for a temperature of 298°K. It was not expected that these nominal values were significantly in error and their use would certainly not affect a qualitative comparison between cook-off situations where the coolant was and was not used. The melting temperature of NACO was provided by reference [12]. The density, heat of explosion, and specific heat for NACO were also taken from reference [11]. The kinetic properties had been previously determined by the Applied Science and Materials Division, NSWC/DL, using the technique of differential scanning calorimetry (DSC). The kinetic properties determined from a DSC test wherein the sample was heated at 20°K/min were chosen to represent those of the NACO propellant. It should be noted that the subsequent analysis justified the choice of these properties by predicting a temperature rise of approximately 20°K/min during the time just prior to cook-off. The properties of the cartridge case steel were found in references [3] and [4] and were relevant to AISI-C-1030 steel.

The orientation of the propellant grains relative to the inside of the case wall was significant in determining their cook-off time. The fastest cook-off occurred when a single grain became essentially isolated from other grains closer to the inside of the bed. This packing was simulated as an annulus of propellant 0.635 cm thick (the diameter of a 5"/54 NACO grain) in perfect thermal contact with the case and insulated on its inner surface. This orientation was used in the remainder of the study because it gave the shortest times to reaction and, thus, the more conservative results.

EVALUATION OF THE SMEAR COOLANT

The effectiveness of the smear coolant in the reduction of a cook-off hazard was estimated by determining the thermal responses of the barrel and charge assembly when the coolant was and was not used. For a given rate of fire, the number of rounds required to put a charge assembly in a 10 min. cook-off condition was calculated using a closed-form, two-dimensional, cylindrical heat transfer solution and a one-dimensional, finite-difference heat transfer solution. For a 10 min. interval after the last round, the closed-form solution provided the temperature history of the chamber wall at 617 cm from the muzzle; this history was a function of the rate of fire and the number of rounds fired. Since the problems of barrel thermal response and propelling charge thermal response were treated in an uncoupled manner, the temperature history from the closed-form solution was used as input to the finite-difference model which then computed the resultant charge assembly cook-off time. If the cook-off time was more than the desired 10 min., the two solutions were repeated for the same firing rate but a greater number of rounds; fewer rounds were used if the cook-off occurred too quickly. This iterative procedure was repeated until the number of rounds was determined that gave the desired reaction time. By repeating the entire procedure for other rates of fire, a 10 min. cook-off curve was generated. This curve correlated the rates of fire with the number of rounds necessary to create a 10 min. cook-off hazard.

In order to understand the relationship between a 10 min. cook-off hazard and the combinations of the number of rounds and rates of fire required to produce this hazard, it is first necessary to look at the thermal response of the barrel to several firing histories. During a sufficiently long constant rate firing schedule, regardless of the rate of fire, any point in the barrel will eventually reach a state of thermal equilibrium. To demonstrate this fact, chamber surface temperature histories for a given number of rounds at five rates of fire are given in Figure 8 for uncooled NACO. As can be seen in this figure, the chamber surface at 617 cm tended toward an equilibrium temperature which was different for each rate of fire. Should these curves have been extended for a sufficient number of rounds, they would have all approached some steady-state condition. The higher the rate of fire, the higher is the equilibrium temperature.

For uncooled NACO, a 10 min. cook-off occurred after 950, 475, 485 and 545 rounds for firing rates of 1.0, 2.5, 5.0, and 10.0 rnd/min., respectively. The chamber wall temperature histories during a 10 min. cook-off corresponding to each of the above pair of conditions are given in Figure 9. The higher the rate of fire prior to cease fire, the faster was the chamber wall temperature rise after the last round was fired. At a rate of fire of 1.0 rnd/min., there was hardly any increase in temperature during cook-off. However, after 545 rounds at 10 rnd/min., the chamber wall was experiencing a temperature increase at the rate of approximately 3.1°K/min. It should also be noted that the chamber wall temperature increase was not greatly affected by the cease fire. For example, at 5 rnd/min., the heating rate of the chamber wall was 18°K in the 10 min. prior to the last round and 16°K in the 10 min. afterward.

The importance of considering axial heat conduction in the gun barrel heating problem is effectively demonstrated by the results in Figure 9. For all rates of fire except the 1 rnd/min. case, the chamber wall temperature at 617 cm from the muzzle continued to rise over the 10 min. time interval even though energy input to the barrel had ceased. This result was expected since, as shown in Figure 3, there was a significant discontinuity in barrel heat input at 602 cm from the muzzle. During a 10 min. cook-off, the time averaged chamber wall temperature at the front of the propellant bed was in the range of 440 to 445°K for all rates of fire which were investigated (Figure 9). Of the five rates of fire considered in Figure 8, it is clear that the 2.5 rnd/min. rate would heat the chamber wall to this temperature range after the fewest number of rounds fired. Referring again to Figure 8, a 5.0 rnd/min. rate would require the next fewest rounds to achieve the critical temperature range. Rates of 10.0, 1.0, and 0.5 rnd/min. require, respectively, more rounds to approach the thermal state associated with a 10 min. cook-off hazard. This same trend is concisely indicated in Figure 10 by the uncooled NACO curve.

In the analysis of the cooled NACO propellant rounds, the thermal response of the barrel and the charge assembly was qualitatively similar to what has already been described for uncooled NACO. In calculating the thermal response of the gun barrel, the thermophysical properties of the barrel at 617 cm, as listed in Table I, were used in conjunction with the base-line, single-shot heat input for cooled NACO as given in Figure 3. Shown in Figure 10 by the dashed curve is the locus of rates of fire and number of rounds fired which cause a 10 min. cook-off for cooled NACO. Note in this curve, as in the solid curve of Figure 10, that there is a particular rate of fire which requires the fewest number of rounds to induce a 10 min. cook-off. However, this minimum number of rounds is substantially greater than that for uncooled NACO.

When compared to the predicted results for uncooled NACO at a given rate of fire, many more cooled NACO rounds could be fired before a cook-off hazard occurred. This is because at any given rate of fire, the chamber wall will tend toward a lower equilibrium temperature when the cooled NACO is used.

CONCLUSIONS

The capability has been developed to predict the thermal response of a MK 18 MOD 3, 5"/54 barrel to any firing schedule and to simulate the associated propelling charge assembly cook-off in the hot gun. Even though the work reported here was for a specific gun and ammunition the same general technique may be applied to any other gun and ammunition. In the mathematical model, the primary required input is the exact firing history, i.e., the rate of fire and number of rounds fired in each burst and the length of the cooling periods between bursts. The accuracy of the computer codes and techniques of data analysis have been verified by successfully predicting the bore and chamber surface temperature data taken on the USS TURNER JOY.

Based on the limited barrel temperature data available, 10 min. cook-off curves have been predicted for uncooled and cooled NACO service charges as a function of the number of rounds fired and the rate of fire. A comparison of these results shows that the smear coolant does significantly attenuate the single-shot total heat input to the bore. When compared to the analytical results for uncooled NACO at a given rate of fire, many more cooled NACO rounds could be fired in a single burst before a 10 min. cook-off hazard occurred. Since propellant gas convective heat transfer is the primary driving force for bore erosion, an attenuation of the heat input will also reduce bore surface wear.

The sole purpose of the cook-off curves in this report is to allow a consistent theoretical comparison and thus to qualitatively demonstrate the potential of the smear coolant to significantly reduce the cook-off hazard in a MK 18 MOD 3, 5"/54 barrel. These curves cannot be interpreted as cook-off indicators becasue they were computed for uninterrupted, constant rate of fire bursts and are associated with barrels beginning their thermal response from a specific initial temperature of 20°C. The use of these curves, with an averaged rather than an exact constant rate of fire, to predict a 10 min. cook-off ignores the significant effects of cooling periods and various firing rates upon the thermal state of the barrel and, therefore, upon the charge assembly cook-off.

NOMENCLATURE

A = Actual value of the magnitude of the local heat flux function ($cal/cm^2 \cdot s$)

A' = Nominal value of the magnitude of the local flux function based on a nominal decay rate ($cal/cm^2 \cdot s$)

c = Actual value of the decay rate of the local heat flux function (1/s)

c' = Nominal value of the decay rate of the local heat flux function (1/s)

c_p = Specific heat (cal/gm·°K)

h_{bs} = Local instantaneous connective heat transfer coefficient ($cal/cm^2 \cdot s \cdot °K$)

k = Thermal conductivity ($cal/cm \cdot s \cdot °K$)

Q = Single-shot total heat input to the bore (cal/cm^2)

$\dot{q}''_n$ = Local instantaneous heat flux to the bore surface during the n^{th} shot in a firing schedule ($cal/cm^2 \cdot s$)

t = Time (s)

T_a = Ambient temperature (°K)

T_{bg} = Time averaged bulk gas temperature of the propellant combustion products as computed by the Lagrangian solution (°K)

T_{bs} = Local bore surface temperature during the n^{th} round (°K)

T_i = Initial barrel temperature (°K)

T_R = Representative barrel temperature at which the gun steel thermophysical properties were evaluated (°K)

$(T_r)_{n-1}$ = Residual local bore surface temperature just prior to the n^{th} shot (°K)

T_w = Barrel outside surface temperature (°K)

U = Overall coefficient of heat transfer on the outside of the barrel ($cal/cm^2 \cdot s \cdot °K$)

α = Thermal diffusivity (cal/cm^2)

ρ = Density (gm/cm^3)

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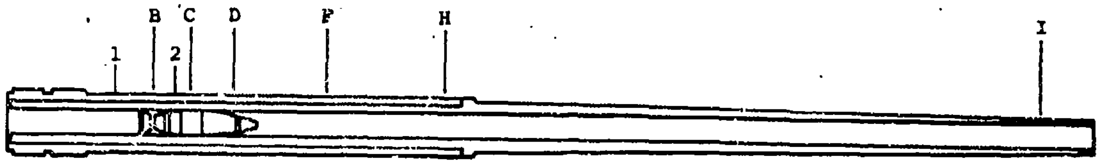

STATION	DISTANCE FROM THE MUZZLE (CM)
B	594
C	575
D	560
F	530
H	442
I	31
1	617
2	586

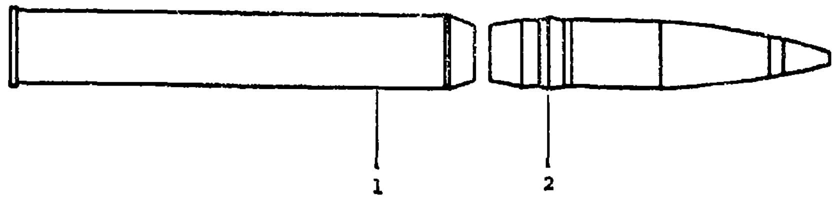

Figure 1. Location of Thermocouple Instrumentation

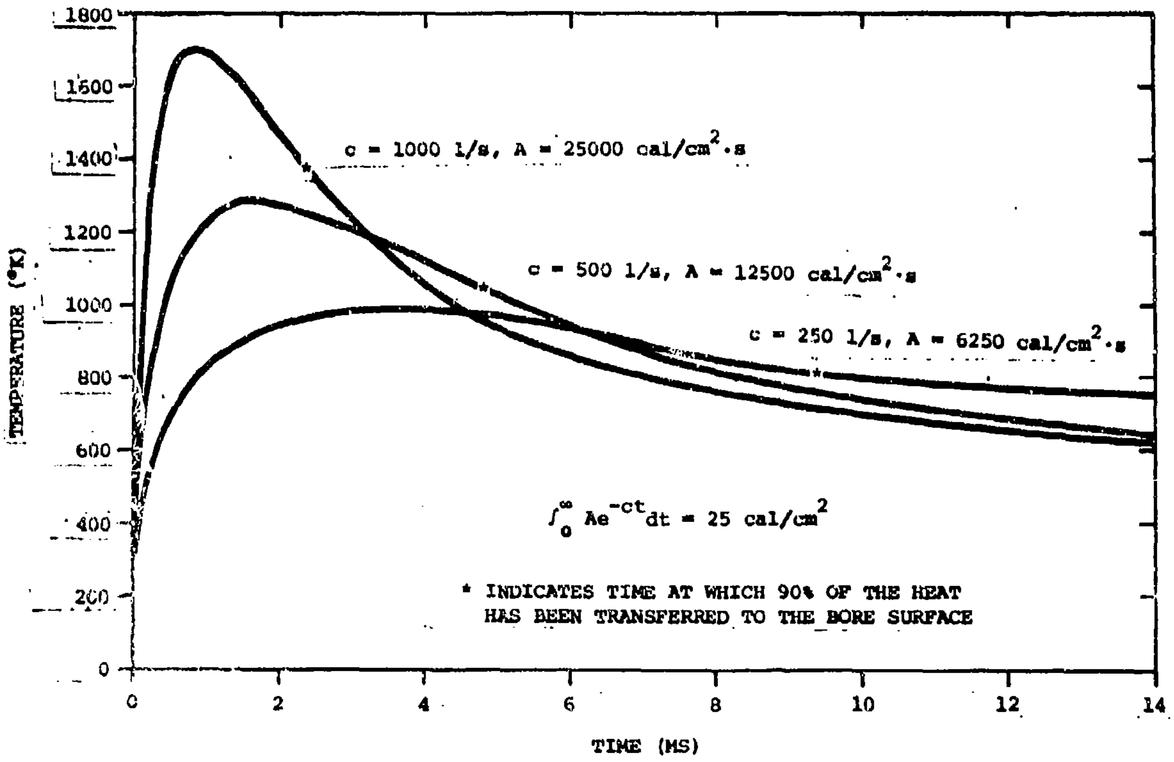

Figure 2. Effect of Heat Input Decay Rate on Bore Surface Temperature History

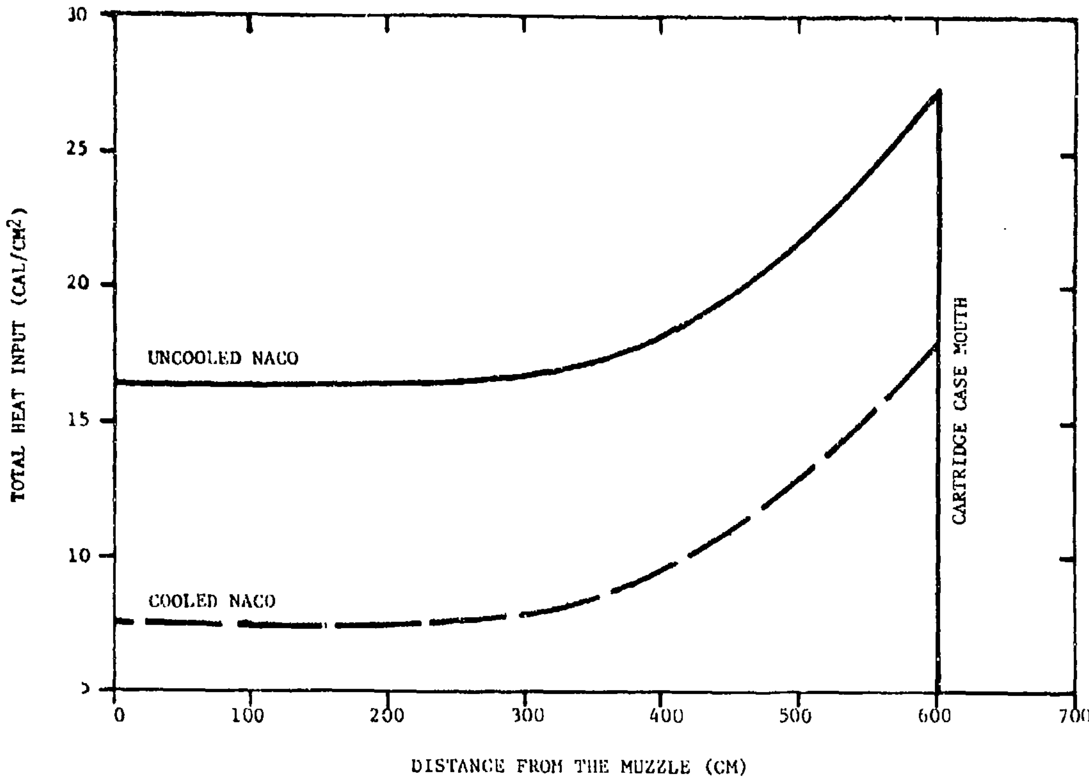

Figure 3. Empirically Determined Local Single-Shot Heat Input to the Bore for Uncooled and Cooled NACO

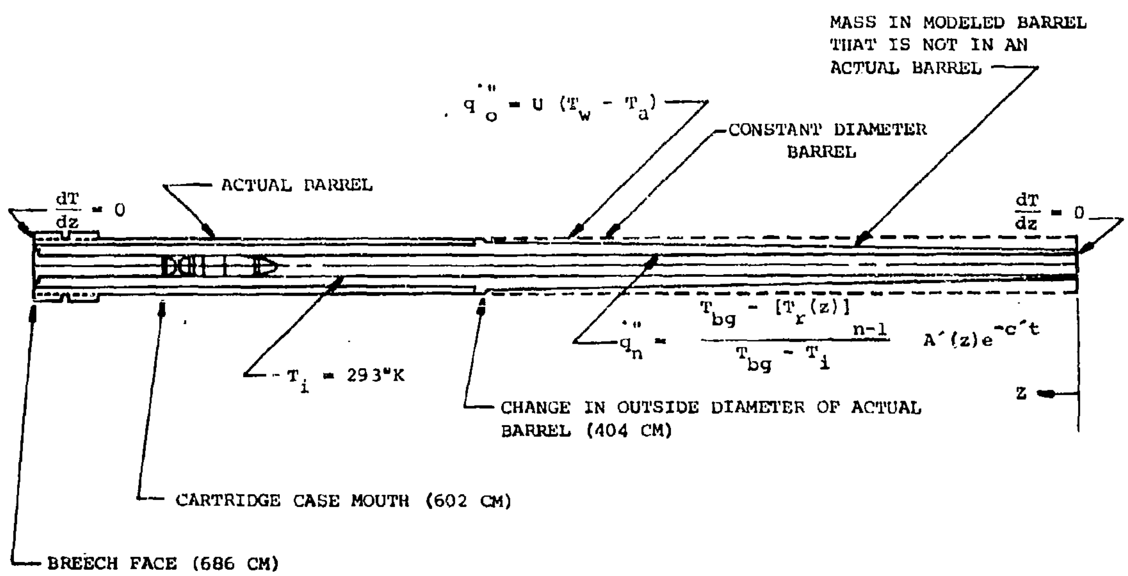

Figure 4. Comparison of an Actual MK 18 MOD 3, 5"/54 Barrel With the Constant Outside Diameter Barrel Modeled for the 2-D Heat Transfer Code

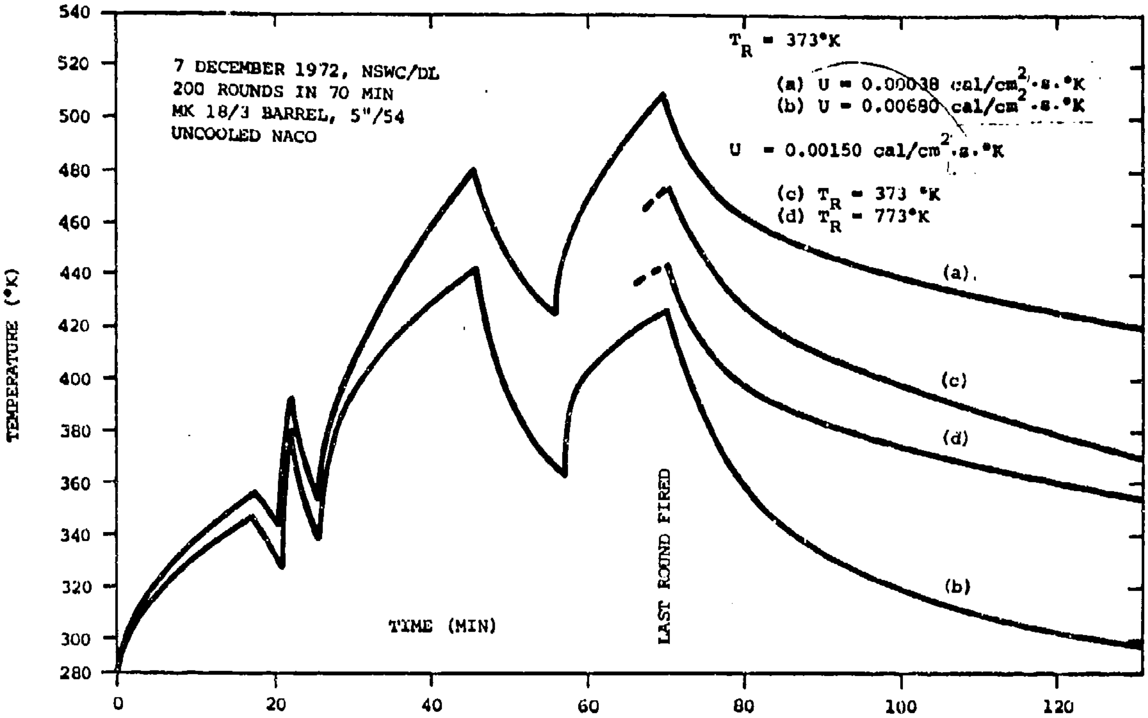

Figure 5. Sensitivity of the Bore Surface Temperature Response to Variations in T_R and U; Computations for 586 cm From the Muzzle

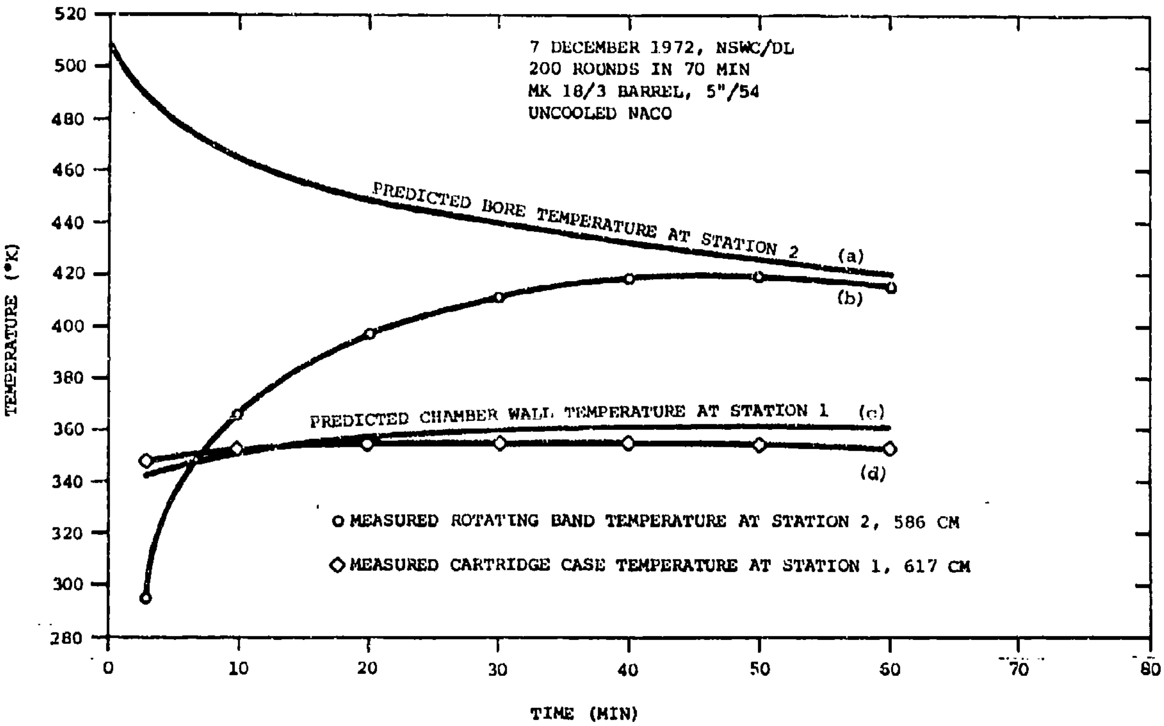

Figure 6. Thermal Response of an Instrumented Projectile and Cartridge Case When Rammed After the Last Shot

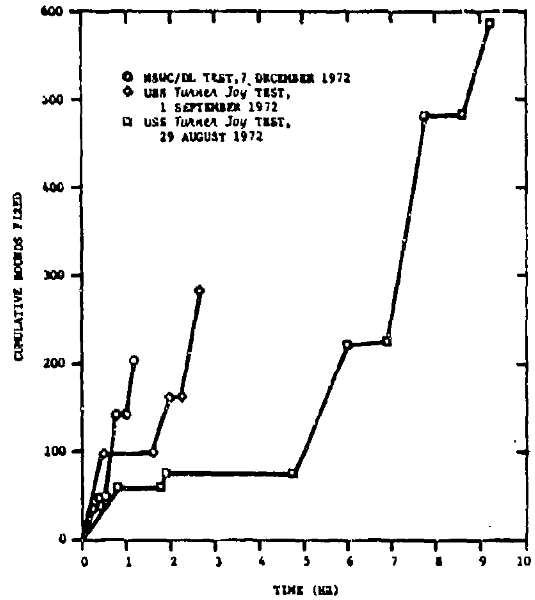

Figure 7. Firing Histories for the NSWC/DL and USS *Turner Joy* Tests

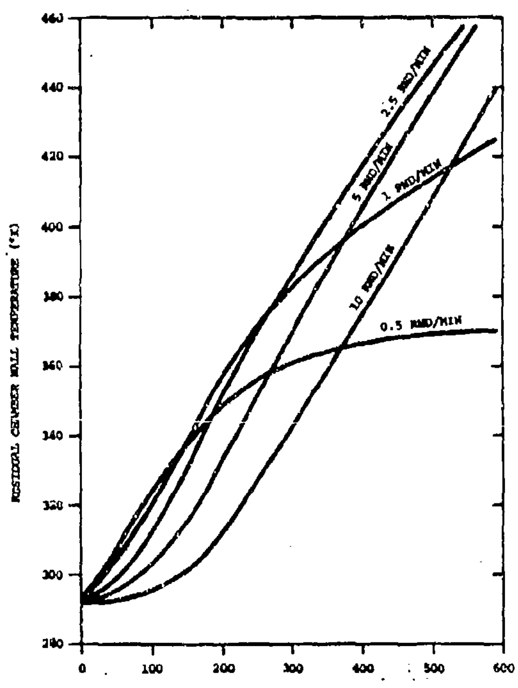

Figure 8. Analytic Chamber Wall Temperatures in a MK 18 MOD 3, 5"/54 Barrel at Station 1, 617 cm From the Muzzle; Uncooled NACO

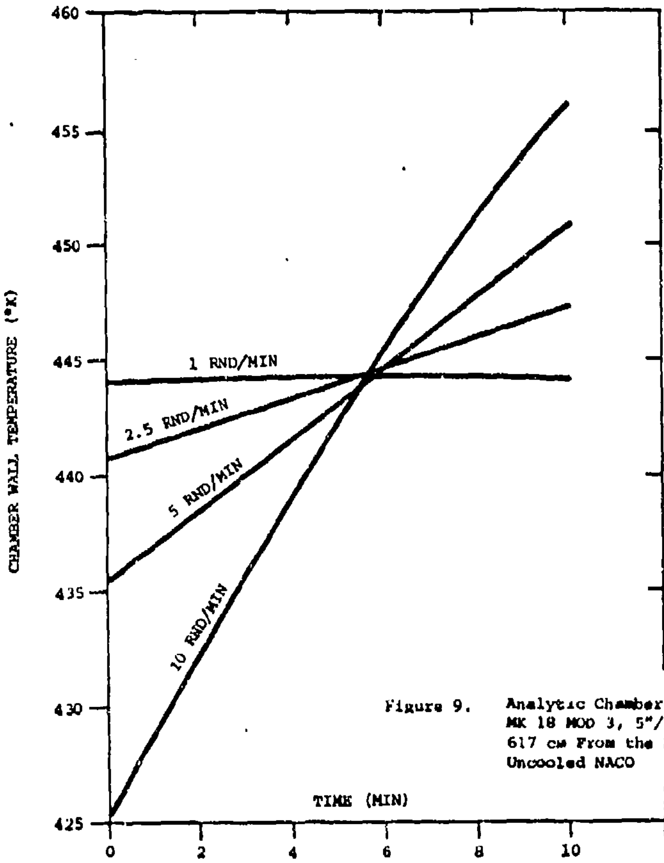

Figure 9. Analytic Chamber Wall Temperatures in a MK 18 MOD 3, 5"/54 Barrel at Station 1, 617 cm From the Muzzle, During Cook-Off; Uncooled NACO

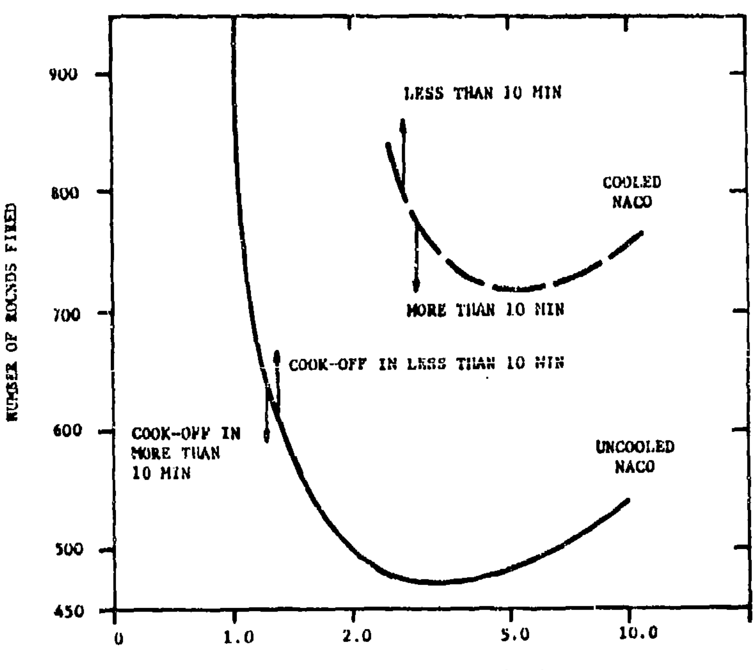

Figure 10. The Locus of Conditions Resulting in an Analytically Determined 10-min Cook-Off

A PROCEDURE FOR GUN BARREL EROSION LIFE ESTIMATION

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ABSTRACT

This paper outlines a procedure by which the erosion life expectancy of a rifled gun barrel firing a given projectile may be estimated. The techniques used in the development of this procedure will be described and improvements will be suggested. The role of the projectile rotating band - gun barrel interface design details in determining the appropriate "end of life" criteria is illustrated.

INTRODUCTION

The principal value of an erosion life estimation method in the development of a gun weapons system is to provide a means whereby a cost-benefit analysis may be performed on propelling package design options. In general, system performance level, size, service life and life cycle cost are strong functions of such parameters as propellant composition, operating pressure, charge weight and bore surface coolant technique. A technique for erosion life estimation is described in this paper.

OBJECTIVE

The objective of the work reported herein is to develop an accurate erosion life estimation technique for rifled gun barrels.

APPROACH

The estimation of the erosion life of a gun barrel - projectile combination involves the calculation of the erosion rate at each stage of the weapons life and the selection of an appropriate "end of life" criteria. Since just prior to World War I, all gun barrel "end of life" criteria have been known to be closely correlated with enlargement of the gun bore at the origin of bore location. It is thus sufficient to model the bore enlargement at this one location to produce an erosion life estimation model.

In reference (a), a method of calculating the new gun erosion rate of a gun barrel was developed and verified. This method is described herein in more detail. It was shown that the erosion rate in a rifled gun barrel is essentially an exponential function of the peak bore surface temperature. The primary source of heat input in a gun barrel is recognized to be convective heat transfer. A second heat input source is the frictional heating generated by the rotating band.

As a gun erodes, the projectile seats further down bore. This effect produces two changes in the heat input. As the effective chamber volume increases the peak pressure realized by a service round is reduced thereby directly reducing the heat input. The second change is that part (and in extremely worn guns, all) of the rotating band seats ahead of the origin of bore location thus exposing the origin to less frictional heating again reducing the heat input. The rate at which the chamber volume increases with bore enlargement is primarily a function of the rotating band - gun barrel interface design details. Thus each worn gun condition is no more than a special case of a new gun condition and the local erosion rate can be calculated accordingly.

Eventually a level of bore enlargement is reached at which some element of the "end of life" criteria will be exceeded. To estimate the service life expectancy, this condemnation limit must be identified and used to terminate the calculation.

CALCULATION OF EROSION RATE

From data reported in references (a) and (b), the metal removal rate on the lands at origin of bore was shown to be an exponential function of the peak bore surface temperature reached during firing. To calculate the peak bore surface temperature, a heat transfer model was required.

A heat transfer model developed by the National Defense Research Committee of World War II (reference (c)) has been coded for use on the CDC 6700 Computer at Dahlgren. This code has been used to predict bore surface temperatures in guns; formulas have been fitted to data from this code to permit hand estimation of peak bore surface temperature for single shots and multiple round bursts.

This heat transfer model assumes that the heat is transferred to the bore only by forced convection from the mainstream gas flow according to the formulas.

$$h = \lambda C_p P_v$$

$$\frac{\partial O}{\partial r}_{\text{bore surface}} = h(O_g - O_s)/k$$

where

λ = friction factor (a constant)
C_p = specific heat of gas at constant pressure(another constant)
P = gas density
v = gas velocity
h = heat transfer coefficient
O = temperature
r = radial distance
O_g = gas temperature
O_s = bore surface temperature
k = conductivity of steel

These formulas for heat input were chosen because they are simple; the more complicated formulas of reference (d) may or may not be an improvement.

The heat conduction problem is then solved in one dimension (radially) by finite difference. To make computer time and storage requirements reasonable for multiple rounds space and time steps are changed as the problem progresses.

It was decided to attempt to obtain a desktop procedure to estimate gun erosion rates. Gun bore surface temperatures were estimated from the heat transfer code mentioned previously and these temperatures were related to measured wear in guns by fitting erosion data for the 5"/38 MK 12 MOD 1 and 16"/50 MK 7 guns to the form $W = Ae^{BT}$.

Accordingly, the following formulas were obtained:

$$T_w = 1.096\ (T_f - \Delta T_c - 600)\ (CP)^{1/2}/d$$

$$T_i = .4632\ (T_f - \Delta T_c - 600) C^{.75} (N-1)^{.6} R^{.5}/d^{1.5}$$

$$W = .4216\ e^{.0049(T_w + T_i)}$$

where

T_f = adiabatic flame temperature of the propellant (^oK)

ΔT_c = coolant correction

= 500K^o for case guns with coolants

= 300K^o for bag guns with coolants

= 0 for no coolants, or ineffective coolants

C = change mass (kg)

P = peak pressure (MP_a)

d = bore diameter (nm)

T_w = peak bore surface temperature rise for a single round (K^o)

R = "effective" firing rate (rds/min)

N = "effective" number of rounds fired

For 1-5 shots over a period of an hour, use $T_i = 0$.

For a single burst, or a period of steady firing, use
R = actual firing rate, N = half the number of rounds fired

For multiple bursts, use

R = firing rate for one burst, N = number of rounds per burst

or

R = average firing rate (including pauses), N = total number of rounds fired to maximize T_i.

It is necessary to specify the limits over which this erosion model is applicable. These limits can be determined by consideration of the phenomenology of erosion and can be confirmed by experimental data. When a gun is fired, the bore surface is exposed to a very substantial transient heat flux. This heat flux causes a thin layer of steel near the bore surface to be heated. When the temperature of the bore surface is heated above 1070°K, a solid state phase transformation occurs in which the tempered Martensite structure of the gun steel near the bore surface is transformed to Austenite. The interior ballistic cycle being very short (typically 5-100 ms) rapid cooling also occurs resulting in the trapping of Austenite and the creation of pearlite and ferrite structures. These transformations produce cracking of the bore surface due to the volume change of the phases. All of these transformations occur in a thin white layer on the bore surface having thickness dimensions approximately 100 μm. The term "white layer" is applied to this transformed region and is known to form only in the presence of nitrogen implying the existence of some chemical reactions. The roughened surface is then subjected to the turbulent flow of hot gases. It is postulated that these vortices stagnate producing spot melting of the grain boundaries which free the grains to be removed by the gas stream. This microscopic metal removal results in the macro phenomenon known as erosion. There is no evidence that metal on metal abrasion has anything to do with erosion in guns larger than 40mm caliber. It is known from experimental data that erosion stops if the peak bore surface temperature is held less than 1070°K and the white layer does not form.

The (T_W+T_i) parameter of the erosion model is an approximation of the bore surface temperature rise (add 300°K to achieve absolute temperature) using the Nordheim model. From cold gun experimental data in 5"/38 and 5"/54 gun barrels, it can be shown that at $750°K < (T_W+T_i) < 800°K$ there exists an erosion cut-off and the creation of "white layer" formation ceases.

The upper limit of the model is the melting point of steel, i.e., 1800°K. Above this level, a molten film exists over the entire bore surface and any further energy input is absorbed in phase change, i.e., heating and boiling the molten steel. Experimental data reported in reference (a) indicates that surface pit melting occurs at a (T_W+T_i) = 1550°K rise and thus an upper limit for the model (T_W+T_i) parameter of about 1700°K rise is appropriate. This upper limit implies that the maximum erosion rate which can exist in any gun is about 0.18mm/rd. This value is consistent with the reported 60 round life of the German Paris gun predicting a bore enlargement of about 10mm at condemnation.

CALCULATION OF WORN GUN EROSION INCREMENTS

As a gun erodes, two elements lead to reduction of the heat input at the origin of bore. The projectile begins to seat further down bore increasing the effective chamber volume. This movement was shown in reference (b) to be the primary producer of muzzle velocity loss and pressure loss. The pressure reduction can be seen to reduce the peak bore surface temperature. The rotating band also begins to seat over the origin location reducing the length of rotating band which must traverse the origin reducing the frictional heat input. Both of these occurrences result in the reduction in the erosion rate.

The peak pressure as a function of increasing chamber volume can be calculated using conventional interior ballistic methods. A more difficult problem is the calculation of the projectile seating distance as a function of bore enlargement. This calculation involves the projectile - gun barrel interface. To illustrate the problem consider the 5"/54 MK 41 projectile in the 5"/54 MK 18 gun barrel. This projectile seats initially on the forward edge of the rotating band, Figure 1. When the origin of bore dimension reaches 128.5mm, the projectile begins to seat forward until an enlargement of 130.05 mm is reached, Figure 2. At this point, the "high lip" contacts the forcing cone and controls the seating. When the origin of bore diameter reaches 132.21mm, the rotating band high lip is located at the origin location, Figure 3. Figure 4 is the rotating band configuration.

If a rough plot were produced of the bore enlargement vs the projectile seating distance for this projectile, Figure 5 would result. Note that until the 128.5mm diameter is reached no forward seating occurs and that after the 130.05mm diameter is reached, the relationship between the bore enlargement and the projectile seating distance is of constant slope. From such a plot as this, the peak pressure vs bore enlargement relationship may be inferred and then the number of rounds required to achieve a bore enlargement in any arbitrary firing schedule can be calculated, Figure 6. As can be seen, the model predicts more erosion in later gun life than actually exists. The reason for this divergence that the effect of the rotating band has been neglected. At present, this effect cannot be compensated for in the model although in the following section a qualitative treatment is provided. If the worn gun (T_W+T_i)

parameter falls below a temperature rise of 750°K before the condemnation limit is reached, the gun will have infinite erosion life as metal removal has ceased.

ROTATING BAND CONTRIBUTION TO EROSION

The rotating band contributes a frictional heat input to the bore surface. The erosion model being semi-empirical has what may be called a "Navy Standard" rotating band incorporated within it, i.e., a gilding metal rotating band giving about 200 MPA contact pressure at engraving. As the gun erodes, less of rotating band traverses the origin of bore and less heat input results. This factor produces the late life divergence of the predicted and actual erosion data.

Often questions arise concerning the effect of changing rotating band materials. On the basis of the data presented, herein and in reference (a), the following observations can be made:

(1) If the rotating band frictional heat input is a substantial part of total heat input, then reducing this component can reduce erosion.

(2) If the intention is to hold muzzle velocity level constant then it is generally true that a low friction rotating band will generate a reduced peak pressure and thus a reduced heat input. The introduction of a low friction rotating band thus can reduce gun barrel erosion rates especially if the comparable copper rotating band is oversized.

(3) If the peak pressure and charge weight is held constant and the muzzle velocity level is allowed to increase the erosion rates will be the same for either plastic or copper rotating bands.

Figure 7 is a plot of rotating band pressure vs caliber for standard Navy rotating bands. Note that the 3"/50 band has an abnormally high band pressure. In Table 1 the rotating band pressures and resisting force is calculated. Figure 8 is a plot of bore enlargement vs rounds fired. Note that the erosion rate with the copper rotating band is higher than the erosion model would predict. Note that when the model predictions are corrected for the peak pressure with the plastic rotating band, the prediction overlays the actual new gun erosion rate achieved with the plastic rotating band despite the fact that no attempt was made to correct the implicit copper rotating band assumption of the model. Note also that in worn guns, the gun firing the plastic rotating band ceased eroding when the bore surface fell below the erosion cut-off temperature.

This data set is the basis for claims that in medium caliber guns the use of plastic rotating bands can reduce the erosion rate by a factor of five. In fact in this particular case, such a claim is correct, but very misleading.

CONDEMNATION CRITERIA

The condemnation criteria which is applicable to a given projectile - gun barrel combination has three elements:

(1) Reduction in Range

(2) Reduction in Accuracy

(3) Reduction in Fuze Performance

The elements are all related or relatable to a certain maximum level of bore enlargement. The critical question in erosion life estimation is the selection of the appropriate level of bore enlargement for a given gun - projectile combination's "end of life" criterion. A survey of "end of life" criteria was performed in which rotating bands and rifling dimensions were correlated with the applicable "end of life" criteria. Certain trends were obvious and are summarized in Figure 9.

The standard Navy rotating band has a high lip which is 1.04D in diameter where D is the gun caliber and the band body is 0.5mm larger than the groove diameter. This band design was also used on the M-73 projectile for the 120mm tank gun. All weapons using this band system consistently have condemnation limits between $1.04D \leq Dmax \leq 1.06D$ with no loss of accuracy with gun erosion.

The 175mm M-437 projectile has a similar rotating band except that the band body is slightly smaller than the groove diameter. This projectile - gun combination is condemned at Dmax = 1.03D due to loss of accuracy.

All rotating bands without a high lip lead to a condemnation limit of Dmax = 1.02D and are condemned for loss of accuracy.

Using these rules it is thus possible to estimate the erosion life of a gun.

RECOMMENDATIONS

(1) A method to incorporate the cooling of the gun between bursts needs to be included in the model.

(2) A method of incorporating the effects of frictional heating by the rotating band into the model must be developed.

(3) Very close attention must be directed to gun barrel projectile interfacing early in development if an accurate weapon system is desired with a long service life.

REFERENCES

(a) JANNAF Paper - Heating and Wear Effects of Smear Coolants in Navy 5"/54 Guns

(b) NWL TR-3152 of Aug 1974

(c) NDRC Rpt #A262 (OSRD No. 3447) of 24 Nov 1944

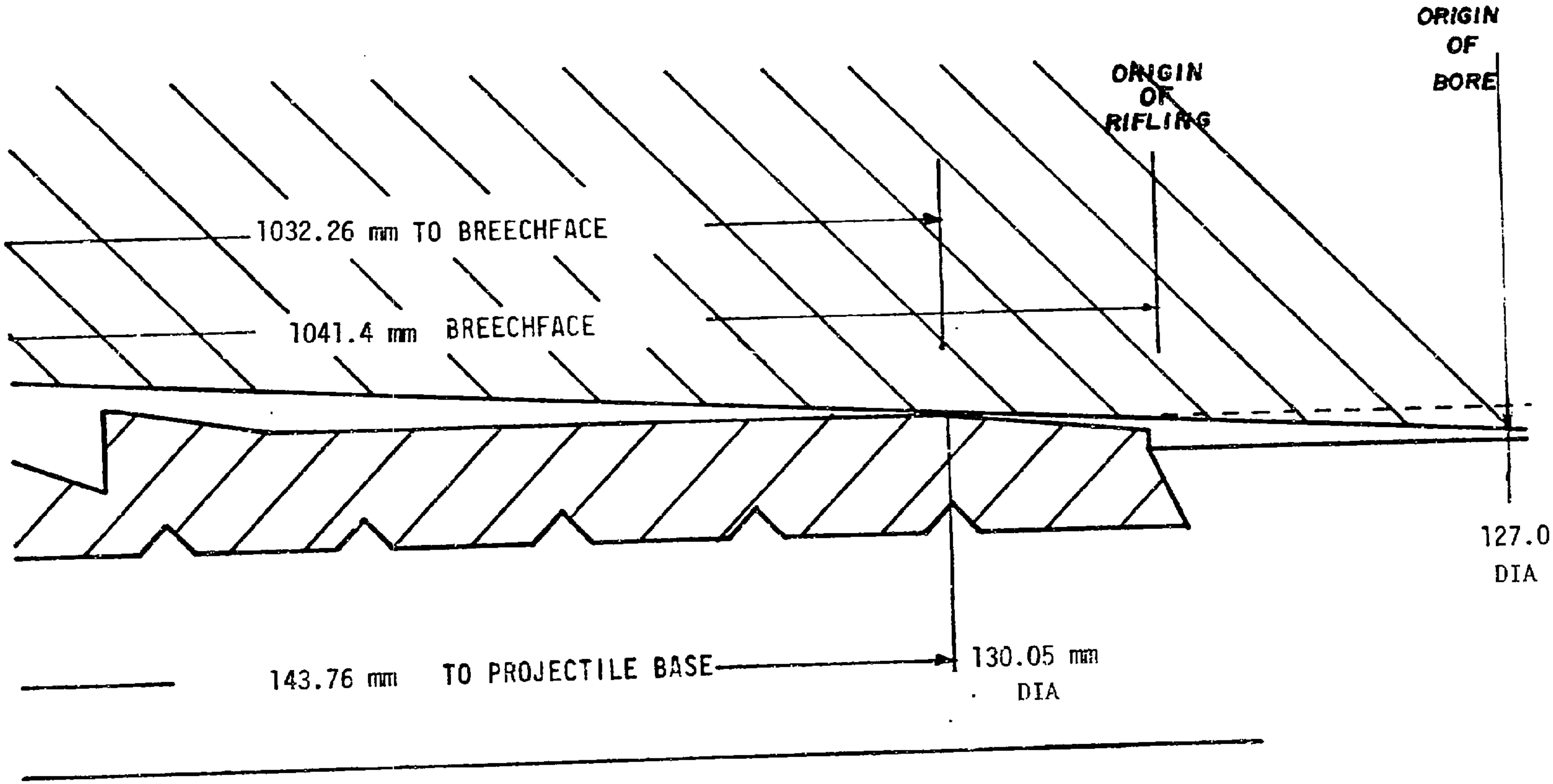
(d) Corner, John, Theory of the Interior Ballistics of Guns, John Wiley and Sons, Inc., New York, 1950

Table 1

3"/50 ROTATING BANDS

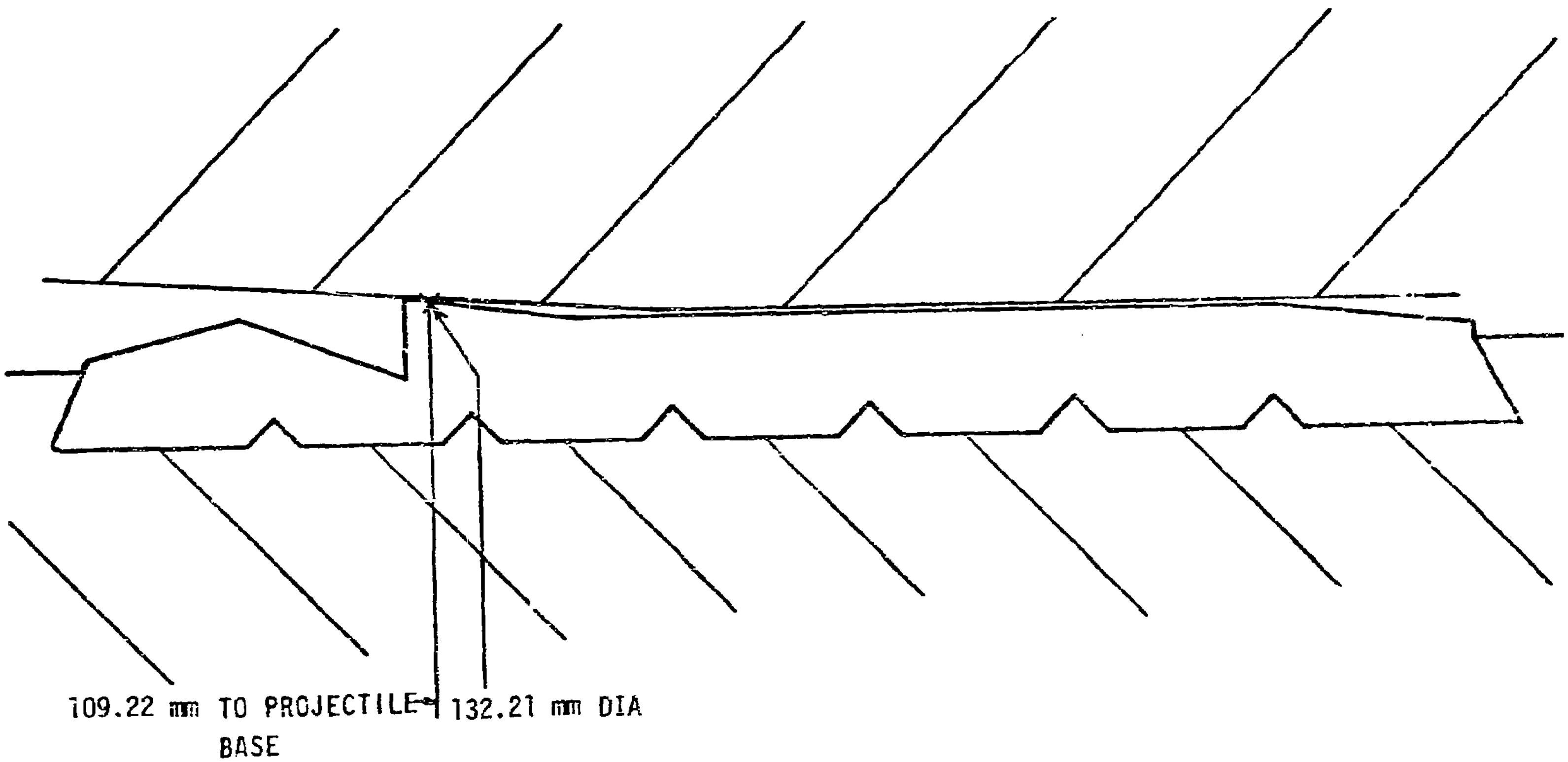
Band Material	D_b (mm)	$D_{band}-D_{groove}$ (mm)	P_b (MP_a)	μ	Resisting Force $F = \mu P_b A$ (kn)
Copper	78.18	0.457	492	.35	53.90
Copper	78.03	0.305	379	.35	41.00
Nylon	78.36	0.635	259	.09	7.19

Note: Band pressure calculation assures rigid projectile and gun barrel. Actual band pressures are about 1/2 these values.



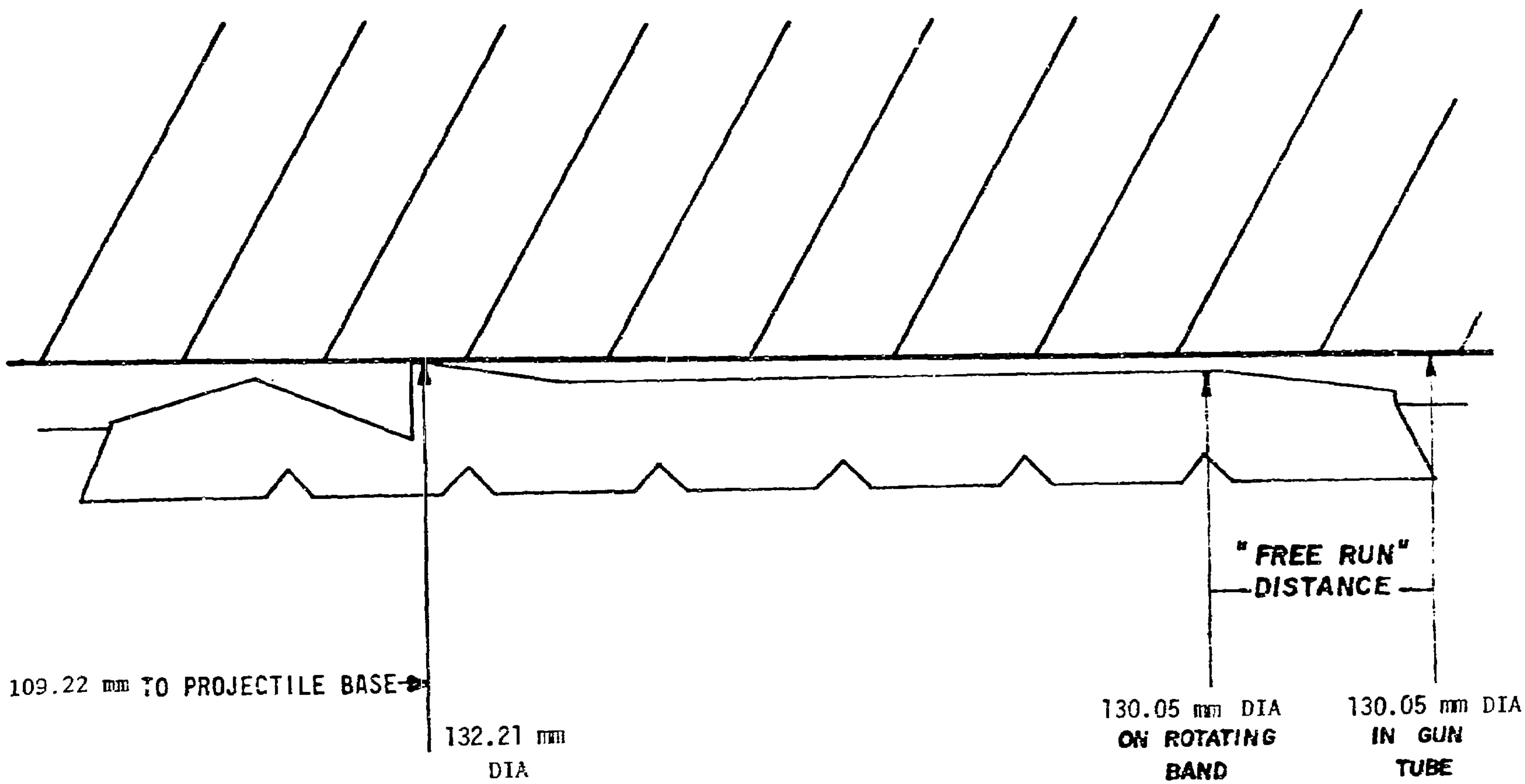
5"/54 MK 41 PROJECTILE SEATED IN NEW GUN

FIGURE 1



5"/54 PROJECTILE SEATING ON COMPRESSION CONE OF WORN GUN

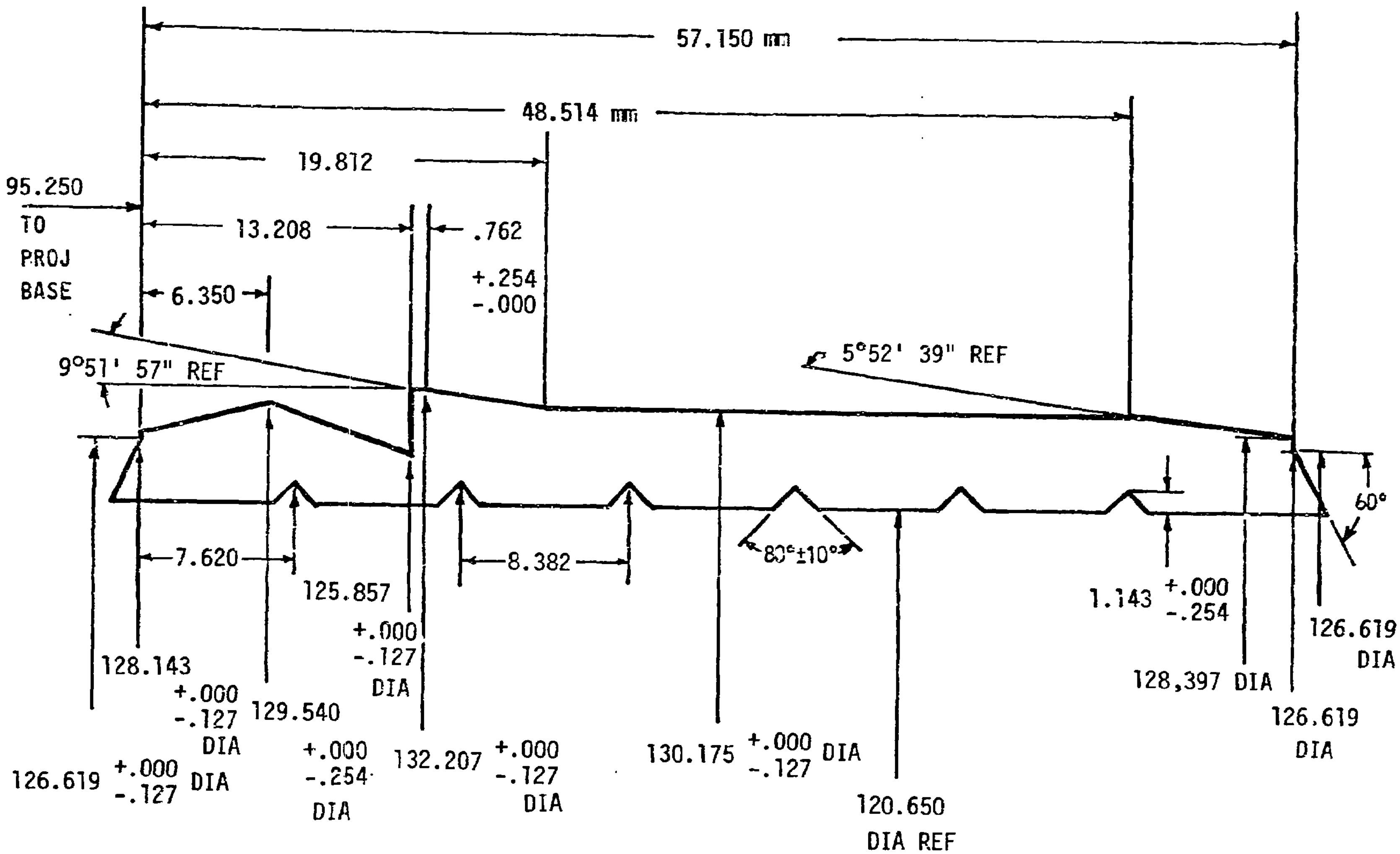
FIGURE 2



5"/54 ***MARK*** 41 PROJECTILE SEATED ON EROSION CONE IN VERY WORN GUN

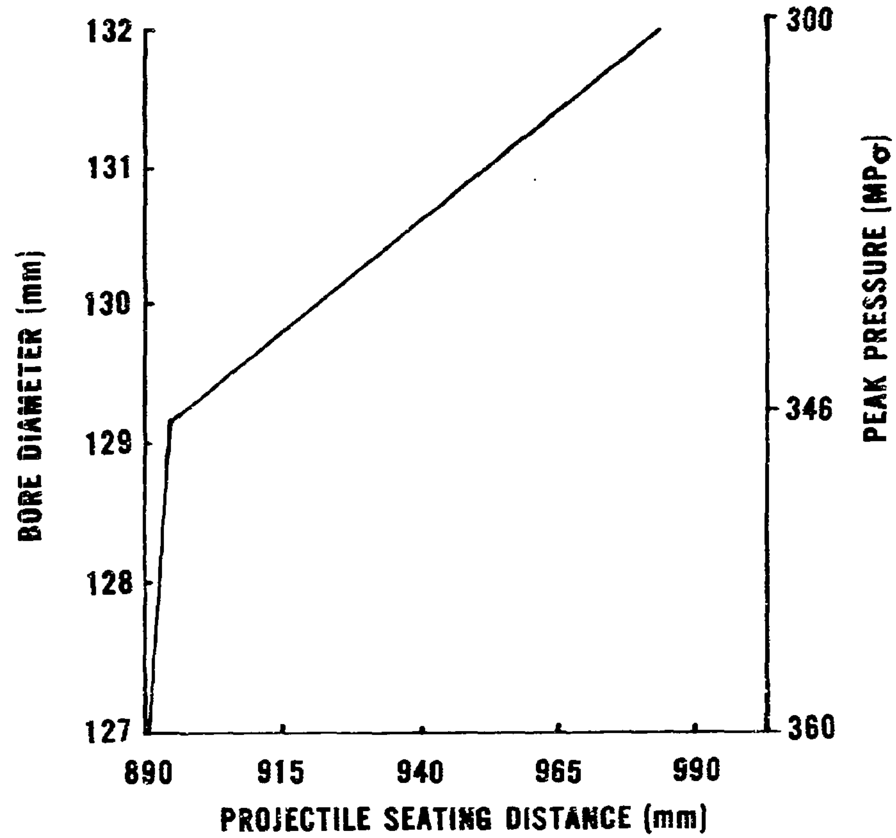
FIGURE 3

-II-410



BAND CONFIGURATION 5"/54 PROJECTILE

FIGURE 4



5"/54 MK 18 GUN BARREL FIRING MK 41 PROJECTILE

FIGURE 5

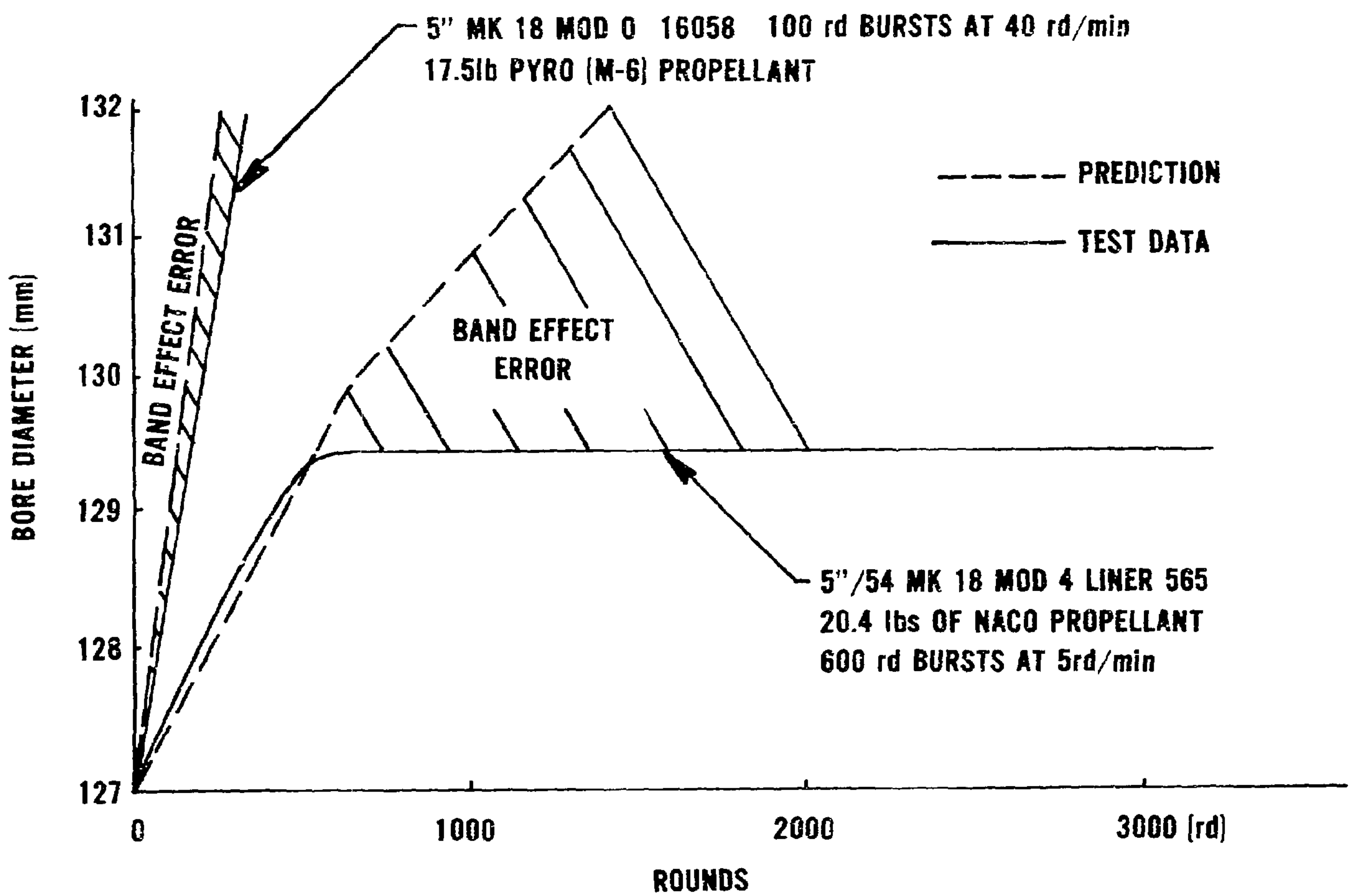
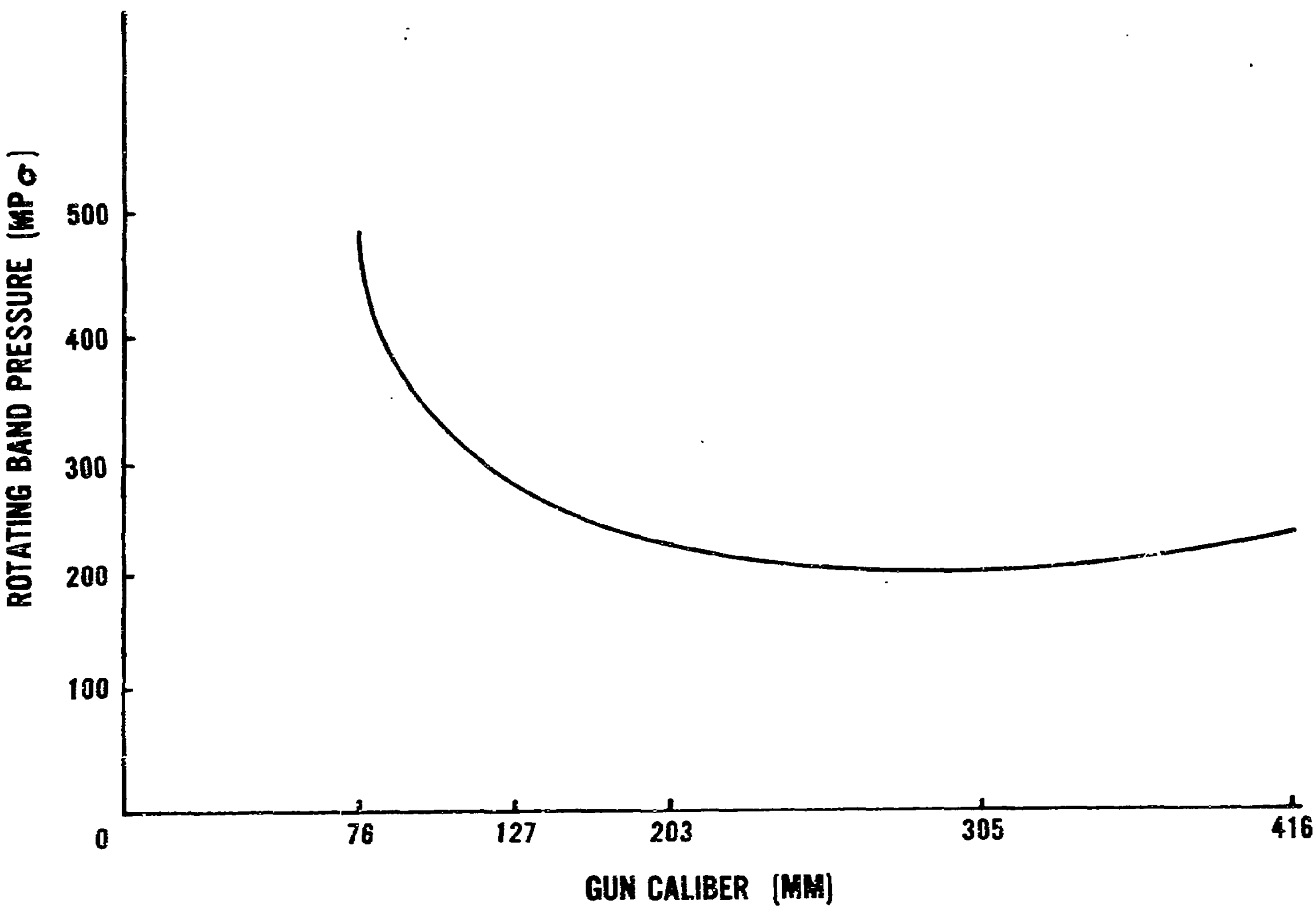
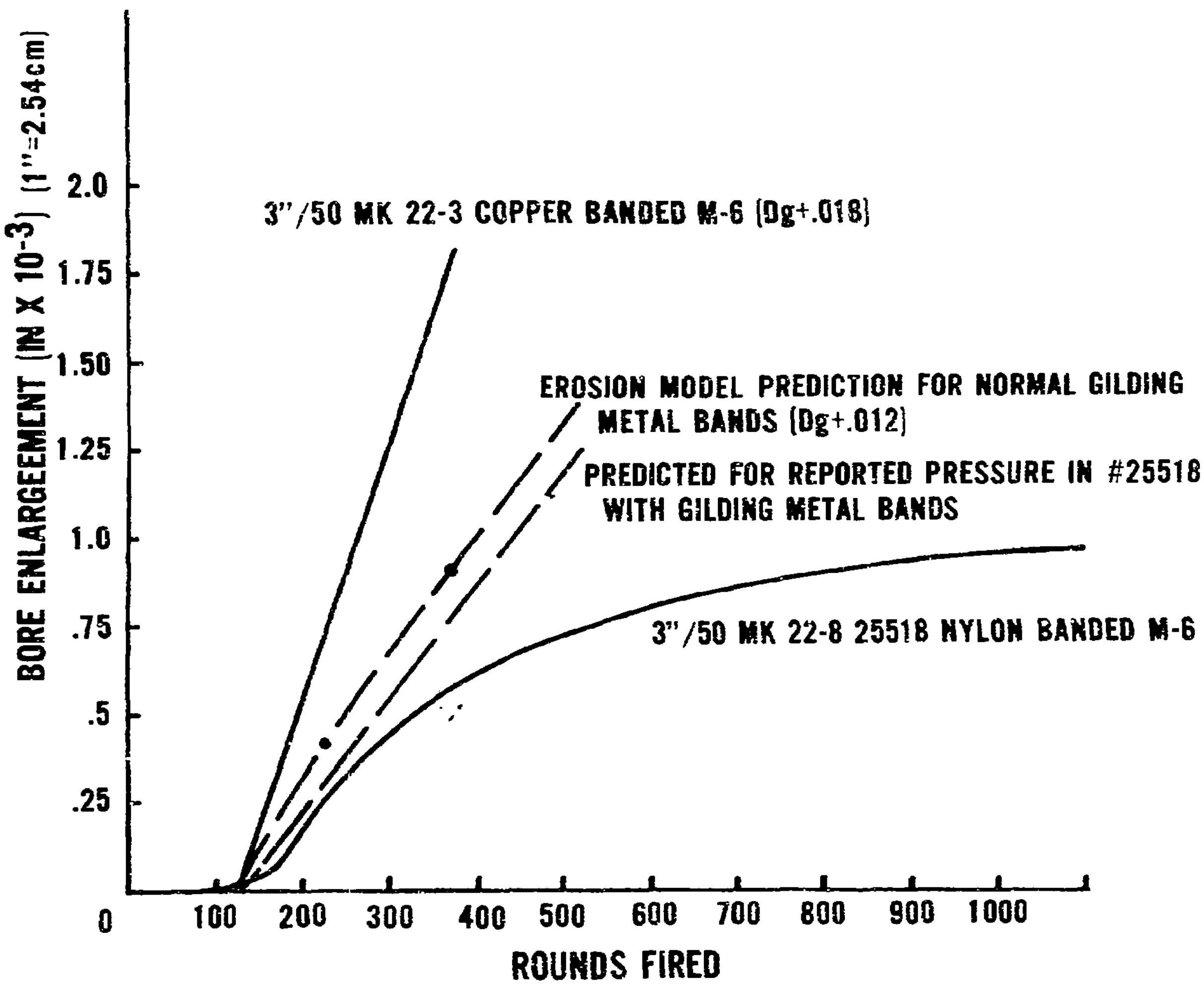

5" MK 18 MOD 0 16058 100 rd BURSTS AT 40 rd/min
17.5lb PYRO (M-6) PROPELLANT
BAND EFFECT ERROR
BAND EFFECT
ERROR
PREDICTION
TEST DATA
5"/54 MK 18 MOD 4 LINER 565
20.4 lbs OF NACO PROPELLANT
600 rd BURSTS AT 5rd/min
BORE DIAMETER (mm)
132
131
130
129
128
127
0
1000
2000
3000 (rd)
ROUNDS

FIGURE 6



NAVY ROTATING BAND PRESSURE (METALLIC)

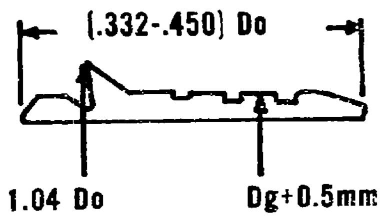
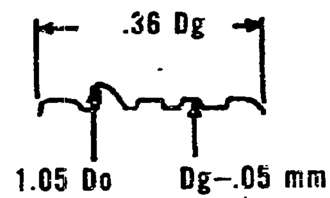
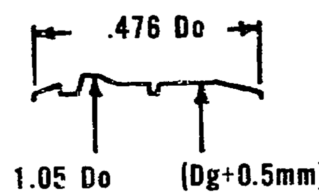
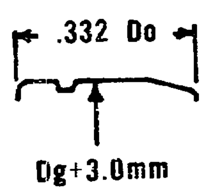
FIGURE 7



3"/50 RAPID FIRE TESTS WITH NYLON AND GM BANDS

(55 RD/MIN, 175 RDS)

FIGURE 8

SHAPE	GUN SYSTEM	% ALLOWABLE BORE ENLARGEMENT
	NAVY METALLIC BAND	4–6
	175mm M 437	3
	120mm M-73	5
	155 mm M101 (GUN)	2

Dg=DIA GROOVES
Do=BORE DIAMETER

CONDEMNATION CRITERIA FOR ROTATING BAND DESIGNS

FIGURE 8

III-416

STEEL EROSION PRODUCED BY PROPELLANT COMBUSTION PRODUCTS*

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INTRODUCTION

Steel erosion in barrels is a complex phenomenon involving the action of several interrelated processes, namely, (a) convective heating of the barrel by the hot combustion products, (b) chemical reactions of the multi-component combustion products with the steel surface, (c) physical interaction (aerodynamic forces) of the high-speed stream of propellant combustion gases with the steel surface, and (d) the mechanical action (abrasion and swagging) of the moving projectile on the surface. Interpretation of the processes is complicated further by the highly transient conditions that are typical of interior ballistics.

There is strong evidence that propellant gas/steel interactions are the primary cause of erosion rather than the mechanical action of the projectile on the bore surface.[1] Therefore, the emphasis of the investigation summarized in this paper was directed toward the experimental evaluation of the thermochemical processes that produce erosion of steel alloys subjected to the action of high pressure and high temperature propellant gases. The experiments were performed using a vented-combustor apparatus for the generation of the combustion gases.

Several investigators have utilized vented-combustor apparatus to study metal erosion by combustion gases. Greaves, et al[2] and Wiegand[3] found that above a critical pressure of the gases in the combustor, erosion is due to melting of the steel specimens. Below this characteristic threshold pressure for melting, erosion was attributed to chemical effects.[3] A comprehensive study of the chemical erosion of steel was made by Evans and co-workers[4,5,6] who studied the erosive effects of high pressure and high temperature gases produced by the combustion of O_2 with excess amounts of CO. They proposed that erosion of steel in the chemical region is

*Performed under U.S. Army Contract DAAG46-76-C-0069

due to the formation of volatile iron carbonyl [$Fe(CO)_5$] by the reaction of CO with Fe. They further concluded that separation of the two mechanisms is extremely difficult because both mechanisms are strongly influenced by the temperature of the system.

The objective of this investigation was to study the erosion characteristics of steel alloys under the action of high pressure and high temperature propellant gases in the laboratory and to relate these results to barrel erosion. The mass removal of steel alloys resulting from the erosive action of propellant gases was correlated with physical parameters such as type of steel alloy, type of propellant, and number of successive exposures. To obtain further insights into the mechanisms of the erosion processes, the eroded surfaces were characterized with respect to physico-chemical changes. A companion paper[7] deals with a related portion of this investigation which considered the erosion produced by pure gases and pure gas mixtures heated by means of a ballistic compressor.

EROSION OF STEEL ALLOYS BY PROPELLANT GASES

Experiments of the erosion of steel alloys by combustion-generated gases were carried out in the vented-combustor apparatus shown schematically in Fig. 1 and described in detail elsewhere.[8,9] The rapid burning of propellant in the combustor generates a hot, high pressure propellant gas that is allowed to flow through the test orifice. For a given propellant loading, the pressure history of the combustor can be partially controlled by adjusting the size of the second orifice, i.e., a vent orifice. The pressure history of the gases in the combustor is monitored by a high frequency Kistler 607 piezoelectric pressure transducer. A typical pressure trace is shown in Fig. 2.

The test specimens were in the form of disks with machined orifices at the center. Both circular and rectangular orifice geometries were used. The dimensions of the orifices are given in Fig. 3. The flat surfaces, provided by the rectangular configuration orifices were utilized for SEM (Scanning-Electron Microprobe) and EMP (Electron Microprobe Analysis) studies.

Most of the erosion tests were performed on AISI 4340 chromium-molybdenum steel. However, for comparative purposes a number of tests were performed on AISI 304 stainless steel and AISI 1020 carbon steel. Typical nominal compositions and room temperature properties of the above steels are shown in Tables I and II. The propellants used were IMR-4198 (T_V = 3000 K) and M1 (T_V = 2523 K) single-base propellants.

After each experiment, the test disk halves were cleansed, weighed, and the eroded surface examined. No qualitative differences were observed when comparing the milled surface with the opposed unmilled region of the matching half-disk. For simplicity, therefore, all results, except mass loss, given in subsequent sections refer to the unmilled side of the orifice. The mass losses quoted are the sum of that measured on both matched halves.

RESULTS AND DISCUSSION

A comparison of the erodibility of the three test steel alloys is shown in Fig. 4. This is a plot of the mass erosion experienced by the circular test orifices as a function of the number of firings. These tests were performed at approximately constant combustor conditions. As shown in Fig. 4, the carbon steel (AISI 1020) eroded the least, followed by the chromium-molybdenum steel (AISI 4340), and then by the stainless steel alloy (AISI 304). This trend of erodibility of these steels is correlated with their corresponding thermal properties. Low values of thermal conductivity and specific heat result in higher values of surface temperature of the specimen. This, in turn, causes higher erosion rates.

The linear behavior of the eroded mass versus number of firings curves shown in Fig. 4 indicates that the erosion experienced by a test specimen produces minor physical and chemical alterations on the eroded surface that apparently do not affect the subsequent erodibility of the material.

The above result has been collaborated by SEM studies of eroded surfaces. Figure 5 is an SEM photomicrograph showing an eroded surface exposed to IMR-4198 propellant gases. A comparison of Fig. 5 with Fig. 6, which shows a typical test surface prior to testing, indicates (a) the presence of a scale on the eroded surface which was found to become less abundant as the peak pressure increased, and (b) the presence of cracks on the underlying metal surface.

The relative decrease of area occupied by the scale at higher pressures suggests that this scale lacks sufficient cohesive strength to resist the higher shearing forces corresponding to the increased combustor pressure. Where the scale does form it has a brittle, flaky appearance.

Electron Microprobe Analysis (EMP) of these eroded surfaces indicated the presence of markedly more carbon in comparison with an unexposed specimen. The oxygen levels are shown to be comparable. It should be kept in mind that EMP quantitative capabilities are limited with respect to light elements. Furthermore, the Electron Microprobe Analysis is incapable of distinguishing whether these erosion products are bound into chemical compounds of iron or whether they have been merely deposited onto the surface with sufficient cohesive strength to resist the cleansing operations that were performed prior to examination. Moreover the EMP's insensitivity to oxygen does not rule out the presence of one or more of the iron-oxide scales.

The linearity of erosion with the number of firings has been observed in some guns. On the other hand, for other guns, the rate of erosion decreased with the number of firings. In the latter case it was considered[1] that erosion was mainly due to abrasive wear. One, however, may also contend that the decrease in the rate of erosion is due to the accumulation of oxide scale on the bore surface with the number of firings and thereby providing successively better oxidation protection. This was verified with erosion tests of steel alloys by high pressure and high temperature air utilizing the ballistic compressor.[7] This apparatus was developed[8,10] at Princeton University to study the erosive action of pure gases and prescribed gas mixtures on metals.

As shown in Fig. 7, the width of the surface cracks increases with increasing peak pressure of the combustor. The values used in Fig. 7 were obtained from SEM photomicrographs. Since the width of cracks varied over the eroded surface, a representative range of widths observed for each combustor peak pressure was plotted. Figure 7, furthermore, shows that a marked reduction in the width of the cracks is obtained using M1 propellant (T_V = 2528 K) instead of IMR 4198 (T_V = 3000 K). This size reduction of the cracks may be attributed to the lower isochoric flame temperature of the M1 propellant which results in a correspondingly lower metal surface temperature. Comparative tests on the three circular steel test orifices using IMR 4198 and M1 propellants showed that in all cases IMR 4198 produced higher mass erosion (by a factor of 2 in the case of steel AISI 304) than the M1 propellant.

The size of the cracks were also shown to vary with the type of steel used. The cracks were wider in the case of AISI 304 stainless steel (lowest thermal conductivity) than in the case of AISI 4340. The smallest width was obtained using carbon steel AISI 1020 which also has the highest thermal conductivity. Thus, crack dimensions are seen to be inversely dependent upon the thermal conductivity of steel. This again points to the affect of surface temperature on crack formation because the surface temperature decreases with increasing thermal conductivity of the material.

All indications thus far are that the mechanism of crack formation is thermal in nature and that cracks are probably formed due to strains, produced by volume changes resulting from the rapid heating and cooling processes, that are relieved by surface cracking. To further investigate the mechanism of crack formation and to experimentally evaluate whether hydrogen embrittlement contributes to the initiation and propagation of cracks, steel test specimens were subjected to the erosive action of high pressure and high temperature H_2 using the ballistic compressor apparatus.[7] These tests showed that even at H_2 pressures of 620 MN/m^2 no cracks were produced on the eroded surface. Since the heating time produced by the ballistic compressor is short (< 1 msec) compared to that of the combustor, the thermal wave thickness is much thinner. Thus, we suspect that the degree of the thermal stress produced by the ballistic compressor is not as great as that produced by the combustor and, as a consequence, the surface cracks are not formed.

A general observation of the SEM photomicrographs of eroded surfaces is that the cracks follow grain boundaries. The removal of such loosened grains by the subsequent firings might be an important process in the overall erosion of steel alloys subjected to repeated exposures of hot, high pressure propellant gases. Indeed, the surface regression of the metal surface per firing ($\sim$ 20 μm) allows the possibility that entire grains (which appear to be of the order of 5 μm in diameter) might be removed in their entirety during a single firing.

Figure 8 shows the surface of a sample exposed five times to IMR 4198 propellant gas at a chamber pressure of 290 MN/m^2. The nature, abundance, and dimensions of the cracks fit the trends established above for single firings so well that it can be concluded that with regard to crack formation, each firing acts independently. This is in agreement with the earlier discussed results that show that the mass erosion rate of steel specimens is independent of the number of firings.

Tests performed on barrels showed that the cracking process has minor effects on the overall erosion process of barrels. Ebihara[11] observed that unplated steel barrels of 7.62 mm machine guns experienced erosion at the origin of rifling earlier in the firing sequence, whereas cracks were observed later in the firing sequence (until approximately 200 rounds). Thus, gun barrel results agree with the present results which suggest that crack formations do not contribute appreciably to the erosion of steel alloys.

SEM studies of the eroded surfaces subjected to the erosive action of combustion gases and pure gases[7] (CO, CO_2, N_2, and O_2/N_2 mixtures) indicate that the erosion of steel alloys is caused by the formation of scales on the steel surfaces which, because of their low adhesive properties, are removed by the aerodynamic forces of the flow. Of course, for excessively long exposure, melting will be significant. Furthermore, ballistic compressor test results[7] disagreed with the carbonyl mechanism of erosion (i.e., erosion is due to the formation of volatile iron carbonyl by the reaction $Fe + 5CO \rightarrow Fe(CO)_5$ proposed by Evans and co-workers. This contradiction stemmed from (a) the inability of CO to produce erosion of steel alloys above the inert background erosion of N_2 and (b) the observed scale formation on the eroded surfaces of CO-tested specimens.

CONCLUSIONS

The erosion of steel alloys subjected to the action of high pressure and high temperature propellant gases was shown to be very dependent on the thermal properties of steel, i.e., the lower the thermal conductivity of the test specimen the higher the mass erosion.

The erodibility of the steel alloys was found to be independent of the number of firings. SEM studies of the eroded steel surfaces showed that these surfaces were covered largely by a scale that can be partly removed by the frictional forces of the high speed flow.

The width of the observed cracks on the eroded surfaces was shown to increase with increasing combustor pressure, isochoric flame temperature, and decreasing thermal conductivity of the steel alloys. Tests performed thus far indicate that the mechanism of crack formation is thermal; cracks are formed to relieve strains produced by volume changes resulting from the rapid heating and cooling processes. However, as in the case of gun barrels, no conclusive evidence was found to relate crack formations to the erosion of steel alloys.

Experimental evidence indicates that prior to melting the erosion of steel alloys by high pressure and temperature propellant gases is caused by the formation of scales resulting from the chemical interaction of the gaseous reactive species present in the combustion gases with the steel surface and the partial removal of the scales by the frictional forces of the flow.

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11. Ebihara, W. T., "Investigation of Erosion in 7.62 mm Machine Gun Barrels," Proceedings of the Interservice Technical Meeting on Gun Tube Erosion and Control, Ahmad, I. and Picard, J. P., eds., Paper No. 1.4, Watervliet Arsenal, Watervliet, N.Y., February 1970.

TABLE I Thermal Properties of Test Specimens Used in This Study

METAL OR ALLOY	DENSITY ρ g/cm^3	THERMAL* CONDUCTIVITY k cal/cm-s-K	HEAT* CAPACITY c cal/g-K	SOLIDUS TEMPERATURE T_M K
AISI 1020	7.86	0.124	0.107	1789
AISI 304	8.02	0.041	0.12	1700
AISI 4340	7.86	0.090	0.107	1778

*At room temperature

TABLE II Nominal Composition of the Steel Alloys Used in the Erosion Tests

	STEEL ALLOYS, AISI		
NOMINAL COMPOSITION	304	1020	4340
C	0.08 (MAX)	0.18-0.23	0.38-0.43
Mn	2.00 (MAX)	0.30-0.60	0.60-0.80
Si	1.00 (MAX)	0.10	0.20-0.35
Ni	8-12		1.65-2.00
Cr	18-20		0.70-0.90
Mo			0.20-0.30

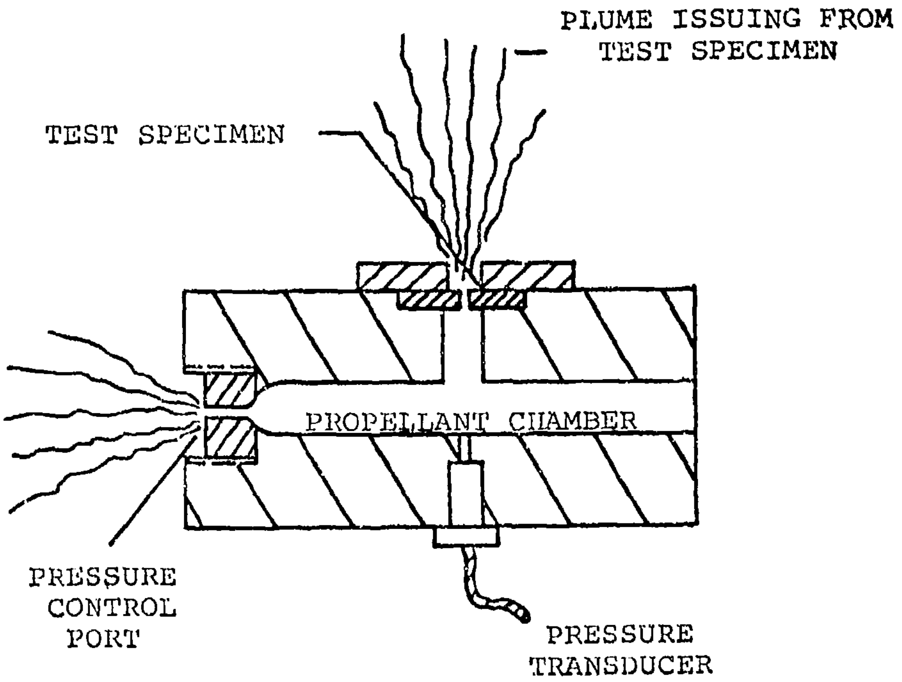
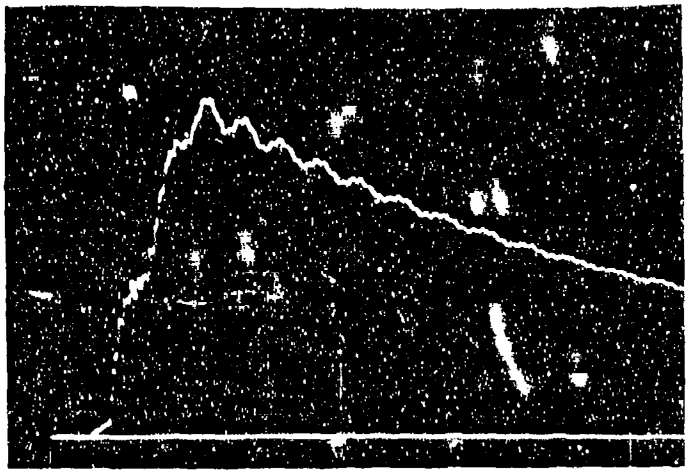


Fig. 1 Schematic diagram of the vented-combustor apparatus.



COMBUSTOR WITH
NO VENT ORIFICE

VERTICLE SCALE: 68.9 MN/m^2/DIVISION
HORIZONTAL SCALE: 0.5 MSEC/DIVISION

Fig. 2 Pressure history of the vented combustor.
Note: 1 MN/m^2 = 145.04 lbf/in^2

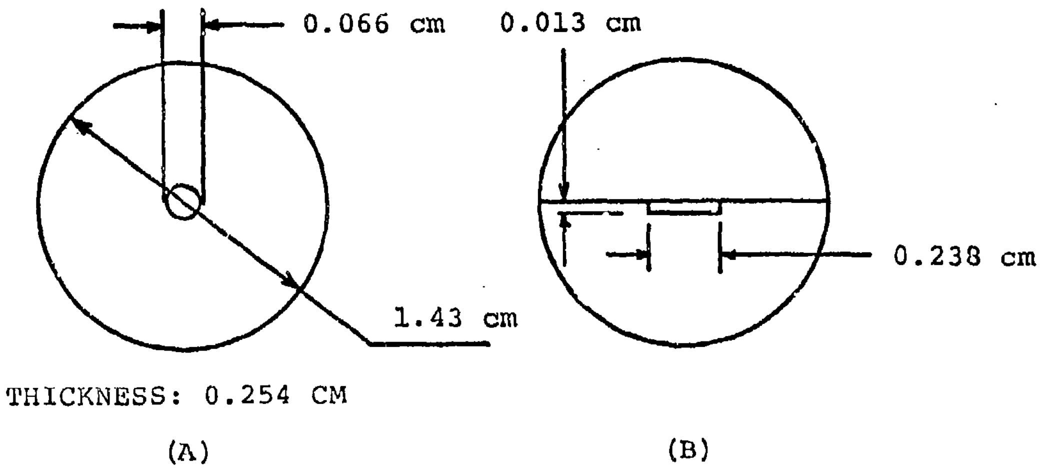


Fig. 3 Dimensions of test specimens; (A) cylindrical orifice, and (B) rectangular orifice.

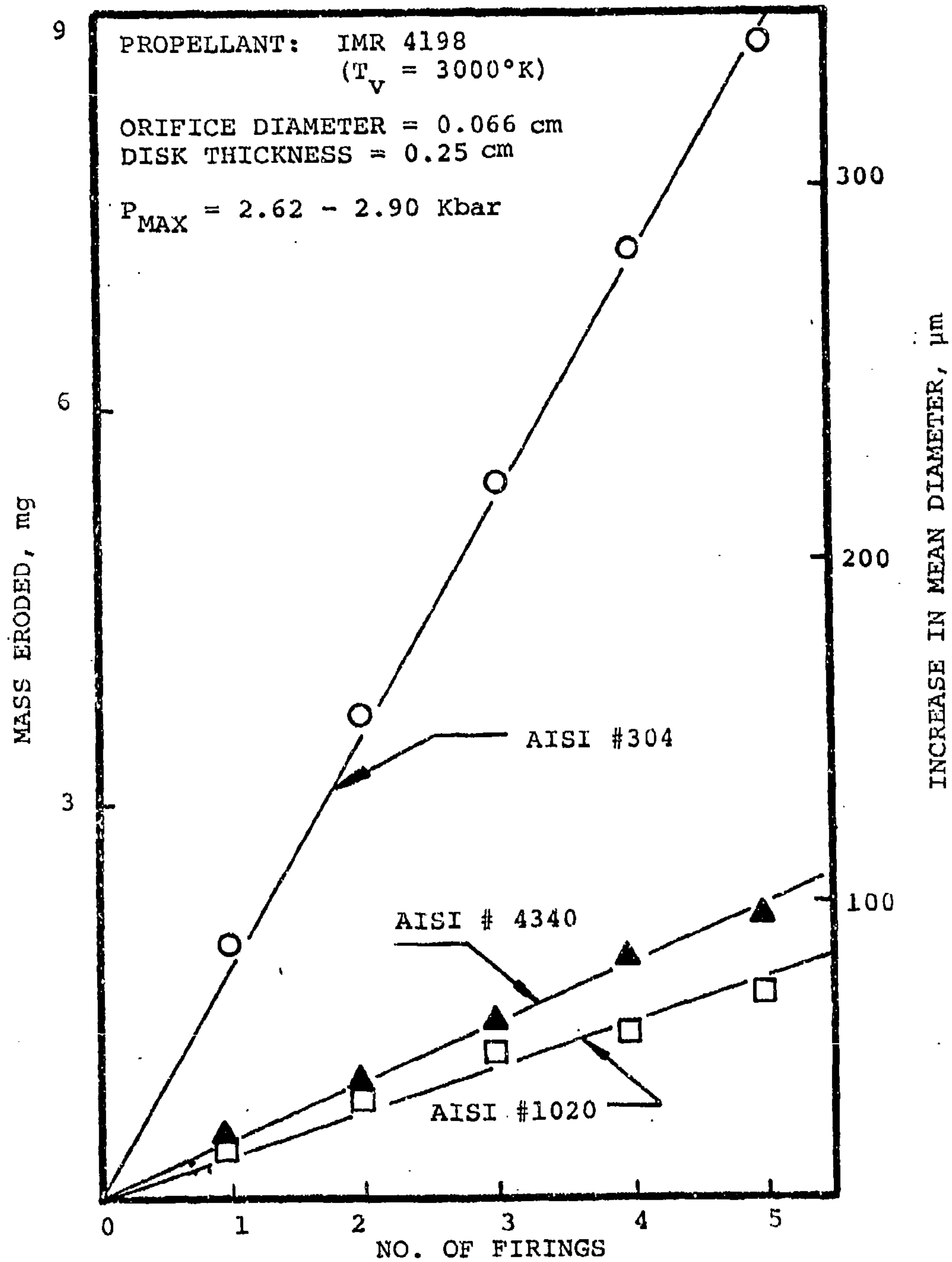
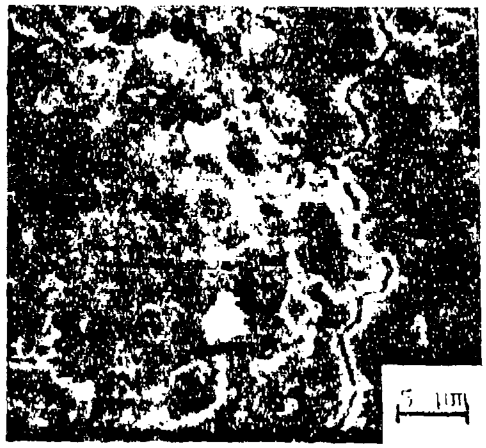
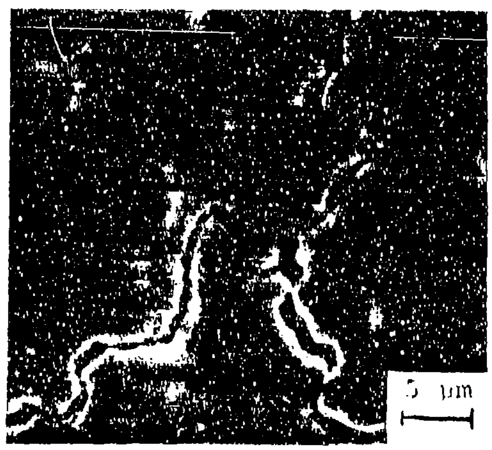


Fig. 4 Erosion of steel alloys as a function of number of firings. The linearity of the curves indicates that the mechanism of erosion does not vary from test to test. (Total number of firings: 5.)



$p_{max} = 213.7\ MN/m^2$
$\Delta m = 2.32$ mg



$p_{max} = 403.4\ MN/m^2$
$\Delta m = 7.02$ mg

Fig. 5 SEM photographs of the surface of AISI 4340 steel specimens showing that the width of cracks produced by the action of IMR 4198 propellant gases increases with increasing pressure.

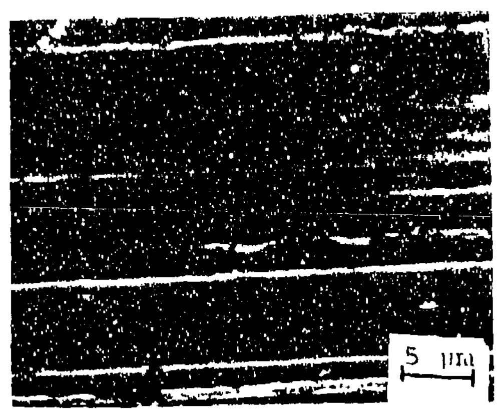


Fig. 6 SEM photograph of the surface of an untested specimen. The horizontal bands are machine tool marks.

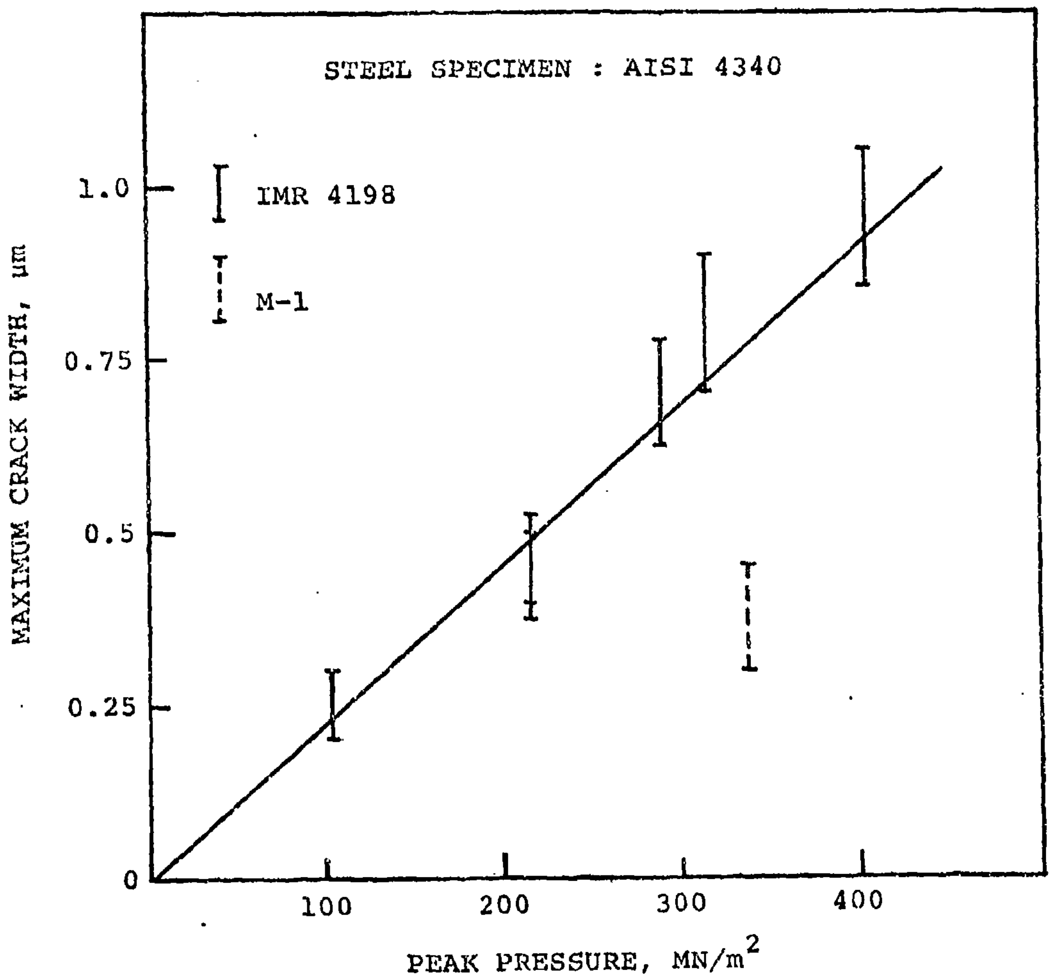
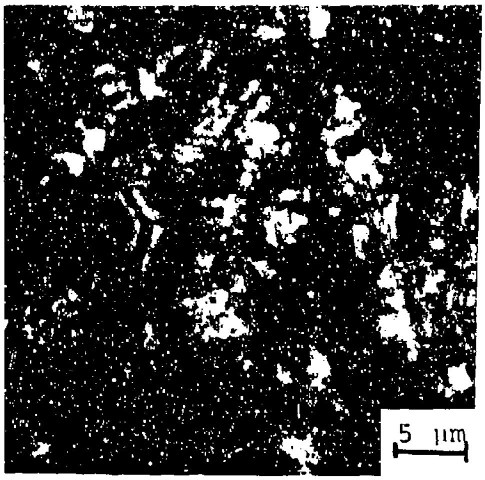


Fig. 7 Maximum crack width vs combustor peak pressure. The width of cracks increases with increasing peak pressure.



PROPELLANT: IMR 4198

T_v = 3000 K

P_{MAX} = 290 MN/m^2

Δm = 10.12 mg

5 Consecutive Tests

Fig. 8 SEM photograph of the eroded surface of AISI 4340 steel specimen showing that consecutive tests do not augment crack formation.

COMPOSITION CHANGES IN GUN STEEL SURFACES DUE TO EROSIVE PROPELLANT BURN

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I. INTRODUCTION

During the course of the erosion of a gun tube surface by the combustion products of burning propellants, significant physical, chemical and metallurgical alterations of the surface occur due to the interaction between the hot gases and the surface. Many gun barrels in use today are affected by such hot gas/metal interactions and consequently reach their erosion life long before their fatigue life resulting in their condemnation. This situation may become more severe as extended range artillery and/or high velocity systems come into use. One step toward a solution to the problem is a more definitive understanding of the processes and mechanisms that are involved in erosion and wear. Some recent experiments have indicated that the condition of the bore surface can significantly affect the erosivity of the system[1]. It would therefore be very interesting and informative to study the changes which occur in the composition of the eroded surface as a result of exposure to a variety of propellants as well as additives.

At BRL, we have developed an ion beam technique by which the outer micron layer of an eroded surface may be examined quantitatively for concentrations and depth profiles of carbon, nitrogen and oxygen. These elements are of greatest interest because all but a small percentage of a given propellant is made up of their compounds. As the propellant combustion products interact with the surface, one can expect various chemical reactions to take place. These will attack and erode the surface and are driven by high temperatures and pressures. Some of these products will remain in the surface so that a measure of the carbon, nitrogen and oxygen concentrations would yield information about the importance of chemical processes involved in the burn. In addition, any film layer or other substance containing carbon, nitrogen and oxygen which might be effective in retarding erosion would show up under such ion beam analysis.

At this point, the experimental procedure, some of the standard measurements, and results will be described. Following this, a short description of the ion beam technique will be given, the results of measurements on eroded samples will be shown and the results will be discussed.

[†]NRC Research Associate

II. EXPERIMENTAL PROCEDURE AND RESULTS

Twelve different nozzles of long-standing BRL design, made of 4140 gun steel, were exposed in a 37 mm gun modified to act as a blow-out chamber. Figure 1 shows a schematic of this chamber. Each of the twelve nozzles was fired with a different combination of the propellants (M2, M30 and M1) and the additives (TiO_2 Wax, Talc Wax, Polyurethane Foam and no additive). The propellant charge was chosen to give approximately the same pressure-time trace with each of the three propellants. With two exceptions, four shots were fired through each nozzle. The firing schedule is shown in Table 1.

Table 1. Firing Schedule.

Nozzle	Propellant (g)		Additive (g)		No. Shots	Isochoric Flame Temperature (°K)
2	M2	85	None		4	3320
3	M2	85	XM1	12.9±1.3	4	
4	M2	85	TW	18.6±0.9	4	
5	M2	85	PUF	11.4±0.3	4	
6	M30	110	None		4	3040
7	M30	110	XM1	12.5±0.9	4	
8	M30	110	TW	18.2±1.6	5	
9	M30	110	PUF	11.7±0.8	4	
10	M1	105	None		4	2400
11	M1	105	XM1	12.4±0.6	3	
12	M1	105	TW	17.2±1.4	4	
13	M1	105	PUF	11.7±1.0	4	

XM1 - TiO_2 Wax, TW - Talc Wax, PUF - Polyurethane Foam

With each shot a pressure-time trace was recorded, and after cleaning the nozzles with a detergent/alcohol wash, mass losses were measured. These results are shown in Table 2 where the average peak pressures, burn durations, and mass losses are given. The no additive mass losses follow the expected temperature dependence of higher mass loss for higher temperature. In all cases the use of additives reduced the mass loss. It should be noted that there were small differences in the burn durations of the three propellants.

The shot-to-shot variation in mass losses is somewhat larger than the measurement uncertainties would indicate. However, other experiments with this chamber and several other test fixtures[2,3], have shown similar shot to shot variations. Although the reason for this variation is not known, we speculate that random variations in the flame patterns are probably responsible.

Table 2. Results of Pressure and Mass Loss Measurements.

Nozzle	Propellant/ Additive	Peak Pressure (K psi)	Duration FWHM (msec)	Mass Loss (mg)
2	M2/NA	24.0±0.5	2.20±0.10	36.8±5.5
3	M2/XM1	26.1±1.6	2.08±0.16	8.5±1.1
4	M2/TW	25.9±1.9	2.06±0.16	6.9±3.2
5	M2/PUF	25.3±0.5	2.14±0.10	9.9±5.0
6	M30/NA	25.9±0.8	2.54±0.04	13.0±1.0
7	M30/XM1	27.0±0.7	2.48±0.04	5.9±3.6
8	M30/TW	26.4±2.5	2.68±0.24	4.6±1.9
9	M30/PUF	27.2±0.7	2.50±0.08	6.5±2.5
10	M1/NA	24.5±0.5	2.36±0.06	4.9±1.9
11	M1/XM1	26.0±2.2	2.26±0.14	4.0±1.1
12	M1/TW	26.1±2.1	2.28±0.12	3.9±1.2
13	M1/PUF	26.0±0.7	2.28±0.10	3.4±1.9

After the cleaning and mass loss measurements, the nozzles were examined with a 675 keV deuteron ion beam. As seen from the insert of Figure 2, a deuteron beam is brought onto a surface containing iron and oxygen. Nuclear interactions occur between some of the deuterons and the iron and oxygen nuclei producing backscattered deuterons and protons which are detected and sorted by their energies. The back-scattered deuterons from iron will show up in a different energy region than protons from oxygen. This can be seen in Figure 2 where; (1) a large yield of deuterons elastically backscattered from iron are shown, and (2) a smaller yield of protons produced from deuterons interacting with oxygen are shown. The height of the oxygen peak is determined by the total oxygen concentration and its width and shape are determined by the depth profile. If the surface examined had also contained nitrogen or carbon, additional peaks would have appeared near channels 260 and 450. A small step in the leading edge of the yield from iron indicates a lower concentration of iron atoms in the outer layers of the sample which are replaced by oxygen in this case.

Even though all three of the elements, carbon, nitrogen and oxygen are observable by ion beam methods, the analysis presented will be only for nitrogen and oxygen. A detailed description of this ion beam analysis method can be found in references 4 and 5.

Three different locations on each nozzle were examined. Concentrations and depth profiles were extracted by averaging the results from the three locations. Figure 3 shows concentration profiles for the nozzles fired with no additives in the propellants. The two peaks are due to oxygen and nitrogen as labeled and the curves are drawn to guide the eye through the data points. The relative uncertainty on data points is roughly their deviation from the lines. The main features of these data are: (1) there is always a surface peak of oxygen with some tailing into the deeper layers; (2) no surface peak is present for nitrogen; (3) the general trend is for thicker layers and higher concentrations with the cooler propellants; (4) and the nitrogen seems to go deeper into the surface with subsequent shots. This latter point may be of considerable significance since other work[6] has shown that nitrogen implanted into the outermost layers of steel makes that steel surface considerably harder. Thus, the nitrogen layers may help retard erosion in subsequent shots. Figure 4 shows similar results for nozzles fired with polyurethane foam additive included with the propellants. The main features in these spectra are similar to the previous ones.

Integration of these peaks gives the total surface concentrations of oxygen and nitrogen with the results being shown in Table 3. The trend toward higher oxygen and nitrogen concentrations with the lower flame temperatures is again apparent. In addition, the nozzles with additives show a somewhat higher oxygen and nitrogen concentration indicating a possible buildup of a protective layer. The results are similar with the TiO_2 Wax and Talc Wax additives but are not shown.

Figures 5 and 6 show the backscattered deuteron spectra from iron for the same sets of nozzles shown in Figures 3 and 4 respectively. The right side of the spectrum for nozzle 10 is lower than the equivalent region for nozzles 2 and 3. This occurs because the iron concentration is lower at the surface of nozzle 10 than of nozzles 2 and 3. The reduction in iron concentration is due to the higher oxygen and nitrogen concentrations in the surface of nozzle 10 as can be seen from Table 3. Thus, the shape of the right side of the backscattered spectra is a sensitive indicator of surface contamination. Calculations utilizing the shape of the backscattered spectra to determine information about the stoichiometry of the iron-oxygen-nitrogen layer are currently under way.

Table 3. Surface Concentrations.

Nozzle	Shot	Oxygen ($x10^{17}$ at/cm^2)	Nitrogen ($x10^{17}$ at/cm^2)*
2	3	0.79 ± 0.11	0.06
2	4	0.89 ± 0.12	0.06
5	3	1.43 ± 0.17	0.20
5	4	1.20 ± 0.15	0.14
6	3	1.02 ± 0.13	0.22
6	4	0.93 ± 0.12	0.25
9	3	1.08 ± 0.14	0.27
9	4	2.25 ± 0.23	0.39
10	3	2.52 ± 0.24	0.54
10	4	2.40 ± 0.24	0.64
13	3	3.43 ± 0.27	0.49
13	4	4.28 ± 0.26	0.85

* No error bars are assigned to the nitrogen concentrations because the $^{14}N(d,p)$ cross sections used involved large extrapolations from existing data.

III. DISCUSSION

The results of this experiment show that large differences do exist between the oxygen and nitrogen surface residues from the burning of various propellant/additive combinations. Penetration depths vary from basically nil (less than 0.1 μm) to between 0.5 and 1.0 μm. A detailed experiment is thus feasible in which the effects of the erosion inhibiting TiO_2 Wax additive can be studied. Any buildup of a TiO_2 layer on the surface would be observable and a concurrent measurement of the amount of surface wear loss would be indicative of the effectiveness of such a layer at erosion reduction.

Nitrogen, implanted in steel surfaces, has been known to inhibit wear from such surfaces as well as significantly change a number of other surface parameters. A nozzle was prepared by implanting a small region with nitrogen ions at 40 keV to a dose of 3×10^{17} atoms/cm^2. The depth of the nitrogen layer as a result of this implantation was less than 500 Å. An ion beam analysis of this nozzle area showed the appropriate amount of nitrogen at about the right depth. The surface composition thus altered, the nozzle was exposed to 85g of M2 propellant in the 37 mm chamber. The results showed that the erosivity from this implanted area was no different than from a non-implanted area and that the whole 500 Å nitrogen layer had been removed with the single shot. Another sample is currently being implanted to a much greater depth with nitrogen with the expectation that better erosion resistance will be found with it.

The results of these experiments also indicate the presence of significant chemical interactions between the propellant gases and the steel surface. Except for very small surface concentrations of oxygen caused by water vapor absorption, only chemical reactions can introduce oxygen or nitrogen into these surfaces. Preliminary analysis of scanning electron micrographs of some of these nozzle surfaces show no evidence of the streaming or flow patterns which would suggest a melt and wipeoff process. Alkidas et al.[7] have observed such flow patterns in their ballistic compressor experiments with hydrogen gas. However, a definitive answer to the question of how surface layers are softened before being blown (or eroded) away, whether by chemical action or by surface melting, requires additional work. The answer to this question is very important for it determines whether materials research or propellant formulation work is most likely to lead to greater erosion reduction.

ACKNOWLEDGEMENTS

The authors wish to acknowledge the following for their various contributions to this work; Dr. S.E. Caldwell for help in the data acquisition, Mr. T. Brousseau for considerable help during the nozzle firing, and Dr. J. Hirvonen of Naval Research Laboratories for doing the nitrogen implantation.

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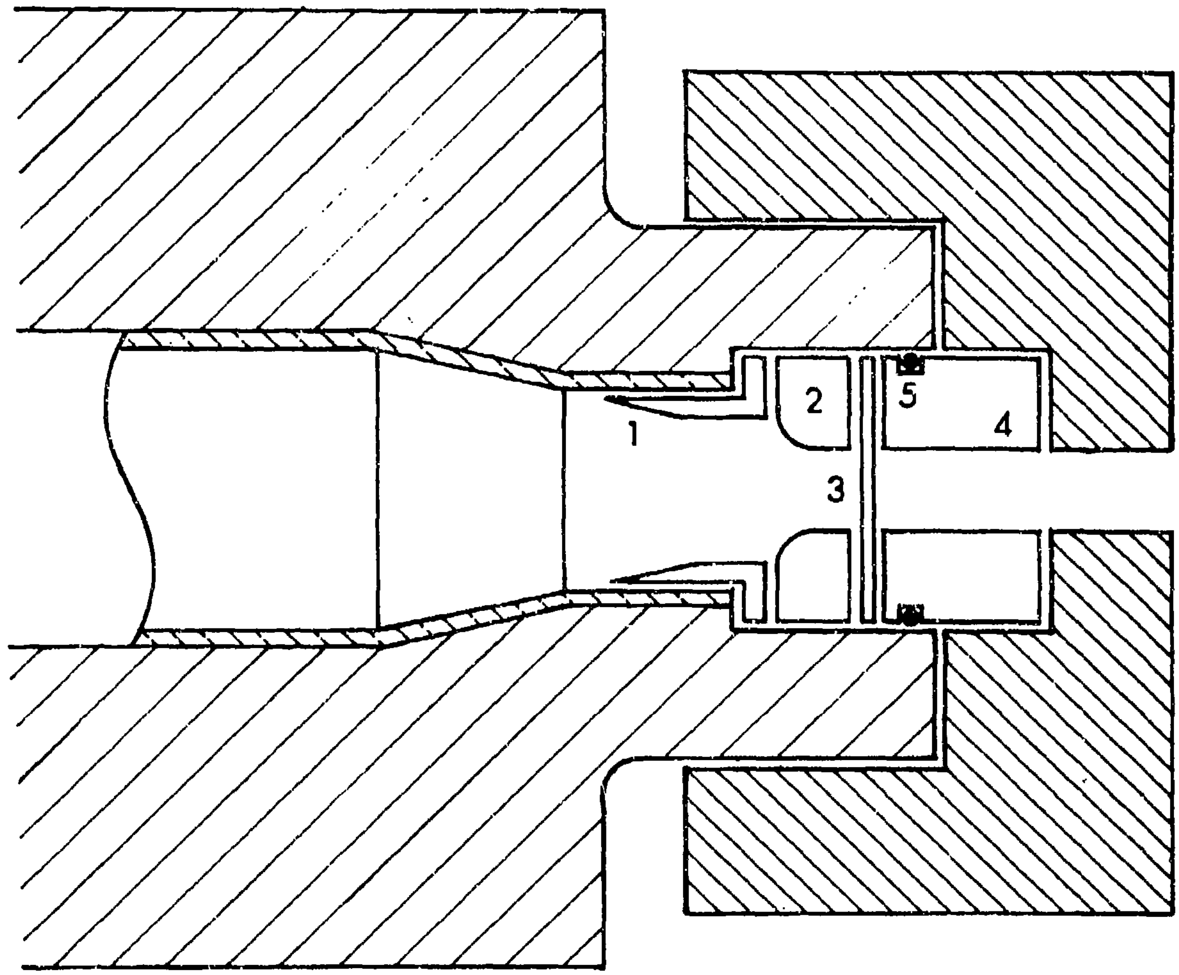

1. RETAINING RING
2. NOZZLE
3. BLOWOUT DISC
4. SPACER
5. O-RING SEAL

Figure 1. Schematic of the 37 mm blow-out chamber.

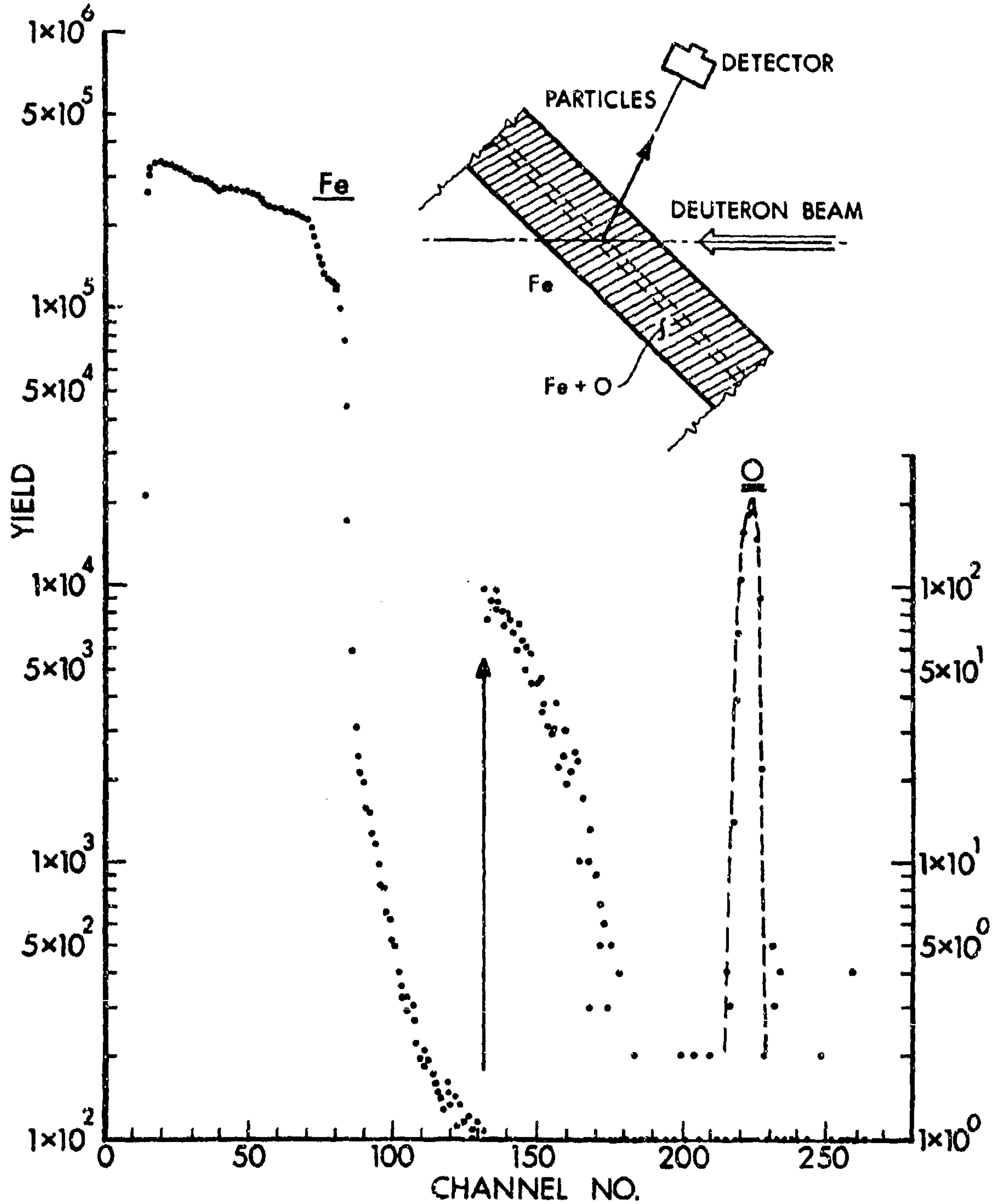

Figure 2. A deuteron beam incident on an iron/oxygen surface produces peaks due to
III-441 Fe and O as labeled. The oxygen concentration is determined from the O peak height and its depth of penetration from the peak width.

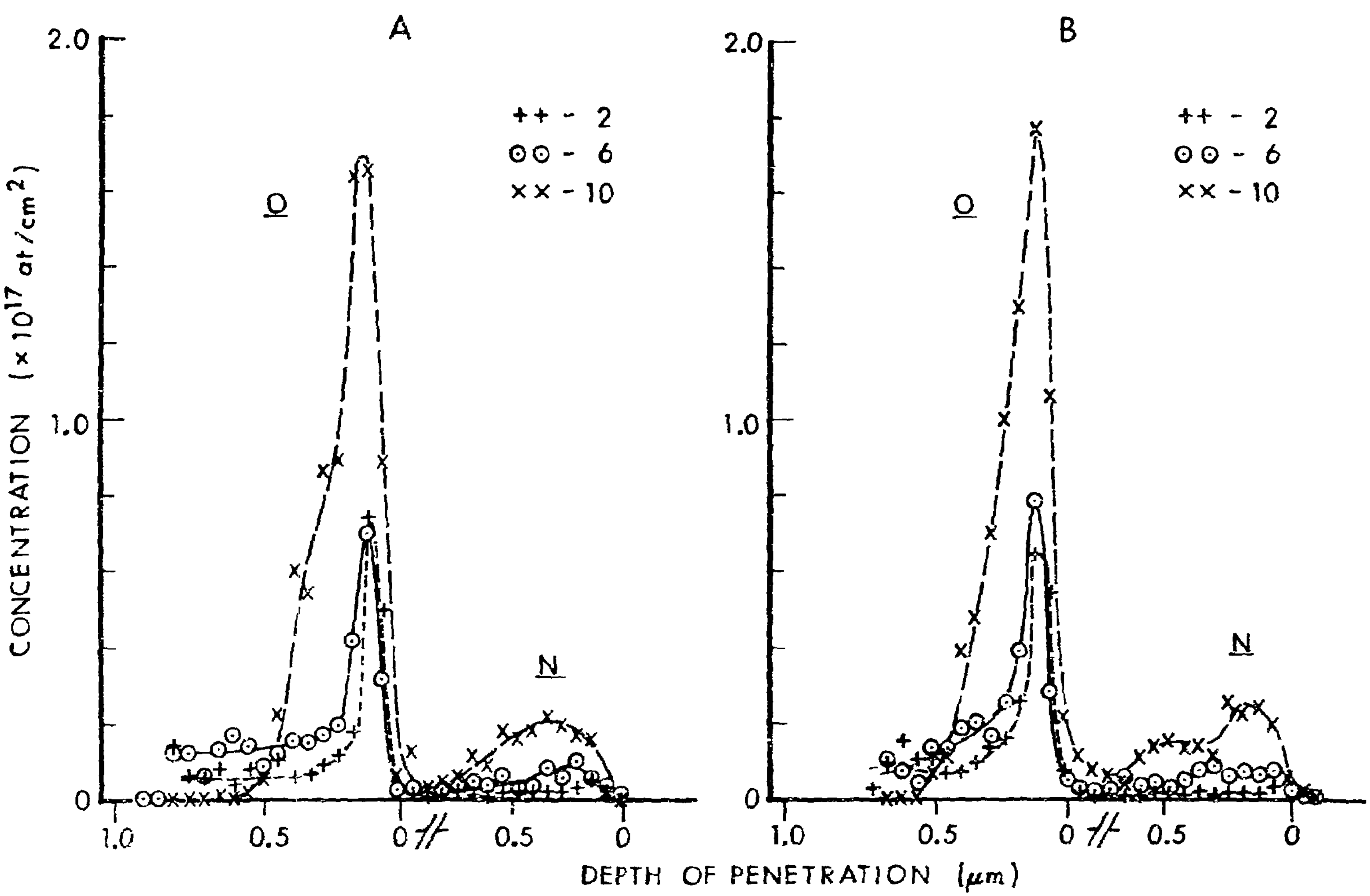

Figure 3. Concentration profiles of oxygen and nitrogen for nozzles fired with no propellant additives. The horizontal scales are in two parts, one for oxygen and one for nitrogen. (A) After three shots. (B) After four shots.

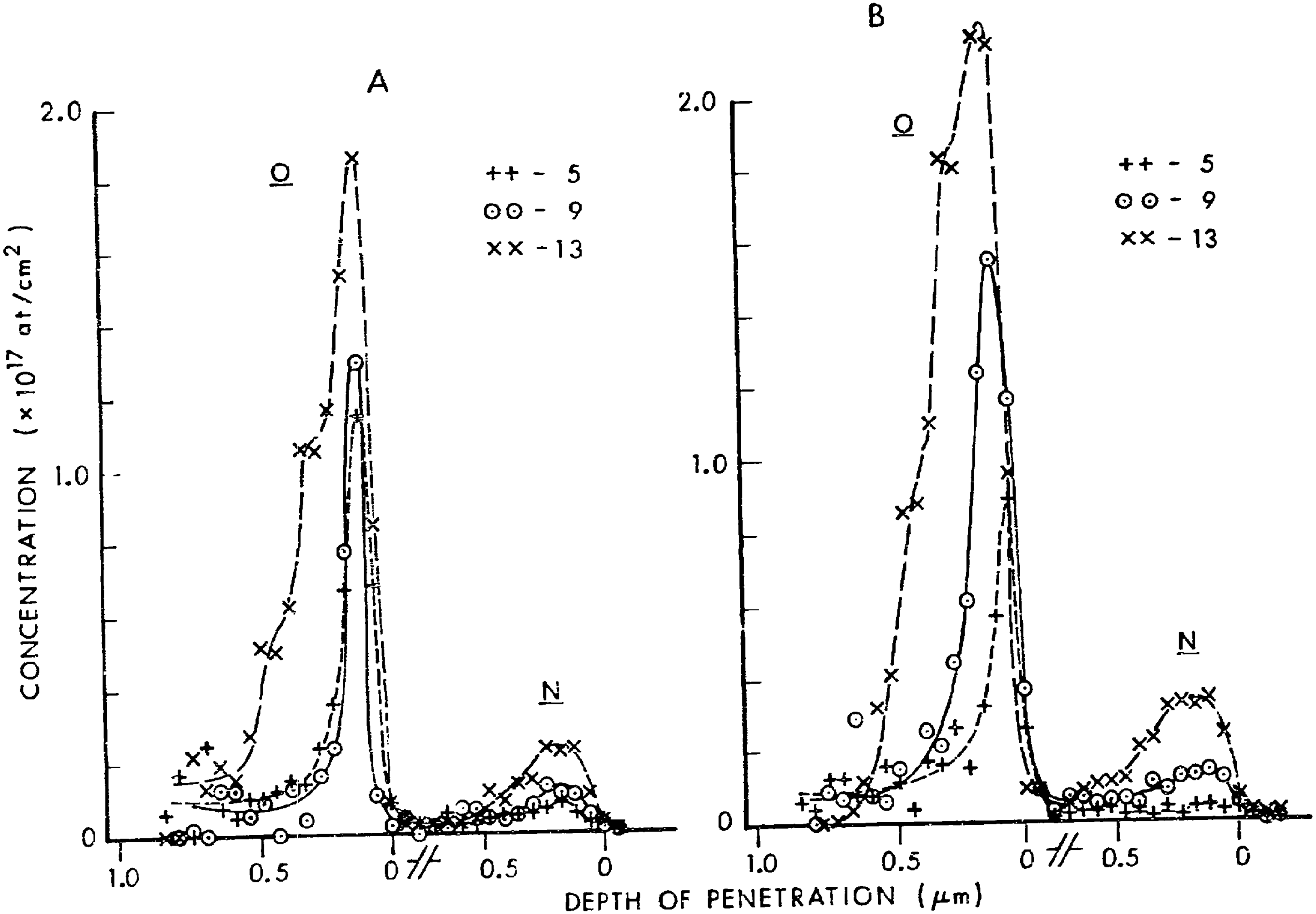

Figure 4. Concentration profiles of oxygen and nitrogen for nozzles fired with polyurethane foam additive. The horizontal scales are in two parts, one for oxygen and one for nitrogen. (A) After three shots. (B) After four shots.
III-443

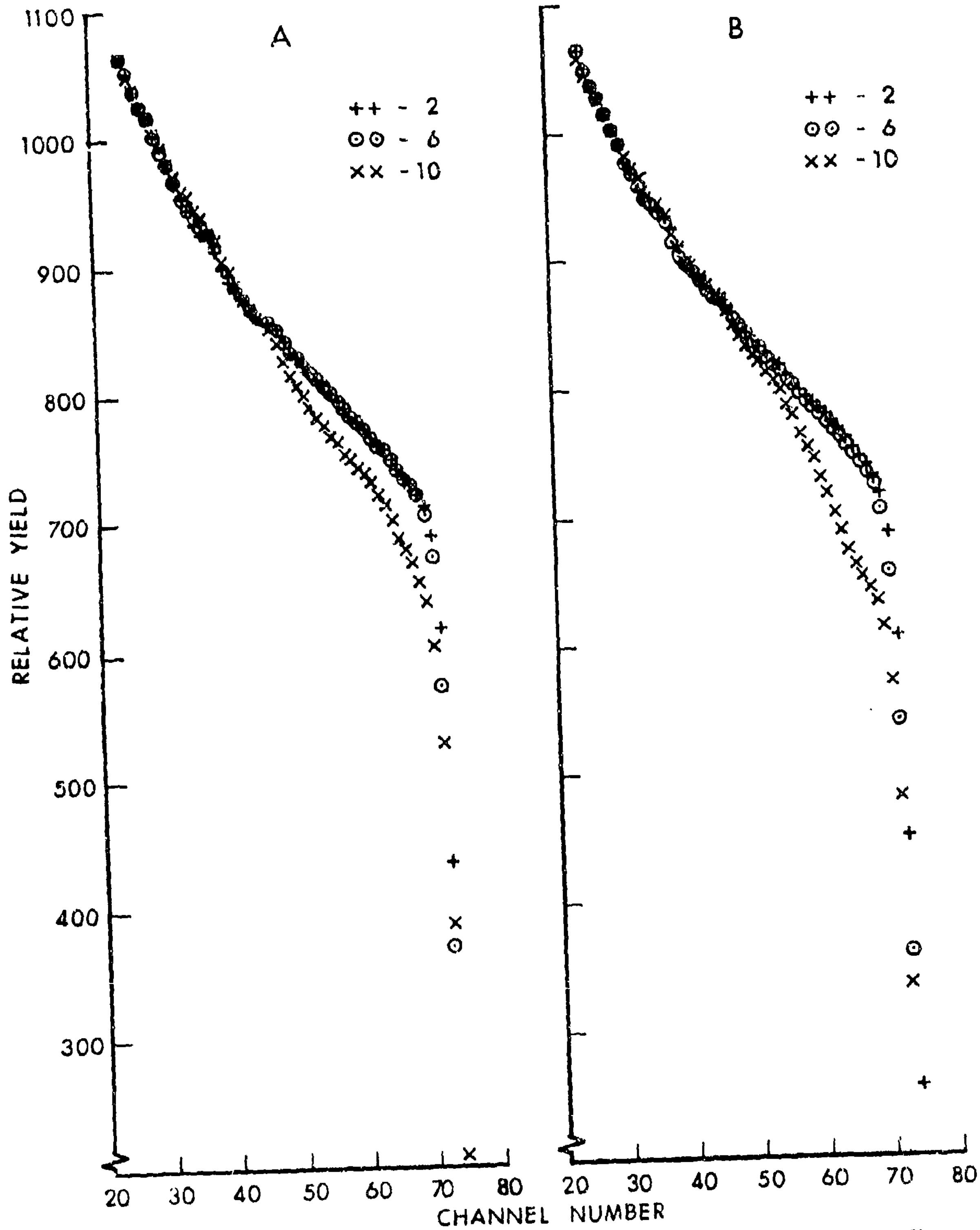

Figure 5. Elastically backscattered deuterons from the nozzle set shown in figure 3. The nozzle 10 spectra show the effect of decreased iron concentration at the surface due to displacement by oxygen and nitrogen. (A) After three shots. (B) After four shots.

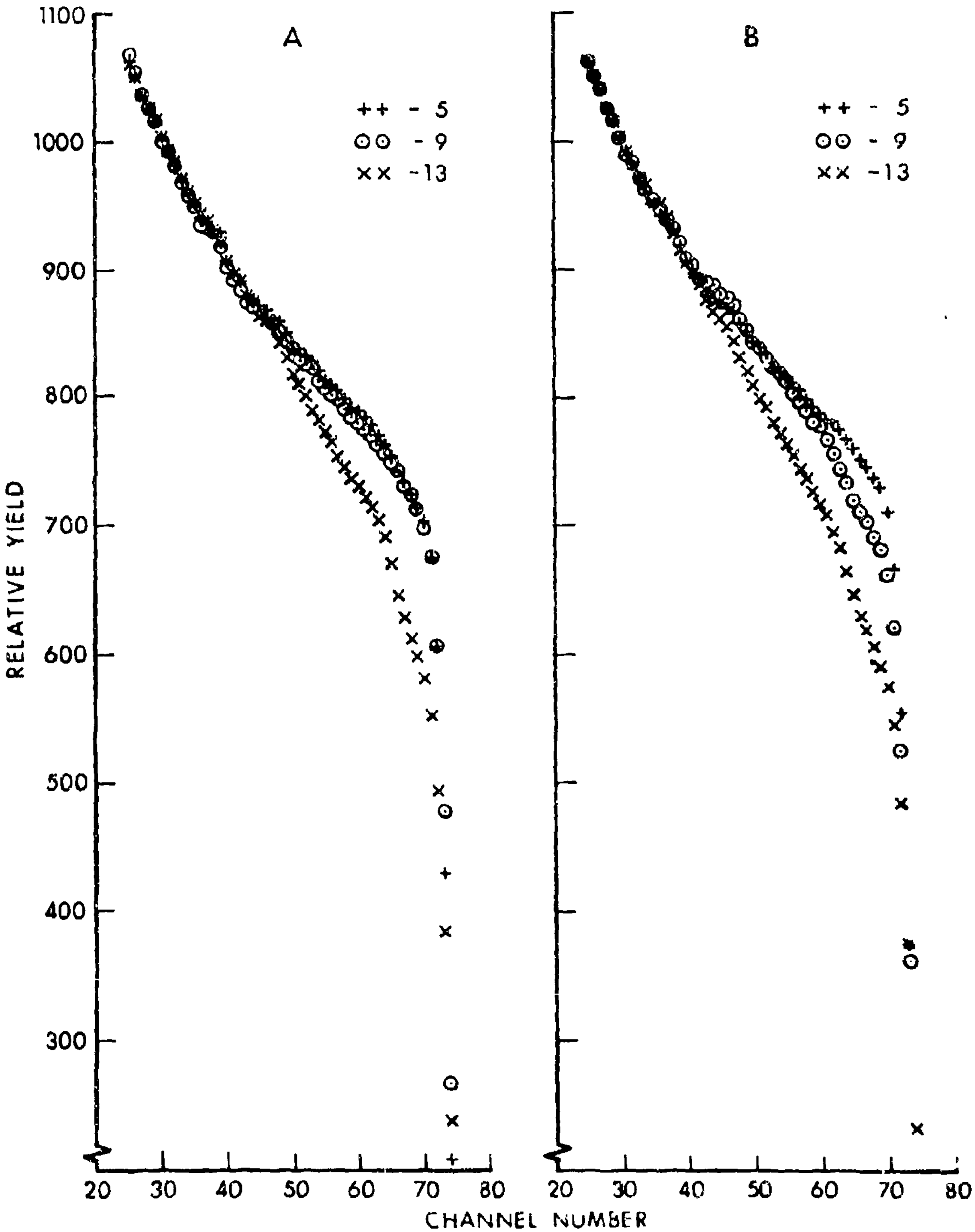

Figure 6. Elastically scattered deuterons from the nozzle set shown in figure 4. The nozzle spectra show the effect of decreased iron concentration at the surface due to displacement by oxygen and nitrogen. (A) After three shots. (B) After four shots.

FRICTION AND WEAR AT THE PROJECTILE-TUBE INTERFACE

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In this paper I will try to summarize the evidence which indicates that the usual mechanism of sliding for a projectile down a gun tube is melt lubrication with the sliding surfaces being separated by a thin layer of molten rotating band material. This is important in understanding the behavior of projectiles because then the sliding is lubricated and friction and wear of the rotating bands is determined only by the characteristics of the molten film and the heat generated at the sliding interface. Much of it is a summary of data published in refs. 1-4 and so the reader is referred to these papers for experimental details.

When the coefficients of friction for projectiles with gilding metal rotating bands fired at low zones in the 155mm howitzer are plotted as functions of the product of bearing pressure and velocity, it can be seen that the initial friction is very high but that it rapidly drops to about 0.1 by the time the bands are completely engraved (see Figure 1). The rate of heat generated at the sliding interface is proportional to this product with constant coefficients of friction. The steady state coefficient of friction, which appears to be about 0.02, is reached after only about 5 in of travel. This means that the mechanism of sliding changes drastically after only a few inches of sliding. It is plausible that this drastic change is the melting of the rotating band surface and the establishment of melt lubrication. Melt lubrication is the only plausible explanation for a coefficient of friction as low as 0.02.

An extensive laboratory study was supported by the U.S. Army from about 1946 to 1956 and a great deal of data was collected at sliding speeds up to 1800 ft s^{-1} using a sophisticated high speed pin-on-disk test device. This device is shown in Figure 2. Wear of pins of gilding metal, copper, and projectile steel and the friction of these metals and annealed iron on gun steel were extensively investigated. In addition, there were a few experiments with pins of other materials. The data for copper sliding on gun steel is typical of the results which were obtained. At low pressure-velocity values both friction and wear rates were very high and were not functions of this product. At the higher sliding velocities and bearing pressures, the coefficient of friction decreased to a more or less constant value and the wear rate became a smooth function of PV (see Figures 3 and 4). This behavior is clarified by plotting friction and wear rate as functions of the rate of heat generated at the sliding interface. It is apparent that both friction and wear rate are not functions of this rate of heat generation at low bearing pressures and sliding velocities. However, at the higher rates of heat generation, friction is almost constant and wear rate is proportional to the square of the rate of heat generation (see Figure 5). This indicates melting of the rotating band surface and establishment of melt lubrication. With melt lubrication, friction is determined by the thickness and viscosity of the molten film and wear rate determined only by the heat generated at the sliding interface.

With melt lubrication, when the same amount of heat is generated at the sliding interface, the wear rate should be a function of the amount of melting. If the linear wear rates of the different materials at the same rate of heat generation are plotted as functions of the reciprocal of their absolute melting points, a straight line results with the lower melting materials wearing considerably faster (see Figure 6). The two materials which fall conspicuously below the line are copper and aluminum, the materials with the highest thermal conductivities. Evidently, a high thermal conductivity results in the material acting as if it had a higher melting point than it actually has owing to the rapid conduction of heat away from its surface.

An apparent [illegible]ainaly in the laboratory work is that the actual values of the [illegible]cient of friction are many times those actually observed in [illegible]tube. While the environment in a gun tube is considerably [illegible]rerent from the laboratory conditions, the exact reasons for the disparity were not clear until the recent publication of a theoretical study on lubrication by a melting solid (5). From this analysis, the coefficient of friction is proportional to the square root of the non-dimensional sliding speed, S. This speed is defined as

$$S \equiv \mu U / x_2 L \qquad (1)$$

where μ is the liquid viscosity, U the sliding velocity, x_2 the length of the slider, and L the volumetric heat. Therefore, the coefficient of friction is inversely proportional to the square root [illegible] the length of the slider for any particular sliding velocity. This could account for a portion of the disparity in the coefficients of friction.

In the study it was also shown that, in the case of a melting slider, the system is not self-sustaining until very high values of non-dimensional bearing pressure. Specifically, the system does not develop its own lubricant supply for complete liquid film lubrication until the non-dimensional bearing pressure, w^*, is greater than about 0.2. w^* is defined as

$$w^* \equiv w/Lx_2 \qquad (2)$$

where w is the load per unit width. Below this value, the leading edge of the slider is not lubricated by the liquid metal and consequently the coefficient of friction is much greater. The value of w^* for the gilding metal bands on the 155mm M107 projectile is about 0.20 during engraving for an engraving pressure of 50,000 lb in^{-2}. It is about 0.19 for the copper bands on the 155mm M483 projectile during engraving at the same pressure. Therefore, in this case, lubrication by surface melting of the rotating bands would be self-sustaining or close to it. However, the maximum w^* for copper or gilding metal in the laboratory experiments was only about 0.07 owing to the small diameter of the pins used. The high coefficients of friction measured in the laboratory pin-on-disk experiments could easily be accounted for the small slider size and low bearing pressures.

After a short distance of sliding, the coefficient of friction for an actual projectile becomes low but not as low as predicted by the analysis (see Figure 7). This may be the result of the complex geometries and bearing pressure patterns of rotating bands. Also the analysis did not take into account such things as the heat conducted from the sliding interface or the heat required to raise the rotating band material to its melting point.

In the usual case, friction and wear remains low the entire length of the gun tube after the molten surface has formed. Where there is muzzle wear, however, melt lubrication is not completely maintained until the projectile leaves the tube. There is always side loading of the projectile on the wall of a gun tube because the projectile travels down the front portion of the tube with the largest yaw possible. If this loading becomes excessive or if the design of the rotating bands is not adequate, the rotating bands will wear sufficiently to allow the projectile body to contact the wall of the gun tube and result in body engraving on the projectile and muzzle wear on the gun tube. Figure 8 shows body engraving on a 155mm projectile in flight and Figure 9 shows an example of muzzle wear in a developmental 155mm howitzer tube. While muzzle wear is not as great as the more usual erosion, it can have a very adverse effect on dispersion. This kind of wear is confined to the lands of the rifling, and in extreme cases, the lands can be worn almost flat and the grooves still show no appreciable wear (see Figure 10).

There is also metallographic evidence to support the idea of melt lubrication of projectiles sliding down gun tubes. Recovered soft iron bands show a thin surface layer of material which evidently had been molten (see Figure 11). This surface layer is more striking on the slide marks on a recovered body-engraved projectile (see Figure 12). Furthermore, when the surface cracks on the bores of cannon firing gilding metal banded projectiles are examined, they are found to be filled with gilding metal which had evidently entered them as molten metal (see Figure 13). Even body engraved magnesium sabots from recovered APDS tank projectiles show a surface layer which indicates a prior melting (see Figure 14). All this evidence corroborates field observations such as that molten gilding metal has been observed driping from the muzzles of cannon during rapid fire and that difficulty was experienced with molten iron being forced into a purge hole located on a land just 1/4 in forward of the origin-of-rifling in the case of the 152mm gun firing sintered iron banded projectiles.

All the available evidence indicates that projectiles are lubricated by melt lubrication throughout their travel except for a few inches at the origin-of-rifling. While this few inches is extremely important and greatly affects the internal and external ballistics of the projectile, the fact remains that, by far, the greatest part of the sliding is lubricated sliding. With melt lubrication, the coefficient of friction is determined only by the character of the film and not by the properties of the sliding surfaces themselves and the wear of the rotating bands is dependent only on the band material and the amount of heat transferred to them.

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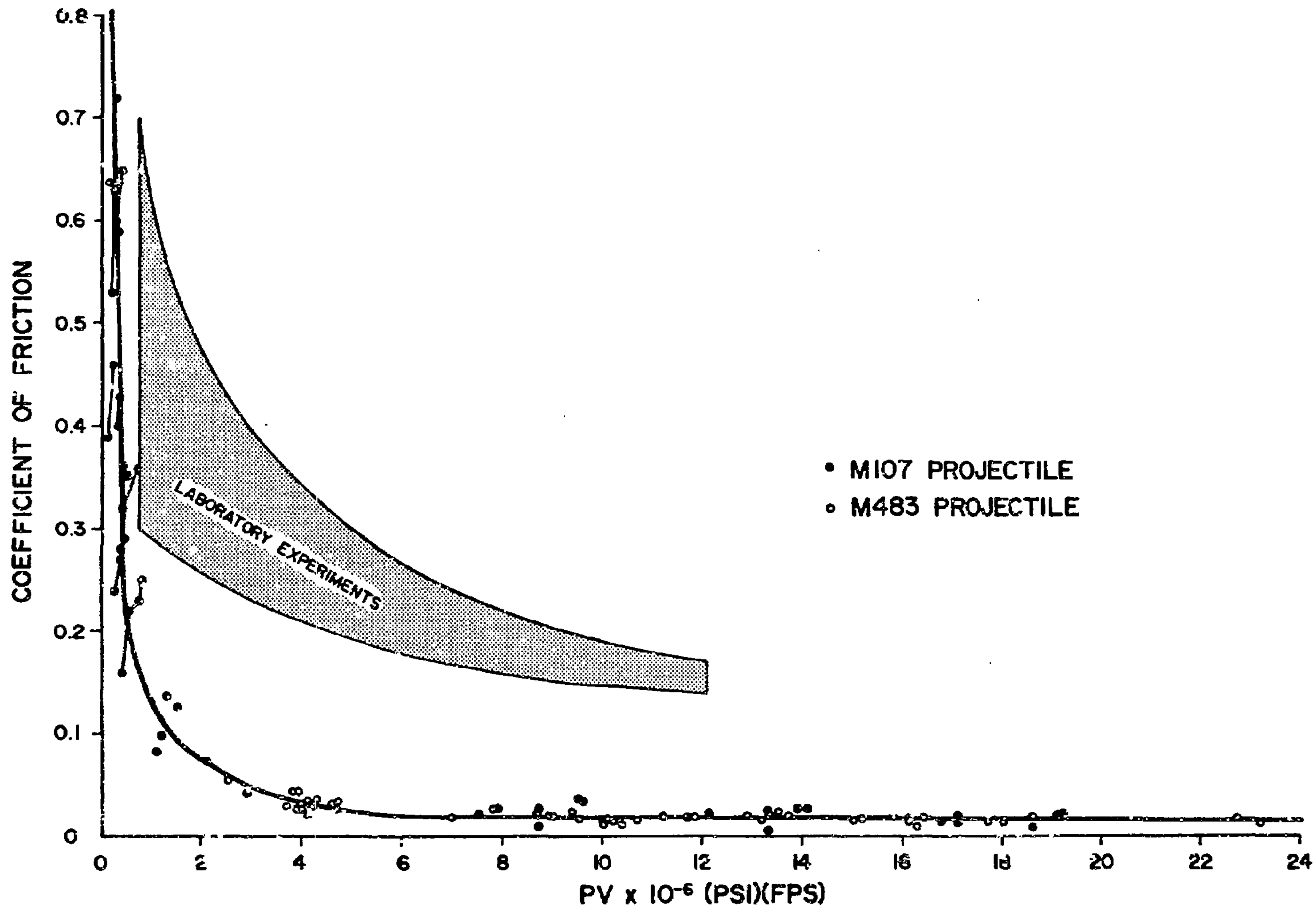

Fig. 1 Coefficient of friction as a function of the product of bearing pressure and sliding velocity for a projectile with gilding metal rotating bands.

SCHEMATIC ASSEMBLY OF FRICTION MACHINE

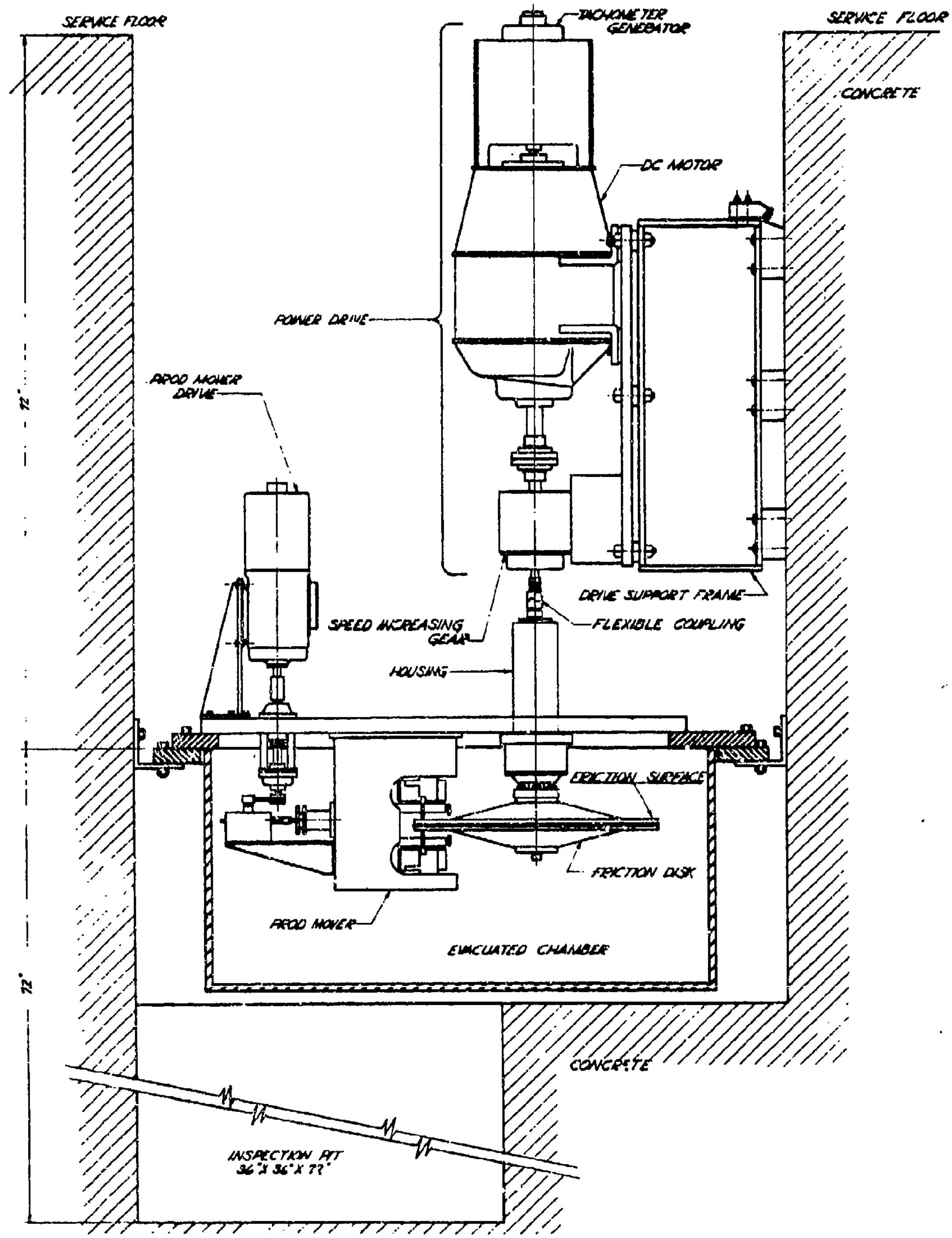

Fig. 2 Schematic assembly of the friction and wear machine used in the laboratory experiments.

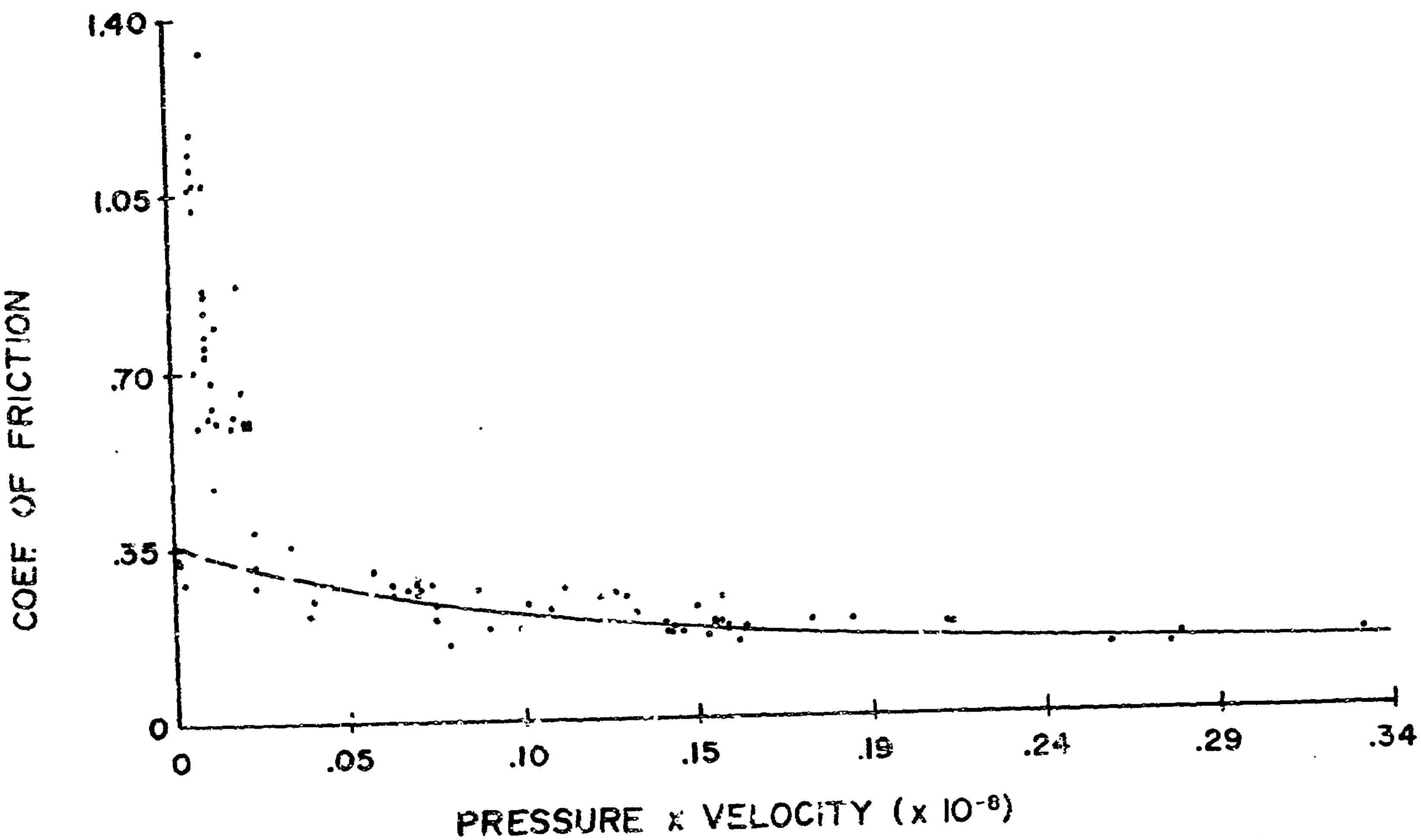

Fig. 3 Coefficient of friction of copper as a function of pressure x velocity (lb in^{-2} ft s^{-1}).

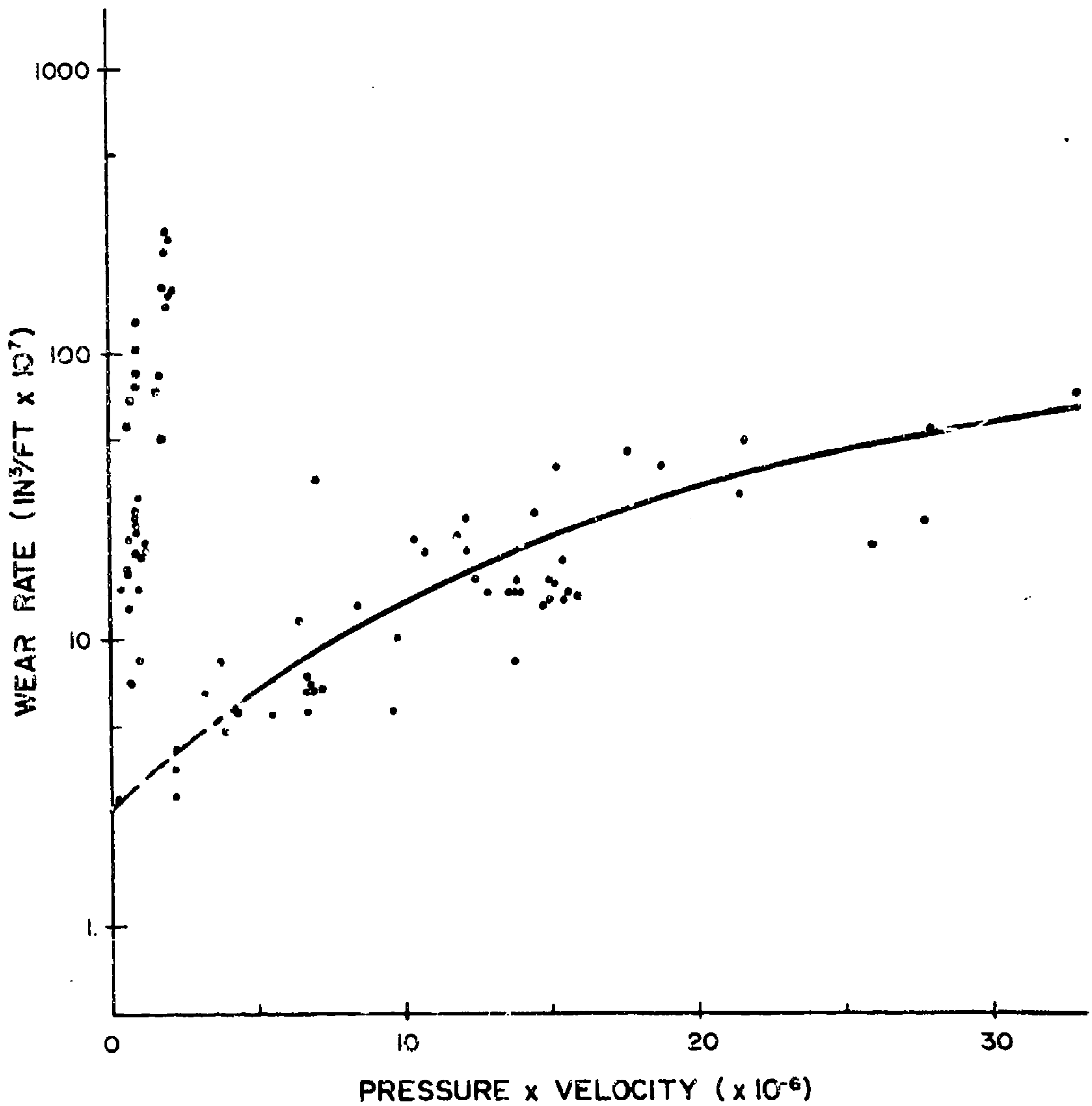

Fig. 4 Wear rate of copper as a function of pressure x velocity (lb in^{-2} ft s^{-1}).

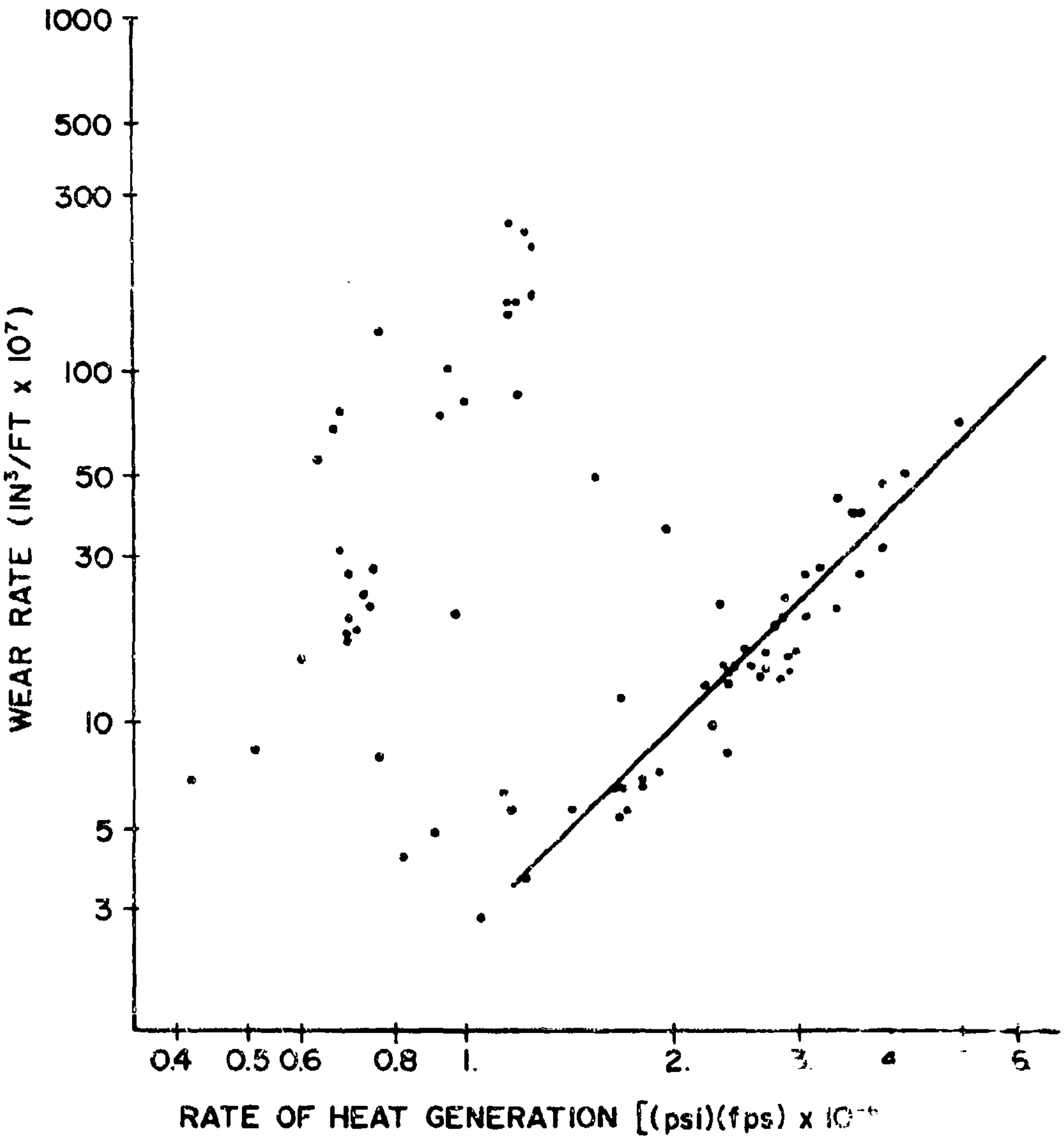

Fig. 5 Wear rate of copper as a function of rate of heat [illegible]

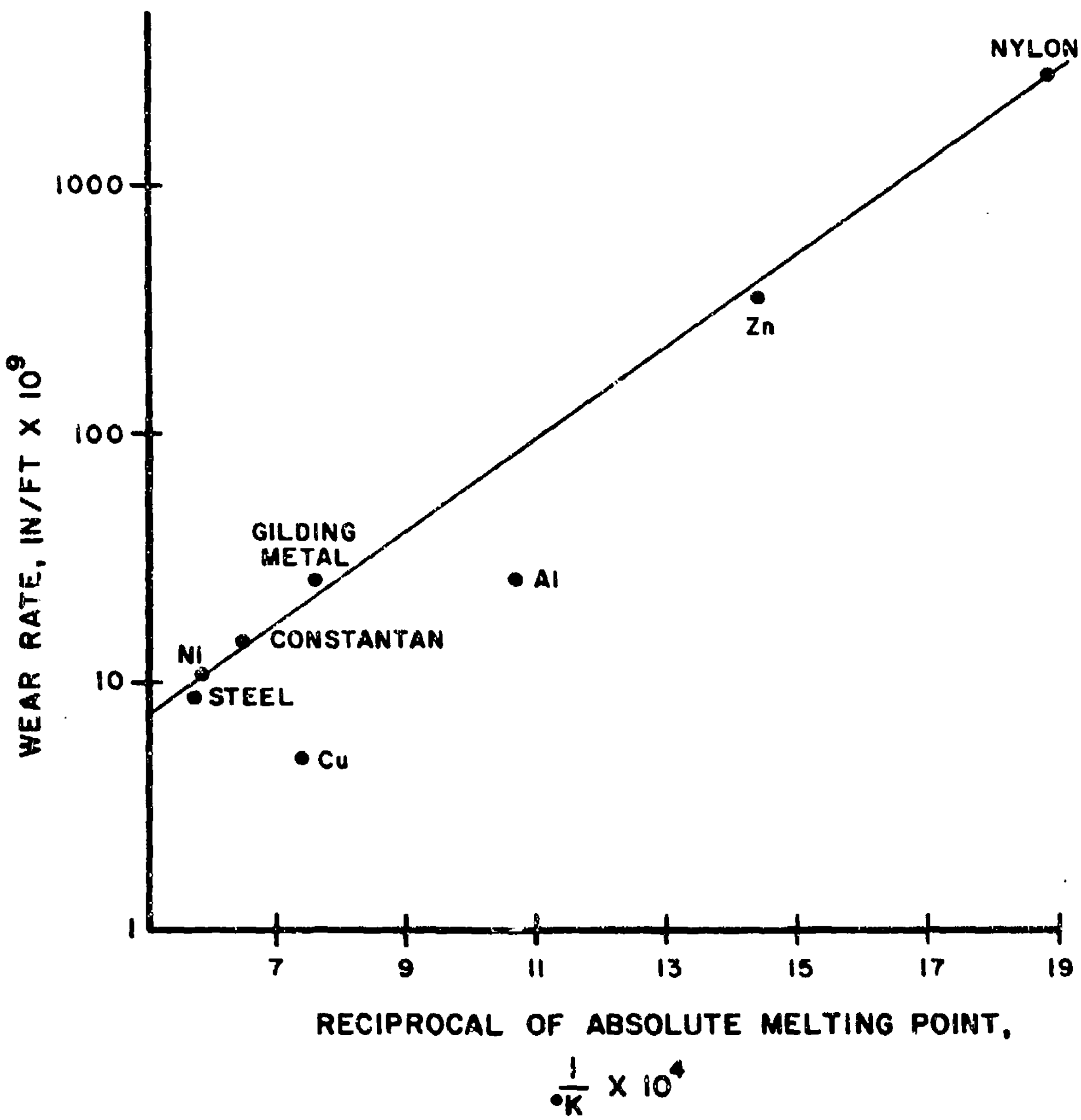

Fig. 6 Wear rates of different materials at a particular rate of heat generation as functions of the reciprocals of their absolute melting points.

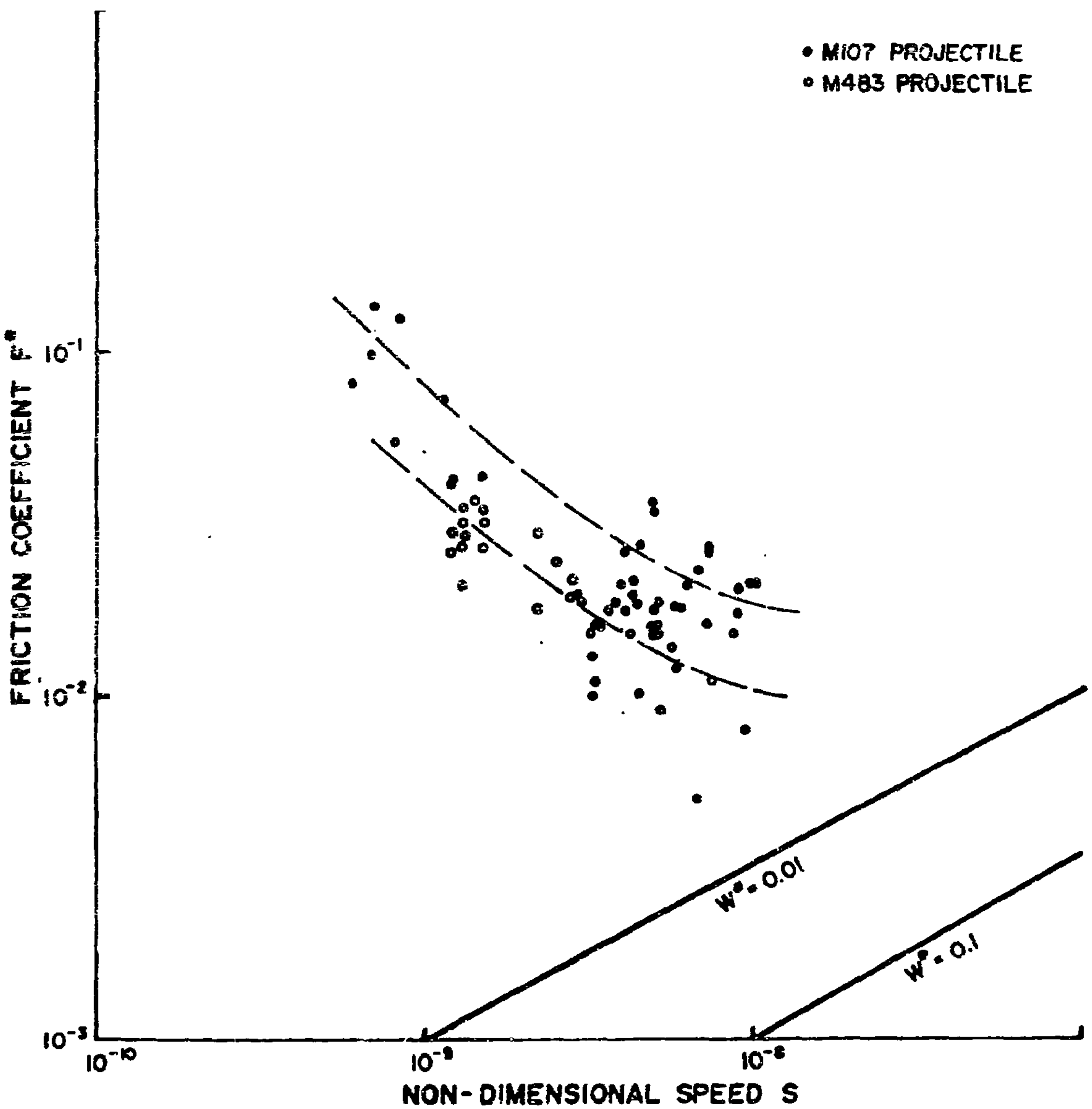

Fig. 7 155mm M185 howitzer friction data as a function of non-dimensional speed.

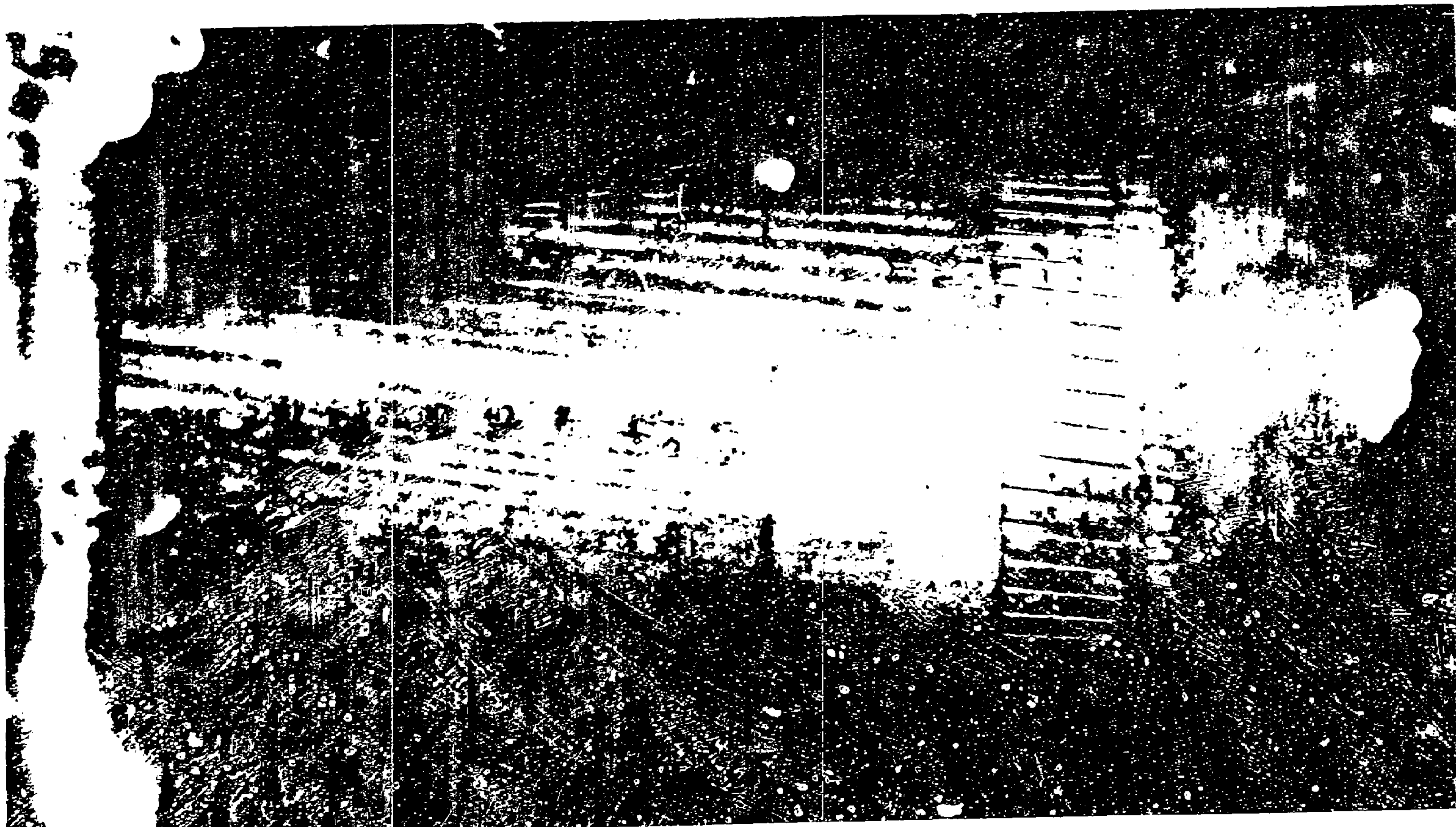

Fig. 8 Photograph of the rear of a projectile in flight showing body engraving.

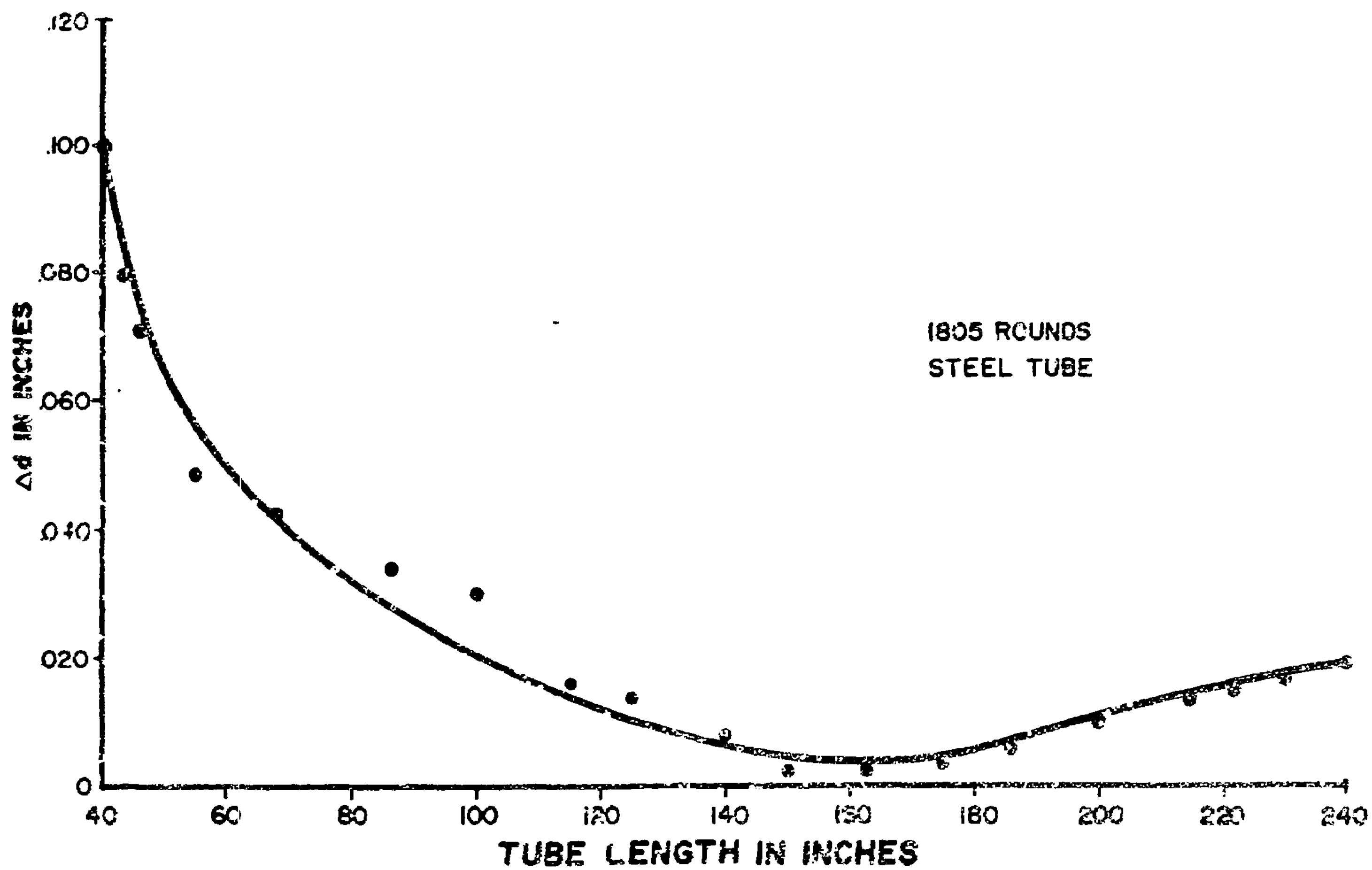

Fig. 9 Change in bore diameter along the length of the tube for a developmental 155mm howitzer after 1805 rounds.

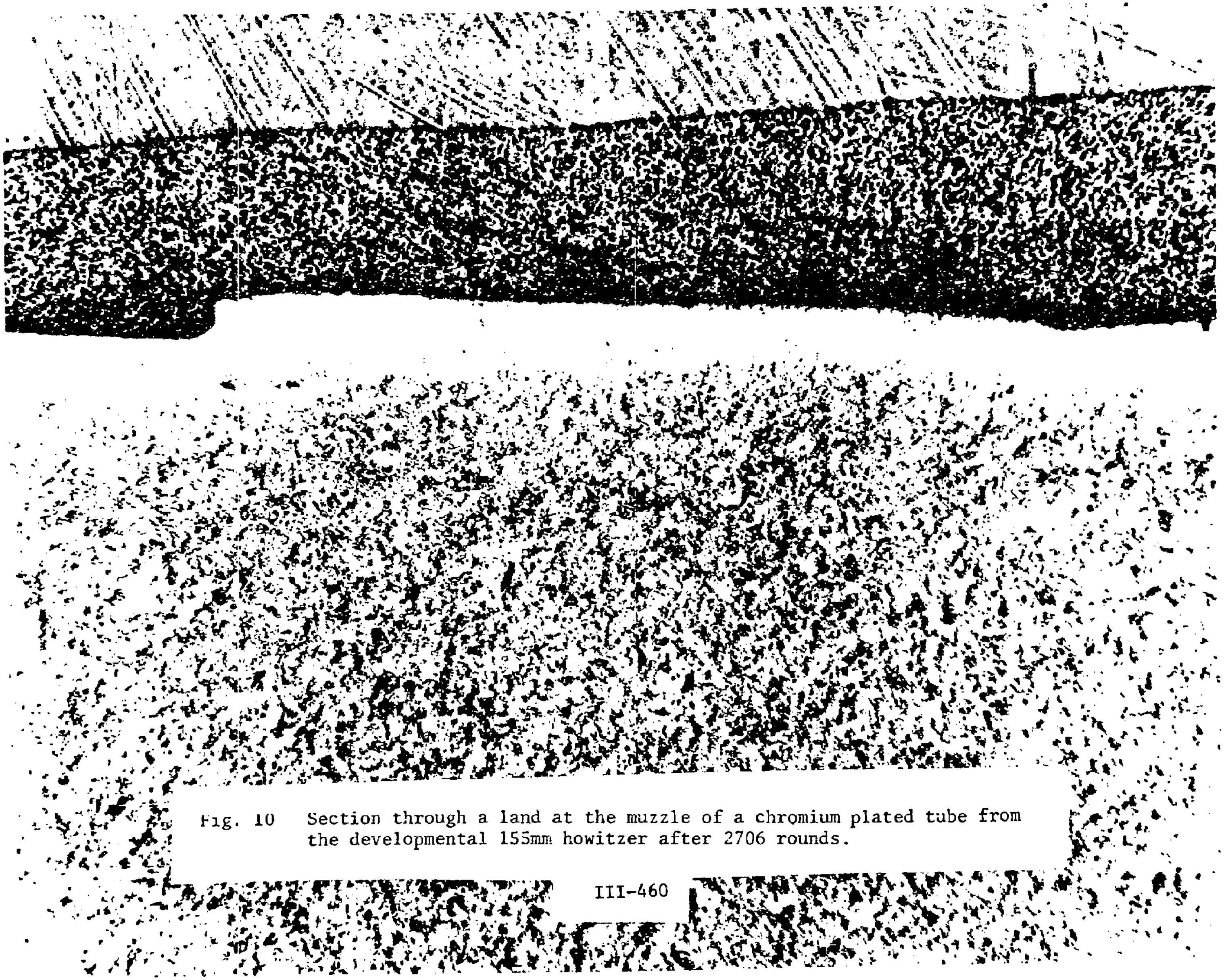

Fig. 10 Section through a land at the muzzle of a chromium plated tube from the developmental 155mm howitzer after 2706 rounds.

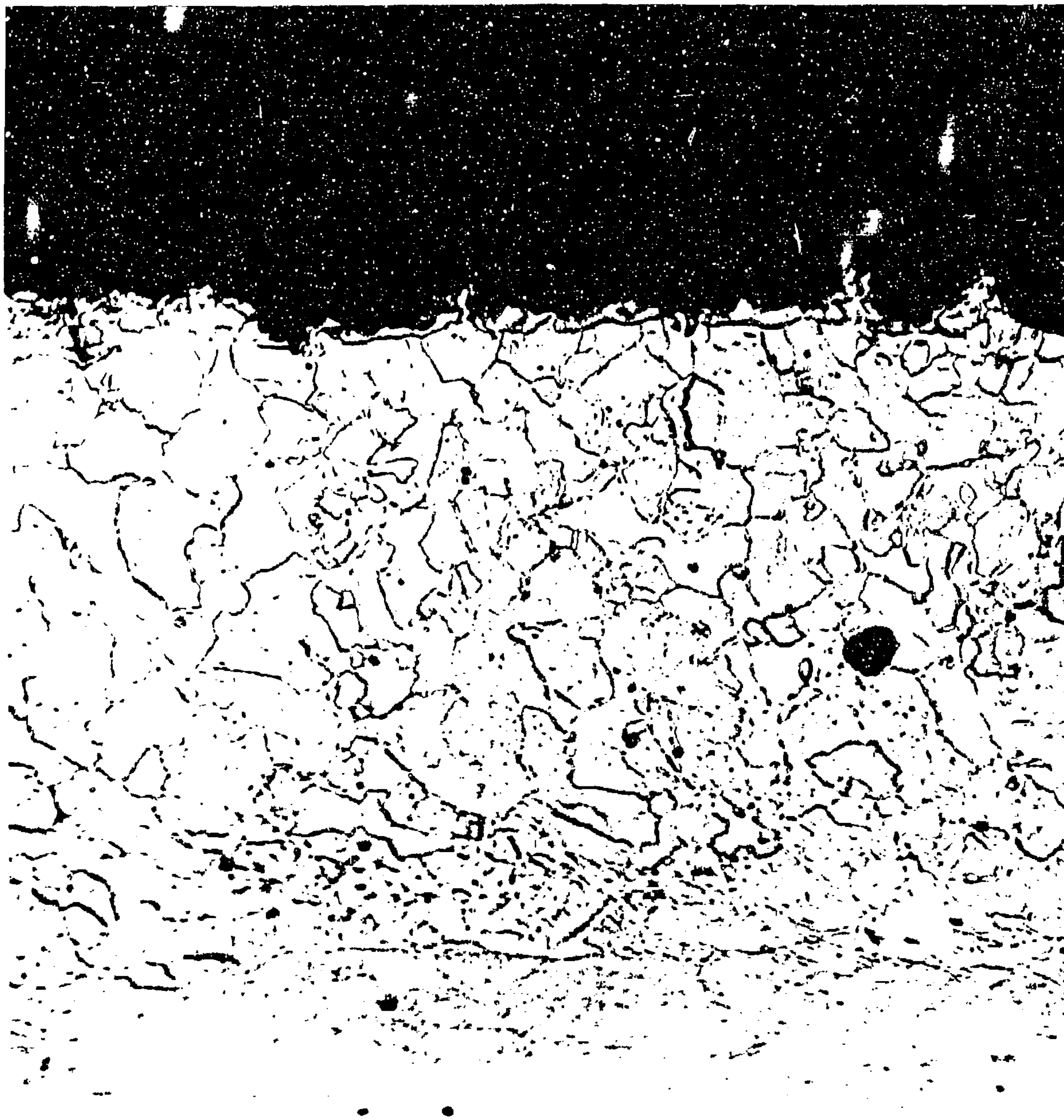

Fig. 11 Section through a soft iron rotating band from a recovered 105mm projectile. (1000x)

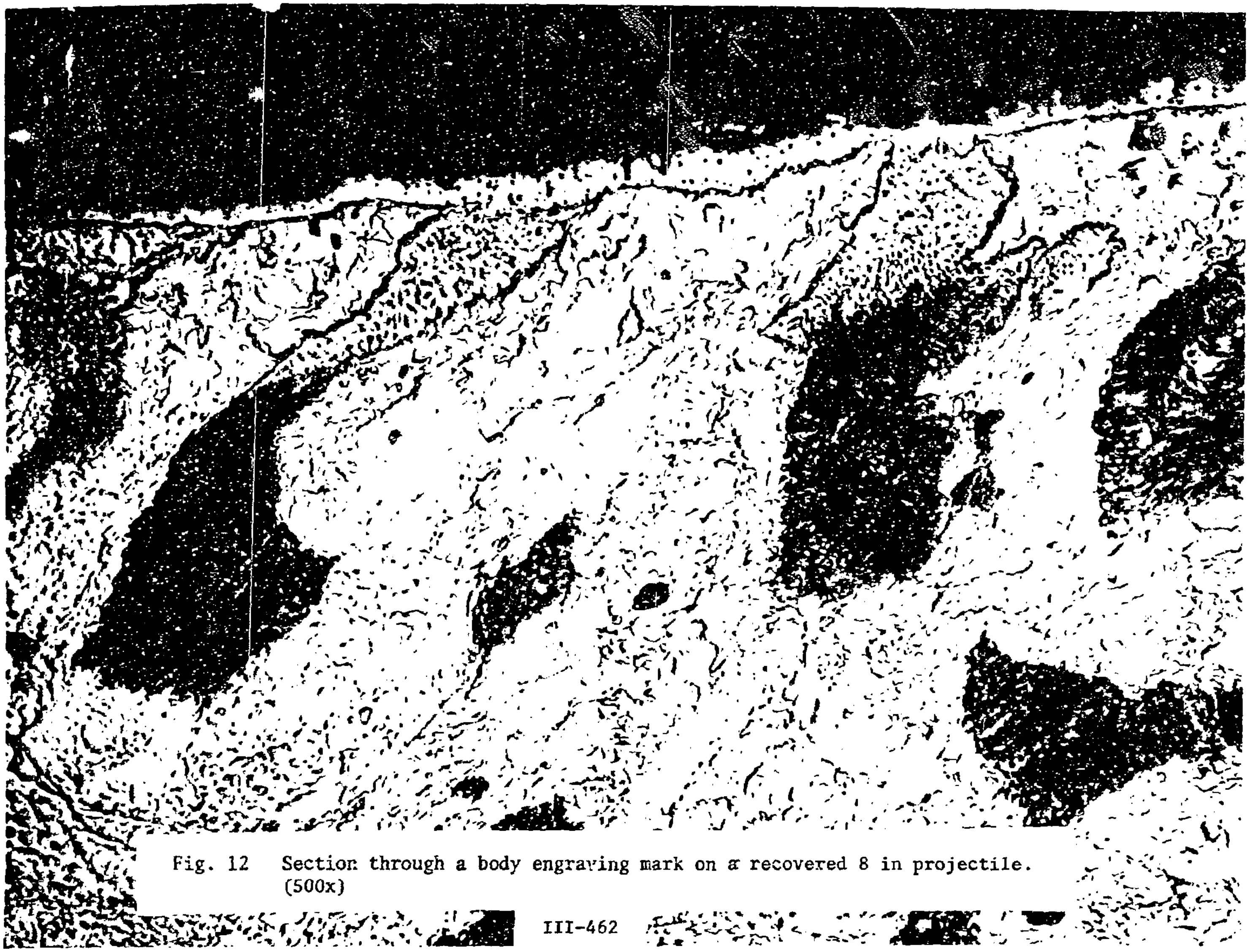

Fig. 12 Section through a body engraving mark on a recovered 8 in projectile. (500x)

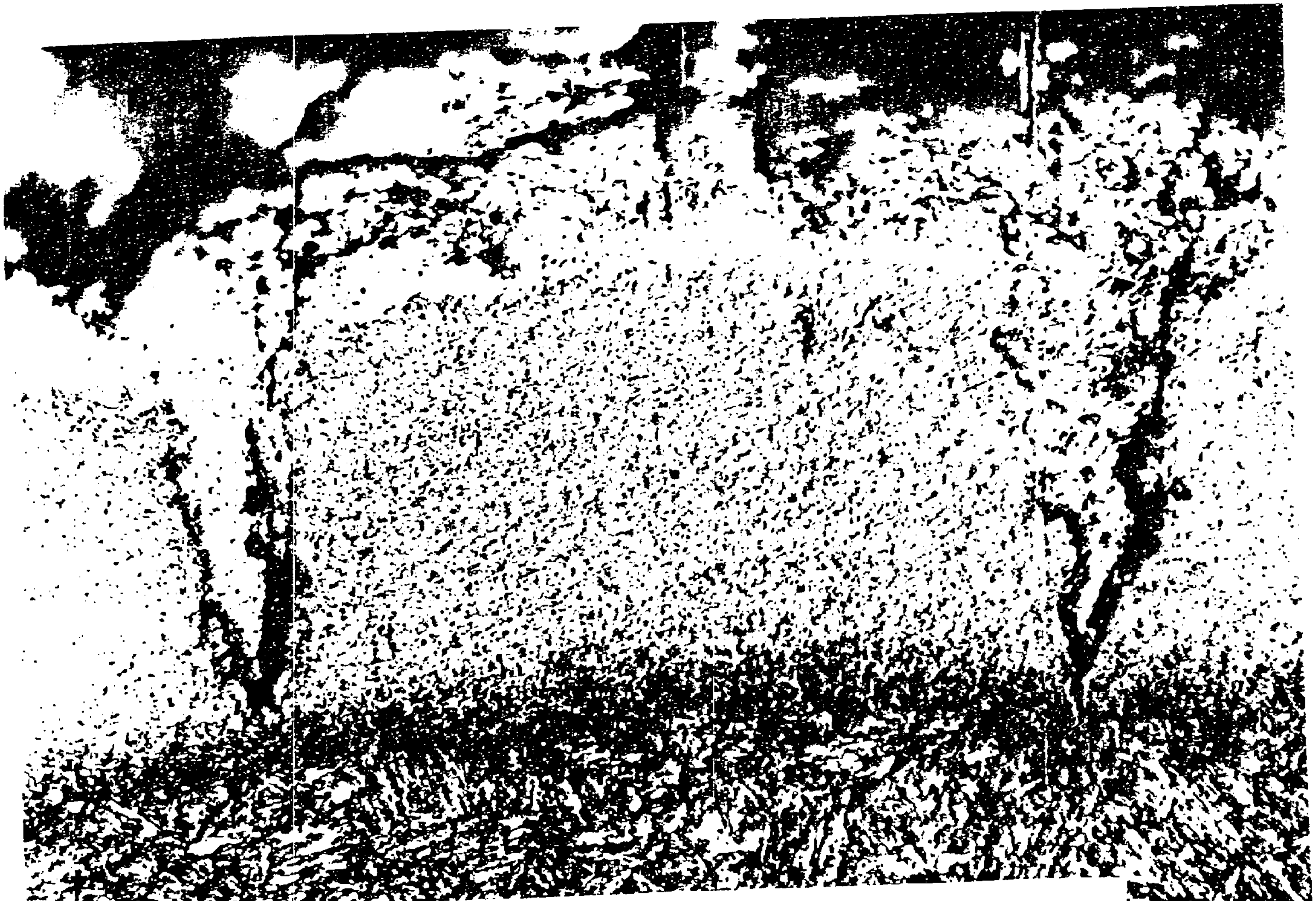

Fig. 13 Section through the top of a land of a gun tube from the developmental 155mm howitzer showing cracks filled with copper alloy in the heat-affected steel. (750x)

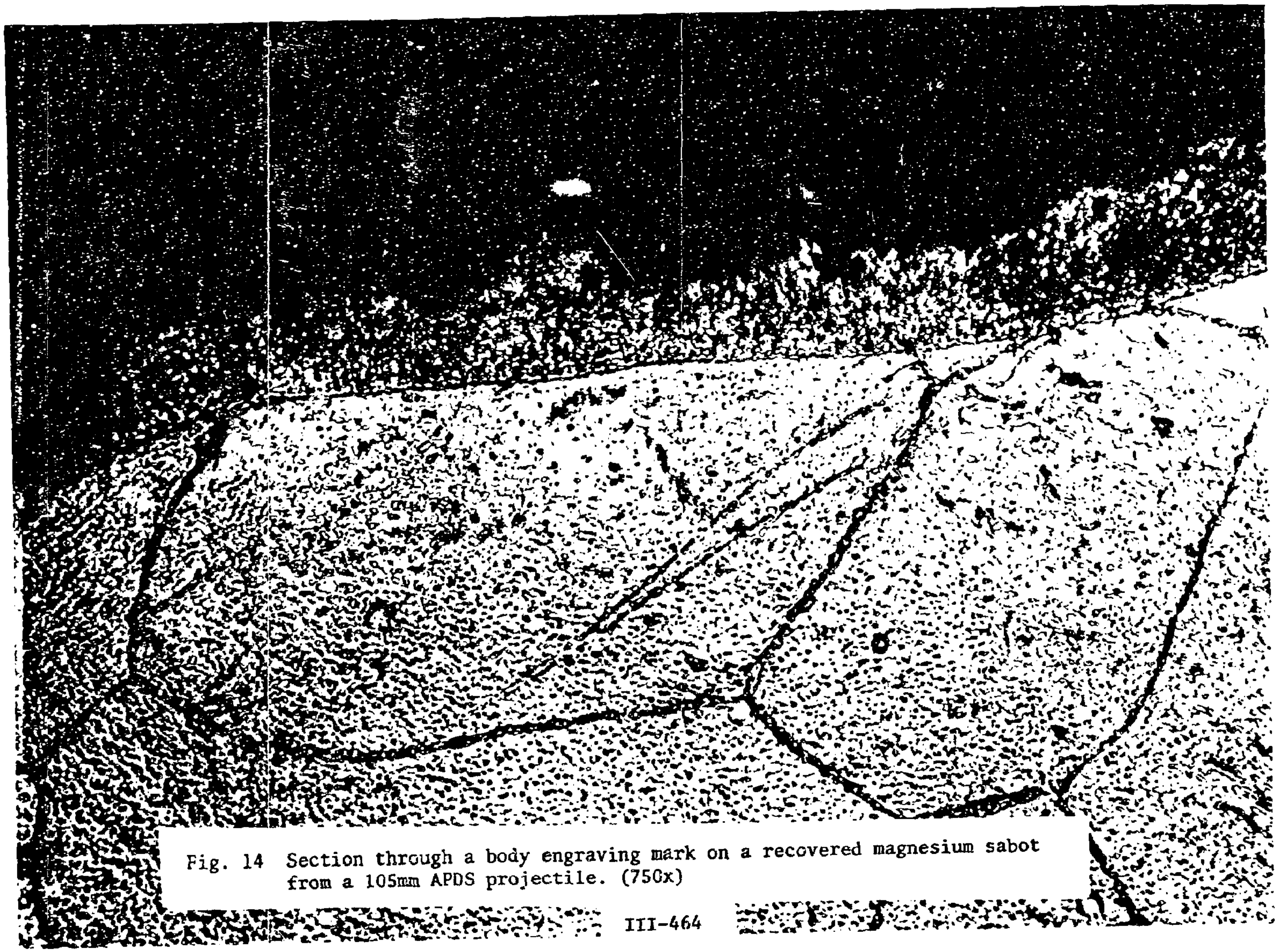

Fig. 14 Section through a body engraving mark on a recovered magnesium sabot from a 105mm APDS projectile. (750x)

CONCEPTUAL DESIGN OF A PSEUDO-SCALED GUN BARREL EROSION TEST FIXTURE

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ABSTRACT

Presented are the constraints which must be imposed upon a model gun in order that the thermal response of its bore surface can be highly analogous to that in an actual large gun. The first set constraints were constructed so that the gas flow dynamics in the simulator would cause the heat flux acting at the bore surface during the time interval of an isolated single shot to be the same as that in an actual gun, provided both barrels were made of the same material. To insure near equivalence in bore surface temperatures during multiple round firings at a given rate of fire, a constraint on the outside diameter of the simulator was defined. A determination was made, via optimization theory, of the outside diameters of two simulators which caused their residual bore surface temperature history to be nearly the same as that which would have been experienced by a 5-inch gun when responding to a firing schedule of 100 rounds at the rate of one round every 12 seconds. The inside diameters of the two simulators were 40mm and 3 inches, respectively. Comparisons were made of the predictions of both residual and peak bore surface temperatures for the 5-inch gun and the two simulators. On the basis of these two highly favorable comparisons, it was concluded that, in the optimized geometric configuration and for the specifically chosen firing schedule, the two pseudo-scaled guns should exhibit bore surface temperature and erosion very nearly the same as that present in the 5-inch gun.

INTRODUCTION

Attempts to substantially improve performance levels of existing and future gun systems without causing a marked decrease in gun barrel service life demand that more effective means of controlling bore surface erosion be developed. Previous efforts to control erosion in gun barrels have generally been of a "quick-fix", "cut-and-try" nature with little or no emphasis on developing a basic understanding of the erosion processes. During the firing of a gun, high velocity, high temperature, propellant combustion gases wash over the bore surface of the gun barrel.

These severe in-bore conditions cause an extremely high convective heat transfer to the barrel and, consequently, enhance the rate of wear at the bore surface. This cause and effect relationship exists because most of the complex, interdependent processes which induce gun barrel erosion are themselves thermally driven phenomena (see references 1 and 2 for information in support of this contention).

If a scaled-down gun could be designed so that the thermal response of its bore surface was the same as that in the actual gun, then erosion processes in the model and the actual gun should be nearly the same. It must be emphasized that such a duplication is possible only when the in-bore gas flow dynamics and the propellant combustion products are also identical. The existence of such a scaled-down gun would significantly reduce reliance on expensive full scale testing as the primary means used to identify erosion mechanisms and to evaluate new concepts for wear reduction. A smaller gun could more easily be adapted to laboratory procedures wherein the quality and quantity of pertinent data could be increased. For example, these benefits could be exploited if the near-bore erosion behavior in a modified 3-inch gun could be made the same as that in an 8-inch gun.

APPROACH

Certain constraints must be imposed if the in-bore gas flow dynamics are to be equivalent in the scaled-down simulator and the actual gun. These idealized constraints have been grouped into four areas and are as follows:

1. Consider the projectiles of the gun and simulator as having the same shot start pressure and as being friction-free and spin-less down the bore. Require pressure-time equivalence throughout the entire projectile in-bore travel time. Insure that projectile travel distance as well as the length of the initial chamber volume are the same in each system. Require the same histories of fractional chamber volume increase as the projectiles move within the bore.

2. Have identical charge loading or energy densities and utilize the same propellant and propellant geometry.

3. Maintain equivalence of the ratios of projectile weight to propellant weight.

4. Establish an upper bound on the ratio of chamber area to chamber volume in the scaled-down gun. This constraint will prevent the heat loss to the barrel from significantly reducing the bulk gas temperature of the propellant combustion products in the simulator.

By imposing all of the above constraints, the heat flux acting at the bore surface during the time interval of an <u>isolated</u> single shot should be the same in the scaled-down gun and the actual gun provided both are made of the same material. Relaxation of two constraints identified in the first grouping, namely that the projectiles be friction-free and spinless down the bore, can be allowed without seriously compromising the heat flux analogy.

To insure equivalence in bore surface temperature during long firing schedules at a given rate of fire, a further constraint on the geometry of the scaled-down gun barrel must be imposed. In identifying this constraint, it was necessary to use a closed form, transient, two dimensional, cylindrical heat transfer solution for pulsating heat flux boundary conditions (see reference 3). Given the single shot, axially dependent heat input to the barrel for the actual gun, this solution will yield the residual bore surface temperature as a function of shot number for any firing rate. It is this time dependent temperature which must be matched by use of the scaled-down gun; otherwise, equivalence of erosion behavior cannot be achieved.

Once the inside diameter of the simulator is fixed along with the number of rounds to be fired at a specific firing rate, the computed residual bore surface temperature becomes a function <u>solely</u> of the outside diameter of the scaled-down gun barrel. Implicit in this statement is the assumption that the convective heat transfer coefficient applicable to the outside surfaces of the two guns is identical. Thus, to determine the constraint on the outside diameter of the simulator, it is necessary to minimize the degree disparity, through any number of shots, between the residual bore surface temperatures in the actual and scaled guns. This minimization process can be accomplished by use of the Fibonacci search technique (see reference 4).

RESULTS AND CONCLUSIONS

In the analysis, the 5"/54 MARK 18 MOD 3 gun barrel was chosen to represent the actual gun. The propellant utilized was NACO. The firing schedule was considered to be 100 rounds fired at the rate of one round every 12 seconds. The optimization process was applied to scaled-down gun barrels having inside diameters of 40mm and 3 inches, respectively. A determination was made of the outside diameters of these two simulators which caused the residual bore surface temperature history to be highly analogous to that which would have been experienced by the 5-inch gun barrel when responding to the above mentioned firing schedule.

Shown in Figure 1 is a comparison of the predicted residual bore surface temperatures for the actual gun and the two simulators. It should be noted that the predicted residual bore surface temperatures for the 5-inch gun which are shown in this figure are relevant to an axial location 43.5 inches from the breech face. This station is located 1/2 inch downstream of the origin-of-bore on a new 5"/54 gun and, thus, is at a location particularly susceptible to gun barrel erosion. Presented in Figure 2 is a comparison of the peak bore surface temperature as a function of rounds fired. Again, the curve for the 5-inch gun pertains to that section of the barrel 43.5 inches from the breech face. The sketch in Figure 3 shows the relative size of the barrel cross-sections. The solid boundaries represent the actual, nominal barrel diameters. The outside nominal barrel diameters for the 5-inch, 3-inch, and 40mm guns are relevant to the MARK 18 MOD 3, MARK 22, and MARK 1, respectively. The indicated values for these diameters apply to the axial location 1/2 inch downstream from the origin-of-bore on all three guns. The dashed circles depict the optimized geometric configurations for the pseudo-scaled gun barrels. It is important to note that the heat transfer analysis used in the optimization process required that the barrel inside and outside diameters be considered as constant. The cross-hatched regions represent the material which would have to be, respectively, removed and added to the conventional 3-inch and 40mm gun barrels. On the basis of the results from the two highly favorable comparisons of residual and peak bore temperatures, it was concluded that, in the optimized geometric configuration and for the specifically chosen firing schedule, the two pseudo-scaled guns should exhibit bore surface erosion very nearly the same as that present in the 5-inch gun.

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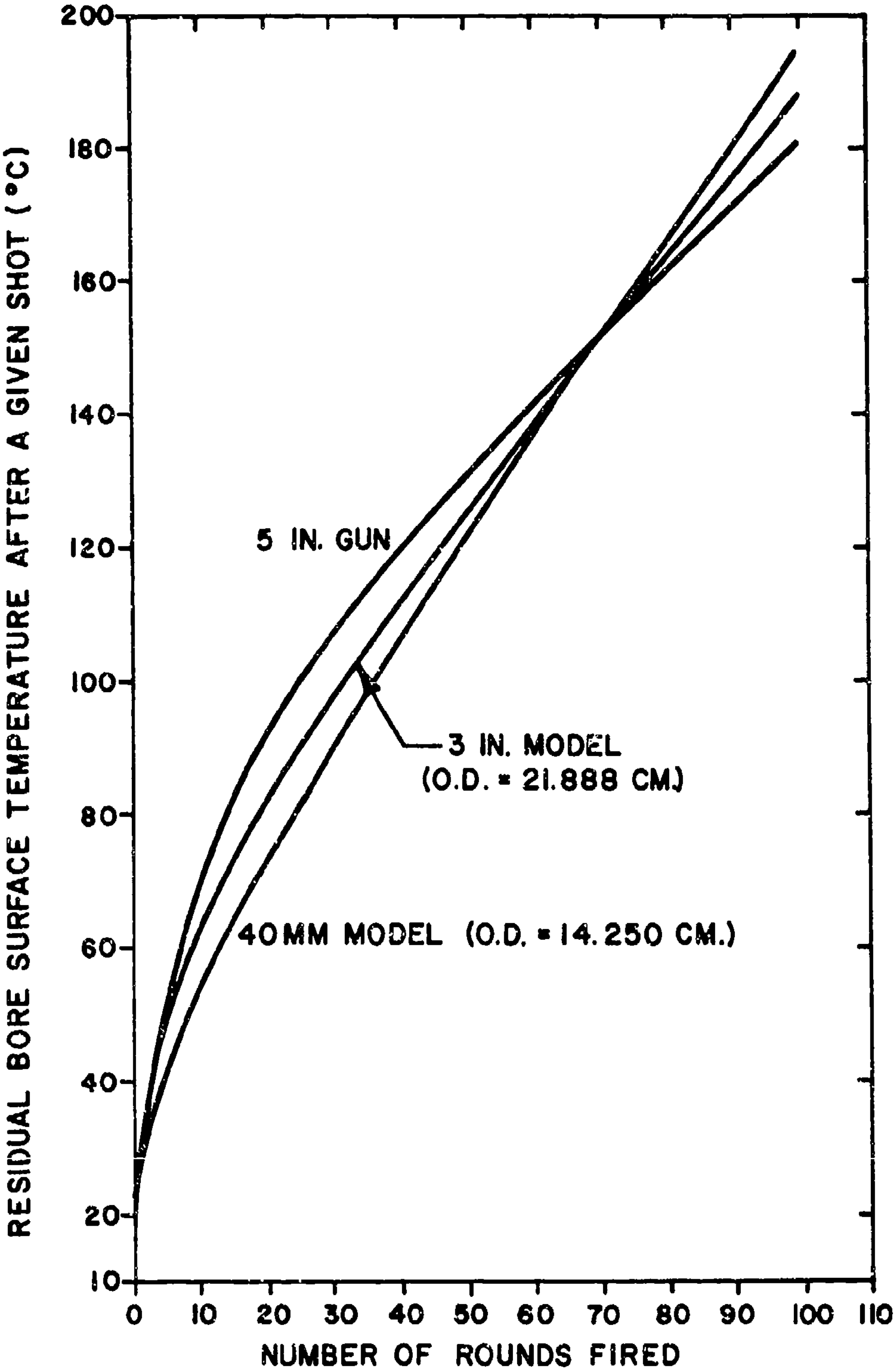

FIGURE 1. COMPARISON OF ANALYTICALLY PREDICTED RESIDUAL BORE SURFACE TEMPERATURES

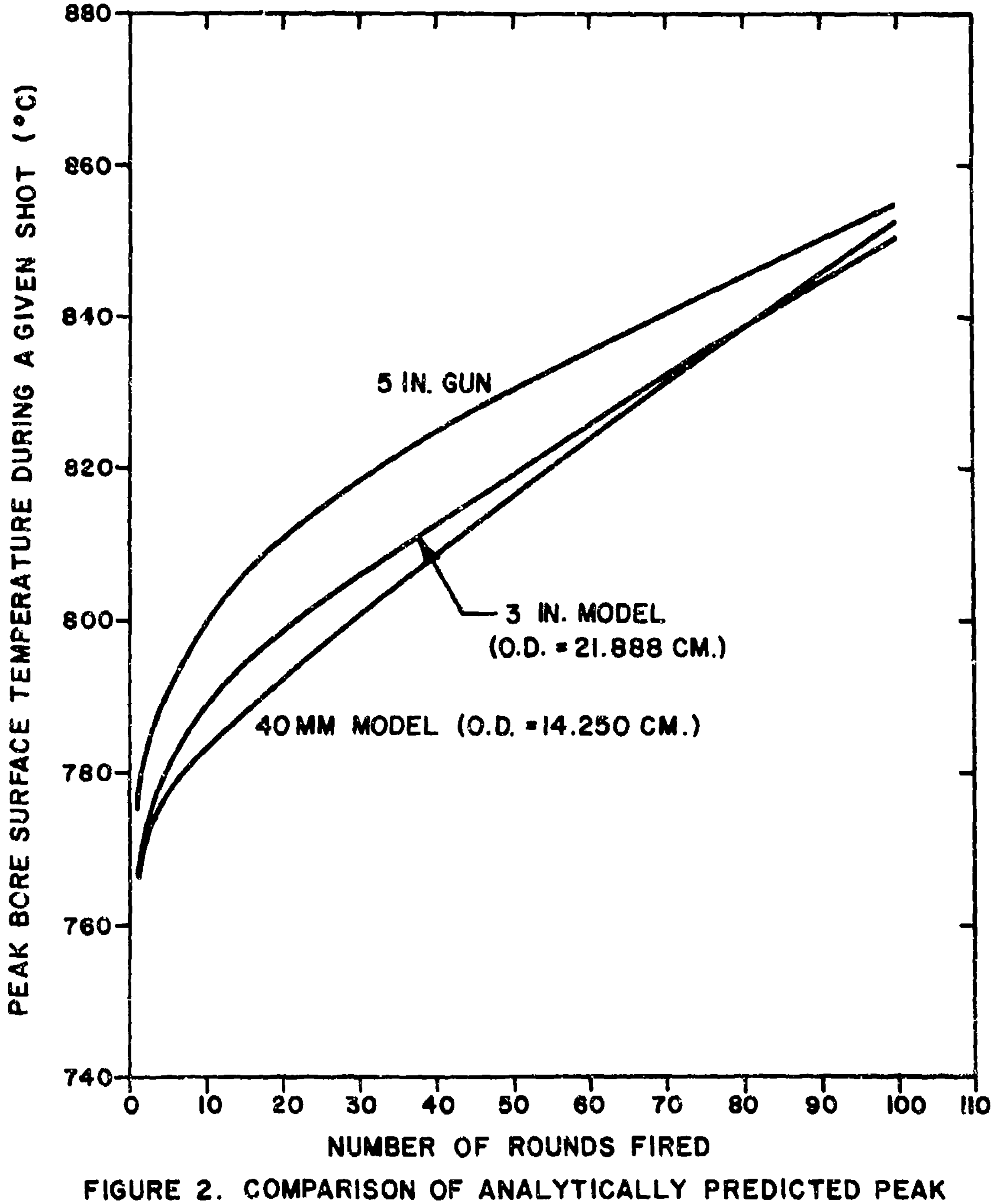

FIGURE 2. COMPARISON OF ANALYTICALLY PREDICTED PEAK BORE SURFACE TEMPERATURES

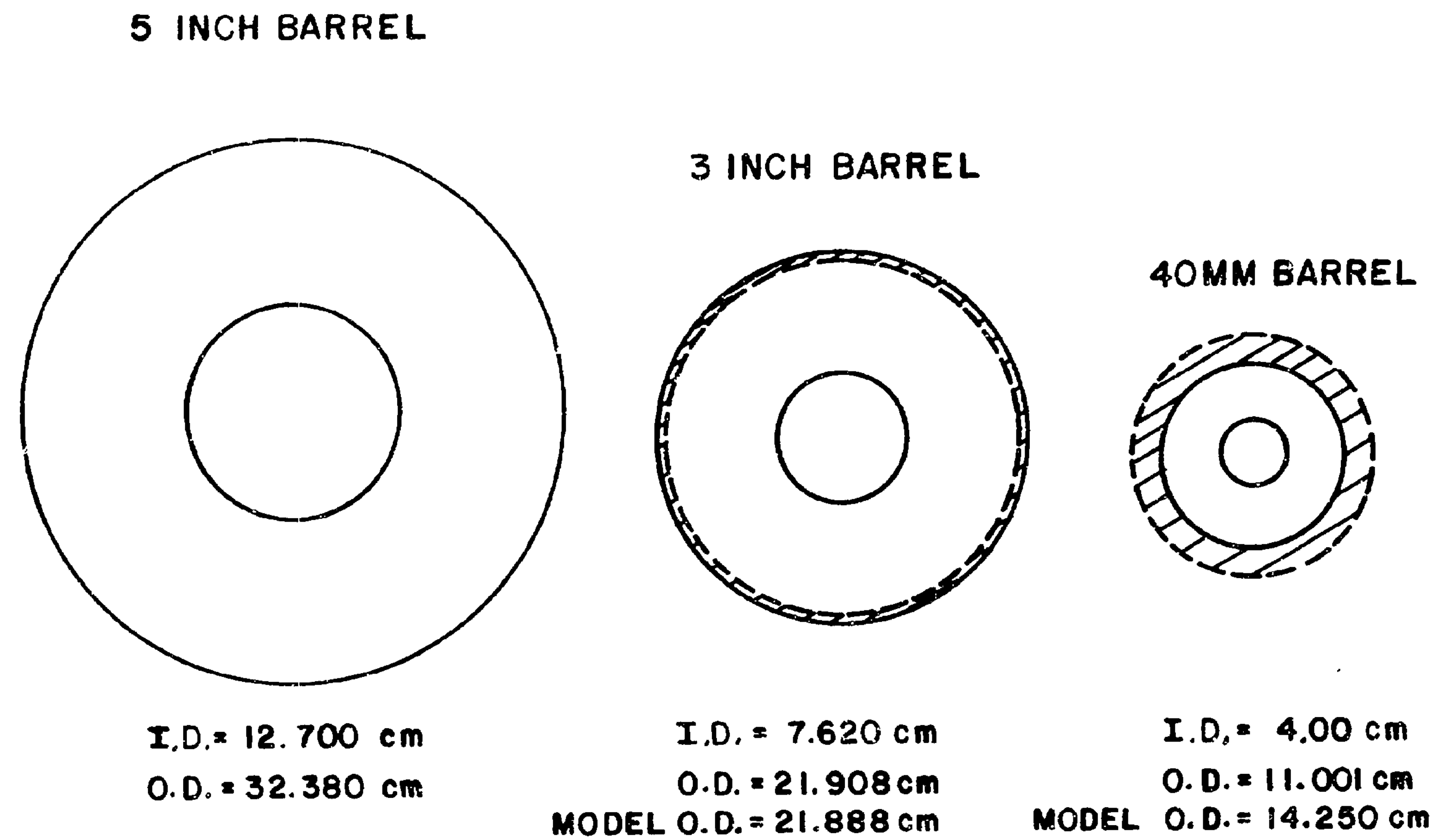

FIGURE 3. COMPARISON OF BARREL CROSS SECTIONS

SESSION IV

PROPELLANTS AND ADDITIVES

Chairman: Dale Davis

Air Force Armament Laboratory
Eglin Air Force Base

DESIGN OF PROPELLANT CHARGES WITH LOW EROSIVITY CHARACTERISTICS

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ABSTRACT

One of the most critical factors responsible for erosion is propellant flame temperature. The use of propellant compositions having a low flame temperature will result in a definite gain to the armament system designer as erosion will be reduced. This paper will show how the low flame temperature propellants can be utilized based upon Analog/Digitial Hybrid computer interior ballistic calculations using the 105mm Gun, M68 as the model. The effects of granular and consolidated propellant charges upon chamber pressure and/or length of projectile travel are presented. Computer calculated flash predicitions for the cooler propellant compositions are reviewed. The success of the concepts discussed in this paper hinges upon the understanding of the role pressure plays in erosion.

INTRODUCTION

Significant advances in future weapon design must depend in part upon new and imaginative approaches taken by the propellant engineer to improve propulsion. Conventional propellant/propelling charge schemes are no longer adequate in meeting the high performance, high rate of fire problems presented by advanced systems. Significant changes in propellant composition alone to meet these objectives are severely limited by constraints such as erosion, fouling, flash, smoke, stability, safety and cost or availability (Ref. 1). Another factor which inhibits the adaption of new propellants is that customary design approaches optimally fix gun parameters and dimension around a specific current standard gun propellant. Once these factors are frozen around a particular composition, significant improvements cannot be achieved without influencing the gun characteristics (chamber volume, length of travel, etc.). The solution to this apparent dilemma, in the face of ever increasing demands for large advances in propellant/propulsion devices, requires the exploration of more imaginative concepts in propelling charge for gun systems introduced at the earliest possible phases in the design cycle.

DISCUSSION

One of the more serious problems confronting armament system designers is the extensive gun barrel erosion encountered. Table I shows the comparative useful tube life and fatigue life of a number of tubed weapons in the Army inventory. The high cost of erosion is obvious from this presentation. High performance weapons being developed indicate even greater wear rates. It is known that erosion reduction additives have proven useful in extending gun tube life. However, they may be approaching their limit of usefulness.

The high temperature of the propellant gases is generally regarded as the leading factor influencing gun tube erosion. Consequently, any effort to reduce these temperatures would be beneficial in decreasing erosion. The thermodynamic characteristic of several standard propellants are shown in Table II. The M30 composition is one of the most extensively used propellants, notably, because of its attractive force-flame temperature relationship. However, it is apparent that there are several other standard propellants with flame temperatures lower than M30 although they are also lower in energy per unit weight. If a scheme could be devised to take advantage of these cooler temperatures, a significant reduction in gun tube erosion should be possible. To achieve high velocity levels with these cooler formulations employed in conventional charge configurations would require large increases in gun chamber volumes. Up to now, this has not been an accepted route to gun design because of mobility loss due to increased weight.

Analog/Digitial Hybrid computer interior ballistic simulation calculations were made using the 105mm Gun APDS-T, M392E3 Round (12.80 lbs.) as the model. The results shown in Tables III and IV show that use of cooler, muti-perforated propellants is potentially feasible if higher chamber pressures or longer gun tubes or some combination of the two are acceptable. The cooler the propellant, the higher the chamber pressure or conversely the longer the gun tube length that is required to achieve the same ballistic level as the base line comparison model using M30 propellant. Table V shows computer calculated flash predictions which indicate lowering of the shock temperatures in the muzzle vicinity when using cooler propellants (Ref. 2). Secondary flash is not entirely eliminated because of the increased combustible gases (CO and H_2) produced by the cooler compositions. However, the tendency to flash is lowered as reflected in the reduced amounts of potassium sulfate required to eliminate flash. About one percent by weight of conventional propelling charge is considered to be optimum before significant reduction in overall energy is noticed.

The work to date indicates that if cooler compositions are to be used effectively in acceptable gun size, some scheme to increase their volumetric energy will be necessary. Conventional granular propellants exhibit a considerable free inter-granular space when packed within the cartridge cases or bag charges. Through compression molding or consolidation this space could be substantially reduced. In this manner, cooler compositions might be used in reasonably sized gun chambers. Table VI and Figure 1 show thermodynamic comparisons of the weight increases or volume reductions that would be required to achieve the same energy output of M30 propellant. If successful consolidation of cool propellants were possible, within the limits indicated, an 9 to 29 percent volume reduction (or weight increase) would mean that flame temperatures could be lowered from 400°K to 800°K below the 3040°K for M30 propellant and still maintain the same ballistic levels. Temperature reductions of this magnitude should give an appreciable decrease in gun tube erosion.

Analog/Digitial Hybrid computer interior ballistic similation calculations were next made for consolidated propellant charges using the 105mm Gun M68 as the model. At consolidation densities below 1.1 grams/cc and large chamber volumes, the interior ballistic calculations for consolidated charges may be made using granular propellant calculation equations without introducing large errors, providing adequate attention is paid to the empirical constants. The results shown in Table VII show that consolidation of cooler propellants in the standard M68 gun will not be practicable as the 4850 fps service velocity is not attained within the 61,800 psi maximum rated pressure of the weapon. The velocities are from 175 fps to 300 fps low for the cooler composition. The optimum loading density for the M31, M6 and M1 propellants is 0.85 g/cc whereas that for the NACO Comp C composition is 0.90 g/cc. This is explained by the fact that the Navy propellant has a higher pressure exponent and therefore can burn up more propellant in the same length of travel. Above these loading densities, there is a drop off in velocity due to the presence of unburnt propellant at the muzzle of the gun tube (Figure 2). The 4850 fps service velocity is only attainable if the chamber pressure is allowed to exceed the maximum rated pressure of the weapon system. The cooler the propellant, the higher the chamber pressure (Figure 3).

The 2600°K Flame Temperature M31 and M6 propellant consolidated charges can be employed in the 105mm Gun, APDS-T, M392E3 Round provided the length of travel is increased 50 inches (Table VIII). The use of the cooler M1 and NACO Comp C propellants will necessitate a slight increase in chamber pressure as well as the longer gun tube.

The computer calculated flash predictions for the cooler propellant consolidated charges are presented in Table IX. The data is very similar to that in Table V for the cooler granular propellants. Approximately, 0.5 to 1.0 percent potassium sulfate will be required to eliminate flash.

These model simulations show a direction for future investigation. They indicate substantial lowering of gas temperatures at the cost of higher gun chamber pressures and/or longer gun tubes. A legitimate question arises, however, as to the significance of higher chamber pressures upon gun tube erosion. The gains yielded by lower gas temperatures might be cancelled by the possible more erosive effects of elevated pressure thus negating any benefits. Unfortunately, the role of pressure with regard to erosion has not as yet been clearly identified. The successful implementation of the consolidation of cool propellants hinges upon the understanding of the role pressure plays in erosion.

Consolidation, it should be mentioned, is not a new concept. Several groups both within the government and at its contractors facilities have employed the idea primarily toward the development of caseless ammunition. Some current activity at ARRADCOM is being focused upon molding larger caliber charges using M30 propellant. However, the concept discussed in this article has not been previously offered.

The design of gun propulsion systems is at a crossroad. New and radical concepts are needed to attain significant gains in performance and overall improvements. This article was presented not only to indicate one such possible approach but also to hopefully stimulate the charge design community along new and more productive paths toward future armament systems.

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TABLE I

COMPARISON OF WEAR AND FATIGUE LIFE OF VARIOUS WEAPONS

TUBE	DOLLARS/ UNIT	ROUNDS TO TUBE LIFE WEAR	ROUNDS TO TUBE LIFE FATIGUE
105 M68	8,000	ca. 1,000	1,000
105 M157A1	6,500	7,500	10,000
155 M185 (M119 Charge)	11,200	ca. 10,000	5,000
155 XM199	12,000	2,500	11,000
175 M115A1	30,000	1,200	2,900
8" XM201	20,000	1,500	10,000

TABLE II

COMPOSITION AND CALCULATED PROPERTIES OF VARIOUS STANDARD PROPELLANTS

	NAVY		ARMY		
Propellant Composition	NACO C	NACO D	M1	M6	M30
Specification	NWWS 11606	NWWS 11606	MIL-STD -652	MIL-STD -652	MIL-STD -652
Composition	95.0	94.0	85.0	87.0	28.0
Nitrocellulose	(12.0)	(12.0)	(13.15)	(13.15)	(12.6)
(%N in NC)					22.5
Nitroglycerin					
Dinitrotoluene			10.0	10.0	
Dibutyphthalate			5.0	3.0	
Nitroguanidine					47.7
n-Butyl Stearate	3.0	3.0			
Basic Lead Carbonate	1.0	1.0			
Potassium Sulfate		1.0			
Ethyl Centralite	1.0				1.5
Cryolite		1.0			0.3
Diphenylamine (added)			1.0	1.0	0.3
Water (Residual)	1.0	1.0	0.50	0.60	0.0
Ethyl Alcohol (Residual)	2.0	2.0	0.75	0.80	0.3
Calculated Thermodynamic Properties					
Isochoric Flame Temp. °K	2210	2184	2417	2574	3040
Force, ft-lbs/lb	282,400	277,000	304,900	316,900	364,000
Heat of Explosion, cal/gm	651	656	701	759	974
Gas Volume, moles/gm	0.04593	0.04559	0.04535	0.04426	0.04308
Unoxidized Carbon, %	8.9	8.8	8.6	6.6	3.2
Combustibles, %	69.3	68.7	71.7	68.2	41.0
Ratio CO/CO_2 in Combustibles	5.0	4.0	6.1	4.9	2.2

TABLE III

EFFECT OF LOADING DENSITY VARIATIONS UPON BALLISTICS *

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Maximum Rated Pressure: 61,800 psi

Propellant	T_v °K	Force ft-lbs/lb	Charge Weight lbs	Loading Density g/cc	Web, MP inches	Travel inches	Point of Burnout inches	Maximum Chamber Pressure at 70°F psi
M30	3040	364,500	12.2625	0.849	0.0457	186	142	59,400
NACO Comp C	2210	282,400	13.4000	0.928	0.024	186	71	72,754
			14.2000	0.984**	0.027	186	99	72,266
			15.0000	1.039**	0.030	186	132	75,098
			15.8000	1.094**	0.032	186	170	80,444
			13.400	0.928	0.026	236	103	61,279
M1	2417	304,900	12.43	0.861	0.023	186	71	71,558
			13.55	0.938	0.026	186	104	71,521
			14.68	1.017**	0.031	186	152	75,122
			12.43	0.861	0.0255	232	99	60,767
M6	2574	316,900	12.010	0.832	0.030	186	71	68,774
			13.304	0.921	0.036	186	114	68,188
			14.598	1.011**	0.041	186	164	72,900
			15.892	1.101**	0.045	186	90% Burnout At Muzzle	82,556
			12.010	0.832	0.0319	215	89	61,267

* Granular Propellant

**The loading density is theoretical and not attainable with granular propellant. The loading density can be attained by charge consolidation.

TABLE IV

EFFECT OF WEB VARIATION UPON BALLISTIC PERFORMANCE OF COOL PROPELLANTS*

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Maximum Rated Pressure: 61,800 psi

Propellant	T_v °K	Force ft-lbs/lb	Charge Weight lbs	Loading Density g/cc	Web, MP inches	Travel inches	Travel Increase inches	Point of Burnout inches	Maximum Chamber Pressure at 70°F psi
M30	3040	364,500	12.2625	0.849	0.0457	186	-	142	59,400
NACO Comp C	2210	282,400	13.4000	0.928	0.0240	186	-	71	72,754
					0.0247	202	16	84	70,068
					0.0254	220	34	94	65,942
					0.0260	236	50	103	61,279
M1	2417	304,900	12.4300	0.861	0.0230	186	-	71	71,558
					0.0238	201	15	82	69,006
					0.0246	217	31	90	65,259
					0.0255	232	46	99	60,767
M6	2574	316,900	12.0100	0.832	0.0300	186	-	71	68,774
					0.0307	196	10	78	67,212
					0.0314	206	20	86	64,473
					0.0319	215	29	89	61,267

*Granular Propellant

TABLE V

PREDICTED FLASH CHARACTERISTICS OF COOL PROPELLANTS *

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Maximum Rated Pressure: 61,800 psi

Propellant & T_v	Calculated Combustibles (H_2 and CO) in Propellant Muzzle Gases	Charge Weight	Web, MP	Travel	Maximum Chamber Pressure at 70°F	Exit Shock Temp.	Flash	K_2SO_4 Concentration in Propellant needed to Eliminate Flash
°K	%	pounds	inches	inches	psi	°K		%
M30 3040	41.0	12.2625	0.0457	186	59,400	1289	Yes	2.0
NACO Comp C 2210	69.3	13.4000	0.0240	186	72,754	933	Yes	0.5
			0.0247	202	70,068	935	Yes	0.5
			0.6254	220	65,942	936	Yes	0.5
			0.0260	236	61,279	938	Yes	0.5
M1 2417	71.7	12.4300	0.0230	186	71,558	986	Yes	0.5
			0.0238	201	69,006	987	Yes	0.5
			0.0246	217	65,259	989	Yes	0.5
			0.0255	232	60,767	991	Yes	0.5
M6 2574	68.2	12.0100	0.0300	186	68,774	1055	Yes	1.0
			0.0307	196	67,212	1056	Yes	1.0
			0.0314	206	64,673	1057	Yes	1.0
			0.0319	215	61,267	1058	Yes	1.0

*Granular Propellant

TABLE VI

VOLUMETRIC FORCE COMPARISON OF STANDARD PROPELLANTS

Propellant	Ft-lbs/lb	Isochoric Flame Temperature °K	% Weight Increase To Match M30 Volumetric Force
M30	364000	3040	-
M31	334000	2599	9.0
M14	327000	2710	11.3
M6	317000	2570	14.8
M1	305000	2417	19.3
M1A1	287000	2225	26.8
MACO	282400	2210	28.9

TABLE VII

EFFECT OF CONSOLIDATED CHARGE LOADING DENSITY UPON BALLISTIC PERFORMANCE OF COOL PROPELLANTS

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Travel: 186 inches; Maximum Rated Pressure: 61,800 psi

Propellant	T_v	Force	Charge Weight	Loading Density	Web, MP	Point of Burnout	Maximum Chamber Pressure at 70°F	Velocity
	°K	ft-lbs/lb	pounds	g/cc	inches	inches	psi	fps
(Currently Used) M30	3040	364,500	12.2625	0.849	0.0457*	142	59,400	4850
NACO Comp C	2210	282,400	12.286	0.85	0.0186	167	59,600	4536
			12.998	0.90	0.0204	186	60,000	4541
			13.729	0.95	0.0225	95%**	59,600	4535
			12.998	0.90	0.0181	120	78,700	4851
M1	2417	304,900	12. [illegible]6	0.85	0.0243	99%**	60,100	4576
			12.998	0.90	0.0269	96%**	60,000	4558
			13.729	0.95	0.0295	92%**	59,700	4506
			12.217	0.845	0.0209	124	76,400	4851
M6	2574	316,900	12.286	0.85	0.0275	98%**	59,900	4621
			12.998	0.90	0.0301	95%**	60,000	4605
			13.729	0.95	0.0333	92%**	59,900	4559
			12.286	0.85	0.0246	152	72,100	4850
M31	2599	334,000	12.286	0.85	0.0224	97%**	59,200	4673
			12.998	0.90	0.0245	94%**	58,900	4654
			13.729	0.95	0.0269	90%**	59,500	4597
			12.152	0.841	0.0204	178	68,400	4849

*MP Grains and not a consolidated charge

**At Muzzle

TABLE VIII

EFFECT OF CONSOLIDATED CHARGE LOADING DENSITY VARIATIONS UPON BALLISTICS

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Travel: 186 inches to 236 inches; Maximum Rated Pressure: 61,800 psi

Propellant	T_v	Force	Charge Weight	Loading Density	Web, MP	Travel	Travel Increase	Point of Burnout	Maximum Chamber Pressure at 70°F	Velocity
	°K	ft-lbs/lb	pounds	g/cc	inches	inches	inches	inches	psi	fps
(Currently Used) M30	3040	364,500	12.2625	0.849	0.0457*	186	-	142	59,400	4850
NACO Comp C	2210	282,400	12.286	0.850	0.0186	186	-	167	59,600	4536
			13.329	0.923	0.0214	202	16	202	59,900	4624
			13.394	0.928	0.0217	220	34	220	60,100	4705
			13.258	0.918	0.0212	236	50	236	60,100	4760
			13.258	0.918	0.0206	236	50	212	64,700	4849
M1	2417	304,900	12.286	0.850	0.0243	186	-	99%**	60,100	4576
			12.428	0.861	0.0252	202	16	202	59,900	4668
			12.636	0.875	0.0258	220	34	220	59,700	4735
			12.632	0.875	0.0255	236	50	235	60,100	4800
			12.632	0.875	0.0247	236	50	219	63,100	4852
M6	2574	316,900	12.286	0.850	0.0275	186	-	98%**	59,900	4621
			12.019	0.832	0.0264	202	16	202	60,000	4703
			12.014	0.832	0.0267	220	34	194	59,800	4791
			12.078	0.837	0.0270	236	50	212	60,100	4850
M31	2599	334,000	12.286	0.850	0.0224	186	-	97%**	59,200	4673
			11.602	0.804	0.0206	202	16	202	59,900	4760
			12.152	0.842	0.0222	220	34	220	59,900	4851
			10.744	0.744	0.0183	236	50	146	60,100	4848

*MP Grains and not a consolidated charge

**At Muzzle

TABLE IX

PREDICTED FLASH CHARACTERISTICS OF CONSOLIDATED CHARGES MADE FROM COOL PROPELLANTS

Analog/Hybrid Simulation Firings for the 105mm Gun, M68

Projectile: APDS-T, M392E3 (12.80 lbs); Velocity: 4850 fps; Maximum Rated Pressure: 61,800 psi;
Travel: 186 inches to 236 inches

Propellant and T_v	Calculated Combustibles (H_2 and CO) in Propellant Muzzle Gases	Charge Weight	Loading Density	Web, MP	Maximum Chamber Pressure at 70°F	Travel	Velocity	Exit Shock Temp.	Flash	K_2SO_4 Concentration in Propellant needed to Eliminate Flash
°K	%	pounds	g/cc	inches	psi	inches	fps	°K		%
M30 3040	41.0	12.2625	0.849	0.0457*	59,400	186	4850	1289	Yes	2.0
NACO Comp C 2210	69.3	13.258	0.918	0.0206	64,700	236	4849	934	Yes	0.5
M1 2417	71.7	12.632	0.875	0.0247	63,100	236	4852	997	Yes	0.5
M6 2574	68.3	12.078	0.837	0.0270	60,100	236	4850	1065	Yes	1.0
M31 2599	49.8	12.152	0.842	0.0222	59,900	220	4851	1097	Yes	1.0
		10.744	0.744	0.0183	60,100	236	4848	1054	Yes	1.0

*MP Grains and not a consolidated charge

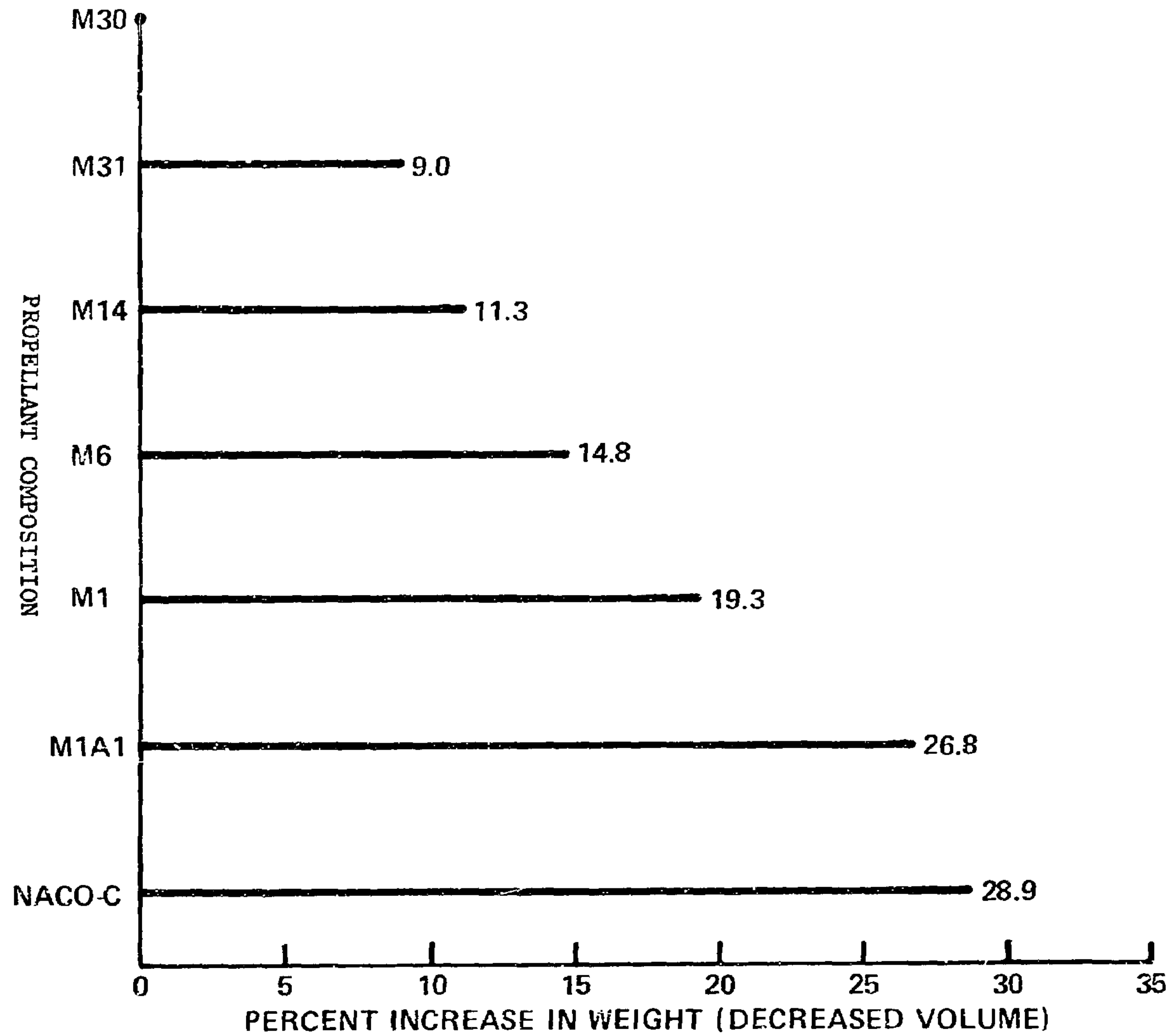
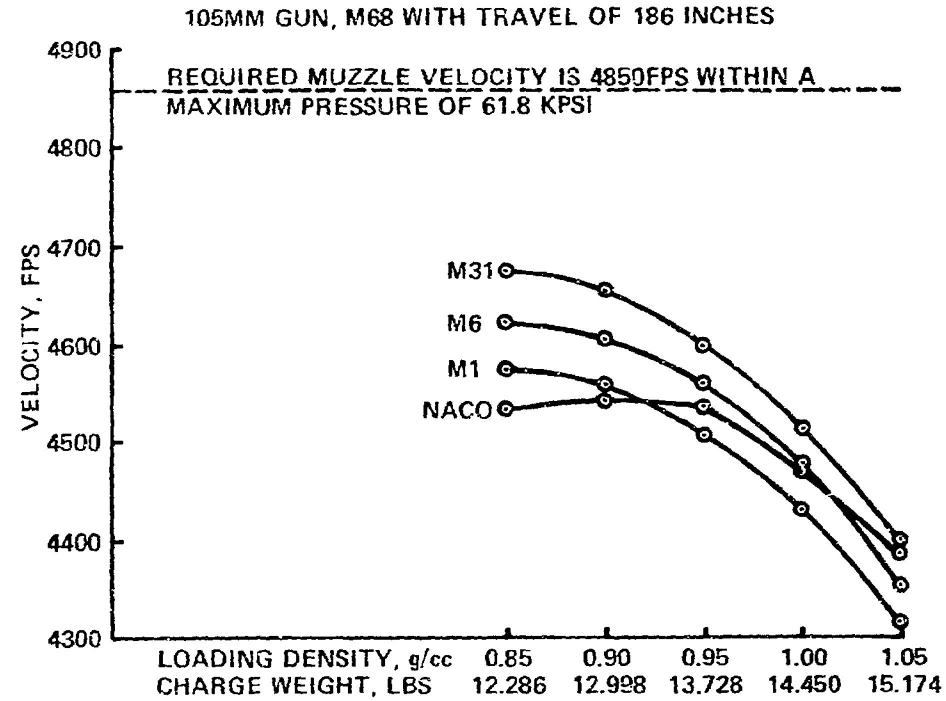


FIGURE 1 CALCULATED VOLUME EQUIVALENCY OF COOL PROPELLANTS TO MAINTAIN M30 COMPOSITION BALLISTIC PERFORMANCE

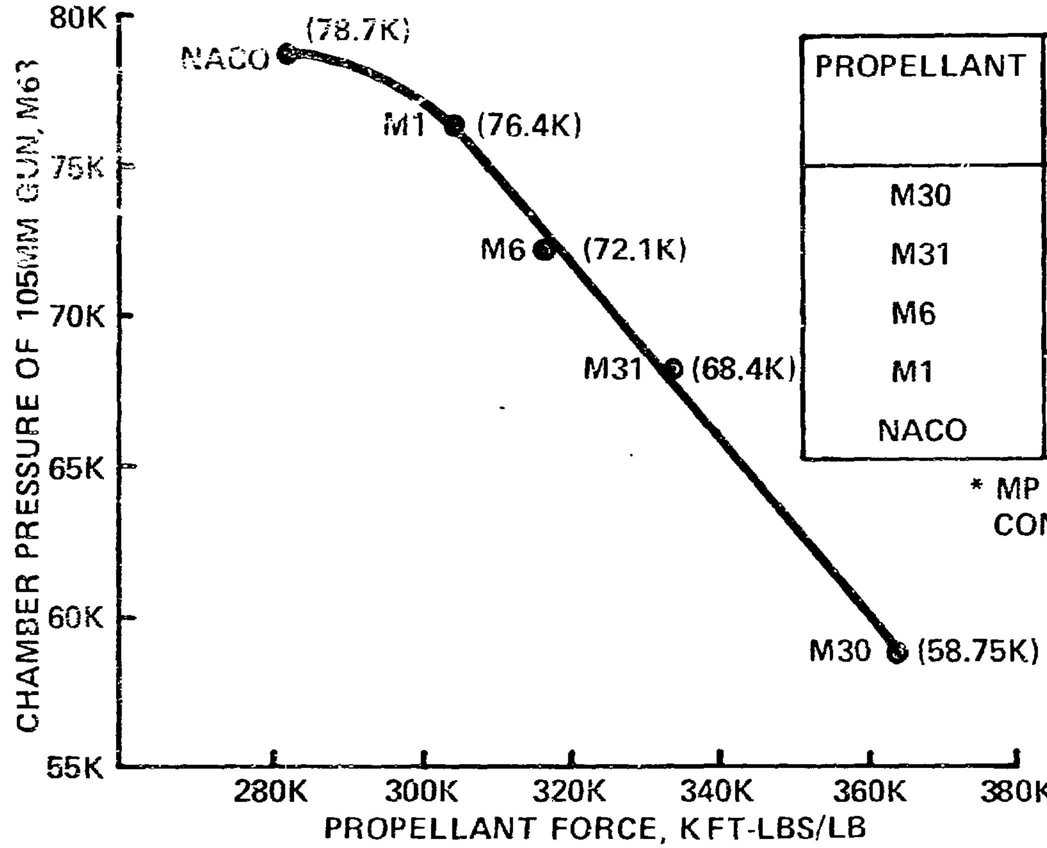


PROPELLANT T_v,°K FORCE, AT OPTIMUM CONDITIONS

		°K	FT-LBS/LB	LOADING DENSITY g/cc	CHARGE WEIGHT LBS	WEB, MP INCH	VELOCITY FPS	PRESSURE PSI
(CURRENTLY USED)	M30*	3040	364,000	0.849	12.2625	0.0457	4848	58,700
* MP GRAINS AND NOT A CONSOLIDATED CHARGE	M31	2599	334,000	0.850	12.286	0.0224	4673	59,200
	M6	2574	316,900	0.850	12.286	0.0275	4621	59,900
	M1	2417	304,900	0.850	12.286	0.0243	4576	60,100
	NACO	2210	282,400	0.900	12.998	0.0204	4541	60,000

FIGURE 2 105MM GUN APDS-T ROUND (12.80 LBS) USING CONSOLIDATED PROPELLANT

105MM GUN, M68 WITH TRAVEL OF 186 INCHES
SERVICE VELOCITY OF 4850FPS



PROPELLANT	CHARGE WEIGHT LBS	WEB, MP INCH	BURNOUT INCHES
M30	12.2625	0.0457*	137
M31	12.152	0.0204	178
M6	12.286	0.0246	152
M1	12.217	0.0209	124
NACO	12.998	0.0181	120

* MP GRAINS AND NOT A CONSOLIDATED CHARGE

FIGURE 3 105MM GUN APDS-T ROUND (12.80 LBS) USING CONSOLIDATED PROPELLANT

EFFECT OF NAVY GUN PROPELLANT ON GUN TUBE WEAR

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ABSTRACT

It is generally agreed that convective heat transfer from the propellant gases and the frictional heating caused by the rotating band are principal causes of gun tube erosion. In other than small caliber gun systems, the heat transfer from the propellant gases is the predominant factor. Thus, for a given gun system, performance level, and ballistic cycle, the flame temperature of the propellant used in the charge will determine the rate of barrel wear per shot. The Navy has, for most systems, chosen to use a very low flame temperature gun propellant and thus reduce barrel wear as much as possible. The new propellant formulations now being evaluated for application to current and future gun systems are selected for their low flame temperatures and moderate to high impetus levels. A discussion of the properties of the propellants currently in use will be presented, including any problems associated with their use. The likely effects from the use of the propellants under development will be described also.

INTRODUCTION

Numerous other papers and studies have been and will be done on the subject of gun tube wear and erosion, describing both the causes and potential solutions. The intent of this paper is not, however, to discuss this subject directly, but rather to present the approach taken by the U. S. Navy in its selection of gun propellants because of their effect on wear and erosion.

It is generally agreed that the properties of the propellant used in the propelling charge are the most important factors in determining the rate of tube wear for medium and large caliber gun systems. The erosion process itself may be thermal or chemical or both in nature, though no firm definition has been provided as yet; but there seems to be a strong relationship between the propellant flame temperature and rate of wear.[1] Most of the available experimental evidence would seem to support the above statements. As an example, one comparison of gun tube erosion rate at the origin of rifling for three different propellants in the Navy's 5-inch, 54-caliber gun

[1]Alex C. Alkidas and J. Richard Ward, "The Role of Additives in Reducing Gun Barrel Erosion," *Proceedings of the 12th JANNAF Combustion Meeting*, CPIA Publication 273, December 1975, pp 79–102.

system showed the erosion rate to be exponentially related to the bore surface temperature and the propellant flame temperature, as shown by Figure 1.[2] The effect of other factors on the wear rate, such as the rotating band material or frictional heating from the rotating band, is considered to be small compared to the effect of the gun propellant properties.

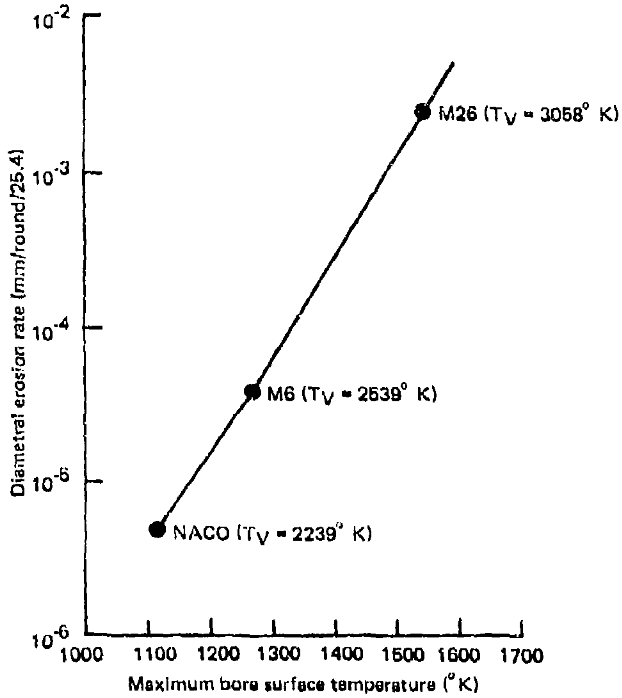

FIGURE 1. EROSION RATE VERSUS MAXIMUM BORE SURFACE TEMPERATURE IN 5-INCH, 54-CALIBER GUN

Both experience and experiment have shown that one very effective method in reducing the rate of barrel wear for a particular gun system is to use a propellant with a lower flame temperature. Implicit in such a statement is the assumption that the ballistic performance levels (maximum chamber pressure and pressure-time profile) associated with the different propellants are as similar as possible. Since it is very seldom, if ever, that a system user is willing to accept a decrease in range or velocity, even in exchange for an increase in gun barrel life, it is reasonable to assume that the performance levels will not change radically. Thus, while the use of a

[2] John S. Foster, J. S. O'Brasky, and C. S. Smith, "Heating and Wear Effects of Smear Coolants in Navy 5"/54 Guns," to be published in the bulletin of the 1976 JANNAF Propulsion Meeting.

lower flame temperature propellant is highly desirable in terms of barrel wear, it may not be feasible when the range and muzzle velocity requirements of a particular gun system are taken into account.

It is with these considerations in mind that the Navy's experience with gun propellants will be discussed.

DISCUSSION

Until the late 1960's the Navy depended primarily on what it called Pyro as the propellant for most of its gun propelling charges. In certain gun systems Cordite N was also used, especially for the 3-inch, 50-caliber and 6-inch, 47-caliber systems. During World War II large quantities of M1 and M6 propellant were made for the various gun systems. These formulations are very similar in their thermodynamic properties despite the differences in their ingredients. Three of these propellants are compared in Table I. Pyro and M6 are both classed as single-base formulations, in which nitrocellulose is the principal ingredient, even though they are quite dissimilar in composition. Cordite N is the third propellant and it is classed as a triple base, containing nitrocellulose, nitroglycerin, and nitroguanidine. All three propellants provided similar rates of wear, though the Cordite N was somewhat better than the others.

Table I

COMPARISON OF STANDARD NAVY PROPELLANTS

Ingredient (%)	Pyro	Cordite N	M6
Nitrocellulose (12.6% N)	100.00	-	-
Nitrocellulose (13.15% N)	-	19.00	87.00
Nitroglycerin	-	18.70	-
Nitroguanidine	-	55.00	-
Dinitrotoluene	-	-	10.00
Dibutylphthalate	-	-	3.00
Diphenylamine	1.00	-	1.00
Ethyl centralite	-	7.30	-
Total volatiles	5.00	0.30	2.50
Impetus (megajoules/kg)	0.960	0.977	0.950
Flame temperature (°K)	2635	2476	2539
Mean molecular weight (kg/mole)	22.90	21.27	22.38

Beginning in the 1950's the Navy began the development of a cool gun propellant to replace the formulations described above. The principal aim of the development program was to increase the useful barrel life, in terms of the number of full service rounds that could be fired prior to condemnation for wear, for the Navy's 3-inch and larger gun systems. The approach taken in the development of this propellant was to utilize a nitrocellulose with a very low nitration level and to add coolants to bring down the flame temperature even further. The nitrocellulose selected had a 12.0% nitration level, and both butyl stearate and ethyl centralite were used as the principal coolants and plasticizers. The resulting propellant was to be designated as NAVY COOL or NACO.

Several different NACO formulations were evaluated during the development program. As a part of this evaluation two of these formulations were used in a large scale barrel wear test in the 3-inch, 50-caliber gun.[3] One of the standard propellants for this gun, M6, was used as the reference for the test. The compositions and thermodynamic properties of the two NACO and the M6 lots are compared in Table II. The same firing program was used for all three propellant lots, and the results, as shown in Figure 2, graphically demonstrated the much lower wear rate resulting from the use of the NACO propellant. After the same number of rounds had been fired with all three propellants, the amount of bore enlargement caused by either of the two NACO lots was only 15% of that caused by the M6 lot. In fact, after firing twice as many rounds, the wear with the NACO propellants was still only 30% of that with the M6 propellant.

The final NACO composition selected is that given in Table III. During the late 1960's NACO was introduced into the Navy's 5- and 8-inch gun systems. This was practical because of the additional space available in the propelling charges, which permitted the loading of the higher charge weights necessary with the NACO propellant. Since that time, actual service use has confirmed the results of the earlier wear tests. In the 5-inch, 54-caliber gun system, barrels only 35% worn have been returned because of fatigue.

The use of NACO propellant in the Navy's gun systems, while very successful in extending the useable barrel life, has not been without problems. The composition and lower energy of NACO were accompanied by lower burning rates compared to the Pyro and M6 propellants, which required smaller webs and grain sizes for NACO to achieve the same performance levels. When used with the ignition systems designed for use with the M6 type propellants, the result was the formation of longitudinal pressure waves of

[3] Naval Proving Ground, Dahlgren, Va., *Barrel Life Tests With NACO (Navy Cool) Propellants of 3"/50 Caliber Barrels Mk 22 Mod 9 Serial Nos. 29533 and 29861*, by J. W. Duch, NPG Report 1479, 23 July 1956. CONFIDENTIAL.

greater magnitude during the ballistic cycle. This caused a reduction in the safety and reliability of the overall ammunition package. This could be, and was, remedied by modifications to the ignition systems; but in the interim, several costly malfunctions occurred. One further problem with NACO has become more serious in recent years. The NACO formulation requires considerably different processing conditions than for the M6 type propellants, and this has resulted in higher costs for NACO than for the other single-base propellants.

Table II

COMPARISON OF NACO AND M6 PROPELLANT LOTS

Ingredient (%)	M6 SPDN 6525	NACO IX-277	NACO IX-278
Nitrocellulose (12.0% N)	-	91.86	91.32
Nitrocellulose (13.15% N)	87.00	-	-
Dinitrotoluene	10.00	-	-
Dibutylphthalate	3.00	-	-
Diphenylamine	1.00	-	-
Butyl stearate	-	6.11	-
Ethyl centralite	-	0.99	7.68
Basic lead carbonate	-	1.04	1.00
Total volatiles	1.30	1.52	2.77
Impetus (megajoules/kg)	0.965	0.845	0.832
Flame temperature (°K)	2621	2134	2143
Mean molecular weight (kg/mole)	22.65	21.75	22.06

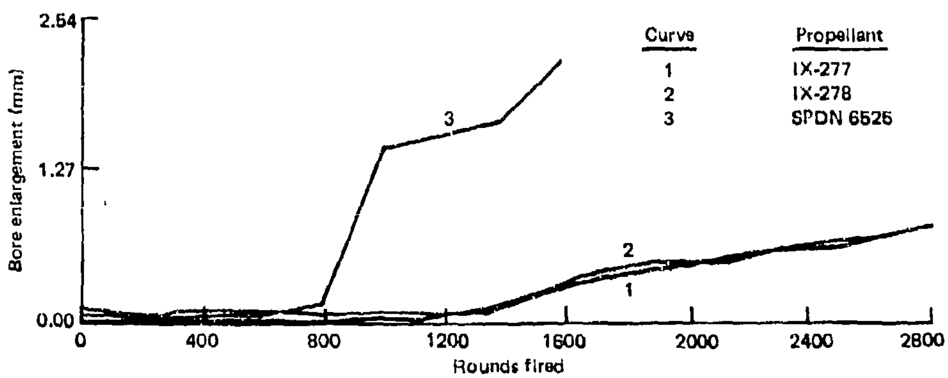

FIGURE 2. BORE ENLARGEMENT AT ORIGIN VERSUS ROUNDS FIRED FOR NACO AND M6 (3-INCH, 50-CALIBER GUN)

Table III

FINAL NACO COMPOSITION[1]

Nitrocellulose (12.0% N)	93.75
Butyl stearate	3.00
Ethyl centralite	1.00
Potassium sulfate	1.25
Basic lead carbonate	1.00
Total volatiles	3.10
Impetus (megajoules/kg)	0.834
Flame temperature (°K)	2239
Mean molecular weight (kg/mole)	22.30

[1]Typical of 5-inch, 54-caliber NACO lots.

With one exception, and that on a limited basis, NACO has been retained as the propellant in the Navy's major gun systems. In an effort to obtain increased range for the 5-inch, 54-caliber gun system, one propelling charge using M26 propellant (Table IV) was developed for use with the new Hifrag projectile. Barrel wear tests indicated, however, that even with a talc-wax liner, the wear characteristics for the charge would probably be no better than for a charge using M6 propellant.[4] As a result, no more than 10% of the propelling charges used with the Hifrag projectile will employ M26 propellant.

Table IV

M26 PROPELLANT COMPOSITION[1]

Nitrocellulose (13.15% N)	67.25
Nitroglycerin	25.00
Ethyl centralite	6.00
Barium nitrate	0.75
Potassium nitrate	0.70
Graphite	0.70
Total volatiles	2.20
Impetus (megajoules/kg)	1.060
Flame temperature (°K)	3058
Mean molecular weight (kg/mole)	23.99

[1]Typical of 5-inch, 54-caliber M26 lots.

[4]Naval Surface Weapons Center, Dahlgren, Va., *EX73 Mod 0 Charge Assembly: Barrel Wear Evaluation*, by T. W. Smith and L. H. Russell, TR 3202, October 1974.

Because of its higher cost, efforts are currently underway to evaluate possible replacements for NACO. The first formulation being studied is M1A1, shown in Table V. It is a variation of M1 never introduced into service use by the Army. No ballistic tests have been conducted as yet in any Navy systems with M1A1. One potential problem area which will be watched closely during the evaluation of M1A1 is the formation of carbon residue. This was a serious problem with several of the early NACO formulations and could be also with the M1A1. If the M1A1 performance is acceptable, a 19-perforation granulation will be evaluated in place of the more normal 7-perforation. This is being done in order to minimize the magnitude of any longitudinal pressure waves formed during the ballistic cycle through the increased permeability of the propellant bed, which results from the use of the larger 19-perforation grains.[5]

Table V

M1A1 PROPELLANT COMPOSITION

Nitrocellulose (12.6% N)	84.50
Dinitrotoluene	10.00
Dibutylphthalate	4.50
Diphenylamine	1.00
Potassium sulfate	1.25
Total volatiles	2.10
Impetus (megajoules/kg)	0.866
Flame temperature (°K)	2260
Mean molecular weight (kg/mole)	22.15

Exploratory propellant development efforts for future systems are being directed toward obtaining more energy from the propelling charge. The principal constraint imposed on these efforts is not to sacrifice the long barrel life which is obtained with the current NACO propelling charges. One approach, on which evaluation is now beginning, is to increase the propellant loading density of the charge. The use of a compacted or compressed propellant bed has been tested several times with some success in 30-mm and smaller caliber systems, but little work has been done in the calibers of interest to the Navy. It does offer the possibility of using moderate to low flame temperature propellants while still obtaining significant increases in the available energy.

[5] J. Rocchio, K. White, C. Ruth, and I. May, "Propellant Grain Tailoring to Reduce Pressure Wave Generation in Guns," *Proceedings of the 12th Annual Combustion Meeting*, CPIA Publication 273, December 1975, pp 275–301.

The second principal effort is on the development of low flame temperature, high impetus propellants. This work has been proceeding for several years, and the emphasis has been on the use of nitramine ingredients.[6, 7] Moderate to high levels of such materials in proper combination with coolants and binders result in decreased mean molecular weight combustion products. This decrease permits an increase in the impetus without a corresponding increase in the flame temperature. Table VI provides general information on the types of ingredients and thermodynamic properties of the propellant formulations evaluated. The effort has been plagued by combustion and burning rate problems. However, these difficulties do not appear insoluble and may yet permit the use of nitramine propellants in large caliber gun systems.

Table VI

NITRAMINE PROPELLANT CHARACTERISTICS

Typical Ingredients	
Nitrocellulose and rubber binders	
HMX, RDX, triaminoguanidine nitrate, and linear nitramines	
Di-normal-propyl adipate, isodecyl pelargonate	
Thermodynamic Properties	
Impetus	>1.076 megajoules/kg
Flame temperature	<2600° K
Mean molecular weight	<20.5 kg/mole

CONCLUSIONS

As demonstrated by this discussion, the Navy has selected the standard gun propellants in use today in order to obtain the greatest possible useable barrel life. For the future, the need for greater propellant energy is also constrained by the desire for low barrel wear rates. The introduction of monobloc, autofrettaged gun barrels will increase the fatigue limits and the number of rounds that can be fired with current systems. Thus it may be advisable to use wear reducing additives even with low flame temperature propellant charges in the near future. Thus, for the present and foreseeable future, even though increased performance is an important goal in the Navy, equal emphasis will be placed on long barrel life and the use of low flame temperature propellants to help achieve it.

[6] Air Force Armament Laboratory, Eglin AFB, Fla., *Study to Lower the Burning Rate Slope of an HMX Gun Propellant*, by M. S. Chang, A. J. Colli, J. Lucnso, and S. E. Mitchell, AFATL-TR-74-166, 15 November 1974.

[7] Naval Ordnance Station, Indian Head, Md., *Exploratory Development in Gun Propulsion: Fiscal Year 1975 Report*, by S. E. Mitchell, IHTR 443, 20 May 1976.

ADVANCED NITRAMINE PROPELLANT FORMULATIONS FOR TANK AMMUNITION

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BACKGROUND

The increased effectiveness of advanced gun weapons is, to an important extent, dependent upon the development of propellants with increasingly higher force levels. Conventional nitrocellulose-based propellants have inherent restrictions in this area, due, in part to the energy limitations of available materials and in part, to the high flame temperatures associated with these compositions in the 370,000-390,000 $ft\text{-}lb_f/lb_m$ range which are on the order of 3500-3700°K. Flame temperatures this high can have adverse effects upon barrel fatigue as well as barrel erosion. Polymers filled with high energy ingredients such as nitramines (e.g. RDX, HMX) appear to offer the most attractive route to achieving high force propellants in combination with relatively low flame temperatures. This paper will briefly identify and discuss pertinent parameters related to formulating a high force nitramine-containing propellant (Reference 1). Most importantly, the essential task has been to identify and to minimize the major causes of the high erosivity of nitramine compositions and to improve the burning properties long identified with these propellants (i.e., high pressure exponent of burning rate and low burning rate).

DISCUSSION

Thermodynamics/Formulations

Theoretical thermodynamic calculations were performed on nitramine-filled propellants which utilized inert elastomeric hydrocarbons, nitrocellulose and a combination of nitrocellulose and elastomeric binders. The inert elastomers used were: Hycar 2121-X-66, a polyacrylic rubber, LUSTRAN 420, an acrylonitrile-butadiene-styrene terpolymer and TUF-FLEX 721, a polystyrene-butadiene copolymer. Figure 1 shows a theoretical calculation for one of these elastomers (Hycar) filled with RDX. The experience with these systems showed that beyond a moderate force level (approximately 355,000 $ft\text{-}lb_f/lb_m$) RDX in excess of 85% would be needed to achieve more meaningful force levels. However, such high nitramine concentrations resulted in extremely brittle propellant and hence not fruitful for further pursuit.

In contrast with the combined elastomeric-nitramine formulations, compositions containing a nitrocellulose binder along with an inert plasticizer, dioctyl phthalate (DOP), alone and in combination with elastomeric binders, displayed force levels ranging from 364,000-402,400 ft-lb_f/lb_m at low (2551°K) to moderate (3108°K) flame temperatures. The nitramine levels for these formulations ranged from 58% to 80% (Tables 1 and 2). Subsequent indications of the high erosivity of several of these systems strongly suggested the wisdom of examing the thermodynamic potential of moderate level (35-40%) nitramine formulations. The partial substitution of liquid nitrate esters and/or alterntive solid energetic fillers for RDX/HMX were also explored. In general, the direct substitution of any of the conventional energetic materials for HMX or RDX was found to be at some expense to high force and/or flame temperature. However, by careful tailoring of some of these compositions, force levels ranging from approximately 364,000 to 400,000 ft-lb_f/lb_m were demonstrated to be feasible with flame temperatures limited to reasonable increases over the more highly filled nitramine systems (Tables 3 and 4). Predicated upon formulations listed in earlier Tables, an examination was conducted with a number of inert plasticizers with respect to their thermodynamic contributions to these systems in comparison to a formulation (PPL-A-6260) containing 5% DOP (Table 5). None of these inert plasticizers investigated offered both a flame temperature and force level advantage over DOP.

Ballistics

Strand burning rates from a number of HMX/RDX formulations show that increases in nitramine particle size produced higher burning rate (Figure 2). It is interesting to note that the influence of particle size is primarily manifested at the higher pressure levels. At pressures below 3000 psi, the formulation containing Class E HMX and Class B HMX have essentially the same burning rates; whereas the formulations containing Class A HMX actually shows a rate less than its Class E and Class B counterparts below 1500 psi. The effect of a number of additives (1% added) upon nitramine propellant burning, were examined in both a nitramine-elastomer formulation containing 84% Class E HMX and a nitramine-nitrocellulose formulation containing 58% RDX. For the most part, all these additives had minimal or no effect on the pressure exponent and offered no substantial increase in burning rate. However, LSTO (lead stannate-TDI oxidized), a sintered complex of lead stannate and tolylene diisocyanate was the most promising of the burning rate additives (References 2 and 3). Also examined for their potential benefit related to the burning properties of these propellants were a number of secondary oxidizers which were employed as a partial replacement for HMX/RDX; triaminoguanidine nitrate (TAGN), triaminoguanidine picrate (TAGP), nitroguanidine (NQ) and ammonium perchlorate (AP). TAGN appears to offer the best promise to significantly contribute to burning rate enhancement in the nitramine based propellant system.

Chemical Compatibility

The propellant compositions prepared for evaluation, were subjected to some combination of the following tests to assess their chemical stability: 120°C and 134.5°C Heat Test, 90°C and 100°C Vacuum Stability Test and 110°C Taliani Test. Results indicated that in general, these nitramine propellants offer good chemical stability, equal to or superior to M30 propellant.

Erosivity

One of the major factors that will determine the ultimate functionality of nitramine-filled systems for advanced gun applications is propellant erosivity. There is an erosion factor associated with the use of HMX or RDX that increases the overall erosive effect of compositions containing them. This results in higher erosion levels for these propellants than would have been anticipated from their much lower flame temperature as compared with conventional nitrocellulose based systems. While there has been generally wide awareness of this phenomena, there has been very little hard information as to what factors produce this effect and in what way they can be moderated or eliminated. In order to eliminate some of the important potential factors affecting the erosivity of these propellants, several formulations were prepared for evaluation on a laboratory erosion test apparatus. This device essentially consists of a closed bomb modified to accept a gun barrel containing one or more erosion sleeves (References 4 and 5). These sleeves are weighed before and after a series of tests and the loss in sleeve weight is a measure of propellant erosivity. One group of formulations was designed to examine the effect of nitramine concentration upon propellant erosivity; PPL-A-6210 contained 76% RDX, PPL-A-6161 had 58% RDX and PPL-A-6208 held 38% RDX. These propellants were designed to be as simple as possible to facilitate interpretation of the data. Four ingredients were used in each: nitrocellulose (12.6%N); Class E RDX, dioctylphthalate (DOP), an inert plasticizer; 2 nitrodiphenylamine, a stabilizer. All these formulations had identical flame temperatures (2729-2736°K) with the NC/DOP ratio adjusted to compensate for the differences in RDX. Erosivity results for these propellants are shown in Figure 3. The erosivity of M30 as recorded on the laboratory device is also shown for reference. The data show that propellant erosivity was lowered from 78 mg/shot at the high RDX concentration (76%) to 37 mg/shot at the 38% RDX level. However, all compositions showed much higher erosivity values than M30, despite having lower flame temperatures. Further tests on these formulations showed that a 2% addition of talc (an erosion reducing additive) in a talc/wax misture (Sierra Supreme talc/Shell 300 wax-45/55) was proportionately more effective at the lower RDX concentration. This information was quite revealing for two reasons: (1) since 2% talc is about the most that can be utilized in current gun systems, the high erosivity values, with or without talc, suggest that high concentrations of RDX/HMX are impractical for most gun applications; (2) the data suggests that a prudent use of RDX/HMX can yield effective propellants with acceptable erosivity levels. Also plotted in Figure 3 is erosivity data from a 58% RDX composition (PPL-A-6181) using a coarse granulation (Class G of RDX in place of Class E used in PPL-A-6161, showing an increase of approximately 20% erosivity where the Class E RDX was replaced with Class G RDX. Once it was established that lower nitramine concentrations produce more desirable erosivity values, a number of formulations possessing force levels of approximately 400,000 ft-lb_f/lb_m were prepared having moderate RDX concentrations (35-40%) and using nitroplasticizers and secondary oxidizers to provide supplemental energy. One of these formulations, PPL-A-6260, containing 36.5% RDX and 5% nitroguanidine (See Table 5) was tested in the erosion apparatus and yielded attractive erosivity data as shown on Figure 3. Based upon earlier data, the erosivity results for PPL-A-6260 were anticipated to be higher than the 37 mg/shot (the value for PPL-A-6208) since it had both a higher flame temperature and force level. However, this was not the case, erosivity results were 16 mg/shot for PPL-A-6260 (less than half that for PPL-A-6208) without an erosion reducer and 4 mg/shot using a 2% addition of talc in a talc/wax mixture. Consequently formulation PPL-A-6260 was selected as a representative high force nitramine formulation for calculation in a gun firing program. This composition has also been evaluated for erosivity in a modified 37mm gun with a blow out chamber at BRL. (See papers by J. R. Ward and A. Niiler for full discussions of BRL employed erosion testing methods and findings for this and other propellants). Formulation PPL-A-6260 was evaluated for erosion at a pressure of 28,000 psi at BRL, employing both a weight loss method and a radioactive tracer technique. Based upon the propellant's flame temperature and force level, it exhibited low wear and erosion properties. Should this high force propellant demonstrate increased erosivity at higher pressure, the use of an erosion reducing additive would still be a viable option in alleviating this problem.

<u>37mm Gun Performance of High Force Propellant (PPL-A-6260)</u>

The high force nitramine based propellant (PPL-A-6260), along with an M30 propellant are being evaluated in a 37mm gun at BRL at temperatures of 336°K (63°C), 294°K (21°C), and 235°K (-38°C). Initial firing results indicate that the high force propellant imparts the anticipated 2-3% increase in projectile velocity as compared with an M30 propellant. These firings also indicate that the high force propellant exhibits greater variability than M30 propellant, a parameter that will need further assessing.

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Table 1

Force and flame temperature comparison between M30 and elastomer-NC-RDX/HMX propellants

Propellants	T_V (^oK)	Force ($ft\text{-}lb_f/lb_m$)
M30	3040	364,000
NC-RDX		
PPL-A-6232	3108	399,900
PPL-A-6161	2729	366,900
HYCAR-NC-HMX		
Comp. 413-26	3003	391,500
Comp. 413-46-1	2752	364,500
LUSTRAN-NC-RDX		
Comp. 445-54-3	3035	401,700
Comp. 445-40	2601	364,200
TUF-FLEX-NC-RDX		
Comp 445-54-2	3032	402,400
Comp. 445-54-1	2551	364,300

Table 2

Composition of nitrocellulose and nitrocellulose-elastomer propellants containing high nitramine concentrations

Formulation	PPL-A-6161 wt. %	PPL-A-6232 wt. %	Comp. 413-26 wt. %	Comp. 413-46-1 wt. %	Comp. 445-54-3 wt. %	Comp. 445-40 wt. %	Comp. 445-54-2 wt. %	Comp. 445-54-1 wt. %
NC(12.6%N)	30.50	24.50	9.00	6.00	14.00	15.50	16.00	12.50
2-NDPA	0.50	0.50	0.50	0.30	0.50	0.50	0.50	0.50
HYCAR 2121-X-66	-----	-----	5.94	11.88	-----	-----	-----	-----
SALICYLIC ACID	-----	-----	0.06	0.12	-----	-----	-----	-----
LUSTRAN 420	-----	-----	-----	-----	7.50	7.00	-----	-----
TUF-FLEX 721	-----	-----	-----	-----	-----	-----	5.50	10.00
DOP	11.50	8.00	4.50	1.70	2.00	6.50	5.00	6.00
HMX (CLASS E)	-----	-----	80.00	80.00	-----	-----	-----	-----
RDX (CLASS E)	58.00	67.00	-----	-----	76.00	70.50	73.00	71.00

Table 3

Composition and thermodynamic properties of nitramine filled propellants containing nitroplasticizers

Formulation	PPL-A-6208 wt %	Comp. 481-28-1 wt %	Comp. 481-28-2 wt %	Comp. 481-28-3 wt %	PPL-A-6267 wt %	Comp. 481-29-1 wt %	Comp. 481-29-2 wt %	PPL-A-6273 wt %
NC (12.6%N)	53.2	55.2	38.2	38.2	32.3	28.8	31.4	32.8
2-NDPA	0.5	0.5	0.5	0.5	-----	-----	-----	-----
EC	-----	-----	-----	-----	1.5	1.5	1.5	1.5
LUSTRAN 420	-----	-----	-----	-----	-----	-----	5.6	5.2
NG	-----	-----	-----	-----	24.0	24.7	24.0	24.0
TEGN	-----	-----	-----	17.0	-----	-----	-----	-----
DEGN	-----	-----	15.0	-----	-----	-----	-----	-----
DOP	8.3	6.0	8.3	5.3	5.7	6.5	-----	-----
RDX (CLASS E)	38.0	38.0	38.0	39.0	36.5	38.5	37.5	36.5
FLAME TEMPERATURE (°K)	2734	2918	2765	2806	3339	3308	3330	3353
FORCE (1000 ft-lb_f/lb_m)	353.0	365.6	363.5	364.2	399.9	400.8	400.0	400.1

Table 4

Composition and thermodynamic properties of nitramine filled propellants containing nitroplasticizers and secondary oxidizers

Formulation	PPL-A-6272 wt %	PPL-A-6264 wt %	Comp. 481-28-4 wt %	PPL-A-6268 wt %	PPL-A-6269 wt %	PPL-A-6270 wt %	PPL-A-6271 wt %	Comp. 481-29-4 wt %
NC (12.6%N)	30.0	23.8	27.0	28.0	29.3	28.8	29.0	33.5
2-NDPA	-----	-----	-----	-----	-----	-----	-----	0.5
EC	1.5	1.5	1.0	1.5	1.5	1.5	1.5	-----
LUSTRAN 420	-----	-----	-----	5.0	-----	-----	-----	-----
NG	17.0	23.2	22.7	23.0	23.7	24.0	13.0	13.0
TEGN	-----	-----	-----	-----	-----	-----	-----	10.0
DOP	10.0	7.3	6.4	-----	4.0	3.7	-----	-----
NQ	5.0	18.0	18.0	5.0	5.0	5.0	20.0	10.0
TAGN	-----	12.0	-----	-----	5.0	-----	-----	-----
TACP	-----	-----	10.0	-----	-----	5.0	-----	-----
RDX (CLASS E)	36.5	14.7	14.9	37.5	31.5	32.0	36.5	33.0
FLAME TEMPERATURE (^{o}K)	2793	2794	2830	3330	3357	3376	3462	345[illegible]
FORCE (1000 ft-lb_f/lb_m)	366.0	364.4	369.2	400.3	401.0	399.8	401.4	399.0

Table 5

Effect of inert plasticizers on the force and flame temperature of an RDX-nitrocellulose propellant

PPL-A-6260 (STD):

NC (12.6%N)	29.3%
EC	1.5%
NG	22.7%
DOP	5.0%
NQ	5.0%
RDX (CLASS E)	36.5%

Plasticizer replacing DOP	T_V (^oK)	Force ($ft\text{-}lb_f/lb_m$)
STANDARD	3339	400,100
Butyl laurate	3277	399,400
Butyl stearate	3260	399,300
Castor oil	3311	399,900
Diallyl phthalate	3450	401,500
Diamyl phthalate	3367	399,200
Dibutyl phthalate	3386	399,600
Dibutyl sebacate	3318	398,200
Dibutyl tartrate	3422	399,400
Dicyclohexyl phthalate	3365	398,400
Diethyl phthalate	3427	398,900
Diethyl succinate	3428	398,400
Dihexyl adipate	3288	394,600
Diisobutyl adipate	3358	399,800
Dimethyl sebacate	3375	399,600
Di-n-propyl adipate	3381	400,300
Diphenyl phthalate	3427	398,600
Ethyl lactate	3435	399,400
Tricresyl phosphate	3451	402,300
Triethylene glycol diacetate	3421	396,100

Table 5 (continued)

Plasticizer replacing DOP	T_V (^oK)	Force ($ft\text{-}lb_f/lb_m$)
Triethylene glycol dihexoate	3398	400,700
Triethylene glycol di-2-ethylhexoate	3356	400,600
Triphenyl phosphate	3468	404,800
Tripropionin	3415	397,000

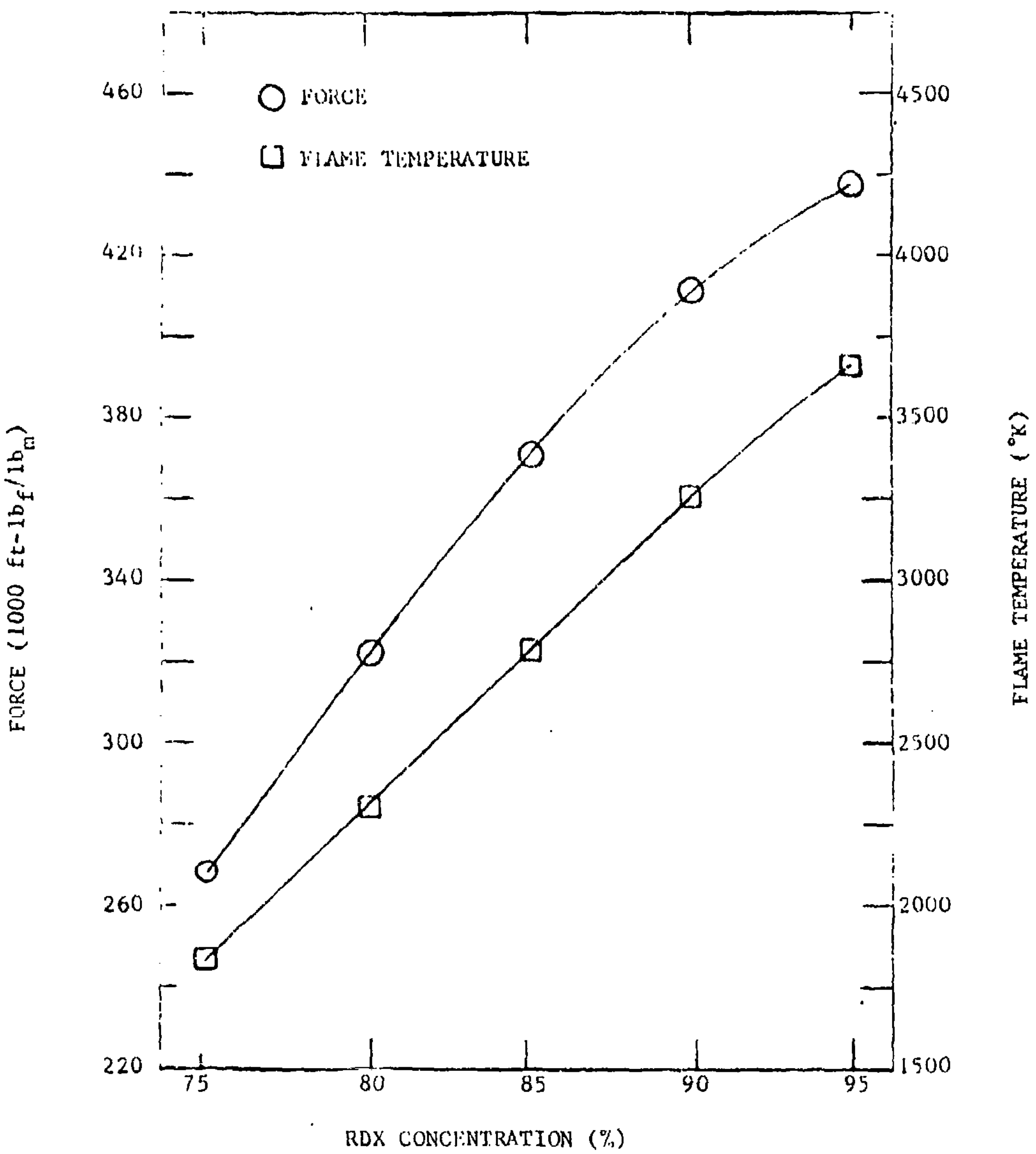

Fig 1 Theoretical force and flame temperature values for Hycar-RDX formulations

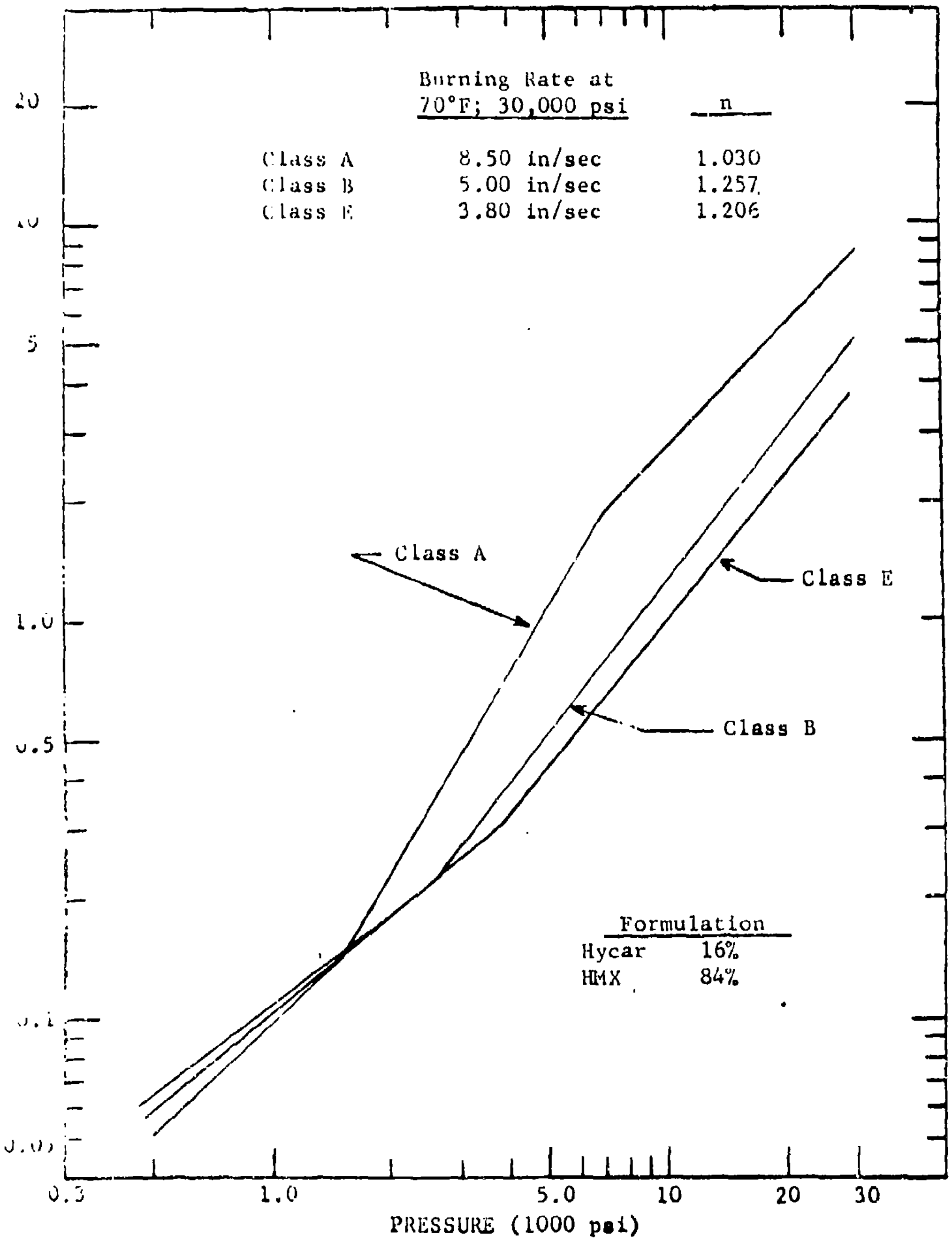

Fig 2 Effect of HMX particle size on the burning rate of a Hycar-HMX propellant

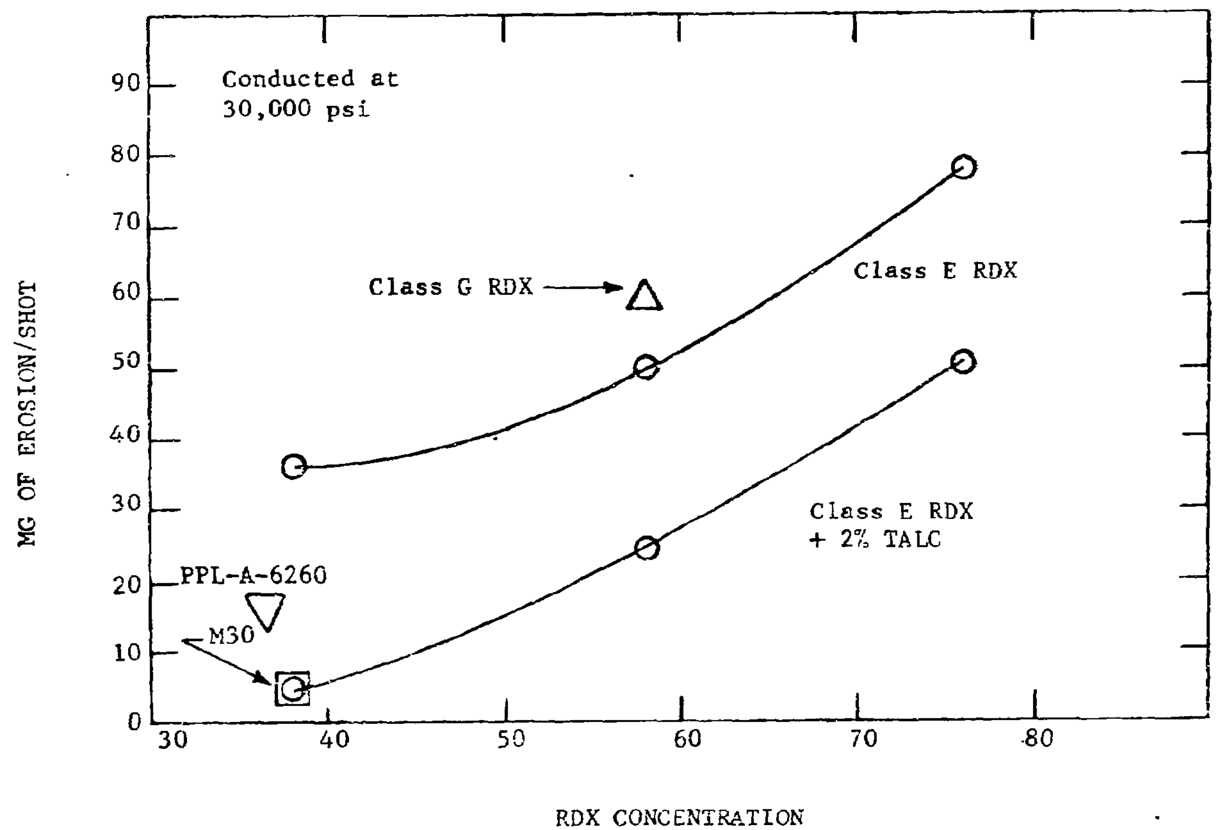

Fig 3 Erosivity data on RDX-filled artillery propellants

EFFECT OF WEAR-REDUCING ADDITIVES ON HEAT TRANSFER INTO THE 155mm M185 CANNON

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ABSTRACT

Heat transfer measurements were made in a 155mm M185 cannon equipped with fast response thermocouples. The thermocouples were constructed by welding thin constantan wires onto gun steel. The thermocouples were placed 101cm from the rear face of the tube at four different distances from the bore surface. From measurements of the temperature rise at 100ms, the total heat input to the gun barrel at 101cm RFT was determined. In addition to temperature measurements, the ignition delay, chamber pressure and muzzle velocity were determined for each round. In selected rounds the initial negative differential pressure was also determined.

Heat transfer measurements were made with the base-ignited XM201E2 charge and the center-core ignited XM201E1, XM119E4, XM203E2, and M119 charges. The heat transfer results suggested that the wear-reducing liner in the XM201E2 charge did not exert any influence on heat transferred to the barrel. It was noticed that the XM201E2 charge had an ignition delay of more than 200ms. By shortening the ignition delay the wear-reducing liner in the XM201E2 charge reduced heat input in the same fashion as the center-core ignited XM201E1 and XM119E4 charges. The wear-reducing liner in the XM203E2 charge was the most effective.

Despite the reduction in heat transfer for the XM201E2 with the shorter ignition delay, the heat input is still significantly greater than the heat input of the M119 charge. It was found that the addition of an extra liner of TiO_2/wax to the XM201E2 charge with a black powder igniter resulted in heat input comparable to the M119. The addition of the extra liner to the XM203E2 charge reduced the heat input in like fashion. This suggests the possibility that the wear life of both the zone 7 XM201E2 charge and the zone 8 XM203E2 charge can be increased by modifying the additive.

Temperature measurements and initial negative differential pressures were taken for M6 and M15 propellant versions of the XM201E2 charge. For the M6 version, heat input was similar to the M119 charge and no negative differential pressures were observed at 25mm stand-off. The M15 version had ignition delays greater than 300ms which meant that significant preheating occurred. Nonetheless, it is felt that the wear life of the M15 version should be similar to the M119 charge.

INTRODUCTION

The 155mm propelling charge, XM201E2, is in the latter stages of engineering development as a replacement for the M119 propelling charge. Among the requirements set for acceptance of the XM201E2 charge is that the wear life of the gun tube must not be reduced. Since the XM201E2 charge is composed of triple-base M30A1 propellant while the M119 charge consists of single-base M6 propellant, the developers of the XM201E2 charge included a TiO_2/wax wear-reducing liner to try to insure that the wear life of both charges would be the same. During the wear test of the XM201E2 charge, it was soon evident that the XM201E2 charge was more erosive than the M119 charge.

A hypothesis was tendered that the wear-reducing liner in the XM201E2 charge was not exerting any influence on the wear rate. In order to test this hypothesis and to suggest ways to reduce the erosiveness of the XM201E2 charge, heat transfer measurements were made in a 155mm M185 cannon firing various propelling charges equipped with and without wear-reducing additives. Similar measurements were performed previously in a 37mm gun and in the 105mm M68 tank cannon(1,2). In both guns the wear-reducing additives reduce the erosion rate of the cannon, and the heat transfer measurements detected significant differences in the total heat transferred to the gun barrel in the presence of the wear-reducing additive.

EXPERIMENTAL

Temperature distributions in the M185 cannon were measured by means of four thermocouples inserted at different radial distances from the bore surface, but all were located at the same axial distance from the rear face of the tube. The thermocouples were made by spot-welding 0.13mm diameter constantan wires onto the gun steel. A detailed description of the technique has been previously published(3); of particular note is the care needed to measure properly the distance from the constantan-steel junction to the bore surface.

The test firings were conducted with cannon serial number 22541 from which 730 rounds had been previously fired. The constantan wires were placed 90° apart at a distance of 101cm (39.6 inches) from the rear face of the tube. The four thermocouples were 0.83, 1.0, 1.5, and 2.6mm from the bore surface (corresponding to 32, 41, 60, and 102 mils from the bore surface), and each thermocouple was placed over a groove. The chamber pressure was measured with a 607C Kistler gage located in the spindle. For selected rounds differential pressures were measured with an additional 607C Kistler gage located at 85.5cm (33.6 inches) from the rear face of the tube. The velocity of the projectiles was measured with two coils a known distance apart. The distance from the muzzle of the gun to the first coil was 25.3m.

Table I summarizes pertinent characteristics of the propelling charges used in this investigation. The two charges with M1 propellant were used as "clean-out" rounds. All charges were conditioned overnight at 21°C (70°F).

The firing sequence is listed in the Appendix. All rounds equipped with TiO_2/wax liners were followed by clean-out rounds. Clean-out rounds were also fired to start each morning and afternoon's firing. The zone 7 charges were fired with a 6mm stand-off except where noted. All XM203E2 firings had a 25mm stand-off.

Sufficient M107 projectiles were unavailable, therefore, M107 projectiles modified for firing at zone 8 had to be used instead. Standard M107 projectiles were fired with the XM201E2 and the M119 charges.

The rationale behind the firing sequence in Appendix is summarized below by listing objectives and the tests made to meet those objectives:

Objective	Tests
1. Compare heat input for the XM201E2 charge with the M119 charge.	XM201E2 and M119 charges with both M107 and M107 modified projectiles.
2. Measure influence of wear-reducing additives on heat transfer.	XM201E1, XM201E2, XM119E4, and XM203E2 charges with and without wear-reducing liners.
3. Test the influence of the projectile.	XM201E2 charge with the M107, M107 modified, M549 RAP, and the M483A1 projectiles.
4. Compare heat input of base-ignited M6 and M15 propellant with the center-core ignited M119.	Base-ignited M6 and M15 charges were fabricated and fired.
5. Test the effect of ignition delay.	Clean-burning igniter in the XM201E2 charge replaced with black powder igniter from the M4A1 charge.
6. Attempt to reduce heat input of XM201E2 charge.	XM201E2 charge with flaps in the TiO_2/wax liner with ablative coolant, and with a TiO_2/wax liner against the chamber wall.

Table II correlates these test objectives with the individual tests in the Appendix.

Table I. Pertinent Characteristics of Propelling Charges

Designation	Lot No.	Propellant	Ignition Mode	Wear-Reducing Additive	Dia, cm	Length/diameter
XM201E2	IND-E-140-74	M30A1	base, CBI[a]	TiO_2/wax, two-piece	13.0	5.6
XM201E1	IND-E-105-73	M30A1	center-core, CBI, benite strands	TiO_2/wax, two-piece	15.5	3.8
M119	IA-B-39740A	M6	center-core, CBI, benite strands	None	15.5	4.0
XM119E4	RAD-64654	M30A1	center-core, CBI, benite strands	TiO_2/wax, one-piece	15.5	3.2
XM203E2	PA-E-09612	M30A1	center-core, BP[b]	TiO_2/wax, one-piece	15.5	4.9
M4A1	IA-39519-57	M1	base, BP	None	-	-
M4A2	RAD-69383	M1	base, CBI	None	-	-
XM201E2-M6	IND-E-140-74	M6	base, CBI	None	14.0	5.0
XM201E2-M15	IND-E-140-74	M15	base, CBI	None	14.0	5.3

[a] Clean burning igniter

[b] Black powder igniter

Table II. Correlating ID Numbers and Program Rationale

Charge	Projectile	Modification	ID Numbers
XM201E2	M107	None	64, 72, 87
M119	M107	None	99, 99A
XM201E2	M107 mod	None	53, 66, 74
M119	M107 mod	None	58, 79, 98
XM201E2	M549	None	83, 103
XM201E2	M483A1	None	85, 105
XM201E2	M107 mod	2.5cm stand-off	151, 153
XM201E2	M107 mod	w/o liner	52, 62, 70
XM201E2	M107 mod	w/o liner, BP igniter	91, 92
XM201E2	M107 mod	w/o liner, ablative coolant	88, 89
XM201E2	M107 mod	w/o liner, TiO_2/wax cap	107, 109
XM201E2	M107 mod	BP igniter	94, 101
XM201E2	M107 mod	BP igniter, TiO_2/wax cap	132, 134
XM201E2	M107 mod	flaps	118, 122
XM201E2	M107 mod	flaps, BP igniter	128, 130
XM201E2	M107 mod	M6 propellant	112, 116, 120
XM201E2	M107 mod	M6 propellant, 2.5cm stand-off	148, 150
XM201E2	M107 mod	M15 propellant	113, 117, 121
XM201E2	M107 mod	M15 propellant, 2.5cm stand-off	147, 149
XM201E1	M107 mod	None	56, 77, 96
XM201E1	M107 mod	w/o liner	55, 71, 76
XM119E4	M107 mod	None	60, 68, 81
XM119E4	M107 mod	w/o liner	59, 63, 80
XM203E2	M107 mod	None	137, 141
XM203E3	M107 mod	w/o liner	139
XM203E2	M107 mod	TiO_2/wax cap	143

Three modifications were made to the XM201E2 to try to reduce the heat input to the barrel. Two modifications were based on previous experience with the TiO_2/wax liner in the 105mm M68 tank cannon. In contrast with the liner in the XM201E2 charge, the TiO_2/wax liner in the M392 APDS round rests flush against the chamber (in this situation, the cartridge case), the liner has flaps folded over the forward end of the propelling charge, the TiO_2/wax side of the liner faces the propellant, and the additive rests against the base of the projectile. The first modification to the XM201E2 liner was to incorporate flaps on the forward end of the zone 7 segment of the XM201E2 charge. The second modification consisted of the construction of a TiO_2/wax liner with the same diameter as the chamber, was equipped with flaps, and was pushed against the rear of the projectile. This modification is referred to as a TiO_2/wax "cap." The cap was made from two TiO_2/wax liners from the 105mm tank cannon. The flaps were lengthened to 9cm; the TiO_2/wax cap was 32cm long including the flaps and it weighed 0.24kg (0.53 pounds). The third modification was to eliminate the TiO_2/wax liner and replace the liner with "ablative coolant," a gelled silicone grease developed by Calspan Corp. A liner of gelled silicone was made by spooning 0.45kg (1.0 pounds) of gelled silicone into a polyethylene bag. The liner was approximately 25mm x 25mm and it was taped to the forward end of the propelling charge.

RESULTS

The estimate of the net heat input was made from the temperature measurements at 100 milliseconds from initial pressure rise. At this time the propellant gases are no longer heating the barrel and axial conduction should be negligible.

The total heat in a given volume of the gun barrel is given by

$$Q = \rho C V \Delta T, \tag{1}$$

where Q = heat input into the barrel
ρ = density of gun steel,
C = specific heat of gun steel,
V = volume,
ΔT = temperature rise of gun steel in volume element, V.

Since the temperature in the gun barrel varies with radial distance into the gun tube, the net heat input into the gun barrel is computed as follows. Equation (1) is recast in differential form as:

$$dQ = \rho C \Delta T dV. \tag{2}$$

For a unit axial length of one millimeter, the temperature is assumed to vary in the radial direction only, therefore

$$dQ = \rho C \ell \Delta T dA. \tag{3}$$

For a hollow cylinder of unit axial length,

$$dQ = \rho C 2\pi r \Delta T dr, \tag{4}$$

and

$$Q = 2\pi\rho C \int_{r_i}^{r_o} r \Delta T dr, \tag{5}$$

where r_i and r_o are the inside radius and outside radius of the gun barrel, respectively, and ΔT is the temperature rise at a distance r into the tube wall. The integral in Equation (5) is solved graphically by visually fitting a smooth curve through the four available values of $r\Delta T$ vs r and then measuring the area under the curve. The values of the density and specific heat of gun steel used in Equation (5) are $7.85 g/mm^3$ and 0.419 J/g-K.

Table III lists the heat transfer results for all firings. A single plot of $r\Delta T$ vs r was made for replicate firings using mean values of ΔT at each r. The XM201E2 charges with the clean-burning igniter had the widest variation in ΔT because of variations in ignition delay which resulted in different amounts of preheating. For the XM201E2 charges, plots of $r\Delta T$ vs r were made for each firing. The values of Q ranged from 820J to 780J. From a single plot of $r\Delta T$ vs r using the mean value of ΔT, Q equalled 813J.

The vertical wear in the grooves at 101cm RFT (39.6 in) was negligible while the wear in the horizontal direction was less than 0.5mm. The vertical land wear was much more significant (2.3mm) especially considering the number of M4A2 and M4A1 charges included in the ninety-nine rounds fired in the course of these tests. These charges cause negligible erosion.

DISCUSSION

The primary objective of these tests was to see if the wear-reducing additive in the base-ignited XM201E2 propelling charge was exerting any influence on the heat transferred to the gun barrel near the origin of rifling. As the results in Table III indicate, the wear-reducing liner does not reduce heat transfer significantly in this charge. In Table IV the effect of the wear-reducing liners for the XM201E2 charge and three center-core ignited charges is summarized along with previous measurements from the 105mm M68 tank cannon firing the M392 APDS round(2). The polyurethane foam liner in the M392A2 round reduced the wear rate from 0.017mm/round to 0.005mm/round(4). For the four 155mm propelling charges, the XM203E2 charge had the largest reduction in heat transfer. Table V compares some physical characteristics of the 155mm charges.

Table III. Summary of Heat Input to the M185 Cannon for Various 155mm Propelling Charges[a]

Charge	Modification	Q, J
XM201E2	None	813
XM201E2	w/o liner	824
XM201E1	None	701
XM201E1	w/o liner	750
XM119E4	None	702
XM119E4	w/o liner	764
XM203E2	None	702
XM203E2	w/o liner	793
M119	None	677
M6 version XM201E2		697
M15 version XM201E2		787
XM201E2	flaps	770
XM201E2	M4A1 igniter	712
XM201E2	M4A1 igniter, w/o liner	764
XM201E2	w/o std liner; TiO_2/wax cap	762
XM201E2	flaps, M4A1 igniter	721
XM201E2	M4A1 igniter std liner & cap	671
XM203E2	std liner and cap	651
XM201E2	M107 standard	764
XM201E2	M549	764
XM201E2	M483	762
XM201E2	Ablator	804
M119	M107 standard	677

[a]M107 modified projectiles unless otherwise noted.

Table IV. Effect of Wear-Reducing Liners on Heat Transferred to Gun Barrel

Charge	Q, J, with liner	Q, J, no liner	percent reduction
XM201E2	813	824	1
XM119E4	702	764	8
XM201E1	701	750	6
XM203E2	702	793	11
M392A2 round	372	426	13

Table V. Correlation Between Heat Transfer Results and Some Characteristics of the Propelling Charges

Charge	Propellant mass, kg (lbs)	Additive mass kg (ozs)	Charge length, cm (in)	Diameter cm (in)	Igniter	Ignition delay, ms	Q, J, no liner	Q, J, liner
XM201E2	7.80 (17.2)	0.23 (9.5)	71.6 (28.2)	13.0 (5.1)	base	210 (av of 6)	824	813
XM201E1	7.80 (17.2)	0.23 (9.5)	58.9 (23.2)	15.5 (6.1)	center-core	137 (av of 6)	750	701
XM119E4	7.94 (17.5)	0.31(11.0)	52.1 (20.5)	15.5 (6.1)	center-core	225 (av of 5)	764	702
XM203E2	11.8 (26.1)	0.50(17.5)	76.2 (30.0)	15.5 (6.1)	center-core	81 (av of 5)	793	702

Since marked heating of the gun barrel was noted during the ignition delay of the XM201E2 charge, experiments were done with the faster burning black powder igniter from the M4A1 charge in place of the clean-burning igniter on the XM201E2. These results are summarized in Table VI. It is clear the total heat absorbed by the gun is reduced in the presence of the faster-burning igniter. Another interesting point is that the wear-reducing liner in the XM201E2 now exerts significant influence on the heat input to the barrel (764 J for no liner to 712 J with the liner). In addition the heat input measured for the zone 8, XM203E2 charge, without liner, is now higher than the XM201E2 charge with a comparable ignition delay (793 for the XM203E2 vs 764 for the XM201E2). Nonetheless, the wear-reducing liner in the XM203E2 charge is still more effective at reducing the heat transfer (11 percent for the XM203E2 vs seven percent for the XM201E2 with the M4A1 igniter).

Three separate modifications were tried to improve the wear-reducing capability of the TiO_2/wax liner in the XM201E2. From Table VII one sees that the addition of flaps lowers the heat transfer to the gun barrel, but the addition of flaps to the XM201E2 charge with the fast-burning igniter does not further reduce the heat input to the barrel.

Table VI. Effect of Ignition Delay on Heat Transfer with the XM201E2 Charge

Charge	Igniter	Ignition Delay, ms	Q, J	Q, J, no liner
XM201E2	CBI	210 (av of 6)	813	824
XM201E2	BP	78 (av of 4)	712	764

Table VII. Effect of Addition of Flaps to the TiO_2/Wax Liner in the XM201E2 Charge

Modification	Ignition delay, ms	Q, J
None	210 (av of 6)	813
M4A1 igniter	78 (av of 4)	712
Addition of flaps	300, 172	770
Addition of flaps + M4A1 igniter	110, 88	721

Another modification tested to try to duplicate the liner in the 105mm tank cannon round was the addition of wear-reducing liner that fit snugly against the chamber wall, was equipped with flaps, had the TiO_2/wax side facing inward, and was placed against the projectile base, which is referred to as the TiO_2/wax cap. Results for the TiO_2/wax "cap" are presented in Table VIII. The TiO_2/wax cap was tested with unmodified XM203E2 and XM201E2 charges to also see the effect of adding additional wear-reducing additive. The addition of the cap to the XM201E2 charge without any liner was about equivalent to adding flaps to liner in the unmodified XM201E2 charge. For both the black powder ignited XM201E2 charge and the XM203E2 charge, the addition of the TiO_2/wax cap afforded an additional 6-7 percent reduction in heat transfer. Whether this additional reduction in heat transfer is due to the extra quantity of additive or to the placement of the TiO_2/wax liner with the cap cannot be ascertained.

Table VIII. Effect of TiO_2/Wax "Cap" on Heat Transferred to the Barrel

Charge	Modification	Q, J
XM201E2	None	813
XM201E2	w/o liner	824
XM201E2	w/o liner, TiO_2/wax cap	762
XM201E2	M4A1 igniter	712
XM201E2	M4A1 igniter + TiO_2/wax cap	671
XM203E2	w/o liner	793
XM203E2	None	702
XM203E2	TiO_2/wax cap	651

As one studies these results, it seems that the preheating of the gun barrel prior to propellant ignition interferes with the wear-reducing capability of the additive, but may not increase the erosivity of the propelling charge. If this is true, then comparison of the total heat inputs to determine erosivity are biased by the inclusion of the preheating. The reason for suspecting that the preheating may not be important is the high value of Q associated with the M15 charge. If direct comparison of Q were a true measure of erosivity, then the M15 charge would be expected to be more erosive than the zone 8, XM203E2 charge containing M30A1 propellant. Also, the XM201E2 charge would be predicted to have a higher wear rate than the XM203E2 charge without any liner. As noted previously, if one compares the XM201E2 charge with black powder to the XM203E2, then the expected trend is followed, namely, the XM203E2 without liner having a higher value of Q than the XM201E2.

Under this assumption the heat input data for the XM201E2 with the M4A1 igniter are the proper values to compare with data for the other charges. The heat inputs for the various charges then fall into the classes shown below:

	Charge	Q, J
I.	XM203E2 w/o liner	793
II.	XM201E1 w/o liner	750
	XM201E2 w/o liner, M4A1	764
	XM119E4 w/o liner	764
III.	XM201E1	701
	XM201E2, M4A1	712
	XM119E4	702
	XM203E2	702
IV.	M119	677
	XM201E2, M4A1, TiO_2/wax cap	671
	XM203E2 + cap	651

The implication of this grouping is that the wear rate of the XM201E2 is of the order XM201E1 and XM119E4 without liners. In the M126 cannon, the XM119E4 charge without liner had a wear life of approximately 700 rounds. The next point is that the reduction in the ignition delay of the XM201E2 will make the TiO_2/wax effective in reducing heat transfer to the gun barrel, but the increase in wear life will be comparable to that experienced for the XM119E4 with additive, namely, a three-fold improvement. This will still not be in the range of heat input measured for the M119 charge. Another point to notice from Group III is that the wear-reducing liner in the zone 8, XM203E2 reduces the heat input such that the wear life of the zone 8 charge should be greater than the zone 7 XM201E2 charge.

Some limited erosion measurements are available to compare with the trends observed from the heat transfer tests. Such results are summarized in Table IX. These results support the contention that the wear life rate of the XM201E2 charge with its TiO_2/wax liner is similar to the XM119E4 charge without its wear-reducing liner. These results also point to the wear rate of the XM203E2 charge being less than the XM201E2 and the XM119 charges, but the wear rate of the XM119E4 which has a wear-reducing liner. Finally, the wear rate of the M119 charge is the smallest as expected from the heat transfer measurements. Thus, the groupings of the charges suggested by the heat transfer measurements are consistent with some limited erosion data.

Table IX. Summary of Erosion Data

Charge	Wear, cms/number of rounds	Wear Rate, cm/round	Cannon
XM201E2	0.13/500	2.6×10^{-4}	M185
XM119	0.14/660	2.8×10^{-4}	M126
XM119E4	0.11/1010	1.1×10^{-4}	M126
XM203E2	0.074/522	1.4×10^{-4}	XM199
M119	0.089/1000	0.9×10^{-4}	M185

The liner in the zone 8 charge is more efficient than in the zone 7 charges, since the XM203E2 without liner places 793J into the barrel vs approximately 760J for the group II charges, yet the heat input for the XM203E2 charge is virtually the same as the XM119E4 charge. From the data in Table V, some comparisons between the zone 7 and zone 8 charges can be made. The diameter of the XM119E4 and XM203E2 charges is the same; the ratio of the weight of the additive to the weight of the propellant is similar (0.042 for the XM203E2 to 0.039 for the XM119E4); the major difference is the length of the charge. The XM119E4 charge is 24cm shorter than the XM203E2 charge, thus the wear-reducing liner in the XM203E2 charge is much closer to the projectile base than is the liner in the XM119E4 charge. Previous results in the 105mm M68 cannon noted that the closer the liner was positioned to the projectile the lower the measured heat input.

The results with the TiO_2/wax cap added to the XM203E2 charge and the XM201E2 charge equipped with the black powder igniter indicate that these two charges should have similar wear rates as the M119 charge.

The results for the charges containing single-base M6 and a so-called "cool" triple-base M15 propellant are listed in Table X. Clearly the heat input from the XM201E2 charge is considerably greater than the M119 charge. The base-ignited M6 charge is slightly higher than the M119 charge, but it is significantly less than the XM201E2 charge. The triple-base M15 charge is markedly higher than the M6 propellant charges due presumably to the considerable preheating. On the basis of these heat transfer tests, the base-ignited M6 would be expected to have a similar wear rate to the M119 charge.

Table XI lists initial negative differential pressure measurements. For a 25mm stand-off distance, no negative differential pressures occur for the base-ignited M6 charge. A negative differential pressure of 8.6 MPa was noted for one of the two M15 charges fired with a 25mm stand-off.

Table X. Heat Transfer Measurements with M6 and M15 Propellant in the Propelling Charges

Charge	Propellant mass,kg Ignition Mode	Ignition delay,ms	Q, J
M119	center-core	170 (av of 3)	677
XM201E2 w/M6	base-CBI	237 (av of 3)	697
XM201E2 w/M15	base-CBI	346 (av of 3)	787
XM201E2 (unmodified)	base-CBI	210	813

Table XI. Initial Negative Differential Pressure Measurements for the M6, M15 Propellants and the XM203E2 Charge

ID	Charge	ΔP, MPa, (psi)
112	M6, 6mm stand-off	23.5, (3406)
116	M6, 6mm stand-off	3.4, (493)
120	M6, 6mm stand-off	4.0, (575)
148	M6, 25.4mm stand-off	0 , 0
150	M6, 25.4mm stand-off	0 , 0
113	M15, 6mm stand-off	8.5, (1231)
117	M15, 6mm stand-off	0 , 0
121	M15, 6mm stand-off	5.9 (862)
147	M15, 25.4mm stand-off	8.6, (1247)
149	M15, 25.4mm stand-off	0 0
137	XM203E2	0 0
139	XM203E2, w/o liner	4.2, (616)
141	XM203E2	0 0
143	XM203E2 + TiO_2/wax cap	0 0
145	XM203E2	2.3 (328)

CONCLUSIONS

1. The wear-reducing liner in the XM201E2 charge does not reduce the heat input to the gun barrel.

2. The failure of the wear-reducing liner in the XM201E2 charge to exert any influence on heat transfer may be attributed to the ignition delay with the clean-burning igniter. This could result in the liner beginning to melt, or more likely this could preclude the formation of the cool boundary layer.

3. The liner in the center-core ignited XM201E1, XM119E4, and the XM203E2 charges reduce the heat input to the gun barrel from six to eleven percent. The greatest reduction is observed with the XM203E2 charge. Since the XM203E2 charge is longer than the zone 7 charges, the wear-reducing liner in the XM203E2 charge is closer to the projectile than is the liners in the zone 7 charges.

4. The heat input from the XM201E2 charge can be reduced to comparable values for the XM201E1 and XM119E4 either by shortening the ignition delay or by placing the wear-reducing liner against the chamber wall adjacent to the projectile. Such solutions by themselves would be expected to yield a wear life comparable to the XM119E4 or the XM203E2, but still less than the wear life of the M119 charge.

5. The addition of a wear-reducing liner equipped with flaps placed against the projectile base in conjunction with a shorter ignition delay and the existing liner in the XM201E2 charges reduces the heat input to a value comparable to the M119 charge. The addition of the extra liner to the XM203E2 charge substantially reduces the heat input for this charge as well. It appears that it is possible to design a zone 7 and zone 8 charge with wear rates comparable to the M119 charge.

6. The version of the XM201E2 charge containing M6 propellant had similar heat input as the center-core ignited M119 charge. Significant preheating occurred with the M15 charge. With comparable ignition delay, one would expect the M15 charge to have the same heat input as the base-ignited M6 version. No negative differential pressures were observed for the base-ignited, M6 charges with a 25mm stand-off.

7. No significant differences in heat input were observed when different projectiles were fired with the XM201E2 charge.

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APPENDIX

Firing Sequence

ID	Round Number	Charge	Projectile	Ignition delay, ms	Chamber Pressure MPa, (kpsi)	Coil Velocity, m/s, (ft/s)	Remarks
50	1	M4A1	M107 mod			557.5 (1829)	
51	2	M4A1	M107 mod			558.1 (1831)	
52	3	XM201E2 w/o liner	M107 mod	185	215 (31.2)	676.0 (2218)	
53	4	XM201E2	M107 mod	205	216 (31.4)	678.5 (2226)	
54	5	M4A2	M107 mod			562.1 (1844)	
55	6	XM201E1 w/o liner	M107 mod	88	215 (31.2)	681.2 (2235)	
56	7	XM201E1	M107 mod	106	221 (32.1)	681.8 (2237)	
57	8	M4A2	M107 mod			559.0 (1834)	
58	9	M119	M107 mod	216	188 (27.3)	[illegible].0 (2162)	Lacing Jacket Removed
59	10	XM119E4 w/o liner	M107 mod	237	221 (32.1)	690.1 (2264)	
60	11	XM119E4	M107 mod	418	219 (31.8)	693.7 (2276)	

USE OF INORGANIC WEAR REDUCING ADDITIVES TO PROVIDE INCREASED 7.62mm BARREL LIFE WITH SINGLE BASE EXTRUDED PROPELLANTS

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ABSTRACT

Through use of inorganic wear reducing additives incorporated in the propellant base grain, the barrel life obtained with single base, extruded IMR 8138M type propellant assembled in 7.62mm M80 ammunition has been doubled. The increased barrel life is considerably greater than that obtained with the current standard WC 846 ball propellant.

The wear-reducing additives, talc, molybdenum trioxide, and calcium carbonate, were each incorporated in the propellant base grain at a nominal 0.5% level. Barrel life tests were fired in accordance with the NATO erosion schedule with each ammunition type fired in triplicate. The results are as follows:

Wear Reducing Additive in IMR 8138M Type Propellant	Average Barrel Life (Rounds)	Relative Barrel Life
None (Control)	7750	1.00
Talc	8460	1.09
MoO_3	10,420	1.34
$CaCO_3$	15,750	2.03

Changes in velocity, cyclic rate and bore dimensions were monitored during the tests and the data obtained are presented and analyzed.

INTRODUCTION

As a result of a comprehensive coordinated experimental program conducted during the last five years, our understanding of the phenomena associated with barrel fouling, erosion and barrel life in small caliber weapons has increased greatly. A sequence of investigations, of which the one discussed in this paper is an element, has revealed among other things, that calcium carbonate plays an important but heretofore unsuspected role in reducing barrel erosion in the 7.62mm system.

Calcium carbonate was originally used in the ball propellant process to neutralize any acid which might possibly have been absorbed on the nitrocellulose fibers during nitration and subsequently released when the fibers were dissolved to form the lacquer in the hardening process. The presence of acidic materials in the finished propellant would adversely affect long term storage stability.

This additive was subsequently found to cause severe gas tube fouling in the 5.56mm, M16A1 rifle.[1]* As a result, the permissable calcium carbonate content in 5.56mm ball propellant was reduced from a maximum of 1.0% to 0.25%. Long term stability tests demonstrated that ball propellant stability was not impaired by this reduction. Since 5.56mm and 7.62mm ball propellants are made in the same process, reduction of calcium carbonate levels in 5.56mm necessarily imposed a similar reduction in the 7.62mm propellant. Subsequent studies indicated that a lowering of the calcium carbonate concentration in ball propellants increased origin erosion in the 5.56mm rifle[2] raising the suspicion that barrel life with ball propellant in the 7.62mm machine gun might be adversely affected by this reduction.

This suspicion was supported by other information. In the early 1950's considerably greater barrel life had been obtained with ball propellants than with corresponding extruded propellants in caliber .50 and caliber .60 systems. This was attributed primarily to the lower adiabatic flame temperatures of the ball propellants. With the introduction of cooler burning formulations in which methyl centralite or ethylene dimethacrylate deterrent coatings replaced dinitrololuene, 7.62mm extruded propellants emerged that had flame temperatures similar to those of the corresponding ball propellant. However, the ball propellant still yielded longer barrel life.

A two-fold approach to study the effect of calcium carbonate, as well as other inorganic additives, upon 7.62mm barrel life was therefore initiated. One was directed toward assessing the effect of calcium carbonate reductions in 7.62mm ball propellants upon barrel life. The results of that study indicated, clearly, that lowering the calcium carbonate level severely reduced 7.62mm barrel life.[3] The second line of investigation, which is the subject of this paper, was directed toward determining whether addition of inorganic additives, e.g., calcium carbonate, would increase barrel life with extruded 7.62mm single base IMR 8138M type propellant. The other inorganic additives selected for study in this program were talc which was previously shown to reduce barrel wear in 20mm [4,5,6,7] and molybdenum trioxide which was shown to be effective in reducing origin erosion in the 5.56mm M16A1 rifle[2].

*See REFERENCES

EXPERIMENTAL METHODS

The barrel life tests were conducted in accordance with standard NATO 7.62mm test procedures(8,9). These procedures are designed for quality assurance purposes and erosion tests are usually conducted as part of initial production sample testing. Thus, they are not completely suitable for exploring, in depth, the propellant and ammunition characteristics which affect barrel life. Additional measures were therefore taken, as had been done in an earlier study(3), to more fully characterize and monitor the various phenomena occurring during the barrel erosion tests. For example, each barrel was fired to the end of barrel life, rather than to a maximum of 5000 rounds as is done in the standard test. Each barrel was also gaged before every test, at the end of each 5,000 round cycle, and at the end of the test. Velocities and cyclic rates were continually monitored.

The firing test procedure was as follows: Cartridges were assembled in the appropriate links and fired in bursts of twenty-five cartridges at twelve second intervals until 500 rounds were fired (four minutes). The ammunition was linked with a dummy cartridge located after every twenty-five live cartridges so that when the dummy round moved into the chamber the gun ceased firing. A four minute cooling period was allowed after every 500 rounds. Cooling was not assisted in any way. If the firing period exceeded four minutes, then the cooling period immediately following was reduced accordingly. This sequence of four minutes firing followed by four minutes cooling was repeated until 5,000 rounds were fired.

At the end of each 5,000 round cycle, the barrel was cooled, cleaned, and boregaged, i.e., land and groove diameters were measured over the entire length of the bore. A clean muzzle bearing was installed and firing was then resumed according to the above schedule until the barrel was rendered unserviceable or an additional 5,000 rounds were fired. This procedure was then repeated for each of the barrels utilized in this test. The criteria for barrel disqualification were "(a) velocity drop of 200 feet per second or more below the initial level obtained, or (b) 20 percent or more cartridges in any burst showing keyholing which is defined as yaw exceeding 15 percent at 1,000 inches range" whichever occurs first (8,9).

The test weapon utilized was the 7.62mm T65E1 machine gun, which is a modification of the Caliber .30 M1919A4 machine gun. The T65E1 uses a chrome plated steel 7.62mm barrel and the 7.62mm ball cartridges are linked with special links that will feed into a Caliber .30 mechanism but hold a 7.62mm cartridge case.

MATERIALS

Propellants

The four propellants evaluated in this test were fabricated at Radford Army Ammunition Plant. They were all single base extruded IMR 8138M types and were designed to differ only in the inorganic wear-reducing additive present. The chemical and physical characteristics of the four propellants are shown in Table I. Since the particle size of the erosion reducing additives may play significant roles in their effectiveness, the additives are further identified in Table II which also gives the commercial source.

TABLE I. Propellant Description

Ingredient	Lot 438-1	Lot 438-2	Lot 438-3	Lot 438-4
	Composition (%)			
Nitrocellulose* (NC)	92.86	91.37	91.42	91.73
Nitrogen in NC	13.12	13.12	13.12	13.12
Diphenylamine	0.86	0.94	0.88	0.88
Potassium Sulfate	0.84	0.84	0.82	0.85
Ethylene Dimethacrylate	3.85	4.38	3.88	4.06
Graphite (Glaze)	0.10	0.30	0.55	0.15
Total Volatiles	1.49	1.72	1.77	1.81
Moisture	1.17	1.47	1.48	1.39
Residual Solvent	0.32	0.25	0.29	0.42
Erosion Reducing Additives				
Molybdenum Trioxide		0.45		
Talc			0.68	
Calcium Carbonate				0.52
Grain Dimensions (in.)				
Length	0.042	0.042	0.042	0.042
Diameter	0.030	0.030	0.030	0.030
Perforation Diameter	0.006	0.006	0.006	0.006
Web	0.012	0.012	0.012	0.012
Bulk Density (g/cc)	0.904	0.875	0.906	0.915

*By difference

TABLE II. Characteristics of Additives Used

Molybdenum Trioxide - Chemical Grade

<u>Typical Analysis</u>

MoO_3	90-92%	
Through 100 mesh screen	100%	
Moisture	0.53%	(RAAP determination*)

<u>Supplier</u>

Molybdenum Corporation of America
New York, New York

Talc - Sierra Supreme USP

<u>Analysis (RAAP)</u>

Through 100 mesh screen	100%
Through 325 mesh screen	78.3%
Retained on 325 mesh screen	21.7%
Moisture	0.39%

<u>Supplier</u>

United Sierra Division, Cyprus Mines Corporation
Trenton, New Jersey

Calcium Carbonate - Multifex MM

<u>Typical Analysis</u>

$CaCO_3$	98.8%	
Through 325 mesh screen	100%	
Moisture	0.69%	(RAAP determination)

<u>Supplier</u>

Diamond Shamrock Chemical Company
Cleveland, Ohio

*Radford Army Ammunition Plant

Ammunition

The propellants were loaded into 7.62mm M80 ball cartridges at Lake City Army Ammunition Plant using gilding metal clad steel projectiles.

Ballistic tests were conducted to assess the velocities and pressures produced by each lot. The results are given in Table III.

TABLE III. Ammunition Characteristics

Lot No.	Additive Used	Charge Weight (grains)	Corrected Velocity at 78 ft. (ft/sec)	Corrected Peak Pressure Copper (psi)
438-1	None	44.2	2639	38,000
438-2	MoO_3	44.2	2646	38,900
438-3	Talc	44.3	2699	38,400
438-4	$CaCO_3$	44.2	2679	38,300

All four lots were loaded to case capacity. It should be noted that even though the charge weights were relatively high for IMR 8138M type propellant (which usually runs about 42 grains), the velocities were somewhat lower than specification value of 2750 $\pm$ 30 ft/sec. The fact that the chamber pressures, also, are low (specification value is $<$50,000 psi) indicates that the propellant lots were prepared to slightly less "quickness" than is required for this cartridge. This, however, is not believed to affect the results in terms of relative effects of various additives on barrel life.

Barrels

Chrome plated steel barrels for the T65E1, 7.62mm machine gun were used in this test. To minimize the possibility of barrel variation, the barrels were selected so that their serial numbers were as close together as possible. In addition, the barrels were gaged as part of the selection procedure and care was taken to ensure that all barrels used were dimensionally similar.

RESULTS AND DISCUSSION

Cyclic rates were continually monitored throughout the barrel life tests. Examination of the results indicates that relatively small changes in cyclic rate occurred with all the lots fired and that no noticeable differences in cyclic rate variations could be attributed to specific additives.

Lot 438-1 (No Additive)

The results of the barrel life tests conducted with Lot 438-1 containing no additive are given in Table IV.

TABLE IV. Barrel Life Test Results, Lot 438-1 (No Additive)

Barrel No.	5315	5319	5258
No. of Rounds to Disqualification	7500	8300	7450
Cause of Disqualification*	V	K	K
Velocity Loss (ft/sec)	> 200	185	152

Avg. Barrel Life: 7750 Rounds

*V - velocity loss; K - excessive keyholing

All three barrels yielded essentially similar results. Although only one failed on velocity, the velocity loss with the other two barrels was near the 200 ft/sec criterion when they were disqualified on the basis of excessive keyholing.

The manner in which the velocity changed as firing progressed with one of the barrels in this test is shown in Figure 1. The values plotted, which are representative of all three barrels, indicate the difference between the initial velocity (i.e., during the first twenty-five round burst) and that obtained at the end of each 500 round cycle. The slight velocity increase obtained early in the test is fairly common and may be due to buildup of solid deposits at, or near, the origin (before significant erosion occurs). This would increase the resistance to initial projectile movement, causing the cartridge to function at a higher than normal pressure level. The velocity drop obtained in the later stages is primarily due to barrel wear which permits escape of propellant gas around the projectile thus reducing the available energy which serves to drive the projectile.

The phenomena that cause a barrel to fail can often be better understood by examining the changes that occur to the bore dimensions. Figures 2 and 3 show, respectively, the changes in vertical land and groove diameter of the bore between that measured at the beginning and end of the test. Since three barrels were used, the mean values and extreme variations are shown. No data are available regarding change in land diameters in the first five inches from the origin since the increase in diameter was in excess of the range of the boregage. It is seen that severe land erosion also occurred in the region of the muzzle. There were, on the other hand,

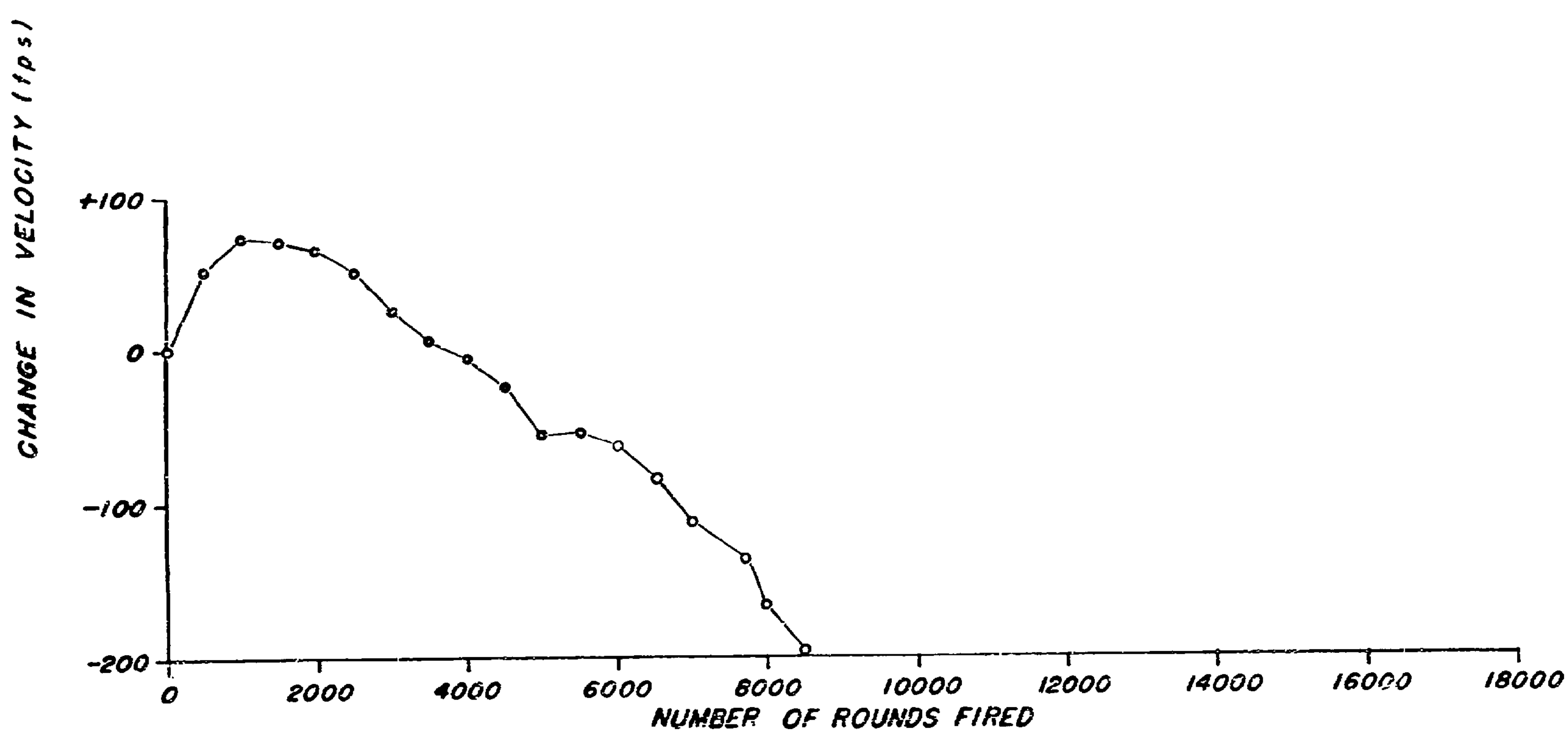

FIGURE 1. CHANGE IN VELOCITY WITH NUMBER OF ROUNDS FIRED, LOT 438-1 (NO ADDITIVE)

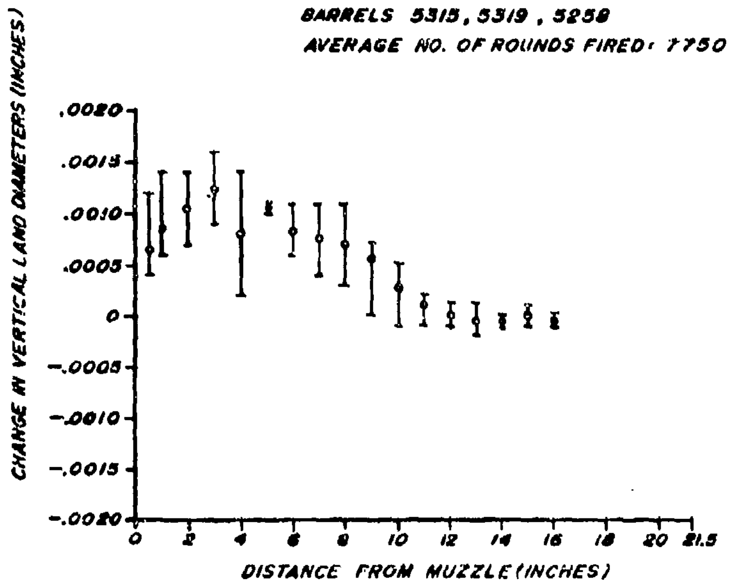

FIGURE 2. CHANGE IN VERTICAL LAND DIAMETER VS. RIFLING DISTANCE, LOT 438-1 (NO ADDITIVE)

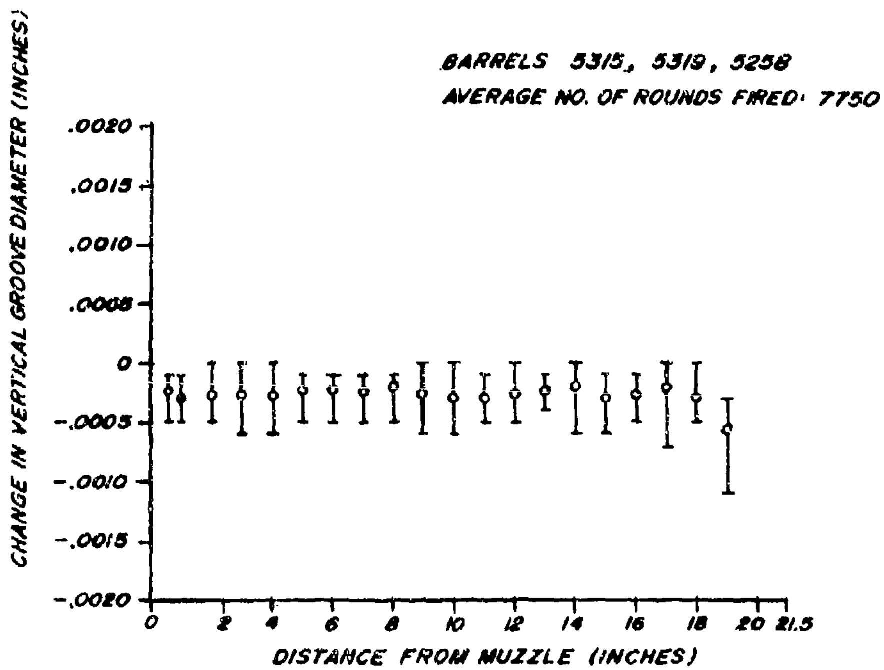

FIGURE 3. CHANGE IN VERTICAL GROOVE DIAMETER VS. RIFLING DISTANCE, LOT 438-1 (NO ADDITIVE)

only very slight changes in the groove diameters. The relatively large degree of muzzle erosion obtained with the control lot (as well as with the others) is due to mechanical effects associated with the high velocity motion of the projectile near the muzzle end of the barrel. It has been previously shown that use of M80 projectiles with a gilding metal clad steel jacket, as used in this test, results in substantially less barrel life than do M80 projectiles with an all gilding metal jacket.[3]

Lot 438-2 (Molybdenum Trioxide)

The barrel life test results for Lot 438-2 which contained molybdenum trioxide are summarized in Table V.

TABLE V. Barrel Life Test Results, Lot 438-2 (MoO_3)

Barrel No.	5236	5254	5312
No. of Rounds to Disqualification	10,925	9325	11,000
Cause of Disqualification	K	K	V
Velocity Loss (ft/sec)	165	142	>200

Avg. Barrel Life: 10,420 Rounds

All three barrels again yielded essentially similar results, exhibiting 34% greater average barrel life than was obtained with the control lot. Figure 4 is a plot of the change in velocity vs. number of rounds fired for barrel 5236 which failed due to keyholing. (The general relationship is similar for all three barrels.) It is seen that, at keyholing failure, the velocity had severely degraded and that the barrel would very likely have soon failed on velocity drop.

Changes in vertical land and groove diameters in the bore, as produced by the firing tests, are shown in Figures 5 and 6. While in general the erosion profiles of the lands are similar to those exhibited by the control ammunition, land erosion in excess of the range of the boregage was only encountered in the first two inches from the origin. Furthermore, it should be borne in mind that the barrels fired with the MoO_3 containing ammunition were exposed to approximately 34% more firings. It is this factor which probably accounts for the slightly greater muzzle erosion which, as discussed above, is caused by mechanical factors. The constriction in the grooves appears to be very light, indicating that while deposition of additive combustion products may be occurring it is not severe enough to adversely affect performance.

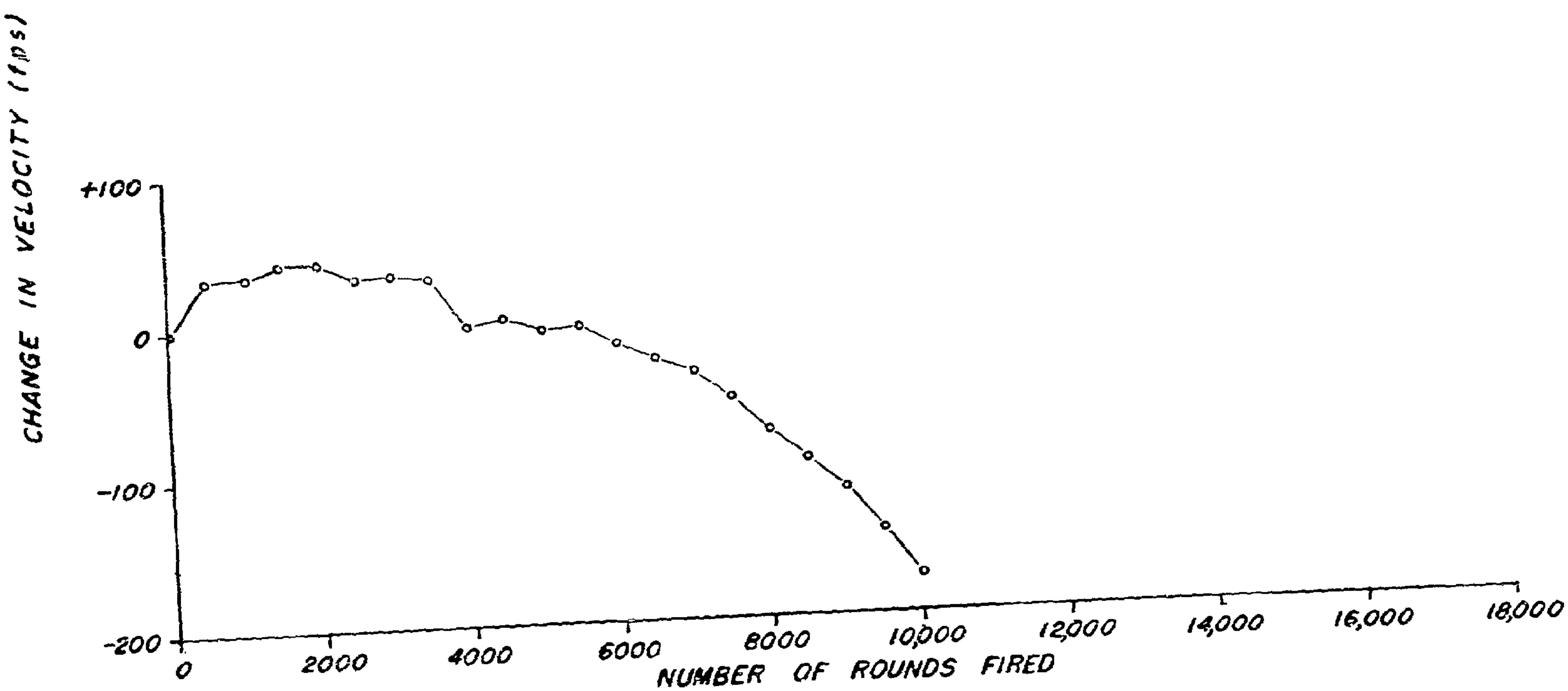

FIGURE 4. CHANGE IN VELOCITY WITH NUMBER OF ROUNDS FIRED, LOT 438-2 (MoO_3)

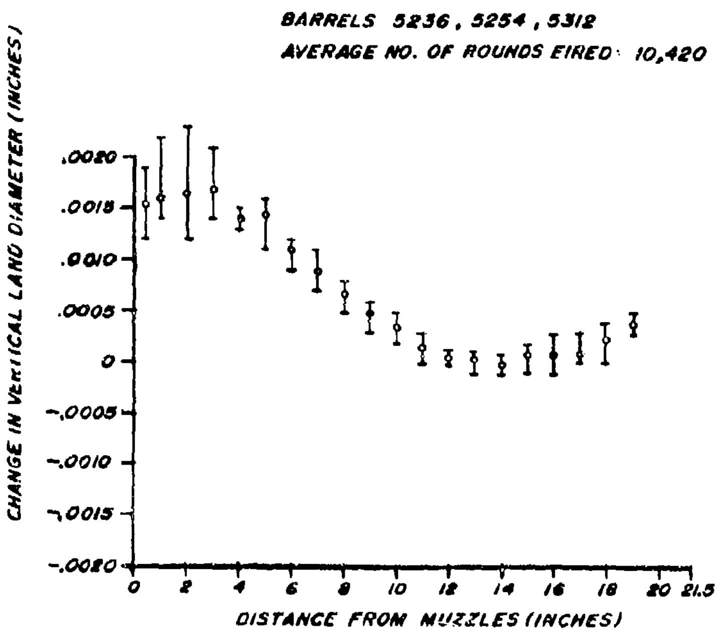

FIGURE 5. CHANGE IN VERTICAL LAND DIAMETER VS. RIFLING DISTANCE, LOT 438-2 (MoO_3)

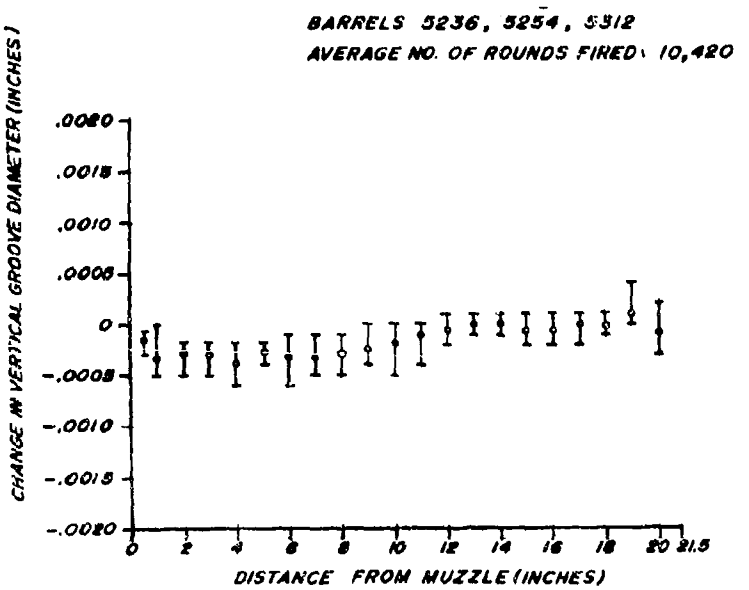

FIGURE 6. CHANGE IN VERTICAL GROOVE DIAMETER VS. RIFLING DISTANCE, LOT 438-2 (MoO_3)

Lot 438-3 (Talc)

A summary of the results from barrel life tests with Lot 438-3 which contained talc as the additive is given in Table VI.

TABLE VI. Barrel Life Test Results, Lot 438-3 (Talc)

Barrel No.	5199	5213	5202
No. of Rounds to Disqualification	7900	8775	8700
Cause of Disqualification	K	K	K
Velocity Loss (ft/sec)	-26	10	20

Avg. Barrel Life: 8460 Rounds

The talc additive was almost ineffective in increasing 7.62mm barrel life, as defined by the criteria of excessive velocity loss or yaw. However, in contrast with the previous two lots, there was no significant change in velocity even after firing approximately 8000 rounds. This is also evident from Figure 7 which is a graph of velocity change vs. number of rounds fired for barrel 5202. It clearly indicates that failure was not attributable to erosion. However, there is a trend toward velocity loss after 8000 rounds. Unfortunately, no reliable bore gage data could be recorded over most of the rifling distance with the barrels used with this lot. Fouling was so heavy that the air gage probe could not be used. It is presumed that heavy fouling deposits formed in the grooves along most of the barrel length. It appears that talc did serve to effectively prevent barrel erosion, as has been found in other studies[4,5,6,7]. The problem here, however, is primarily one of barrel fouling by the additive or its products, which caused relatively early disqualification due to yaw. It had been intended to keep the additive level at approximately 0.5% for all test lots. As can be seen in Table I the talc content was above this level, viz. 0.68%. It is probable that a reduced level of talc would be sufficient to protect the barrel against erosion without producing the excessive fouling that apparently occurred with this lot.

Lot 438-4 (Calcium Carbonate)

The major results of the barrel life tests conducted with Lot 438-4 which contained calcium carbonate as the additive are shown in Table VII.

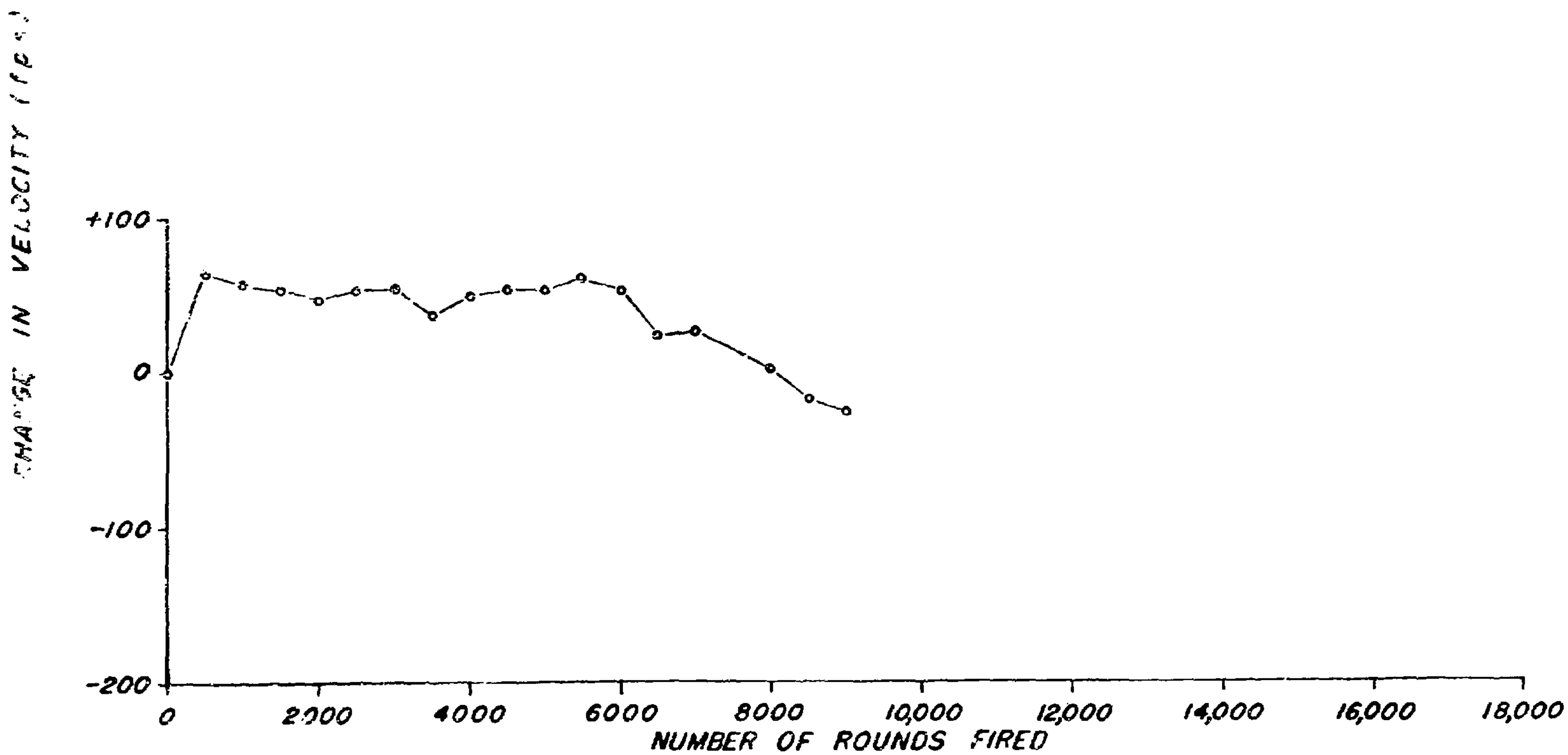

FIGURE 7. CHANGE IN VELOCITY WITH NUMBER OF ROUNDS FIRED, LOT 438-3 (TALC)

TABLE VII. Barrel Life Test Results, Lot 438-4 ($CaCO_3$)

Barrel No.	5038	5353	5283
No. of Rounds to Disqualification	16,725	13,150	17,375
Cause of Disqualification	K	K	K
Velocity Loss (ft/sec)	148	140	147

Avg. Barrel Life: 15,750 Rounds

The data indicate that the calcium carbonate additive was highly effective, providing a barrel life approximately 100% greater than that obtained with the control lot having no additive. All three test barrels yielded results considerably superior to the control and all had similar velocity losses when they failed due to excessive keyholing. Figure 8 shows the pattern of velocity change vs. number of rounds fired for barrel 5038, which is representative of all three barrels. It is seen that only negligible velocity loss occurred up to 12,000 rounds. This is even superior to the data obtained with talc where the velocity remained essentially constant for about 8000 rounds.

The changes in land and bore diameters that occurred during the test are shown in Figures 9 and 10, respectively. Considering the large number of rounds fired, the extent of erosion in the origin region of the barrel was relatively small. It is in this area, where thermal factors are usually the primary cause of erosion, that the additive appears to play its major role. Erosion in the muzzle region, which is primarily due to mechanical factors, was also reduced considering the number of rounds fired. However, the calcium carbonate additive does not appear to be as effective in reducing erosion in region.

CONCLUSIONS

1. Calcium carbonate when incorporated in single base extruded propellant at a level of 0.5% provided an increase of approximately 100% in the barrel life of 7.62mm M80 ammunition, as judged by the criteria of velocity loss or excessive yaw.

2. Molybdenum trioxide when incorporated in single base extruded propellants at a level of 0.5% increased the barrel life of M80 ball ammunition by 34%.

3. Talc when incorporated in single base extruded propellant at a level of 0.7% did not substantially affect M80 ball ammunition barrel life. However, there was no significant change in velocity in the course of the entire test. Barrel disqualification was a result of

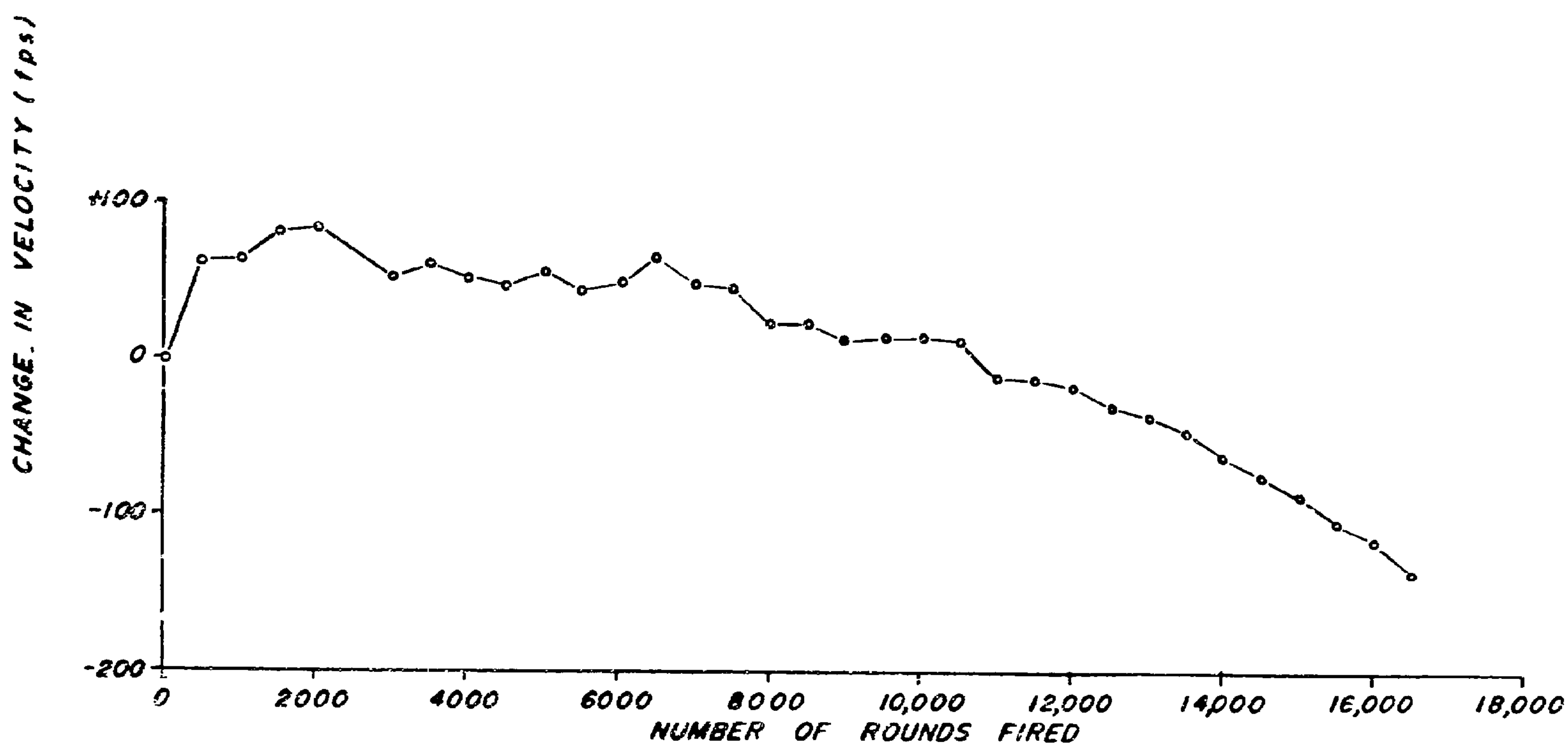

FIGURE 8. CHANGE IN VELOCITY WITH NUMBER OF ROUNDS FIRED, LOT 438-4 ($CaCO_3$)

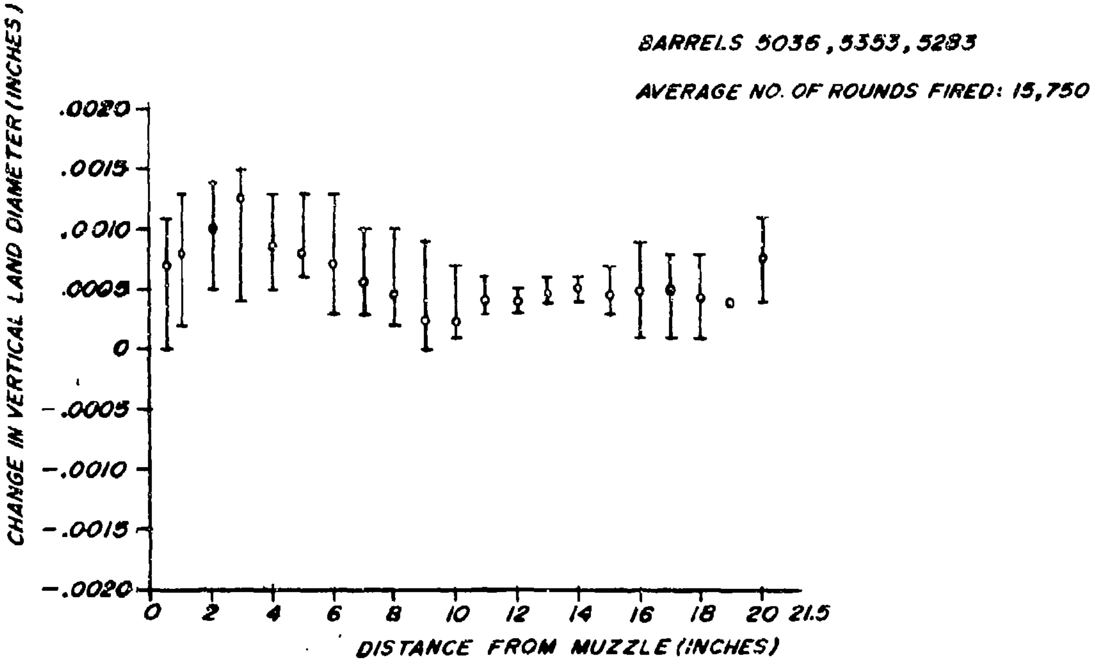

FIGURE 9. CHANGE IN VERTICAL LAND DIAMETER VS. RIFLING DISTANCE, LOT 438-4 ($CaCO_3$)

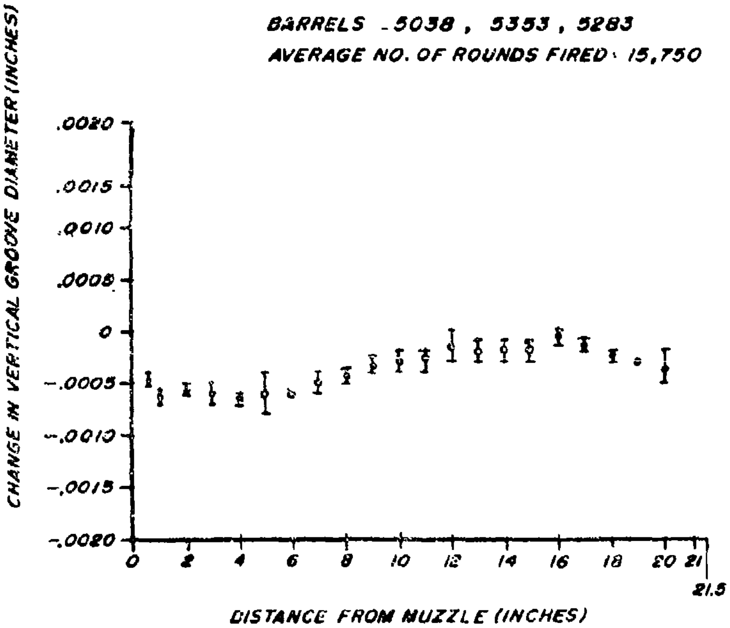

FIGURE 10. CHANGE IN VERTICAL GROOVE DIAMETER VS. RIFLING DISTANCE, LOT 438-4 ($CaCO_3$)

considerable fouling which induced excessive yaw.

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3. R. Fedyna and L. Stiefel, "7.62mm Barrel Life as Affected by Certain Inorganic Propellant Constituents and Bullet Designs", Frankford Arsenal Report FA-TR-75045, July 1975.

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9. "Manual of Proof and Inspection Procedures for NATO 7.62mm Ammunition", North Atlantic Treaty Organization, Small Arms Ammunition Panel (AC/116), May 1960.

TALC AS A PROPELLANT ADDITIVE TO IMPROVE
BARREL LIFE IN 20mm AUTOMATIC CANNON
AND 4.32mm RIFLE SYSTEMS

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At the Tri-Service Gun Propellant Symposium held at this location in 1972[1], we reported on the results of preliminary barrel life tests using talc as a wear reducing additive in extruded single base propellant. This paper presents additional results of barrel life tests using talc as the additive in double base rolled ball propellant.

Talc is a naturally occurring substance that is mined in a number of locations in the United States. It is generally described as a hydrous magnesium silicate. The type of talc used in the barrel life tests with the rolled ball propellants is United States Sierra Supreme 2.3 microns.

The reason for using this type goes back to the results reported in the previous presentation. The initial project, using extruded 20mm propellant as a test vehicle, was performed with several types of talc. Due to the high cost of both 20mm ammunition and the test firings it was decided to use only one type of talc in the confirmatory stages of the extruded propellant effort. Since the greatest increase in barrel life had been obtained with Supreme 2.3 micron talc, this type was selected for all future work.

It might be well at this point to give the basic technical reasoning behind the development of a ball type propellant with talc as an additive. One of the conclusions of the previous study[2] was: "Small amounts of talc incorporated in the base grain of extruded propellant gave increased barrel life in engineering tests performed in the 20mm, M39 Machine Gun. An increase of approximately two or three times the average barrel life was obtained when 1/2% talc is added."

While these results were obtained with single base extruded propellant (viz., CR 7814), efforts conducted on a previous project had resulted in the development of a low flame temperature rolled ball propellant, identified as X2048, for 20mm application. The latter, even without a barrel wear additive, gave a barrel life 50 to 100% greater than did the CR 7814 extruded propellant. In addition to the potential of even greater barrel life, the ball propellant also could be loaded to a higher charge weight.

The optimum propellant for the 20mm M50 series Automatic Cannon Ammunition therefore appeared to be a rolled ball type which would have a higher loading density, would be relatively cool burning and to which talc or some other effective wear reducing additive could be added to provide even greater barrel life.

With the switch from extruded to ball propellant, changes had to be made in the way the talc was introduced into the gun system. The standard method of adding talc to an artillery system is to impregnate a wax envelope with the talc and then fit the envelope inside the cartridge case. However this method is not practical for small arms cartridges since they are loaded to almost zero propellant air-space leaving no room in the cartridge case for the envelope. The talc was therefore incorporated in the base grain of the small arms extruded propellant at the time of manufacture.

However, because of the way ball propellant is manufactured, it is not possible to incorporate an inorganic material like talc into the base grain of production ball propellant. One of the problems is that talc being incorporated into the 20mm propellant would also end up in the base grain of the 5.56mm M16 rifle propellant. This rifle system is very susceptible to gas tube fouling and it would be expected that 5.56mm propellant containing talc could foul the M16 rifle gas system.

Therefore the decision was made to surface coat the talc on the ball propellant. Surface coating of additives in ball propellant is nothing unusual. In fact, the present 20mm ball propellant WC870 has both a flash suppressant and an antifoulant on the surface together with a small amount of graphite. Although it would appear that no serious processing problem would develop when adding talc to the surface of the X2048 type propellant, it was suspected that another inorganic placed on the surface of the propellant would probably slow propellant ignition.

Two lots of rolled ball propellant were manufactured by the Olin Corp. The reasoning for the two lots of propellant was that there had been a long standing debate concerning the need for antifoulants in small arms propellants. Since this would be a new type of propellant it was agreed to make lots both with and without the tin dioxide antifoulant. The two developmental lots of propellant are identified as X3325, the lot with 0.49% talc and 0.84% tin dioxide and X3326 which contained 0.79% talc.

Table I shows that these two lots of propellant have calculated flame temperatures similar to the previous lots of rolled ball X2048 propellant. Therefore, the barrel life tests with these propellants should indicate the effect of the talc additive and not a large lot-to-lot propellant flame temperature variation.

Although the surface coated talc slowed propellant ignition, the 20mm cartridges which were loaded with propellants X3325 and X3326 met all of the 20mm cartridge specification requirements at ambient temperature and were therefore suitable for barrel life testing. There were, however borderline results with respect to action time at -65°F only. These will be discussed later in this paper.

The barrel life data presented was obtained in the 20mm M39 Automatic Aircraft Cannon by firing production 20mm M55A2 Target Practice cartridges loaded with the various propellants. The firing schedule employed was designed a number of years ago to simulate the most severe Air Force use of this type of ammunition. The schedule consists of firing a two hundred round burst followed by complete cooling, and then repeating this procedure until failure due to excessive yaw or velocity drop. Excessive yaw is defined as yaw of 15° or more at 1000 in. range with 20% or more rounds in any consecutive 40 rounds of any burst. Excessive velocity drop is defined as the average velocity of any 20 consecutive rounds of a burst being 6% or more below the average velocity of the first 20 rounds fired in a new barrel.

The barrel life data shown in Table II is for several lots of rolled ball propellant X2048 without talc added. You will note a barrel life of about 3000 rounds should be expected from this type of propellant and, moreover, the calcium carbonate level appears to have no effect on 20mm barrel life. With regard to the calcium carbonate level, we have previously presented data(3) on eight lots of the regular 20mm ball propellant WC870 which also shows the calcium carbonate level has no effect on 20mm barrel life. Therefore I think you can see from the results reported in the 7.62mm presentation at this symposium(4) that data regarding barrel wear additives cannot be transferred from one gun system to another.

Table III gives the results of the barrel life tests with propellant X3325. This is the lot that contains both the antifoulant tin dioxide as well as talc. You will observe that the end points of approximately 3000 rounds are not significantly different from the results reported for the X2048 type propellant without the talc added.

However, if we were discouraged by these results, our gloom was short lived. Table IV gives the barrel life test results with propellant X3326. As you remember the difference between the two lots of experimental rolled ball propellant is that X3326 has only talc as an additive with no antifoulant tin dioxide.

We had loaded all available propellant into slightly less than 9000 rounds of 20mm ammunition and after firing 8400 rounds we found that the ammunition lot had been exhausted but the barrel was still firing in a satisfactory manner. Moreover, no yaws had been recorded and the average burst velocity had dropped only 87 f/s. (A loss of 196 f/s in velocity was required to disqualify this barrel.) Since this is the only barrel tested with this type of propellant it is not possible to estimate at what point the barrel would have been disqualified if a much larger quantity of ammunition were available. However I don't think one would have to be too optimistic to assume at least reaching 10,000 rounds. This was by far the longest barrel life ever achieved in tests using the 20mm M39 automatic cannon.

A second lot of X2048 type rolled ball propellant with surface coated talc was then manufactured by the Olin Corporation. The lot, identified as X3326.1, has a flame temperature of 2650°K which is within the extremes of the previous lots of X2048 type propellant and is similar to the first lot of X3326 propellant. The amount of talc is 0.74%, almost the same as the 0.79% surface coated on the original lot that gave the 8400 plus rounds barrel life. Lake City Army Ammunition Plant has just completed the production loading of about 20,000 cartridges 20mm, M55A2. It is expected that the barrel life tests will start in early April.

With regard to the slight ignition problem mentioned earlier, the X3326.1 lot was also completely acceptable at ambient and high temperatures, but at -65°F the action time was again borderline. Table V shows the action times for both lots at -65°F. The controlling specification requirement is that the average action plus four standard deviations shall not exceed 4 msec. The numbers are seen to be within the requirement but close to the limit.

It is planned to make a small number of standard M52A3B1 primers with additional primer mix added to the cup. This is possible because the present cup is not 100% full. The specification requirement is 2.25 to 3.00 grains of mix. It is expected that we will be able to get about 4 grains of mix in the cup. This additional mix will improve the propellant ignition at -65°F and make propellant X3326 completely acceptable for this type of 20mm ammunition.

Another project in which talc has been used as a barrel life additive with rolled ball propellant is the experimental 4.32mm

Advanced Rifle Development Program. In the early 1970's, Frankford Arsenal and the Olin Corporation developed a low flame temperature rolled ball propellant X3116 for use in the 4.32mm full bore ammunition. Two developmental lots of X3116 type propellant were manufactured. One lot was surface coated with the talc that gave the long barrel life in 20mm ammunition and the other surface coated with the calcium carbonate that gave long barrel life in the 7.62mm ammunition project.[4]

In this case the findings from two separate gun system projects are being used in an attempt to increase the barrel life in a third weapon system. Ammunition loaded with both propellants has been delivered to Rock Island Arsenal and erosion testing has just started.

In summary the following conclusions may be drawn. First, talc or hydrous magnesium silicate can be satisfactorily surface coated on a rolled ball propellant. Second, the barrel life tests to-date indicate that adding the talc significantly increases 20mm barrel life and presents no serious difficulties in the operation of this automatic cannon weapon system.

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(3) A. V. Nardi, "The Effect of Propellant on Barrel Li · of a 20mm High Velocity Weapon System". Presented at International Sympo ium on Gun Propellants, Picatinny Arsenal, October 1973.

(4) R. Fedyna, M. E. Levy, and L. Stiefel, "Use of Inorgan c [illegible] Reducing Additives to Provide Increased 7.62mm Barrel Life with Single Base Extruded Propellants". Presented at Tri-Service Gu Tube Wear and Erosion Symposium, Picatinny Arsenal, March 1977.

TABLE I. FLAME TEMPERATURES OF 20MM ROLLED BALL PROPELLANT LOTS

PROPELLANT LOT	FLAME TEMP. ^{o}K
X2048-1	2600
X2048-2	2628
X2048-3	2607
X2048-5	2647
X2048-6	2660
X2048-7	2582
X3325 (TALC AND TIN DIOXIDE)	2642
X3326 (TALC AND NO TIN DIOXIDE)	2631

TABLE II. BARREL LIFE - X2048 BALL PROPELLANT (NO TALC)

PROPELLANT LOT	AVERAGE CYCLIC RATE OF TEST	REASON FOR DISQUALIFICATION (VELOCITY LOSS, FT/SEC)	IND. BARREL DISQUALIFICATION PT. (ROUNDS FIRED)
X2048-1	1810	VELOCITY DROP (-223)	3840
(0.53% $CaCO_3$)	1779	VELOCITY DROP (-229)	3400
	--	STILL GOOD*	2200+
X2048-6	1673	VELOCITY DROP (-199)	3020
(0.60% $CaCO_3$)	1778	VELOCITY DROP (-215)	2420
	1779	STILL GOOD** (-115)	2400+
	1735	STILL GOOD** (-145)	2400+
	1583	STILL GOOD** (-172)	3365+
X2048-5	1702	VELOCITY DROP (-237)	3043
(0.05% $CaCO_3$)	1688	VELOCITY DROP (-216)	3220
	1799	STILL GOOD** (-167)	3000+

*TEST DISCONTINUED - SUPPLY OF AMMUNITION EXHAUSTED

**TEST DISCONTINUED - SEALS PROBLEM

TABLE III. BARREL LIFE - X3325 BALL PROPELLANT

PROPELLANT LOT	AVERAGE CYCLIC RATE OF TEST	REASON FOR DISQUALIFICATION (LOSS IN VELOCITY, FT/SEC)	IND. BARREL DISQUALIFICATION PT. (ROUNDS FIRED)
X3325 (0.49% TALC) (0.84% SnO_2)	1739	VELOCITY DROP (-224)	2820
	1646	VELOCITY DROP (-204)	3020
	1643	STILL GOOD - AMMUNITION SUPPLY EXHAUSTED (-45)	2000+

TABLE IV. BARREL LIFE - X3326 BALL PROPELLANT

PROPELLANT LOT	AVERAGE CYCLIC RATE OF TEST	REASON FOR DISQUALIFICATION (LOSS IN VELOCITY, FT/SEC)	IND. BARREL DISQUALIFICATION PT. (ROUNDS FIRED)
X3326 (0.79% TALC)	1513	STILL GOOD - AMMUNITION SUPPLY EXHAUSTED (-87)	8400+

TABLE V. ACTION TIME VALUES

PROPELLANT LOT	AVERAGE ACTION TIME AT -65°F (MSEC)	AVG. PLUS 4 STD. DEV.* (MSEC)
X3326	3.526	3.938
X3326-1	3.603	3.951

*SPECIFICATION REQUIREMENT: $\leqslant$4 MSEC

EROSION REDUCTION WITH 20MM ABLATIVE AMMUNITION

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Buffalo, New York

ABSTRACT

M55A2, 20mm ammunition was modified by placing 5cc of ablator between the propellant charge and the projectile. The function of the ablator, a gelled dimethyl silicone compound, was to reduce barrel heating and erosion by coating the bore surface to form an insulative and ablative shield against the hot propellant gases.

Several thousand ablative and standard rounds were fired in instrumented M39 and M61 guns. Very substantial reductions in heating and erosion were experienced with ablative ammunition when compared with standard ammunition in the critical high erosion areas of the barrels of both guns. Under very severe burst firing conditions, reductions in erosion of as much as 800 percent were realized at the origin of rifling.

INTRODUCTION

Barrel heating limits firepower because excessive heating:

1. increases barrel erosion,
2. produces cook-off hazards,
3. tends to promote stoppages and parts failures,
4. increases maintenance time, logistic manhours, and procurement dollars,
5. restricts combat firing schedules, and
6. makes for heavier guns.

Therefore it was with alleviation of the above effects in mind that we began the development of ablative ammunition.

Ablative ammunition is an anti-heating and anti-erosion ammunition which usually uses a grease-like material or ablator situated near the base of the projectile so that when propellant gases are generated, they force the ablator to coat the bore surface and act as an insulator before the hot gases reach the bore. This is shown in Figure 1. When the projectile leaves the muzzle and the pressure drops, the hot ablator will have a tendency to vaporize and thereby

*Abstracted from "Performance of 20mm Ablative Ammunition," Technical Report No. AFATL-TR-71-164, W.R. Brown, E.B. Fisher, D.J. Sudlik, F.A. Vassallo, December 1971.

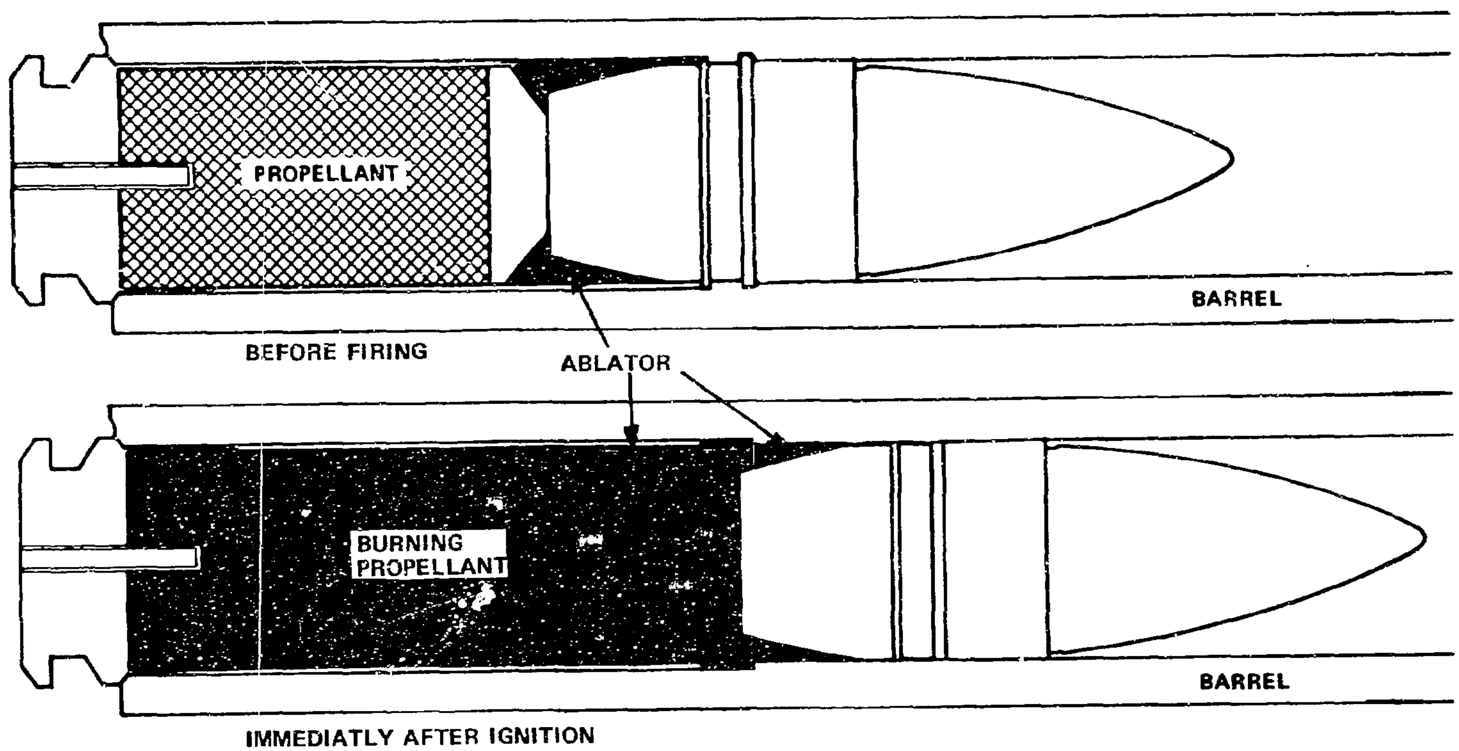

PROPELLANT
BARREL
BEFORE FIRING
ABLATOR
BURNING
PROPELLANT
BARREL
IMMEDIATLY AFTER IGNITION

Figure 1

providing a cooling function. The whole process is then repeated for each round fired.

Calspan first introduced a rifle-type ablative ammunition employing beeswax as the ablator in 1948 with some success but it was not until the last 10 years that serious development efforts were pursued. This has included ammunition for the 4.2 inch mortar, the 155mm M185 cannon, the XM140 30mm helicopter gun, the 20mm aircraft cannon, the 7.62mm machine gun, the 4.32mm rifle, and the Ares 75mm cannon. In each development, the ablative ammunition has been very successful in diminishing the barrel heating and erosion problems encountered. Whereas all attempts produced excellent results, occasionally spectacular results have been achieved. As much as a 3,000 percent improvement in the erosion resistance has been produced at the barrel entrance. With conventional fixed ammunition, it is not uncommon to experience at least a 40 percent decrease in heating at the origin of rifling, the area where erosion is usually most severe.

For the purposes of this paper, work sponsored by the Air Force Armament Laboratory to improve the erosion performance of the 20mm M55 ammunition for the Air Force's aircraft cannons is described. This is an example of ammunition which was designed more than a quarter of a century ago to accommodate all of the Air Force's 20mm M39 and M61 aircraft cannons. For that reason and because of a large inventory, it was important not to modify the exterior configurations of the round.

For the development of this ammunition the following constraints were imposed:

1. the ammunition had to utilize the same case and projectile as used in conventional M55 ammunition;
2. the exterior ballistics had to be at least as good as that in conventional M55 ammunition;
3. the maximum chamber pressure had to be less than 60,000 psi; and
4. the system had to be applicable to both the M39 and M61 cannons.

SINGLE-SHOT TESTING

At the outset, a number of single-shot tests through an M61 barrel instrumented with a pressure tap and thermal sensors were fired. Through a brief evolutionary development, the configuration shown in Figure 2 was adopted for extensive rapid-fire testing.

20 mm ABLATIVE AMMUNITION

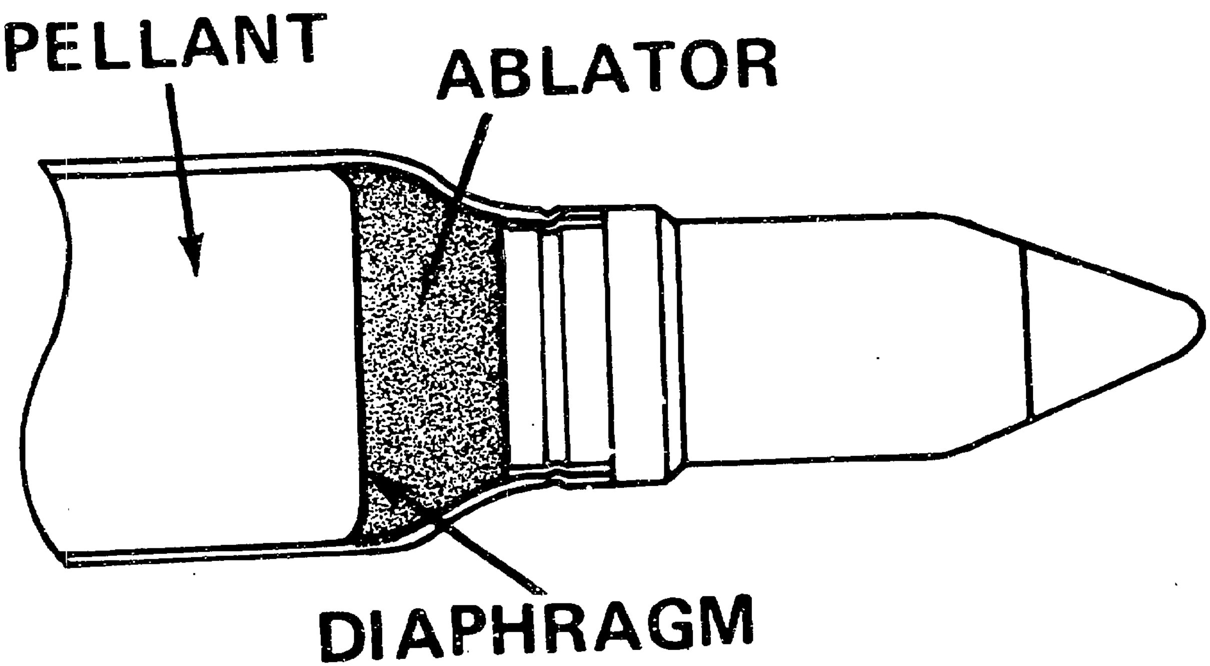


Figure 2

This configuration essentially duplicated the muzzle velocities normally experienced with M55 rounds. To achieve the required muzzle velocities with ablative ammunition, less propellant (585 instead of 612 grains) was required but a minimal amount of propellant compaction was utilized to allow for 77 grains of ablator. The ablator was composed of 95 percent dimethyl silicone gelled to a grease-like consistency with 5 percent of fumed silica. The ablator was separated from the propellant by a thin plastic diaphragm.

After rapid-fire testing, a Brown and Sharpe Intrimik micrometer gage, specially adapted for measuring the inside diameter of M39 and M61 barrels at both the lands and grooves, was used for erosion measurements. Yaw measurements were made with a moving target of 6 mil thick polyethylene situated 1000 inches down range. A stationary witness grid was also used to determine the dispersion during each test.

TESTS WITH AN M39 GUN

Following the single-shot testing phase, the M39, gas operated, revolver type gun was used for the first rapid-fire tests. This gun is capable of firing 1800 rounds per minute through a single barrel utilizing a drum containing five chambers. The M39 was used first because it would require a minimum of ammunition and because it would provide a test of potential ablator fouling of the gas operated mechanism.

A summary of the basic data recorded with the M39 gun is given in Table I. The table shows that both barrels through which standard ammunition was fired, failed before the 250-round burst was completed. The barrel using ablative ammunition failed after 1211 total rounds were fired. Essentially, three complete bursts were fired while three bursts with stoppages at 80, 170, and 162 rounds also contributed to the erosion.

The ablative ammunition diminished drum heating by approximately 50 percent as shown in Figure 3. This shows that the drum can be lightened appreciably before cook-off becomes a problem. The erosion data are perhaps best visualized by referring to Figure 4. Note that after a 250-round burst the ablative ammunition eroded only 3 mils at the origin of rifling, as compared to about 24 mils for the standard M55 ammunition. Barrel temperatures for a 250-round burst at the station 1" from the barrel face is shown on Figure 5. This shows a range of temperatures from the two types of ammunition in various tests. Note that the ablative ammunition not only has lower temperatures, but in addition, has a narrower range of temperature variation.

M39 BARREL LIFE TESTS

	BARREL NO.	ROUNDS LOADED	ROUNDS FIRED	TOTAL ROUNDS	ROUNDS TO FAILURE
STANDARD	1	250	250	250	230
STANDARD	2	250	250	248	240
ABLATIVE	3	250	250	250	NO
	3	250	80	330	NO
	3	250	170	500	NO
	3	250	162	662	NO
	3	250	248	910	NO
	3	250	250	1160	NO
	3	250	250	1410	1211

Table I

M39 DRUM TEMPERATURES AFTER 250-ROUND BURST

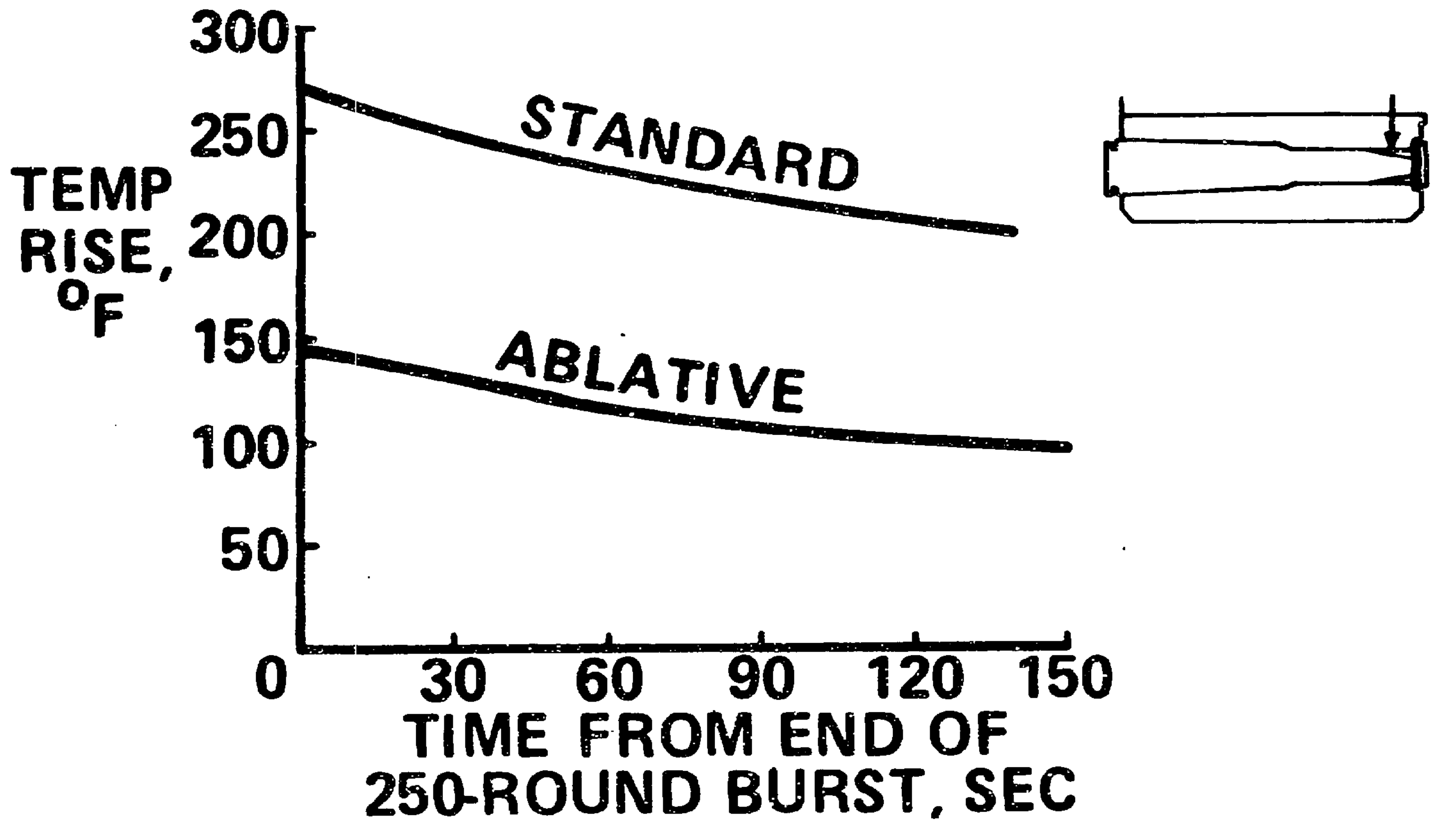


Figure 3

M39 BORE EROSION PROFILES

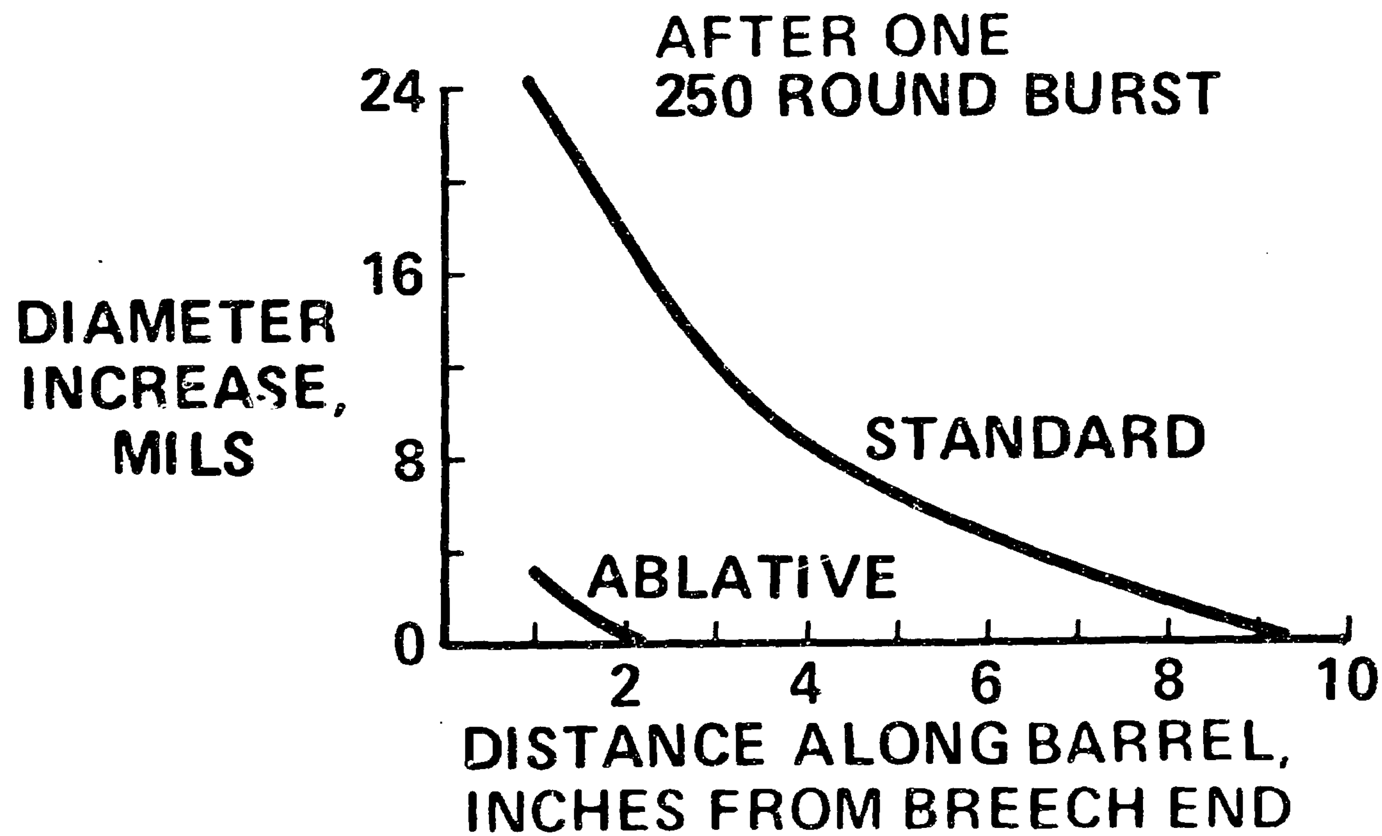


Figure 4

M39 BARREL TEMPERATURES IN 250-ROUND BURST

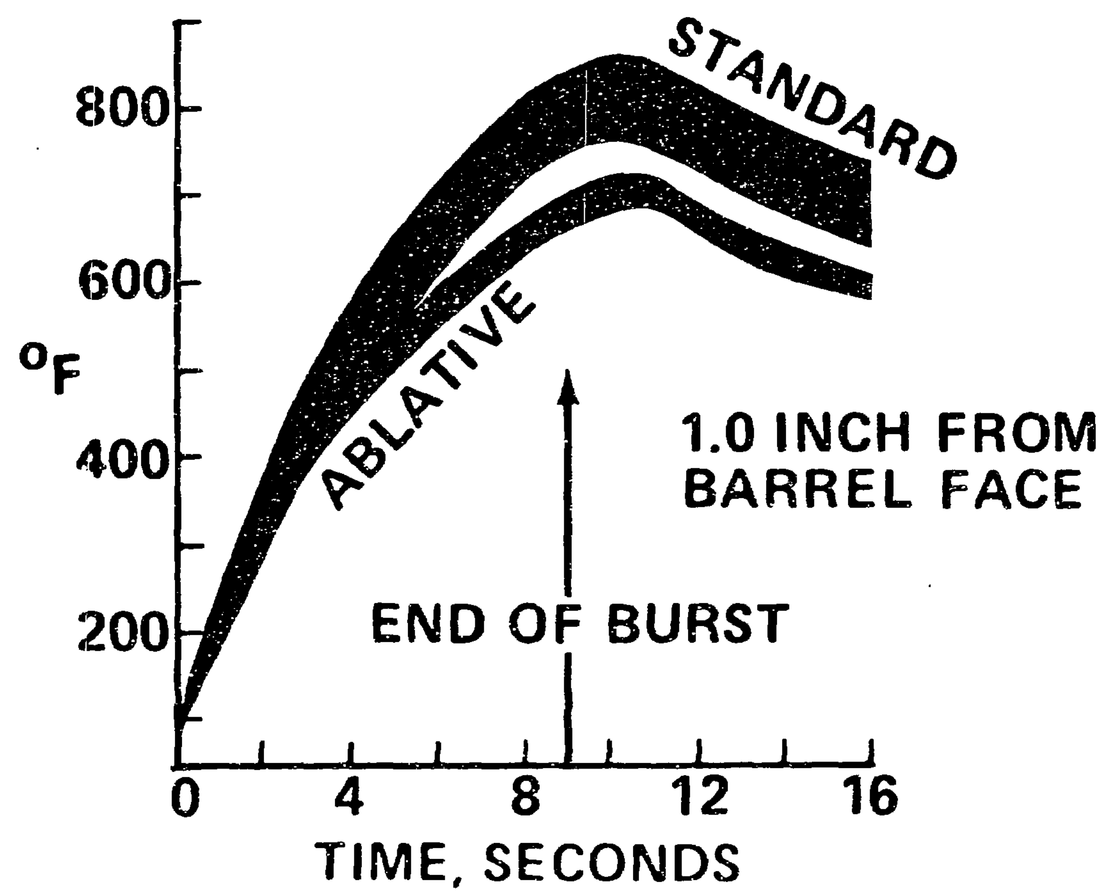


Figure 5

There was no effect of the ablative ammunition on the normal operation of the M39 gun. Therefore, there was no fouling of the gas operated cycling mechanism.

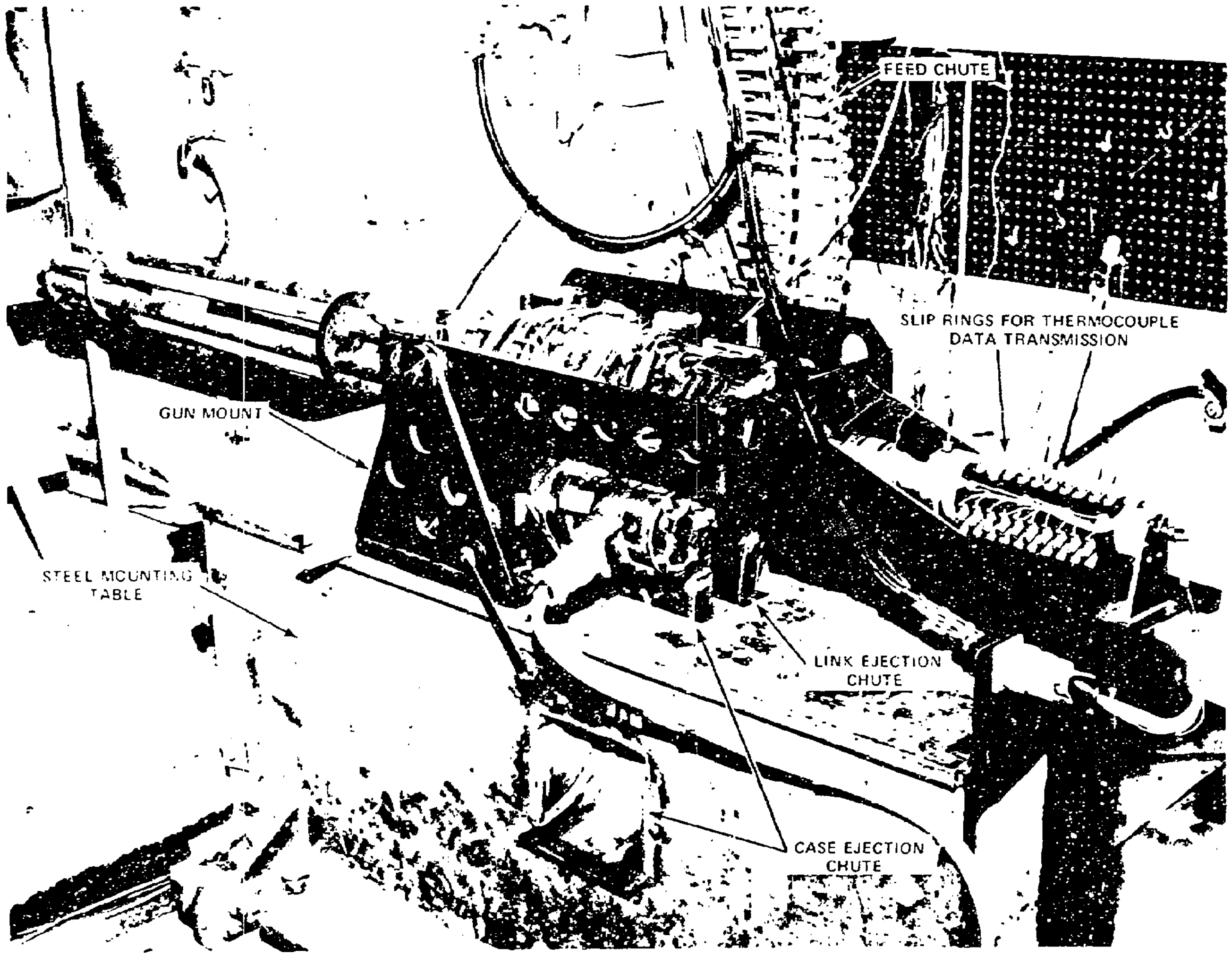
TESTS WITH AN M61 GATLING GUN

An M61 Vulcan cannon was instrumented with thermocouples which were conducted through slip rings to recording potentiometers. The gun and the slip ring system is shown in Figure 6. The testing with the M61 was rather severe from an erosion standpoint in that bursts of 200 rounds per barrel were used. The firing rate was approximately 4400 rounds per minute. The barrels were not plated.

An example of the temperatures that were experienced during and immediately after a burst is shown in Figure 7. Note that at the time of the end of the burst, the standard ammunition had transmitted 37 percent more heat into the barrel than the ablative ammunition at the point 5 inches from the breech. These effects are only partly illustrated in Figure 8. A color photograph shows that the barrels through which standard ammunition was fired are hotter over their length than the ablative ammunition barrel.

The magnitude of the erosion for the breech end of the barrel is shown in Figure 9, after three bursts of 200 rounds per barrel. Erosion at the origin of rifling was 3 mils for the ablative barrel compared to 24 mils for the standard barrel. This ratio of 8 times erosion at the origin duplicates that experienced in the M39. The negative erosion shown in the ablative barrel is illustrative of an increase in coppering of ablative ammunition in an unplated barrel. The amount of coppering shown after three bursts is very similar to that shown after one burst so it appears not to be accumulating or increasing. It was later found that the coppering was much less severe in chrome plated barrels than in the unplated barrels. Further, there again seemed to be a stabilization of the amount of coppering after the first burst in that it did not increase in subsequent bursts.

The effect of ablative ammunition on the cook-off susceptibility of the M61 gun is shown in Figure 10. Note that the cook-off threshold is never reached with ablative ammunition in a single complement of 1200 rounds while the standard ammunition reaches the ablative threshold at approximately 670 rounds. Actually with the lesser heating of ablative ammunition, it is calculated that 1370 rounds can be fired before the cook-off threshold is reached when the gun is fired at room temperature.



M61 Test Set-up
Figure 6

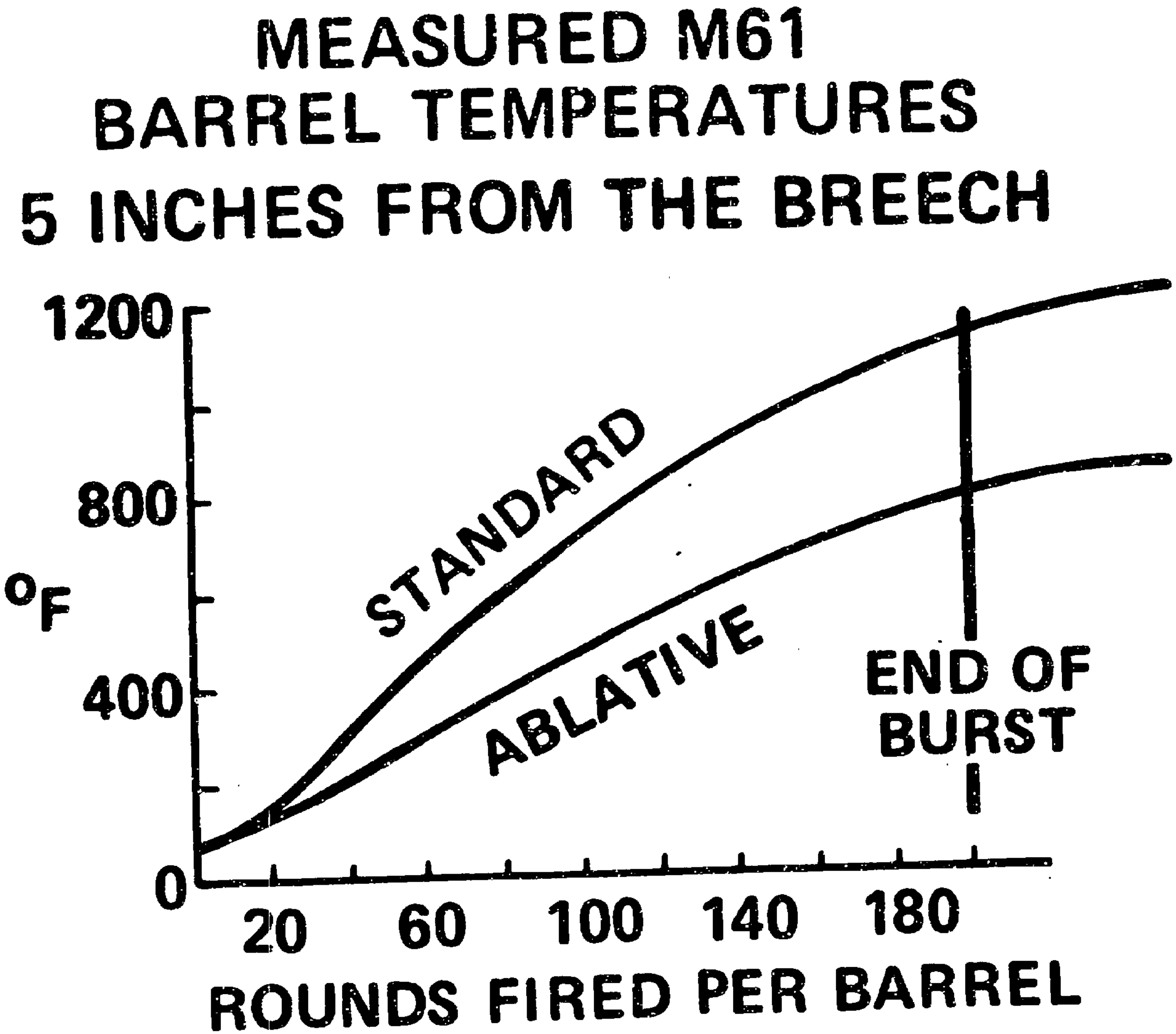

MEASURED M61
BARREL TEMPERATURES
5 INCHES FROM THE BREECH
1200
800
400
0
°F
STANDARD
ABLATIVE
END OF
BURST
20
60
100
140
180
ROUNDS FIRED PER BARREL

Figure 7

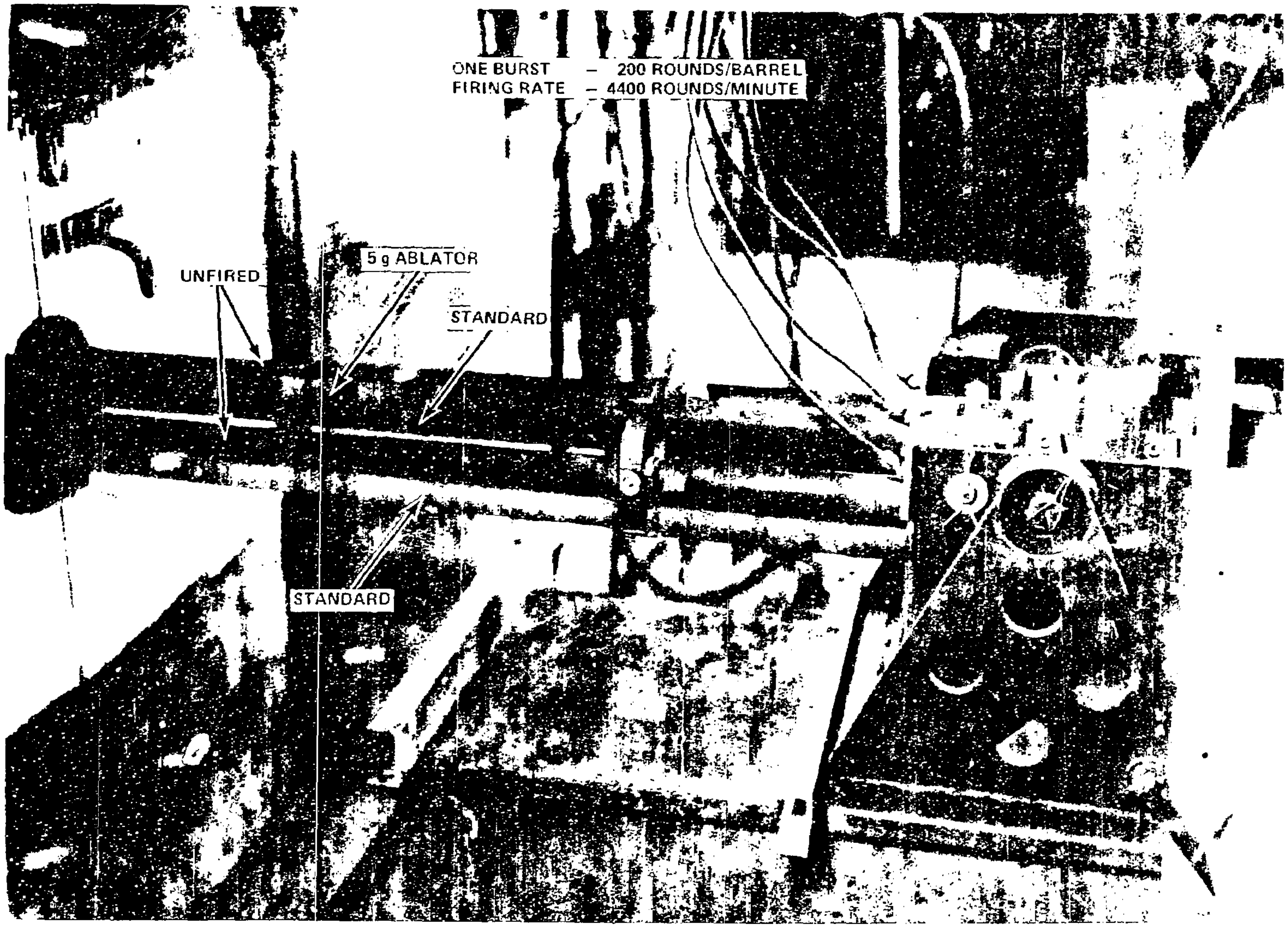

ONE BURST – 200 ROUNDS/BARREL
FIRING RATE – 4400 ROUNDS/MINUTE
5 g ABLATOR
UNFIRED
STANDARD
STANDARD

Figure 8

M61 EROSION PROFILES LAND DIAMETER

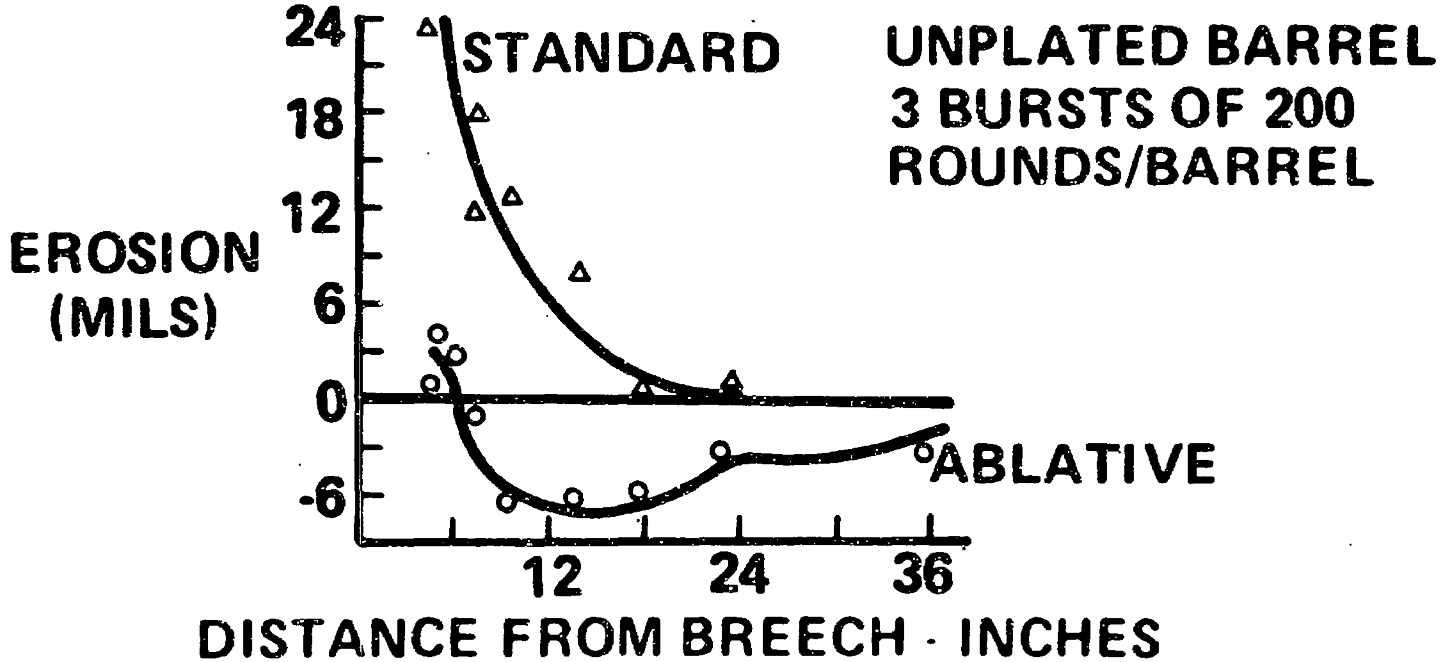


Figure 9

AVERAGE BARREL TEMPERATURES AT 5 INCHES FROM THE BREECH

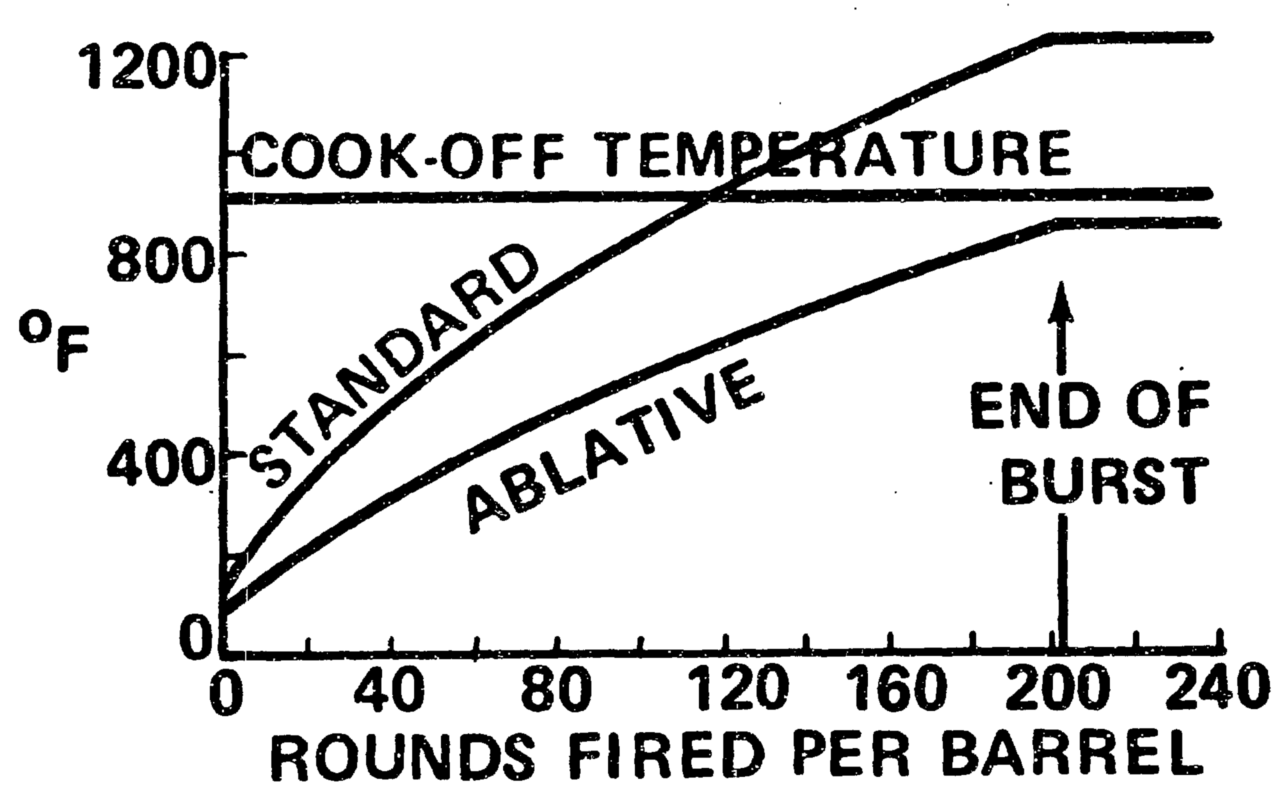


Figure 10

SUMMARY

Based on the limited testing possible in the effort just reported, the following conclusions can be drawn. Under severe burst firing conditions in the M39 gun, ablative ammunition:

1. at least triples barrel life,
2. reduces barrel erosion drastically (8 times at the origin of rifling),
3. reduces barrel and drum heating appreciably, and
4. because of great reductions in temperature, permits important weight reductions without impairing performance.

Under severe burst firing conditions in the M61 gun, ablative ammunition:

1. reduces barrel erosion significantly (8 times at the origin of rifling),
2. reduces barrel heating appreciably,
3. more than doubles the number of rounds to cook-off,
4. increases coppering, particularly in unplated barrels.

A WEAR AND EROSION RESISTANT ALLOY FOR GUN BARREL LINERS

George J. Westcoat
Senior Research Metallurgist
Teledyne Wah Chang Albany
Albany, Oregon

Good afternoon, Ladies and Gentlemen. It is a distinct privilege to have become a participant at this symposium, even though it happened with very little prior notice. Since this presentation was added after the scheduling of the other papers, it was suggested that it be brief and concise. You will be pleased to know that it is both brief and concise.

Becoming a participant, rather than a spectator, seems now to be timely and appropriate since the agenda of technical paper presentations is very top heavy with identification and quantification of conditions contributing to excessive wear and erosion of gun tubes; and is very light on potential solutions to problems created by these conditions. This presentation will, hopefully, introduce a corrective concept for your consideration and possible utilization. The concept of using liners in gun tubes is certainly not novel; however, the material proposed for this application will, undoubtedly, be both new and novel to most of those in attendance here today.

As the largest producer of refractory metals and alloys in the "free" world, Teledyne Wah Chang Albany has naturally been instrumental in the development of alloys for very high temperature applications. Notable of these developments is WC-3015, a columbium alloy containing approximately 30% hafnium and 15% tungsten, whose properties were compatible with the environments encountered by rotor blades during early developmental stages of turbine engines. Improved turbine design decreased enviromental hostility for rotor blades allowing less refractory, and less costly materials, to provide satisfactory performance; and 3015 thus saw little application for which it was intended. Modifications of the basic 3015 alloy, however, present interesting properties for satisfactory performance at the breach end of certain gun barrels. However, a much more complete knowledge of the operating conditions must be obtained, than has been previously known, if an alloy is to provide optimum performance.

Optimum performance must be recognized as the most satisfactory compromise obtainable in a combination of adverse and opposing conditions.

Designers, not necessarily well versed in materials technology, can be somewhat reassured in the knowledge that those whose business is materials technology can provide a greater latitude in meeting specified design properties through the several mechanisms of fabrication sequence, alloy modification, and thermal treatments. Thus, it is possible, through knowledge of a weighted effect of each alloying element upon individual physical or mechanical properties, to custom tailor (within reasonable limits) a material to an application.

Just as the developer of an armor piercing round must have total knowledge of the characteristics of the armor he must defeat, so must the materials engineer have total knowledge of the conditions and environments to be combated by his material. Priorities must be established as to the most troublesome effect to be overcome. These priorities are also dependent upon the gun tube configuration; i.e., whether homogeneous (a single alloy), or composite (two or more alloys), as is the case of a barrel liner.

When two or more materials in intimate contact with each other are subjected to rapid temperature variations of fairly great magnitude, coefficients of thermal expansion and thermal conductivity become design considerations of great importance. Since most chemical reaction rates vary as a logarithmic function of absolute temperature, the maximum predicted temperature has a great influence on the amount of erosion or corrosion resistant element included in the alloy. If wear is the greatest degradation factor, then relative hardnesses, coefficients of friction, available lubricants, etc., have great importance.

It is our belief that the past two years of work allow us to offer a material which has attained a status requiring very little further refinement to become a means whereby the functional life of gun tubes can be extended beyond their present capabilities. Excellent oxidation resistance is achieved when WC-3015 alloy is exposed to oxygen at elevated temperatures, as elemental hafnium and columbium oxidize preferentially. The HfO_2 then dissolves in the columbium oxide Cb_2O_5 to form $6HfO{-}Cb_2O_5$. This complex oxide has a tight cubic lattice which resists the diffus of oxygen into the substrate. After 20 minutes exposure at $1835^{\circ}F$, oxide penetration is 0.001-0.003 inch. The alloy is also highly resistant to attack by normal acidic environments.

TABLE 1

WC-3015 ALLOY

Chemical Composition

Hafnium	28-30
Zirconium	1- 2
Tungsten	13-16
Tantalum*	0- 4
Titanium**	0- 5
Carbon***	0.07-0.33
Nitrogen	0.02 max.
Oxygen	0.03 max.
Hydrogen	0.001 max.
Columbium	Remainder

* Tantalum is listed because it occurs naturally in columbium ores. Higher level tantalum does not alter properties, except to increase densities. WC-3015 can be furnished with reduced tantalum if desired.

** A five percent titanium addition is optional. Titanium increases oxidation resistance and fabricability and decreases stress-rupture strength.

*** Carbon content may be tailored to the need within 150 ppm. Increased carbon improves strength at a modest cost in oxidation resistance.

TABLE 2

WC-3015 ALLOY

Physical Constants (Approximate)

Density, lb/cu in. (No Titanium)	0.38
(Titanium-containing modification)	0.36
Specific gravity (No Titanium)	10.5
(Titanium-containing modification)	10.0
Melting point, °F	4540
°C	2500
Specific heat, cal/g/°C	0.07
Modulus of elasticity, psi	15×10^6
Modulus of rigidity, psi	6×10^6
Coefficient of Thermal Expansion (cm/cm)/°C	$7.97 \pm .08 \times 10^{-6}$

TABLE 3

WC-3015 ALLOY

Typical Mechanical Properties
(Hot extruded and recrystallized by annealing)

Property	Tested at 75°F	Tested at 1400°F	Tested at 2200°F	Tested at 2400°F
Tensile Strength, psi	140000-147000	100000-116000	- - -	40000
Yield Strength (0.2% Offset),psi	130000-140000	70000- 90000	- - -	36000
Elongation (4D),%	5-35*	5 min	- - -	20 min
Stress Rupture (100-hr test),psi	- - -	- - -	18000-20000**	- - -

* Elongation depends on thermal history, see Table 2

** Attained by using the necessary thermal processing

FIGURE 1

At one time, the room temperature ductility of 3015 was considered to be poor; the final slide graphically illustrates that this problem has been overcome. The slide discloses what resulted from an attempt to shrink fit a 3015 liner tube into a 4340 steel gun barrel by refrigerating the tube in liquid nitrogen and attempting to press it into the barrel. When the heat transferred from the massive steel to the liner, it expanded and was caught in the barrel; but while still at a very low temperature, the liner had sufficient ductility to balloon as shown, without any indication of cracking or failure.

In summary, the intent of this presentation was to provide you with data on a refractory metal alloy with potential for solutions to some of the erosion and war problems in gun tubes. Obviously, no panacea for all these problems can exist since opposing properties of materials are needed for their solution. However, because of the uncommon properties it has, it may very well serve a useful function in prolonging gun tube life.

The proprietary nature and security classification of an experimental program recently conducted with a European arms manufacturer greatly limits the information which can be discussed here. What can be said is that a steel barrel alone survived fewer than half the rounds that were fired in a weapon with a 3015 modified liner, before a premature liner failure concluded the test. Little or no erosion or wear were noted in the liner, which had cracked at a contaminant inclusion location. Incidentally, homogeneity and cleanliness difficulties have been eliminated from recent alloy heats.

Additional development work is, undoubtedly, required before optimum performance can be achieved, but a tremendous amount of prior development has already been completed. For any interested in pursuing this avenue of effort, Teledyne Wah Chang Albany would be very pleased to work wi... you.

Thank you for your attention.

SESSION V

MATERIALS, ROTATING BANDS AND DESIGN

Chairman: Dr. Philip Parrish

US Army Research Office

EROSION ANALYSIS AND CONTROL STUDIES AT ROCK ISLAND ARSENAL - A REVIEW

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ABSTRACT

A summary of gun barrel technology efforts at Rock Island Arsenal in the area of small caliber gun tube erosion and its control is presented. The intent of this review is to apprise the technical community of such information so that duplication can be avoided and also so that further improvements can be made on methods to select materials and to design for rapid fire weapons in the future. Considerable effort has been made on analyzing erosion in rapid fire gun tubes since 1970. Structural damage has been characterized for a number of gun systems with various material construction and under various firing schedules. Methodology to analyze erosion damage was developed. A number of material systems were selected and tested. Numerous iron alloys, superalloys and refractory alloys were fabricated into various structural configurations (monolythic, layered and coated systems) and then test fired and analyzed. New processes to apply erosion resistant materials (sputter, chemical vapor deposition and electrodeposit coatings) on gun tube ID surfaces were evaluated. Material systems have been evaluated and tested in systems ranging from 4.32mm to 30mm. Thermal measurements and analytical techniques to ascertain temperature distribution and heat flow were developed and evaluated. The effect of mass distribution on gun tube service performance was also investigated. Limitations of present methods to design and select materials for rapid fire weapons are cited.

INTRODUCTION

The studies on gun tube erosion during World War II constitute a significant portion of what is known on the subject.[1] Although limited work was continued after 1946 to unravel basic erosion mechanisms, most of the R&D effort was on erosion control measures -- improved propellants, wear reducing additives and chromium plating. However, until the Vietnam involvement in the late sixties, significant changes had not occurred on methods to control erosion in small caliber gun tube systems. Furthermore, the majority of erosion study and control work up to this period was concentrated on large caliber systems.[2]

This paper reviews the efforts at Rock Island Arsenal to analyze and control erosion in small caliber, rapid-fire systems. No attempt is made to discuss ammunition or weapon design studies. This paper gives a broad overview of materials and thermal analysis activities at Rock Island from the period 1970 to present. No attempt is made to give a comprehensive dissertation on the subject matter. Instead, documentation of reports and activities is given. The initial phases included characterization of erosion in conventional rapid-fire gun tubes and thermal analysis. Other phases involved methods to control erosion through the application of heat resistant material systems. This aspect involved considerable work on the development of fabrication techniques to produce monolythic and multilayer gun tube structures. Test firing and post-firing analysis were performed.

EROSION ANALYSIS

The erosion analysis to characterize the chronological development of structure alteration and damage involved several analytical techniques: electron and scanning election microscopy, election microprobe, optical metallography, X-ray diffraction, microhardness measurements and selective chemical etching.[3] Although several materials were tested, the major emphasis was on the basic Cr-Mo-V steel used in the 7.62mm systems.[4] On addition to actual test firings, laboratory tests including vented-bomb,[5] wear,[6] fatigue,[7] elevated temperature fatigue[5,7,8] and instrumented charpy,[9] hot hardness,[5,10] adhesion,[11] thermal and chemical reactivity tests [3,8,12] were performed. These studies were performed to isolate important erosion parameters and to provide rationale for gun tube materials selection. It was evident that no single method could provide a good screening test for materials selection. Certain tests as the vented bomb could not be correlated with actual test firing results.[5] The results of all the tests performed are available in the literature as cited. The analysis of the altered gun tube structure is given in another paper at this conference.[13] Measurement of transient bore temperatures were also performed.[14]

MATERIALS

Prior to 1969, the materials generally used for small caliber were primarily limited to medium alloy steels either unplated or chromium plated and with or without cast cobalt alloy Stellite 21 breech insert liners. The physical limitations of shrink fitting liners precluded full length lined gun tubes. However, with materials and fabrication technology developed for aerospace and nuclear industrial needs, new heat-resistant materials were now possible for gun tube application.

In the early 1970's, Rock Island Arsenal investigated numerous heat resistant material systems for small caliber, rapid-fire gun tube application. New methods as roll bonding[15] and co-extrusion[16] made possible the fabrication of full length lined gun tubes -- gun tubes with heat-resistant liner materials jacketed with less-espensive but high strength materials. Other methods as pressure bonding were attempted.[17] Newer rifling rechniques as rotary forging or swaging[18,19,20,21] were developed to impart internal rifling to these new difficult-to-machine alloys. Further, new and improved coating methods were applied to deposit heat-resistant materials to the ID gun tube surfaces.[22,23,24,25] Test firing data and post-firing analysis were obtained for over thirty material systems. (See Tables 1 through 3).

Three basic material classes were evaluated: (a) refractory alloys, (b) heat resistant iron-base alloys and superalloys and (c) low cost steels. Because the multivariant factors causing gun tube wear and erosion make the quantitatively description of ideal gun' tube material system or the evaluation by means of a simple simulation test virtually impossible, the several material systems were fabricated into 7.62mm gun tubes and evaluated by actual test firing. Two types of 7.62mm gun systems were used for the evaluation: the M134 minigun, (six-gun tube Gatling) and the M60 weapon. The rate of material deterioration was found to be heavily dependent on the firing schedule. Generally, the firing schedules used for the M134 and the M60 systems were, respectively, as follows:

(a) (500-round burst at 4000 spm rate with a 10-second cooling interval) repeated 4 times before complete cooling and then repeated to failure. (Schedule for six barrel complement).

(b) (125 round burst at 650 spm rate with a 10-second cooling interval) performed 6 times prior to complete cooling; this schedule was repeated to failure.

All ammunition used in these tests was the standard 7.62mm M80 NATO ammunition containing WC 846 propellant. Projectile yaw, loss of velocity or loss of accuracy constituted the criteria for gun tube failure.

Figure 1 summarizes the test firing results for a monolythic structure in the M134 and M60 gun tube configuration. Except for Pyromet X-15 and CG-27, the service life appears to increase with elevated temperature tensile strength. However, these monolythic structures do not have the resistance to gun tube wear as the chromium plated gun barrels (Figure 2). The general mode of failure for the monolythic systems was muzzle end wear as opposed to severe breech-end erosion found for the Cr-plated steel gun tubes. As

Table 1. Monolythic Gun Barrel Materials Investigated

Steels	
	Cr-Mo-V steel
Iron Base Alloys	
	Armco 21-6-9
	Armco 22-4-9
	Allegheny A286
	Crucible CG-27
	Pyromet X15
Nickel Base Alloys	
	Inconel 718
	Udimet 700
Cobalt Base Alloys	
	HS 25 (L605)

Table 2. Coated Gun Barrel Systems

Base Material	Coating
Cr-Mo-V steel	Cr TiC Nitride Tufftride W (Sputtered)* W (CVD)*
AISI H 11	Cr
Vascojet MA	Ta-10W (CVD)*
Crucible CG-27	Cr
Incaloy 903	Cr
Inconel 718	Cr
Inconel 718	Ta-10W (CVD)*

*either unsuccessful application or unsuitable for test firing purposes

Table 3. Experiemental Multilayer Gun Barrel Systems

Jacket Material	Liner
CG-27	W*
4150 steel	Ta-10W
4150 steel	T222 (Ta-alloy)
CG-27	Mo-TZM
CG-27	Mo-TZM (borided)
Marage 300	Mo-TZM
A286	M-TZM*
CG-27	WC 103Y (Cb-alloy)
CG-27	WC 129Y (Cb-alloy)*
A286	WC 129Y*
V57	HS21 (P/M, wrought)
Cr-Mo-V steel	HS21 (cast)
Marage 300	Mar M509 (Co-alloy)
4150 steel	HS25 (Co-alloy)
Marage 300	Udimet 700 (Ni-alloy)
4150 steel	Mar M509

*unsuccessful fabrication

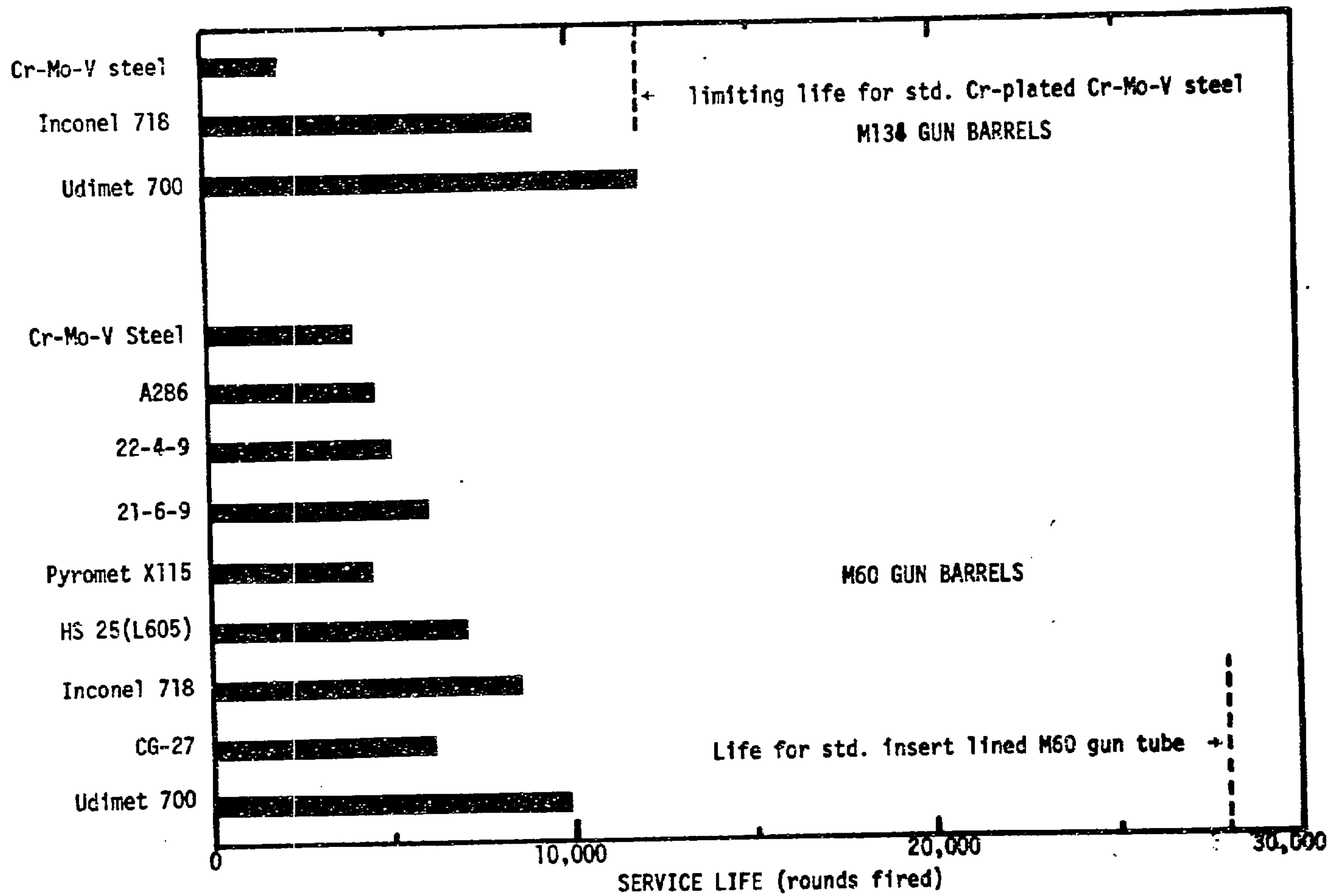

FIGURE 1. Effect of Material on the Service Life of Monolythic 7.62mm Gun Tubes

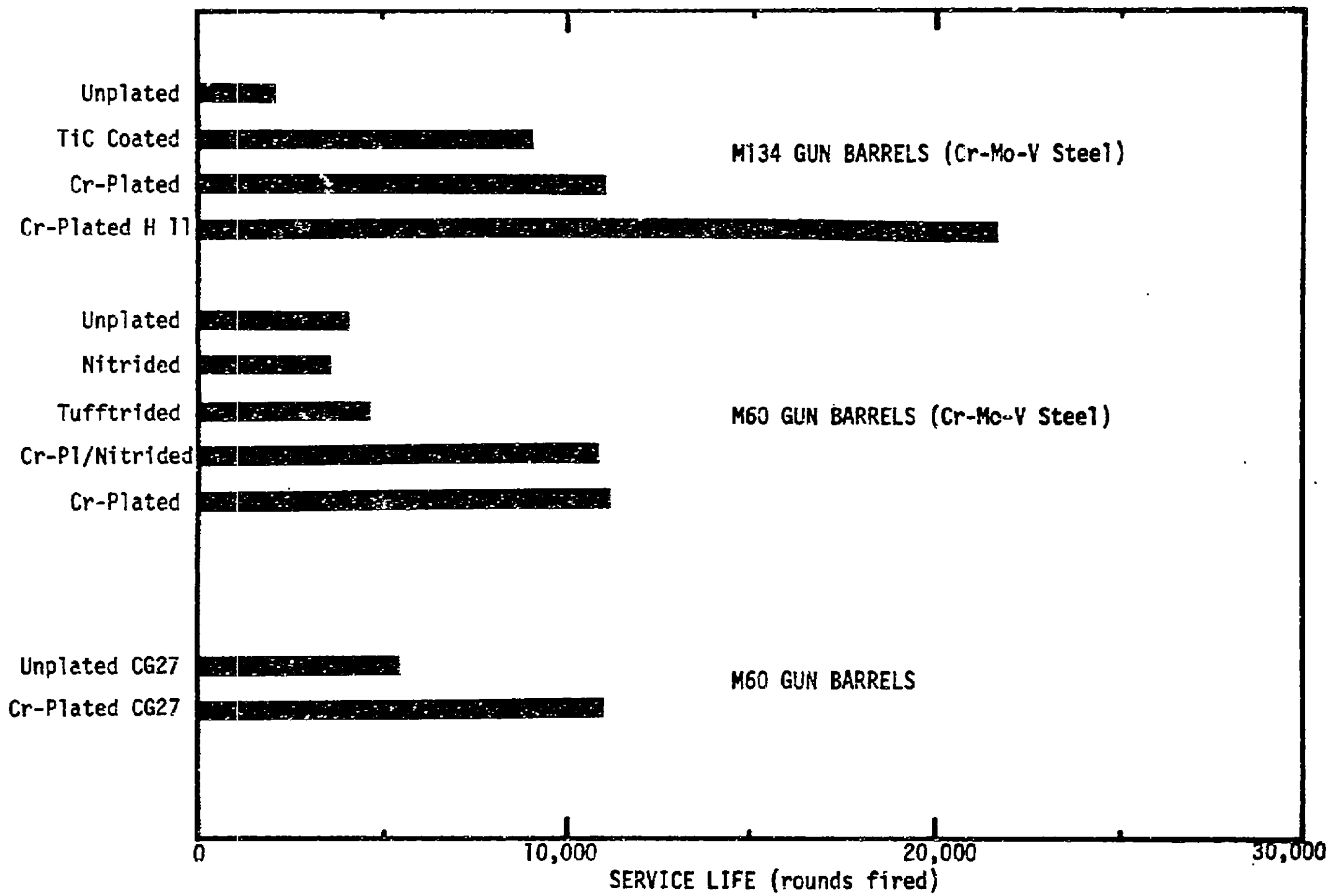

FIGURE 2. Effect of Coating/Substrate Material on Service Life of 7.62mm Gun Tubes

shown in Figure 2, for the minigun (M134) Cr-Mo-V steel gun tubes, wear life is increased by the TiC- and Cr-coating. However, if Cr-coated die steel, AISI H-11, is substituted for Cr-plated Cr-Mo-V, then the wear life can be doubled. The H-11 steel can be substituted for conventional steel now used in gun tubes. Apparently, the increased chromium content in H-11 steel over that in Cr-Mo-V steel gives enhanced hot strength and corrosion resistance. In Figure 2, test firing results are also shown for nitrided and tufftrided M60 steel gun tubes. Note that these surface treatments did not appreciably improve wear life. When electrolytic chromium is coated on bare or nitrided steel, the wear life was increased significantly. However, the wear life of chrome plate steel is appreciably less than that of the standard M60 gun tube with the breech HS-21 liner. Chromium plated CG-27 is shown to exhibit double the service life of bare CG-27. However, the adhesion strength of Cr-plate on many of the superalloys appears significantly lower than that on Cr-Mo-V or H-11 steel.

The test firing results for multi-layered gun tubes is graphically illustrated in Figure 3. Several observations can be made from this graph. The service life the full length lined gun tubes is not necessarily dictated by the melting point of the liner material. The molybdenum alloy TZM- and columbium alloy WC103-lined gun tubes exhibit lower wear life than the cobalt alloy HS-21 breech lined Cr-plated steel M134 gun barrel. Also, the HS-25 (L605) lined gun tube shows nearly the equivalent service life as the tantalum alloy T-222 lined 4150 steel gun barrel. Also, the material performance in multi-layered structures appear to be significantly different from monolythic forms. The cobalt alloy HS-25 exhibits significantly better wear life when backed with a 4150 steel jacket than alone in a monolythic gun tube. The differential in thermal expansion between the steel and HS-25 may be important in inhibiting enlargement of the bore. An important aspect of erosion or wear in most small caliber gun tubes is that surface melting does not appear important in the deterioration of the gun tube material.

THERMAL ANALYSIS

Considerable effort has been performed in the area of thermal analysis related to rapid-fire gun barrels. Computer aided Fourier analysis and implicit finite difference techniques coupled with experimental data have yielded information on transient heat distribution at various gun tube locations as a function of firing schedule.[26-31] Models incorporating various flow conditions and boundary layers have been examined.[32] Practical aspects of the thermal studies have yielded application of insulative oxide intermediate layers in gun tubes to reduce outer wall temperatures[33-35] and

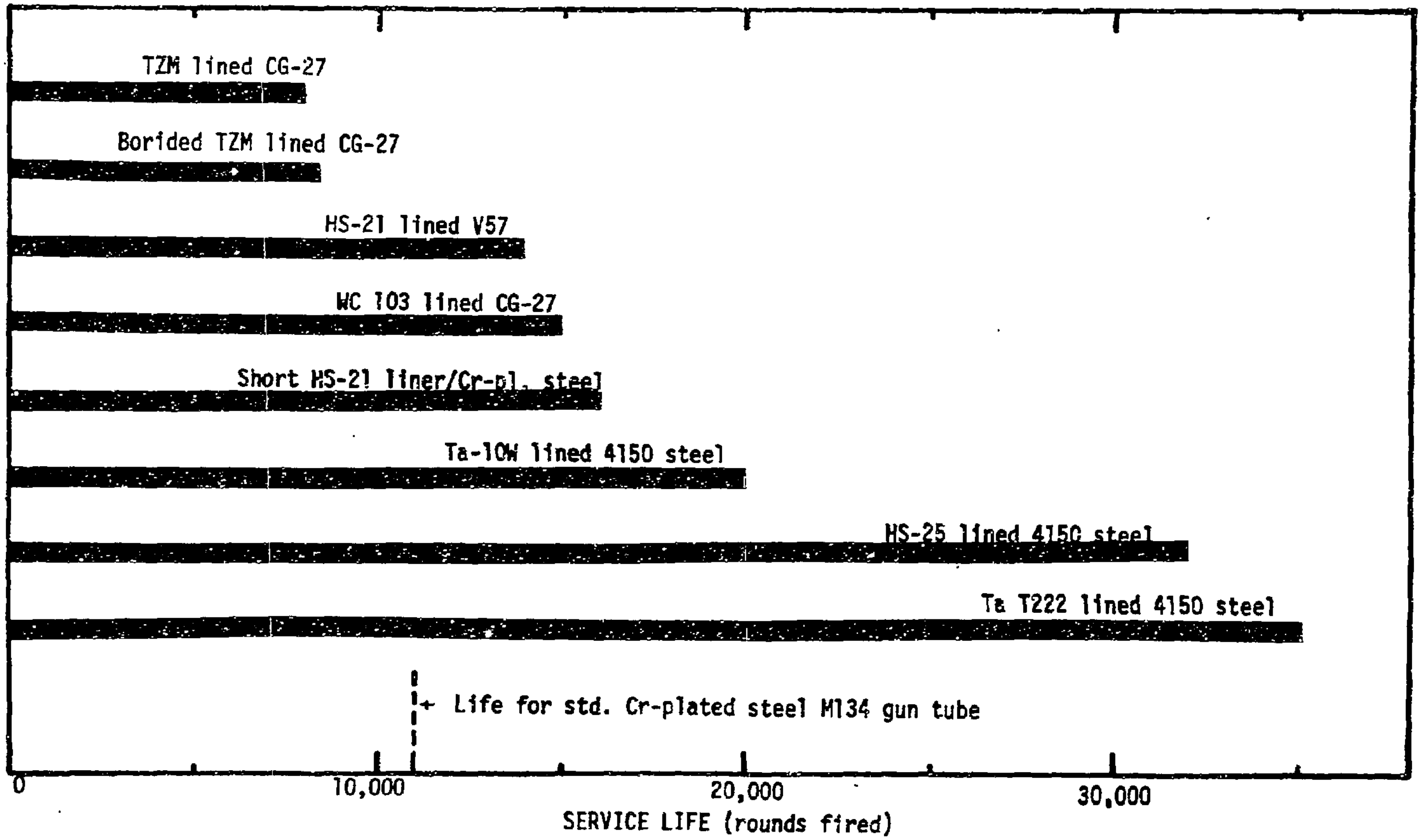

FIGURE 3. Effect of Liner/Substrate Material System on Service Life of 7.62mm M134 Gun Tubes

geometric and material systems considerations have been incorporated into gun tube design. Design models have been applied to such systems as the 30mm gun and certain furture rifle systems. The effect of geometry or material mass on erosion has been studied in the 7.62mm gun system utilizing various OD configurations. [36,37] An inverse correlation between wear and the amount of gun tube material mass was noted. Other methods to promote lower gun tube temperature as ablative cooling[38] and use of material phase changes [39] were applied.

APPLICATION OF EROSION STUDIES TO WEAPON SYSTEMS

4.32mm

Material systems found to be effective in combating erosion in the 7.62mm systems are currently being tested in the 4.32mm system. This system incorporates the 5.56mm propellant with the smaller projectile. Both soft and hard chromium plated H-11 steel,[40] Cr/Co platings on H-11 and co-extruded HS-25 lined H-11 gun tubes are being tested. This project will also examine the effect of propellant additives to combat the severe erosion problem associated with this system. Other material systems evaluated in this project included CVD tungsten and electroless nickel (phosphorous) coatings on steel.[41] Thermal analysis involved utilization of water cooled gun tube which doubled the life of an uncoated steel gun tube dramatizing the role of thermal factors.

5.56mm

This development machine gun system will incorporate Cr-plated H-11 steel gun tubes.

7.62mm

As mentioned earlier, significant improvements were made over the standard Cr-plated Cr-Mo-V steel minigun barrels. Notably, H-11 steel could easily be substituted for the conventional steel without undue cost sacrifices. However, the existing stockpile of spare conventional gun tubes now exceeds a 13-year supply.

Caliber .50

The conventional caliber .50 gun tube is erosion limited. Future improvements will consider thicker wall construction and the use of H-11 steel to replace the standard Cr-Mo-V steel.

20 to 30mm Systems

Many of the machine guns in this classification do not encounter severe erosion problems because of the low rate of fire or low muzzle velocity requirements. Higher performance systems, as the 30mm XM140, have shown successful performances with unplated and Cr-plated CG-27 gun tube material systems.[42] Other developmental systems as the 25mm class using different material systems are now being field tested.

SUMMARY

A significant amount of work has been undertaken related to gun tube erosion as a part of the small caliber gun tube technology mission at Rock Island Arsenal. Evaluation of erosion, thermal analysis, various material systems, and advanced fabrication techniques have been explored and developed. This information should aid current and future weapon technologists in the design, material selection and service prediction of new weapon systems.

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BARREL LIFE IN HIGH RATE OF FIRE GATLING GUNS

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This paper presents barrel life and erosion data on high muzzle energy 20 and 30 mm gatling guns. The 20 mm 6 barrel M61A1 and the 3 barrel M197 use the same barrel and both fire copper (90% copper, 10% zinc) banded M50 series ammunition. A good data base on barrel performance has been established for these two guns. The data has been used to develop an empirical barrel erosion model which accurately predicts erosion for any desired rate and firing schedule. The GAU-8/A is a new system which has fired over 550,000 rounds with plastic rotating bands. Plastic bands significantly improve barrel life while virtually eliminating velocity drop in a worn barrel. Plastic bands produce a unique erosion pattern. Erosion is being studied and tests are being made to obtain even better barrel life.

Figure 1 describes our developed gatling gun systems. The 20 and 30 mm systems were selected for discussion because they fire high energy rounds which erode barrels sooner than the other rounds listed.

Gatling gun barrels frequently operate at elevated temperatures where erosion rates are quite high. The barrel material must be carefully distributed to provide a heat sink where erosion occurs and not add weight where it is not needed. Gun barrels are a significant portion of the gun weight. For example, as shown in Figure 2, gun barrels represent 60% of the GAU-8/A gun weight.

Erosion of the 20 mm guns will be discussed first. Most of the data has been collected on the 6 barrel M61A1 gun. However, since the same barrel and ammunition is used in the 3 barrel M197 the following data can be applied to that gun by dividing the gun rate and gun rounds by two. The M61A1 barrel is a chrome plated barrel made from CrMoV barrel steel per MIL-S-46047. The rifling is a gain twist with an equation $Y = KX^{1.625}$.

Life of M61A1 barrels is primarily determined by the burst length and the firing rate. This is illustrated by Figure 3 showing barrel life for gun firing at 1500 and 6000 spm. Examination of Figure 3 shows that in a worst case combat situation, the barrel can be worn out in a single 15 second burst at 6000 spm (1500 rounds). In actual application, such severe schedules are seldom fired. The minimum technical manual replacement schedules on the ˋˊ¹A1 gun barrel are 21,000 gun rounds and range up to 72,000 gun rounds. (Bree tures may occur with the later replacement schedule.)

Erosion of M61A1 barrels occurs almost exclusively near the initiation of rifling. The point of maximum erosion is approximately 55½ inches from the muzzle (one-half inch in front of the case neck). Typical erosion patterns versus distance from the breech are shown in Figures 4 and 5. Figure 4 shows the progression of erosion on one cluster as rounds are accumulated. Figure 5 presents the erosion patterns for three different worn out clusters.

Figure 6 shows the results of our empirically based erosion prediction model compared with test data. With a fixed firing schedule, erosion per complement 55.5 inches from the muzzle continues at a rather constant rate. This has allowed us to develop a good erosion prediction model for any firing schedule and firing rate based on the bore surface temperature at bullet passage. Predicted erosion has been surprisingly close to actual test data with M55 ammunition. Erosion prediction is done with a computer program which calculates three-dimensional heat transfer combined with a plastic stress analysis. The erosion rate is very sensitive to bore surface temperature and axial heat transfer must be included in the analysis. The effect of axial heat transfer on soak-out temperature (in the GAU-8/A) is shown in Figure 7.

When an M61A1 barrel is fired until it is worn out, the limit of 6% velocity drop is always reached prior to yaw failures. During very severe schedules, yaws may occur; however, yaw failures are of no practical significance, since they occur very close to the rupture point.

Typical erosion of an M61A1 barrel can best be described as a classical loss of chrome plate followed by rotating band and gun gas erosion. After sufficient erosion has occurred near the initiation of rifling, the rotating band no longer contacts the barrel and erosion is caused only by gas erosion. The gas erosion leaves numerous axial depressions, as shown in Figure 8. The depressions extend significantly below the diameter measured by a split collet-type erosion gauge. The depressions typically have a smooth contour and do not show fatigue type cracks. Worn barrels occasionally rupture in this area; however, to date, we have never seen a true fatigue failure in an M61A1 barrel.

The erosion life of the M61A1 barrel can be improved for long bursts at a high rate of fire by adding material to the outside at the initiation of rifling. A test will be conducted in the next few months with three barrels with an outside diameter of 1.8 inches and three standard barrels with an outside diameter of 1.56 inches. If a significant increase in barrel life is achieved, the barrel can be redesigned to remove material in other areas and keep the weight constant. The change will also improve the burst capability of worn barrels. New barrels fail in bursts of 212 to 290 rounds per barrel (1272 to 1740 gun rounds) at the muzzle. Badly worn barrels have failed at the breech in 144 rounds (864 gun rounds) due to loss of wall thickness by erosion.

The GAU-8/A gun fires a high energy round as shown in Figure 9. Chrome plated CrMoV barrel steel was selected for the barrels after an extensive tradeoff including tests with a 30 mm single barrel gun. Low cost and reliability were the primary reasons for selecting a conventional material. Superalloy materials initially looked promising due to weight savings; however, final designs did not give significant weight savings for GAU-8/A firing schedules. The barrel steel meets MIL-S-46047 with an additional requirement. It must have a slow strain rate yield strength of 35,000 psi at 1200°F.

Three rotating band materials have been fired in the GAU-8/A gun. The initial General Electric funded gun development was performed with iron banded ammunition. The Phase I tests were performed with copper bands. Plastic bands were first tested late in Phase I during the competitive shoot-off. They were adopted during Phase II and the GAU-8/A guns have currently fired over 550,000 rounds with plastic-banded ammunition.

Both the copper and iron rotating bands were fired in a gain twist rifling designed to accomodate significant erosion by allowing a 4-inch travel prior to beginning the twist of the rifling. The iron bands were fired in early gun development when firing schedules were not severe. Unfortunately we do not have barrels which fired copper bands with a comparable firing schedule. Our best estimate is that the erosion rate of iron and copper is very similar. Both produce erosion patterns similar to the M61A1 with essentially all of the erosion occuring in the breech region. Progression of erosion for a moderate firing schedule with copper bands is shown in Figure 10. After 3675 rounds per barrel (25,725 gun rounds), the barrels are essentially worn out with a velocity drop of 9% and occasional tumbling of rounds. (Iron banded rounds were still performing satisfactorily.)

2500 rounds of ammunition with glass filled nylon 6-12 bands were fired in one barrel during the Eglin evaluation. Results compared with barrels firing copper bands are shown in Figure 11.

Based on the success of initial testing, plastic bands were selected for Phase II gun development. Breech erosion was not expected so the rifling was changed to constant twist. Results of tests have shown that this was a good decision. Barrel life has not proven to be infinite as it initially appeared. However, it has been much better than with copper bands. Plastic bands have the added advantage that very little velocity drop has occurred when barrels are worn out.

Good barrel life is achieved with plastic bands by giving much longer retention of chrome plate. Once the chrome plate is lost, relatively minor erosion will cause rotating bands to strip and rounds to tumble. An example of the relatively minor erosion which can be tolerated is shown in Figure 12. This is a cross-section of a silicon cast taken in a non-chrome plated barrel after 1500 rounds. Rounds were tumbling at this point.

The first barrels fired to failure with plastic bands were fired at our Ethan Allen Firing Range in a 50,000 round endurance test. The gun was fired with a rather severe schedule including 11-2 second bursts at 4200 shots per minute with one minute between bursts. (1350 total rounds were fired in each complement with free convection cooling.) Failure occurred after 28,000 gun rounds by a unique erosion pattern which we have not found referenced in the literature. Land erosion began approximately 6 inches from the initiation of rifling and became progressively worse until approximately 20 inches from the initiation of rifling the lands were completely worn away. Further down-barrel the erosion became less with essentially zero erosion after 40 inches. The erosion versus distance from the aft end of the barrel is shown in Figure 13.

Erosion vs. rounds is shown in Figure 14. This erosion pattern is similar to muzzle erosion with high velocity rounds. However, rather than continually increasing toward the muzzle, in tests with plastic bands, the erosion reached a peak, then decreased. The rifling cross-section at several points is shown by photographs of a silicon cast in Figure 15 through 19.

Observation of subsequent sets of barrels indicated that the initial erosion occurred about six inches from the initiation of rifling as a loss of chrome plate on the driving edge of the land. After the initial loss of chrome plate on the leading edge, the erosion progresses down-barrel.

Aircraft tests resulted in a different mode of barrel failure. Test conditions are significantly different because good cooling of the aft half of the barrel is provided by an air scoop. The barrels failed after about 26,000 gun rounds due to erosion at the initiation of rifling. Loss of chrome plate followed by minor erosion was sufficient to cause rotating band failures. Down barrel the chrome plate was just beginning to break off from the driving edge of the land. It should be noted that loss of chrome plate at the initiation of rifling would also have occurred on the barrels firing in ground testing if a few more rounds had been fired. A micro-section showing the cracking of the chrome plate is shown in Figure 20.

An interesting difference in barrel heating is shown in Figure 21. Barrels firing plastic rotating bands absorb significantly less heat at the initiation of rifling and slightly more heat down-barrel.

Very significant improvements in barrel life and velocity loss with worn barrels have been made by using plastic rotating bands. However, design of barrels for plastic rotating bands is in its infancy. Many unknowns still exist, such as erosion with very severe firing schedules, the cause of erosion and the variations in heat input with plastic bands. General Electric is currently completing a set of barrels which will provide erosion data with several variations in the barrel design. This set of barrels includes:

1. Thicker chrome plate
2. More lands and grooves
3. A variation in the inside rifling at the initiation of rifling
4. More material on the outside
5. Nitriding prior to chrome plating

In summary, the M61A1 barrels fail by velocity drop due to breech erosion. The erosion is predictable and a good empirical model has been developed to predict barrel life for any desired firing schedule and rate of fire. The GAU-8/A barrels firing iron or copper rotating bands also fail by breech erosion. Plastic bands provide a significant improvement in barrel life while virtually eliminating velocity drop. Failure of barrels firing plastic bands is caused by minor erosion which results in failure of the projectile to spin. The erosion may occur at several points in the barrel and is always associated with loss of chrome plate.

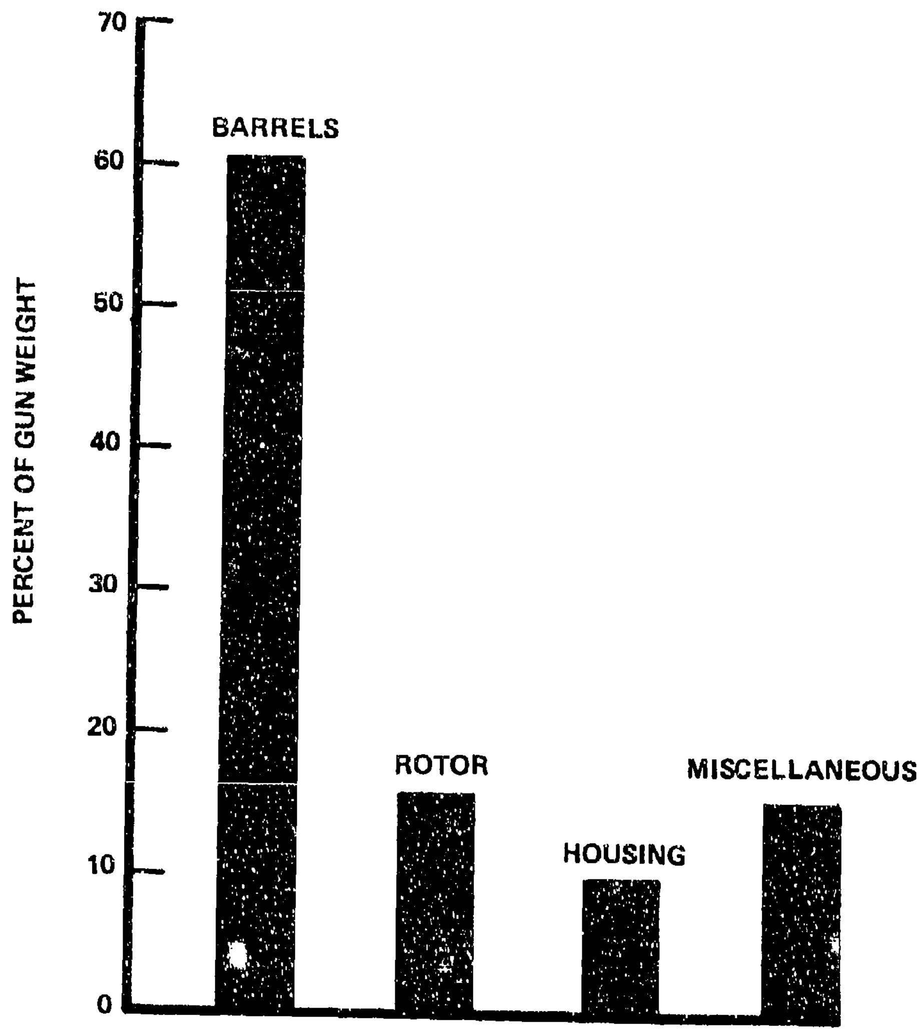
GUN	CALIBER MM	# BARRELS	MAXIMUM RATE SPM	K.E. FT-LBS	MUZZLE VELOCITY FT/SEC
XM-214	5.56	6	10,000	1310	3270
GAU-2B/A	7.62	6	6,000+	2620	2807
M61A1	20	6	7200	41,140	3440
M197	20	3	3000	41,140	3440
GAU-8/A	30	7	4200	147,500	3470
XM188	30	3	3000	36,900*	2325*

* CURRENTLY BEING REDESIGNED FOR ADEN-DEFA ROUND

Developed Gatling Guns

Figure 1.

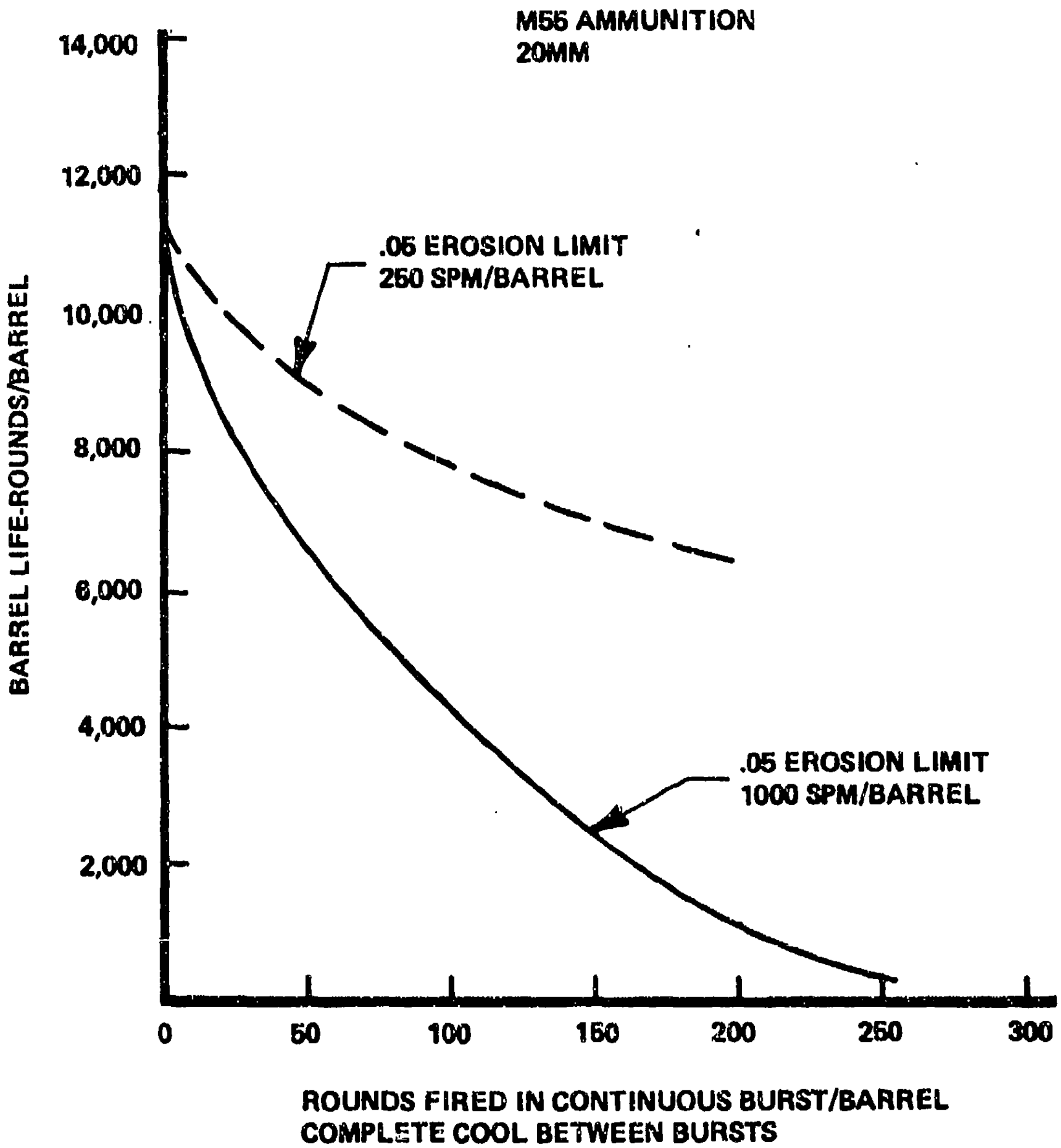
Weight Distribution of GAU-8 A Gun



Weight Distribution of GAU-8/A Gun

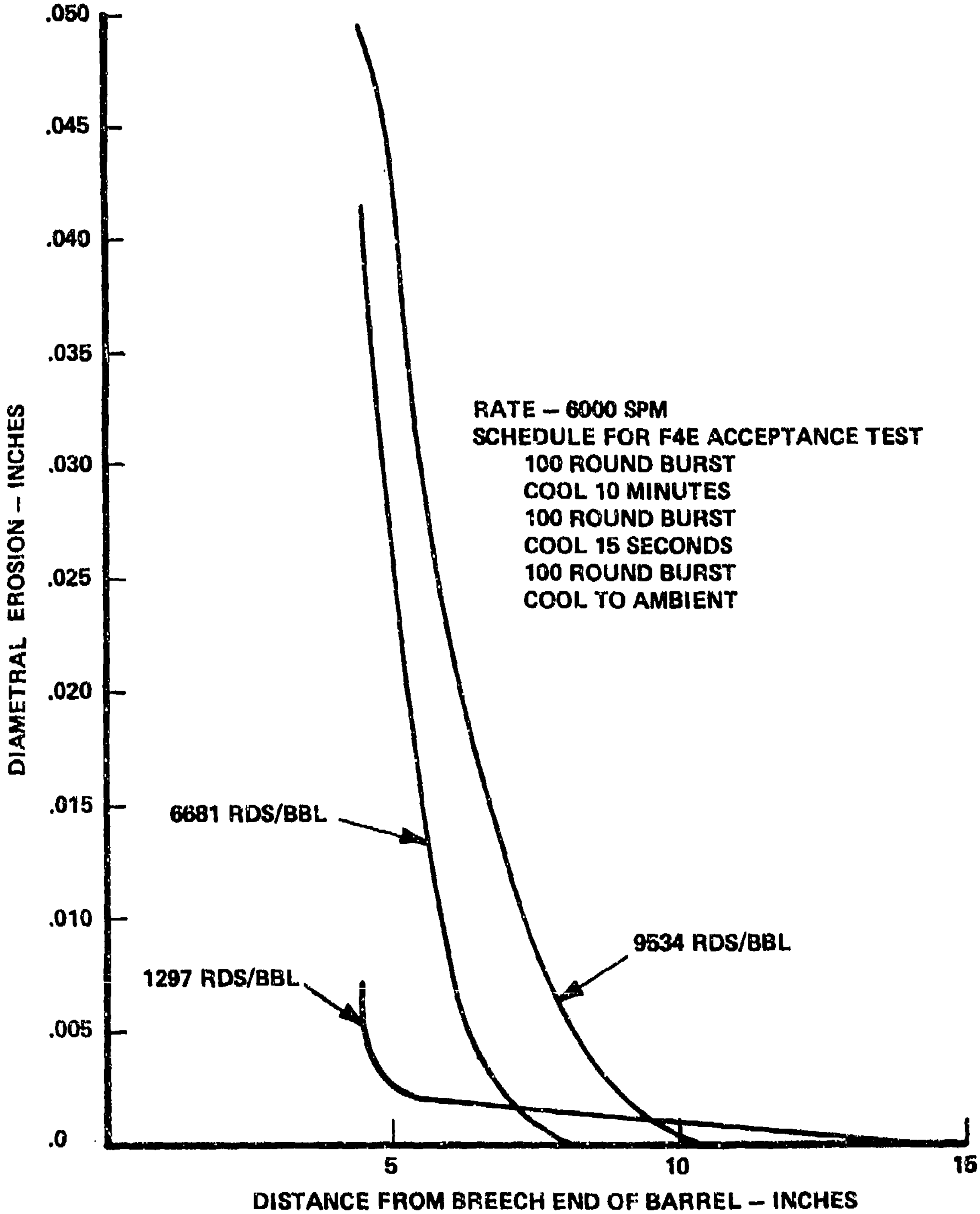
77/3/3

FIGURE 2



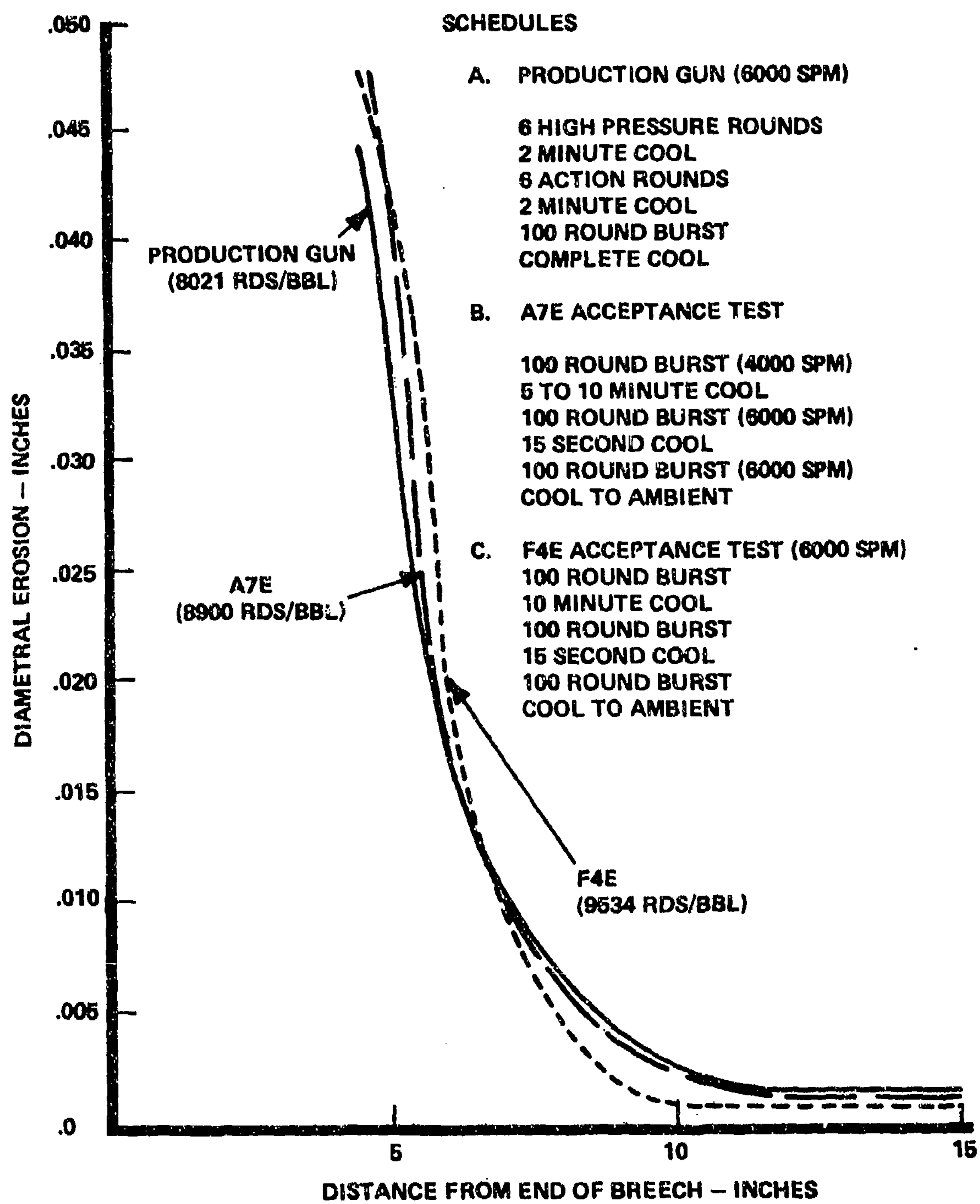
Average Erosion Life Based on Erosion 55.5 Inches from Muzzle

Figure 3.

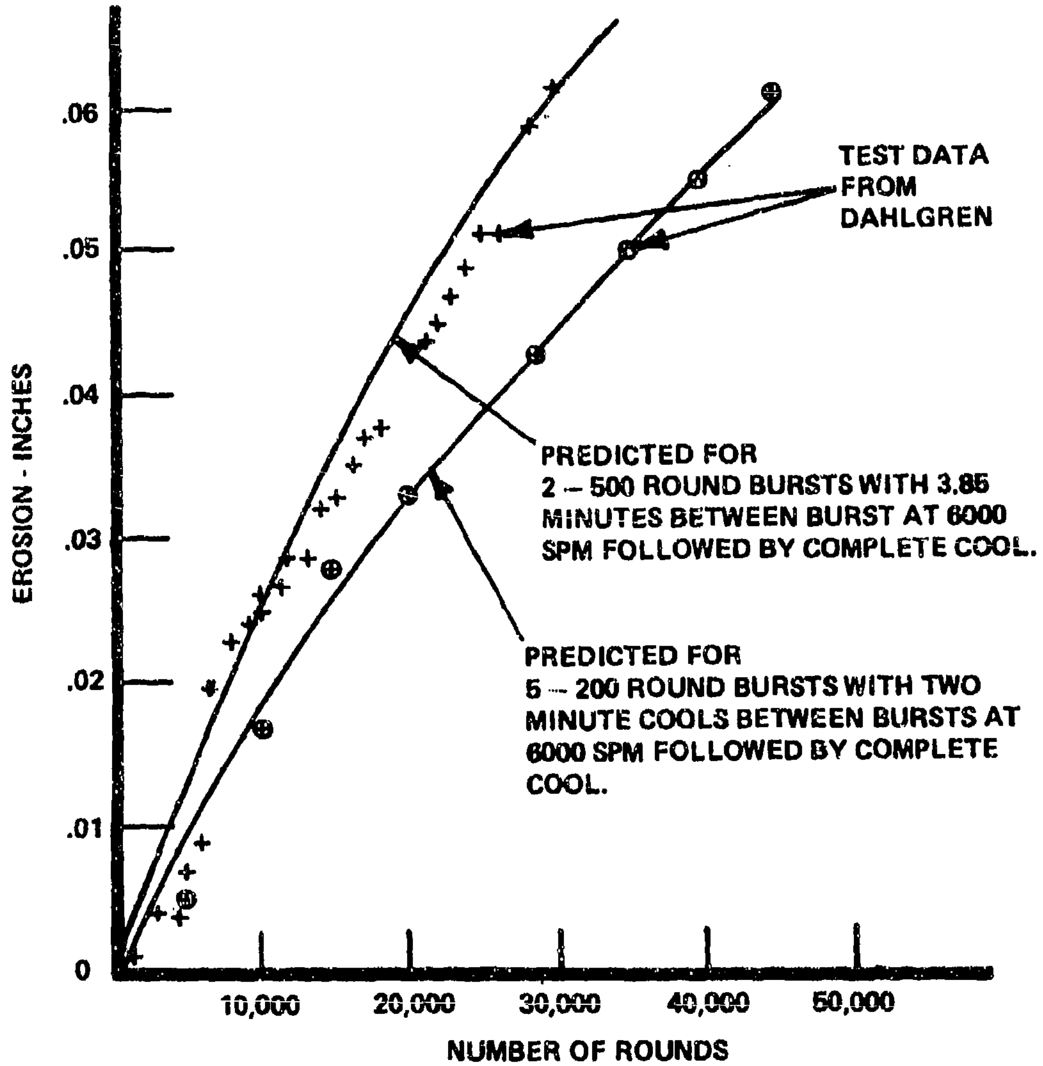


M61A1 Barrel Erosion Measured With M10 Erosion Gage

FIGURE 4

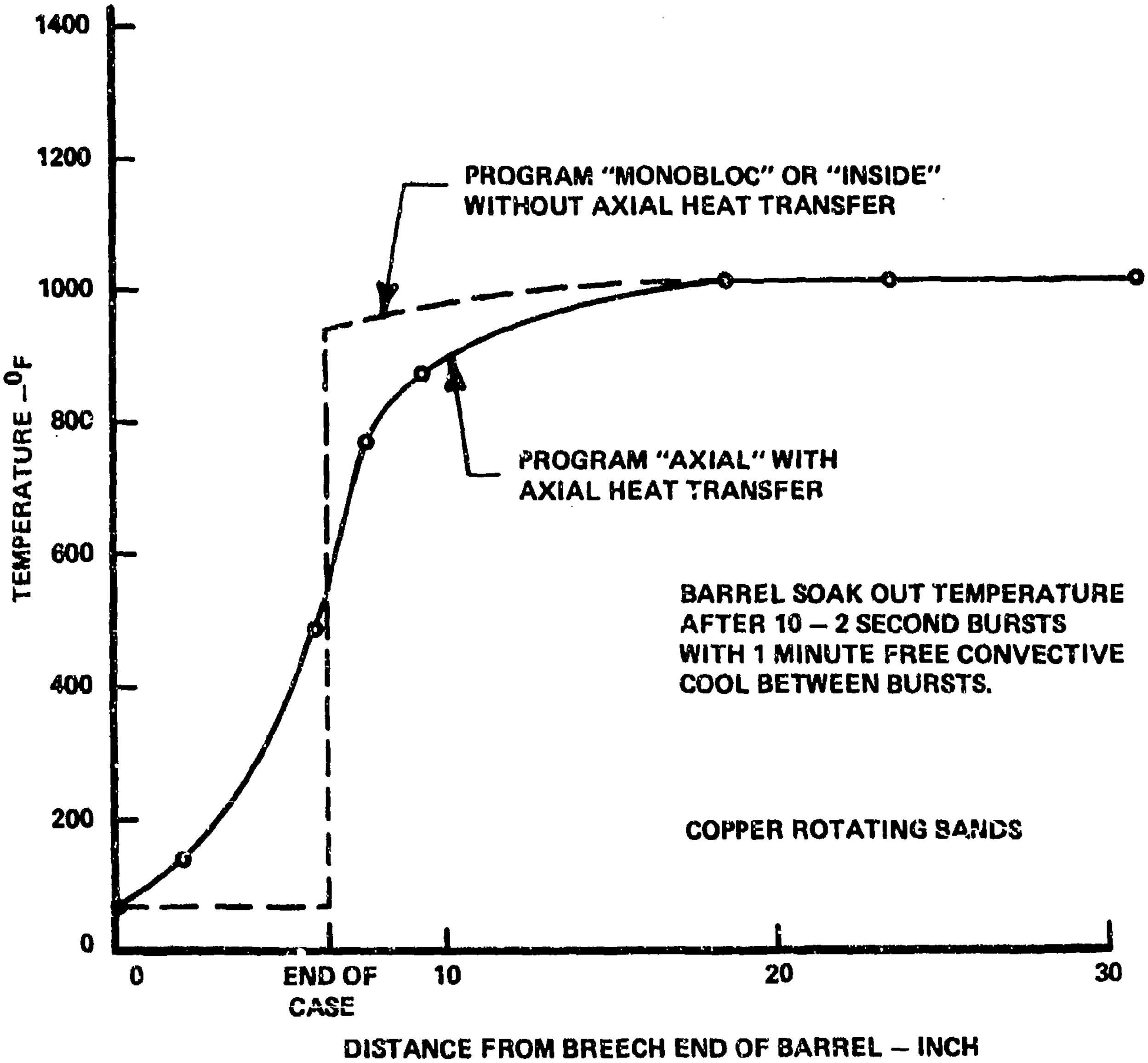


M61A1 Barrel Erosion
Measured With M 10 Erosion Gage
Figure 5.



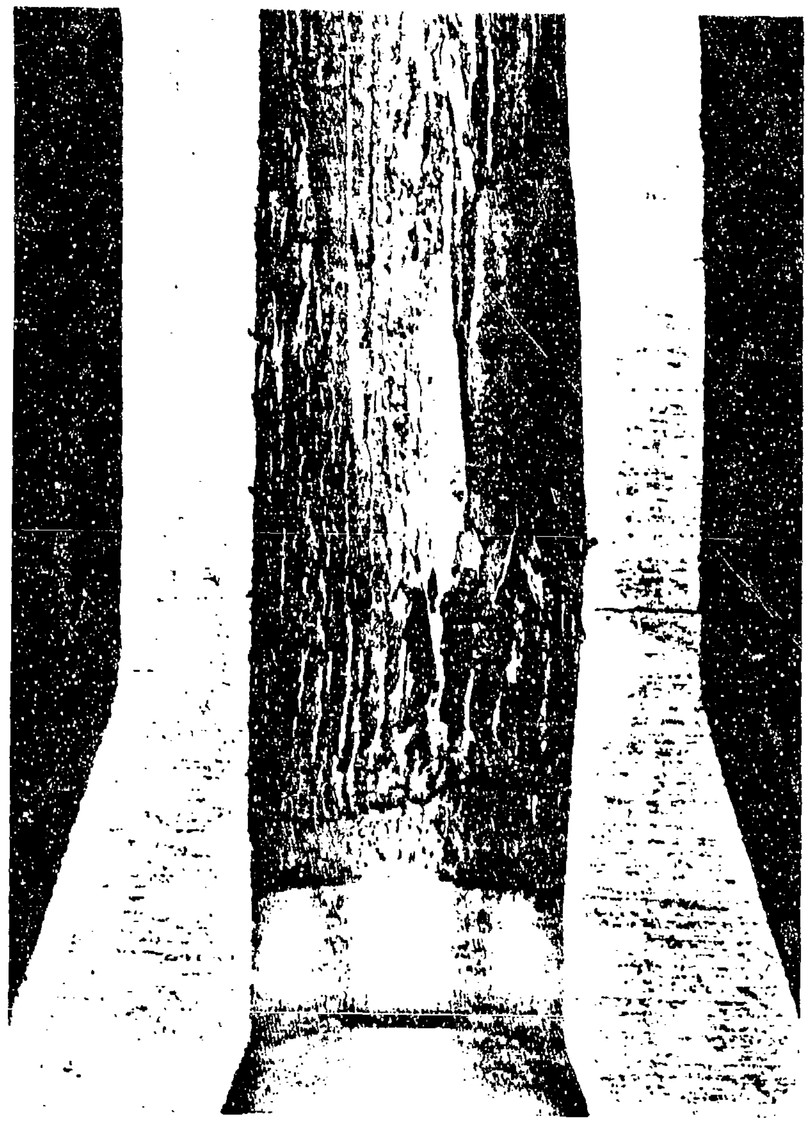
M61A1 Erosion
55.5 Inches from Muzzle

Figure 6.



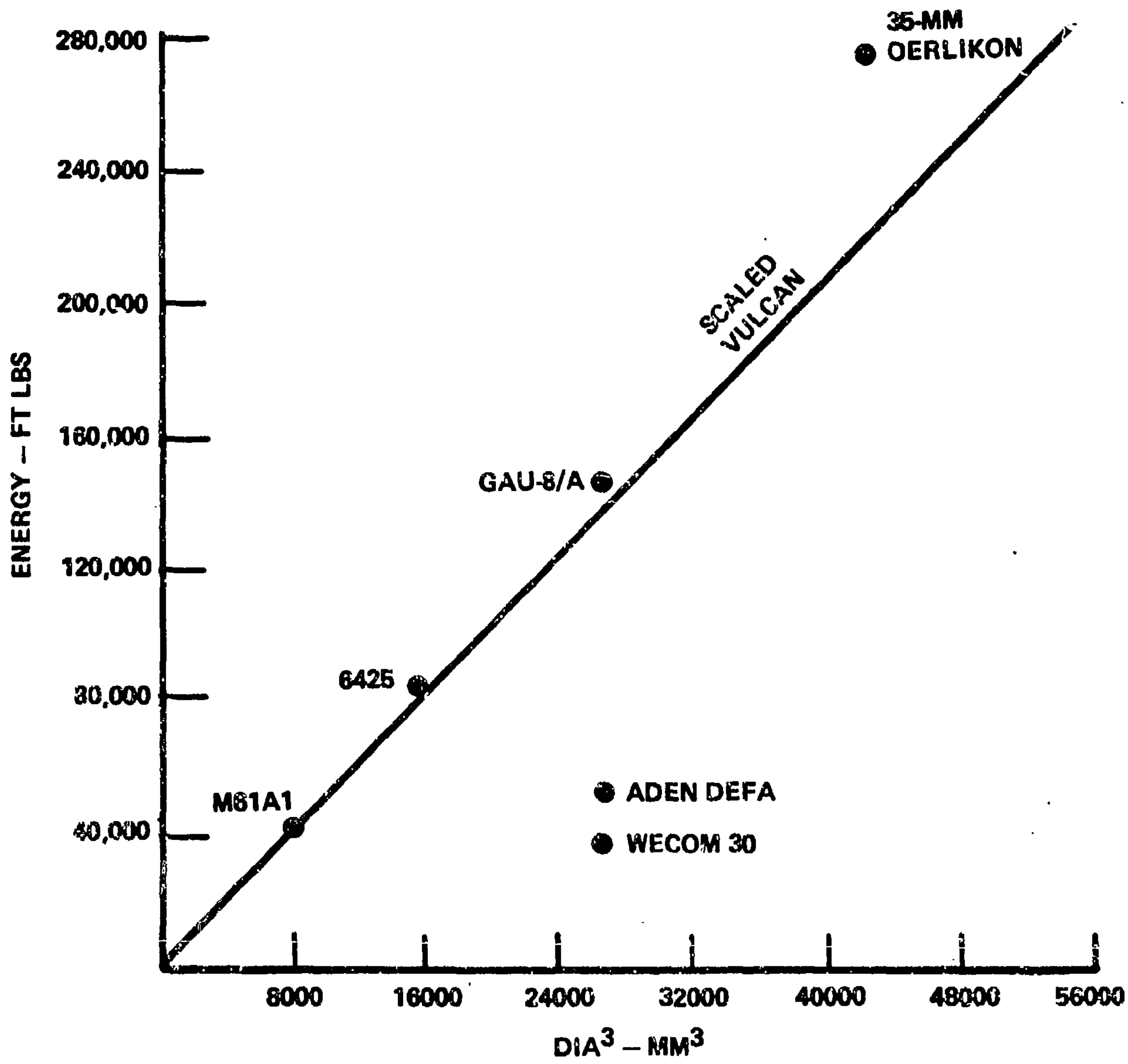
Effect of Axial Heat Transfer
On Temperature

Figure 7.



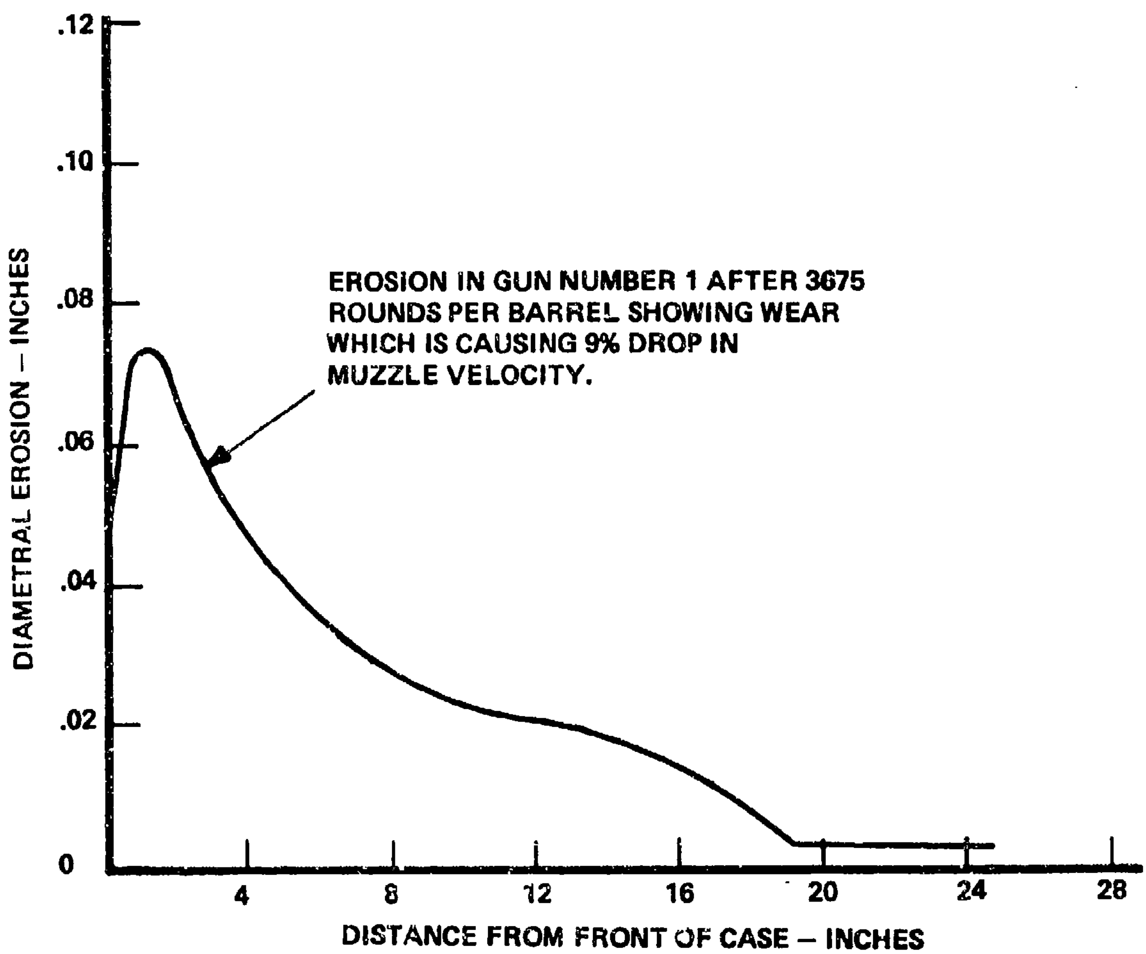
Worn Out M61A1 Barrel

Figure 8.



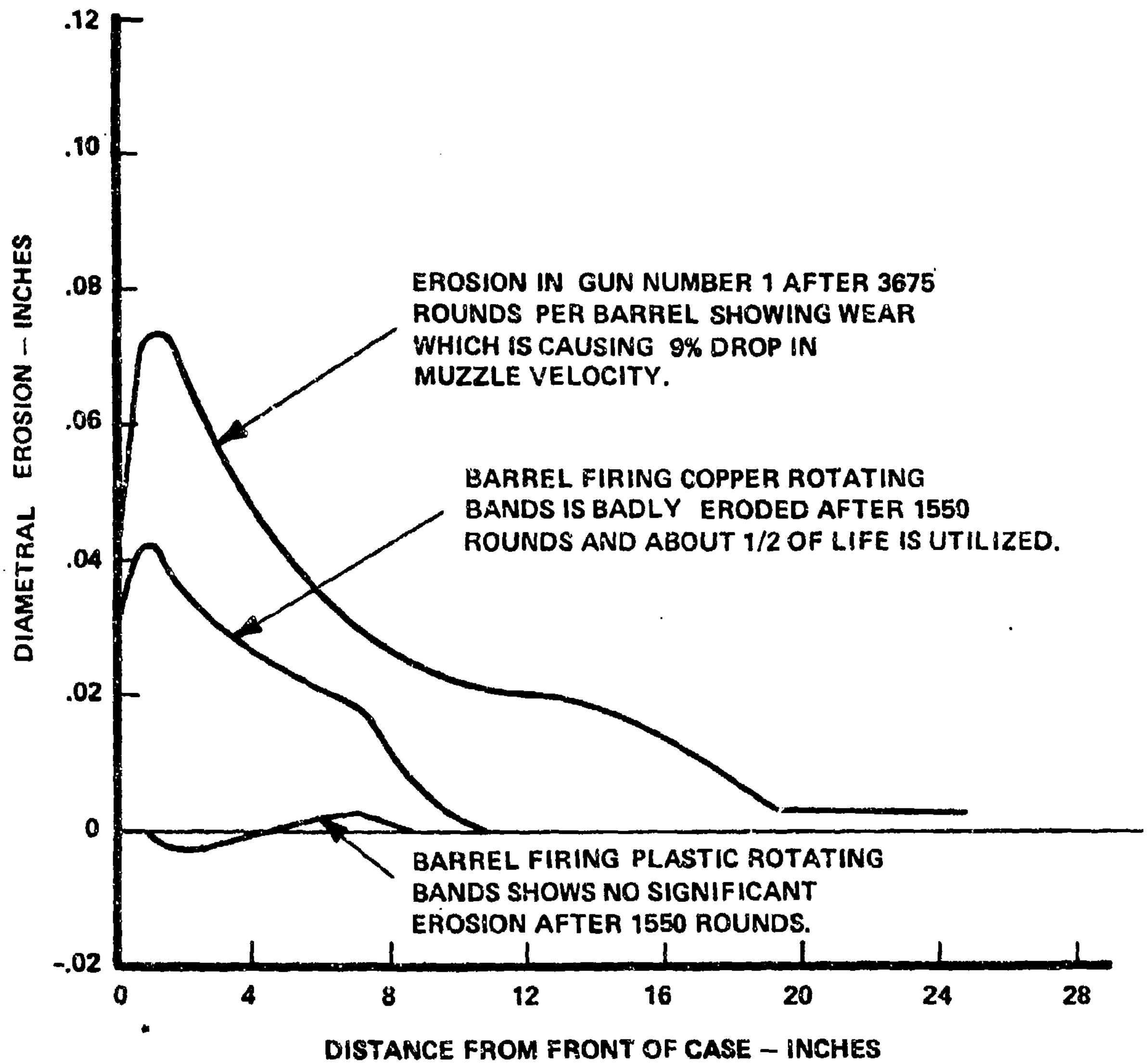
Comparative Weapon Scaling

Figure 9.



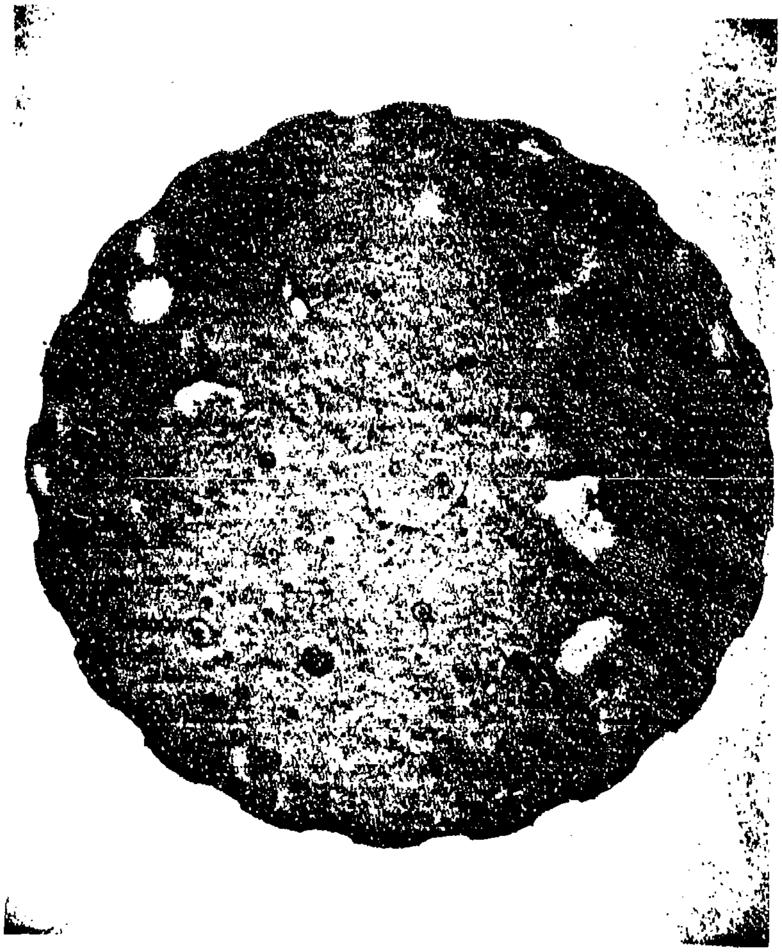
Erosion of Barrels Firing Copper Rotating Band

Figure 10.



Erosion of Barrels Firing Copper and Plastic Rotating Bands

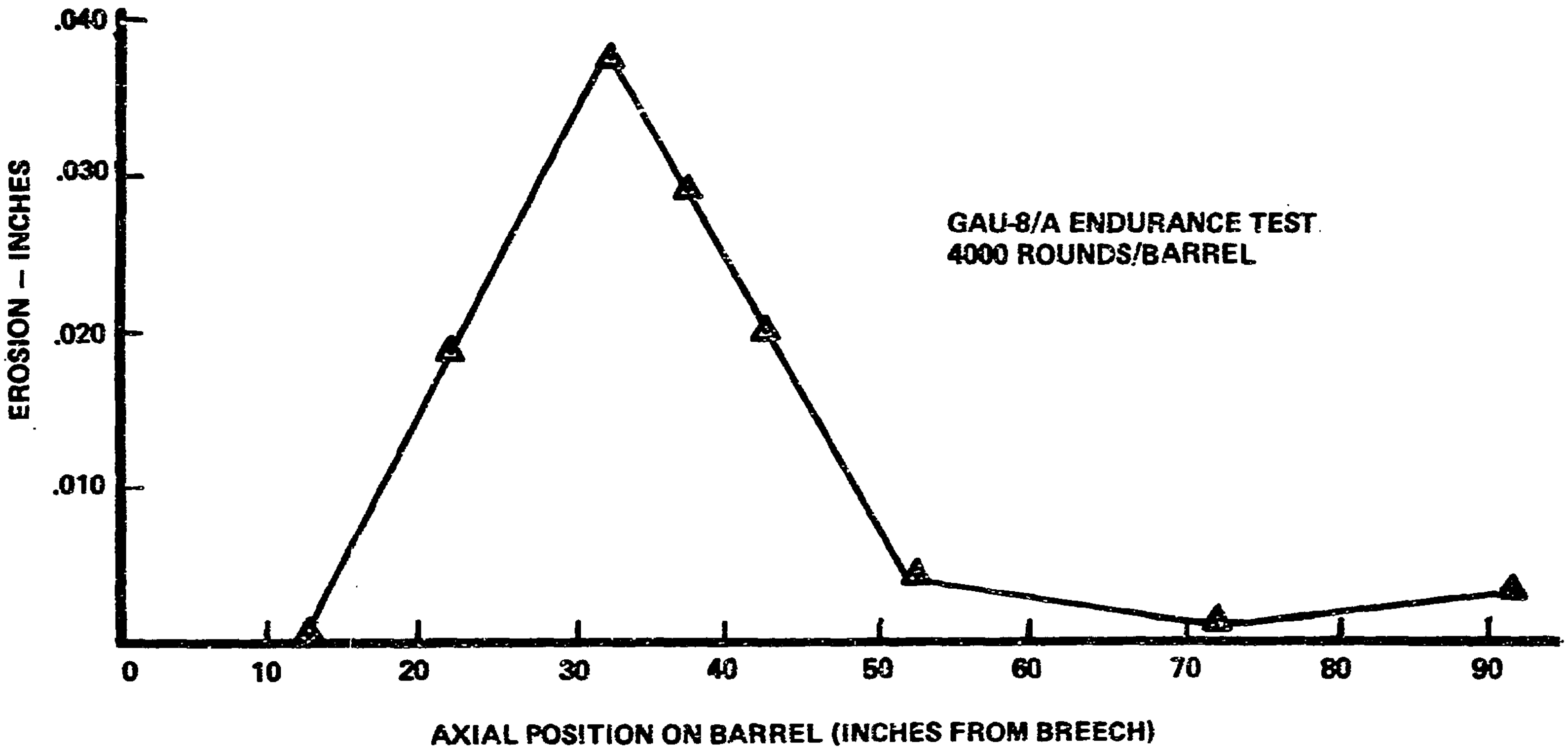
Figure 11.



8 INCHES FROM BREECH END OF BARREL

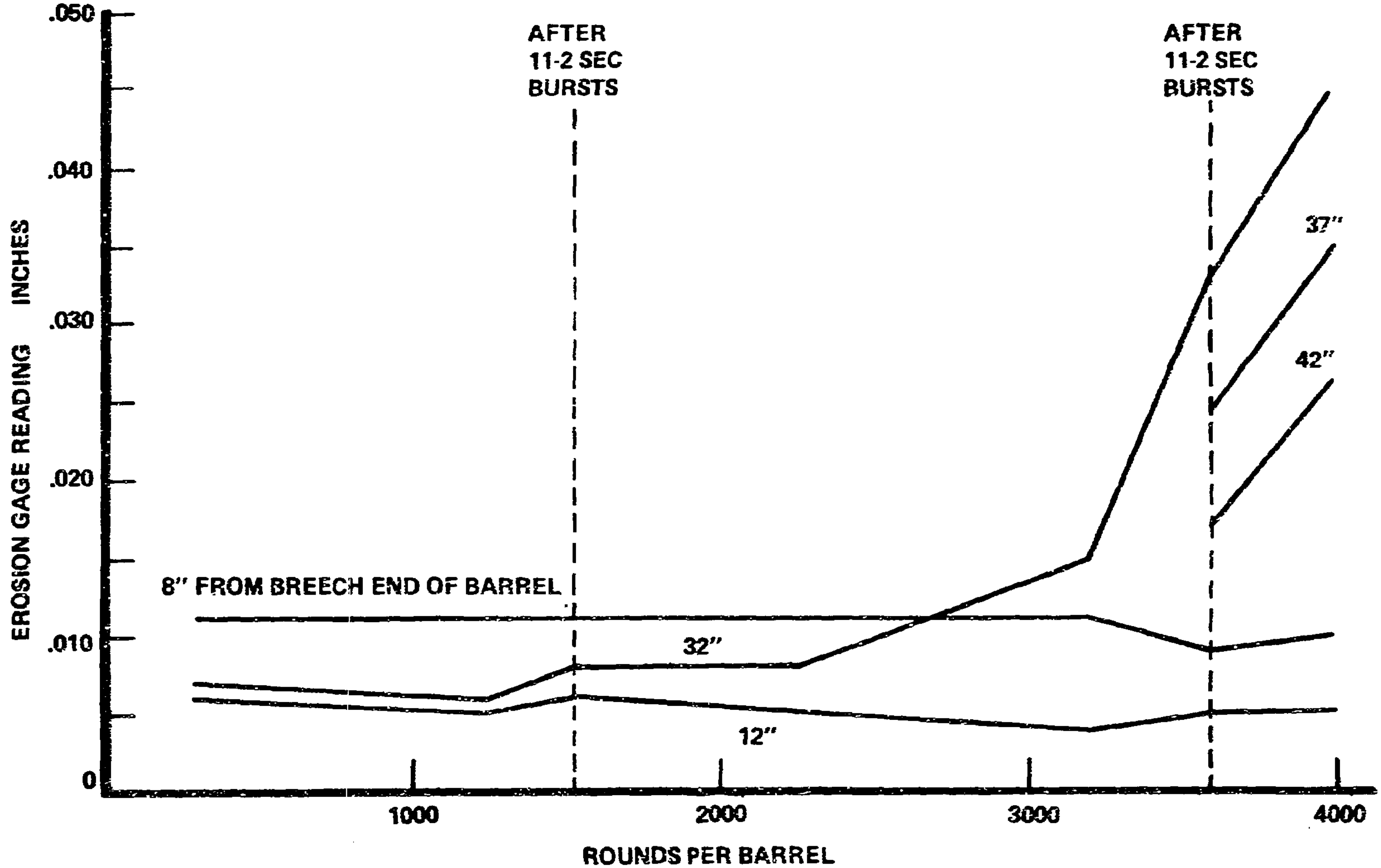
Silicone Cast of GAU 8/A Barrel
With No Chrome Plate After 1 500 Rounds
Showing Erosion of Rifling Which Caused Yaw Failures

Figure 12.



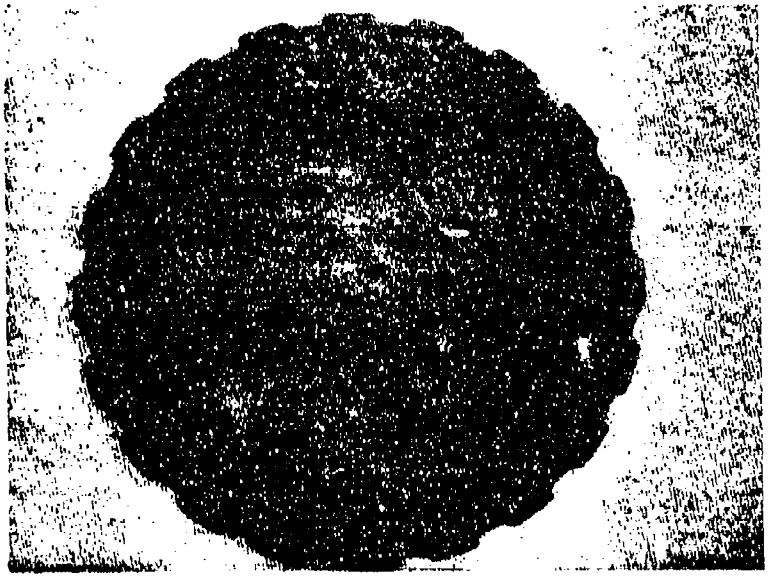
Erosion Vs. Position On Barrel

Figure 13.



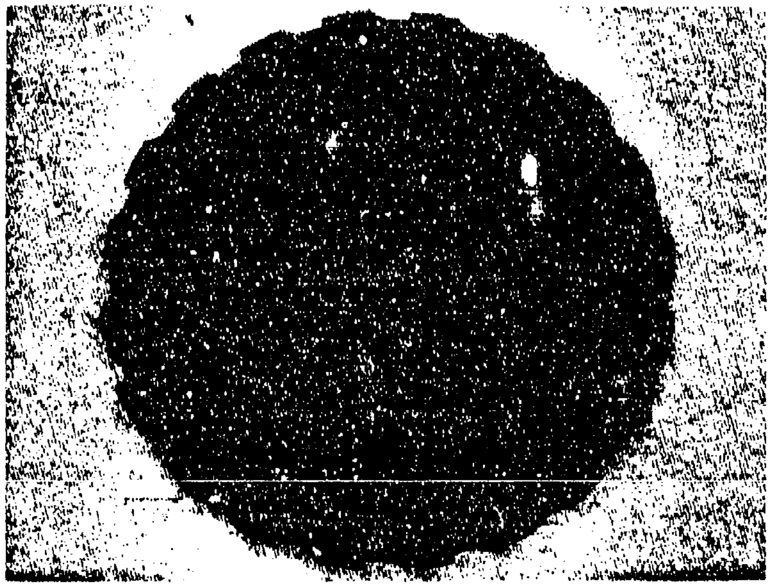
GAU-8/A Endurance Test
Erosion Reading vs. Rounds

Figure 14.



8.5 INCHES FROM BREECH END

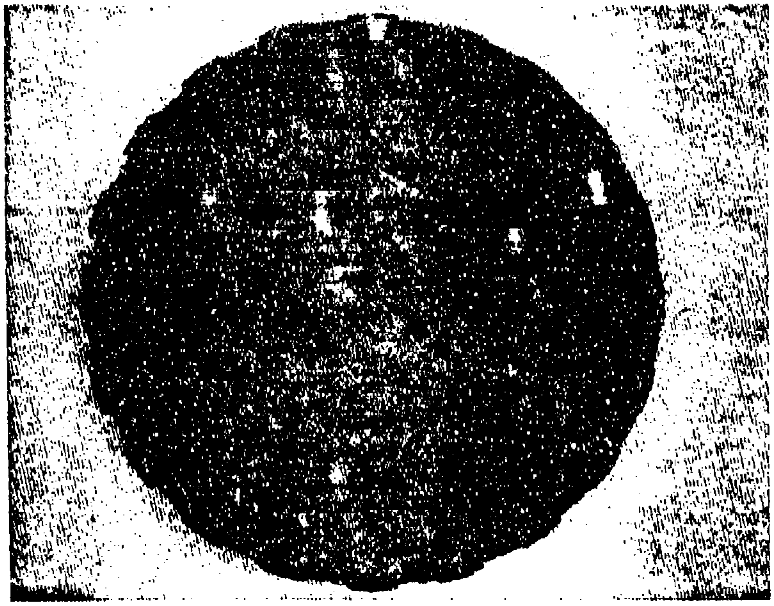
Figure 15.



14 INCHES FROM BREECH END

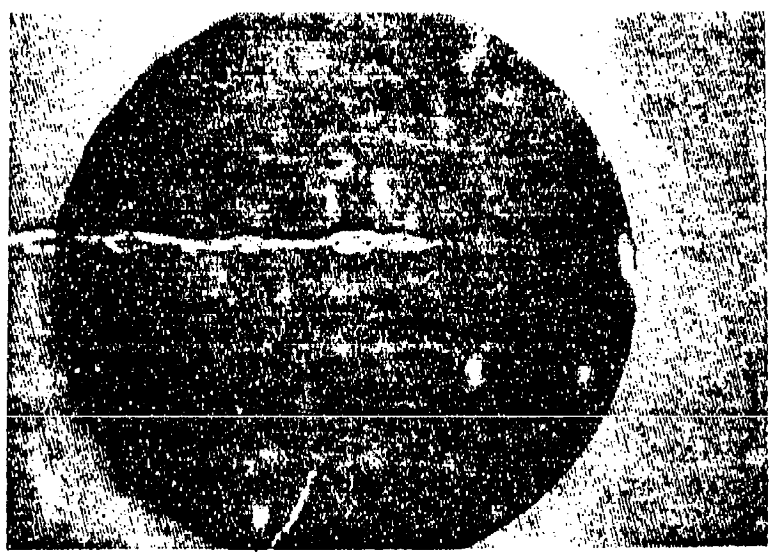
Silicone Cast of GAU 8/A Barrel
After 28,000 Gun Rounds in Endurance Test

Figure 16.



20 INCHES FROM BREECH END

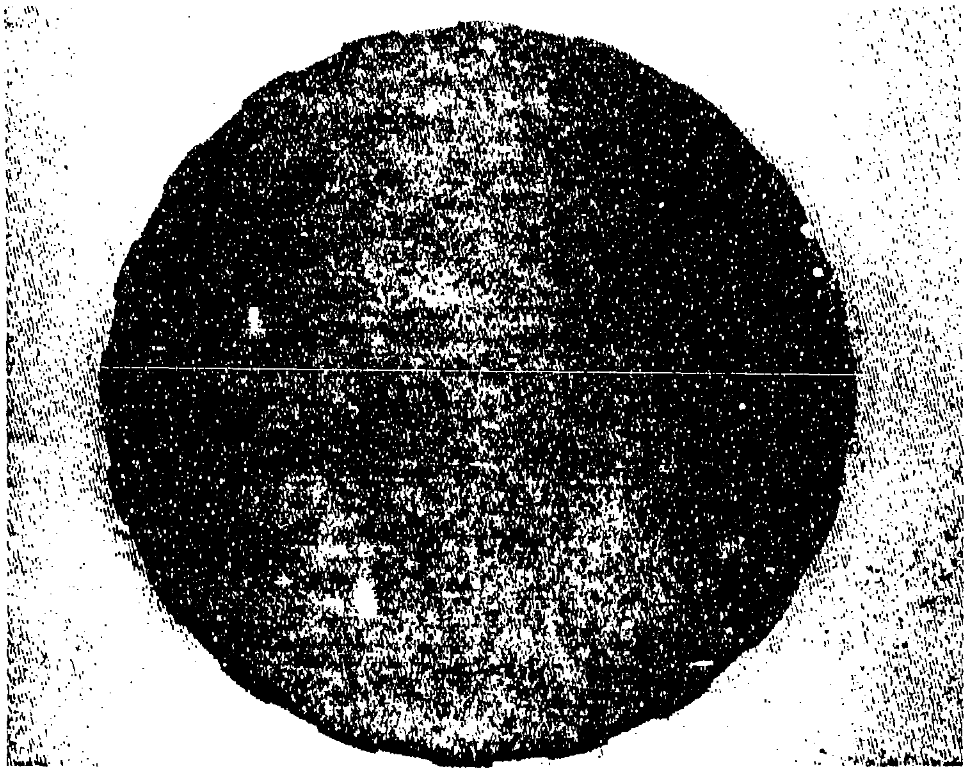
Figure 17.



30 INCHES FROM BREECH END

Silicone Cast of GAU 8/A Barrel
After 28,000 Gun Rounds in Endurance Test

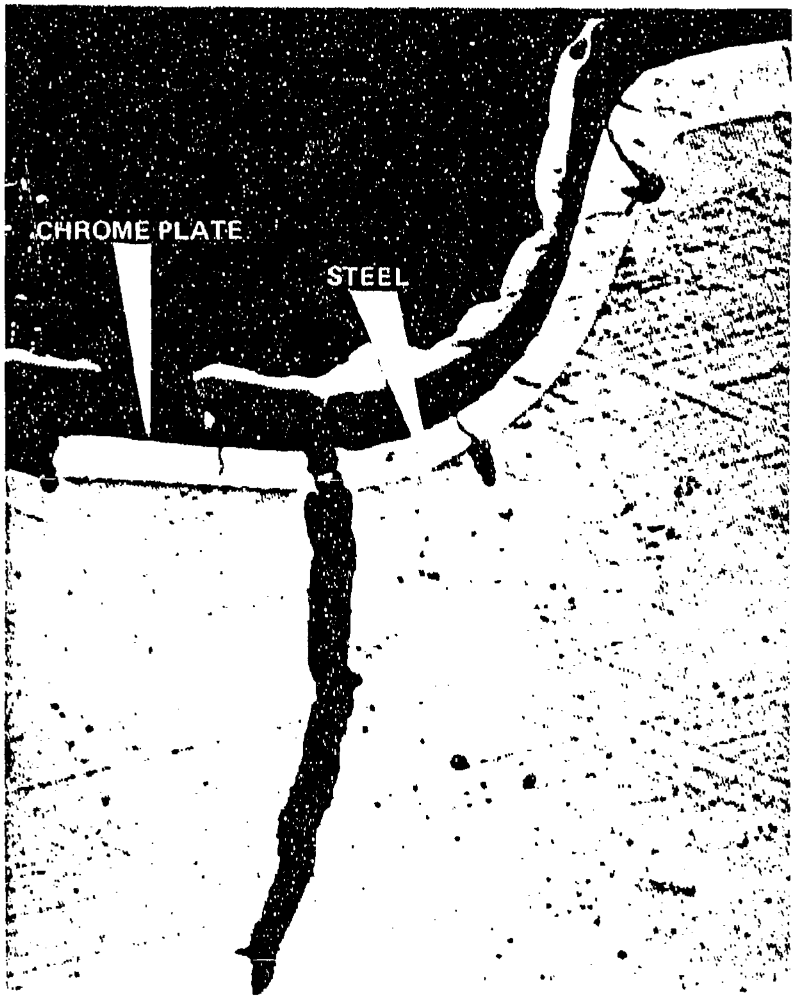
Figure 18.



40 INCHES FROM BREECH END

Silicone Cast of GAU 8/A Barrel
After 28,000 Gun Rounds in Endurance Test

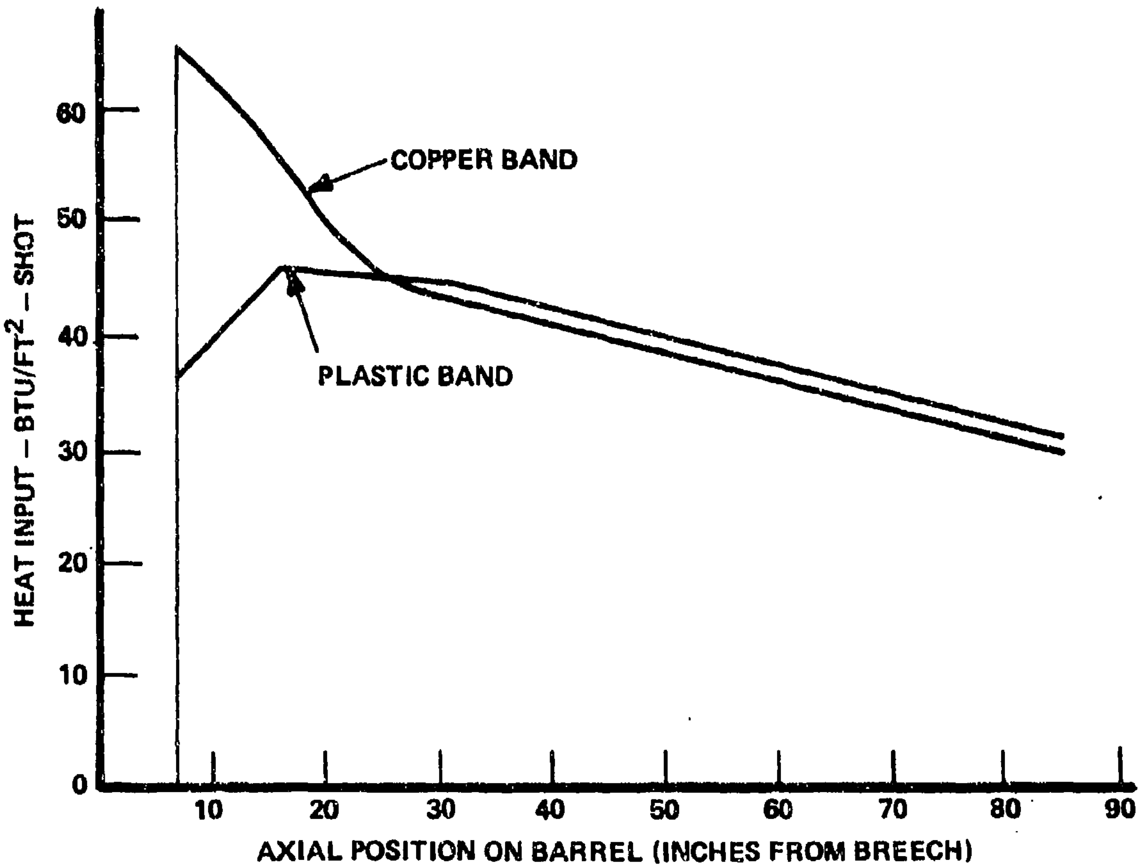
Figure 19.



INITIATION OF RIFLING AFTER 28,000 GUN ROUNDS

Microphotograph of GAU-8/A Barrel

Figure 20.



Effect of Band Material on Heat Input
With GAU-8/A Gun

Figure 21.

AN EVALUATION OF GUN-TUBE WEAR AND EROSION IN THE GAU-8/A CAS GUN SYSTEM

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INTRODUCTION

The USAF was authorized by the Deputy Secretary of Defense in June 1970 to proceed with a competitive prototype development of a high performance 30mm close air support (CAS) gun system. Prior to this directive, considerable effort was expended by government agencies and contractors to determine the optimum performance parameters for a new gun system designed expressly for the close support role. Once the system had been identified, current ammunition technological improvements developed in AFATL were included as part of the basic system design. One of the technological improvements adopted was a plastic rotating band. The advantages of a plastic rotating band are many--round weight reduction, lower cost, use of non-strategic materials, and reduced barrel wear.

BACKGROUND

Non-metallic rotating bands enhance barrel life because the material properties are better suited for this application. Barrel life is improved because a plastic material will obturate at a lower pressure, flow into flaws and more effectively seal high pressure gases, thusly reducing erosion. Material build-up with plastic is impossible because of its low burning temperature, generally around 600°F. This insures that any material left in the barrel will be consumed by the extremely high barrel temperatures.

Because of the plastic materials' elongation properties, relatively low compressive strengths, and excellent lubricity, engraving forces are greatly reduced, thus eliminating failures due to high engraving forces normally associated with metallic bands.

For projectiles of equal weight, projectiles with plastic rotating bands can be launched at the same velocities as projectiles with metallic bands. However, the plastic-banded projectiles can achieve this muzzle velocity at a lower pressure. This offers an opportunity for either (1) increased velocity by increasing the propellant charge, or (2) lower operating pressures at the same velocity level. (Table 1)

Traditionally, projectiles use copper rotating bands because this metal has a good combination of yield strength and ductility. Prior to initiation of the GAU-8A program, AFATL conducted several technology programs to determine the feasibility of replacing the copper rotating band with a plastic one. The technology programs successfully established the plastic rotating band concept, and plastic rotating bands were then included in the GAU-8/A development program.

The first task was selection of a suitable material. The field was narrowed considerably by the following environmental conditions imposed upon the band by the 30mm GAU-8/A system:

a. Storage temperatures from -65° to 160°F.

b. Severe cold-forming stresses induced by the rifling.

c. Chamber pressures of 66,600 psi at high temperatures.

d. Muzzle velocities in excess of 3,400 fps.

e. Barrel type - 10 degree full rifled, constant twist. (Figure 1)

f. Set back acceleration - 100,000 g's.

g. Spin - acceleration (rad/sec^2) - 60,000.

h. Projectile spin (rpm) - 120,000.

The criterion for determining the end of barrel life is when either the muzzle velocity degrades by 10%, or when the number of yawing rounds (15° or more) exceeds 20%.

After a thorough materials search, the field was reduced to 3 basic resin types: nylon, polyarlene and polypropylene. However, further investigation revealed that polyarlene and polypropylene become very brittle at -28°F, and were prone to overstress during engraving. This left only nylon having capability of withstanding the environment and the forces imposed on the band by the weapons system.

Nylon 11 and 12, with and without glass fill (up to 40 percent), were evaluated. These formulations were affected by moisture. Band integrity was poor, especially at elevated temperatures. Glass fill, which was expected to improve the strength of the bands, caused embrittlement at low temperatures with resultant band loss. Evaluation of nylon 612 and 6/6 with glass contents from 0 to 40% were conducted. These nylons perform well and are now qualified rotating band materials for GAU-8/A ammunition. The band is retained on the projectile mechanically. Standard plastic molding techniques are used to mold the plastic band to the projectile.

Figure 2 is of an inert 30mm GAU-8/A HEI round in flight. The photo was taken at the AEDC exterior ballistic range at approximately 400 feet from the muzzle.

DISCUSSION

During Phase I of the GAU-8/A development program a special test to compare the effects of copper rotating bands vs plastic rotating bands was conducted with the General Electric gun number two. The results showed that plastic bands substantially increased barrel life over that obtained with a copper band. This test was set up in the following manner. The barrel identified as number one was the base line barrel through which plastic banded projectiles were to be fired. It should be noted that this barrel had previously been used to fire 1550 plastic banded projectiles. Barrels number two through seven were new barrels.

The ammunition drum was then loaded with plastic banded and copper banded projectiled rounds. They were programmed so that the plastic banded rounds fired through barrel number one and copper banded ones through the remaining 6 barrels. The test was fired in 2 second bursts with one minute cooling between each burst. The firing rate was 4200 rounds per minute. A total of 5250 rounds were fired before the test was terminated. Barrel number one showed insignificant wear while the barrels through which the copper banded projectiles were fired were rendered useless. Table number 2 presents the actual wear measurement readings obtained at the conclusion of the tests. Graph number 1 is a graphical presentation of the same data. Graph number 2 compares the muzzle velocity variation experienced during the test. Note the significant degradation in velocity level experienced with the copper banded projectiles.

Based on their phase I performance General Electric was awarded the phase II advanced development contract. During evaluation of the pre-production guns the relationship between barrel wear and firing sequence was determined. The most severe barrel wear (as evidenced by greatly increased dispersion patterns) occurred when firing high rate sequences (4200 rounds per minute) with short cooling times between bursts. An example of such a burst is identified as schedule seven. This test series involves 10 two-second bursts with one minute cooling between each burst. During ground tests of this type, barrel temperatures can reach 1150°F at the muzzle. When firing schedule seven from an aircraft in flight, maximum temperatures are less due to aerodynamic cooling. The worst temperature pattern experienced from these aircraft firings is:

THERMOCOUPLE LOCATION (inches from Breech)	7.7	16	31	48	96
PEAK TEMP. F°	318	534	640	675	717

Barrel erosion measurement tables 3 through 9 track the erosion history of a set of new barrels which were part of a 50,000 round gun endurance test. The total number of rounds fired on this particular set of barrels was 27,787. The barrels were replaced due to an excessive number of yawing rounds. There was no noticable velocity level decay. Graph 3 is a graphical presentation of the wear encountered with barrel No. 1000001. Plotted are star gauge readings at the start of the test and when the barrel had to be replaced (3667 rds).

Graph 4 and figure 3 present a graphic examble of the serviceability of a plastic rotating band. During full scale development testing of the gun the test technician forgot to set the burst time. The result was that 735 rounds were fired continuously at a 4200 round per minute rate. Barrel temperatures at the muzzle reached 1150°F. As can be seen from graph 4 the muzzle velocity held constant throughout the burst. Figure 3 shows no degradation in accuracy. This excessive burst occurred with approximately 1/2 of the barrel life remaining (12,500 rounds). Testing continued with these barrels to their normal life expectancy of 25,000 rounds.

CONCLUSIONS

The compatibility of ammunition having a plastic rotating band when exposed to the harsh dynamic environment of a Gatling Gun has been demonstrated. The objectives previously mentioned have been met, the most impressive being a increase in barrel life of at least 300 percent. Considerably more work remains to be accomplished before the full potential of a plastic rotating band can be identified. The most important of these is a need for a field testing technique for determining barrel wear conditions. Presently barrels fired from aircraft are discarded after 25,000 rounds (7 per set). No attempt is made to check ballistic degradation or projectile yaw. Since the burst schedules fired during pilot training and those anticipated for combat are not considered severe it is quite possible that the barrel life may approach the fatigue life which is 50,000 rounds per set of barrels.

Over the next 15 months AFATL/DLDA will be firing over 200,000 rounds as part of a combat ammunition lot acceptance verification program. During these tests a bore gauging device will be optimized for field application. This will make it possible to determine barrel life "on the flight line." The net result could be a cost savings in excess of 10 million dollars through 1983.

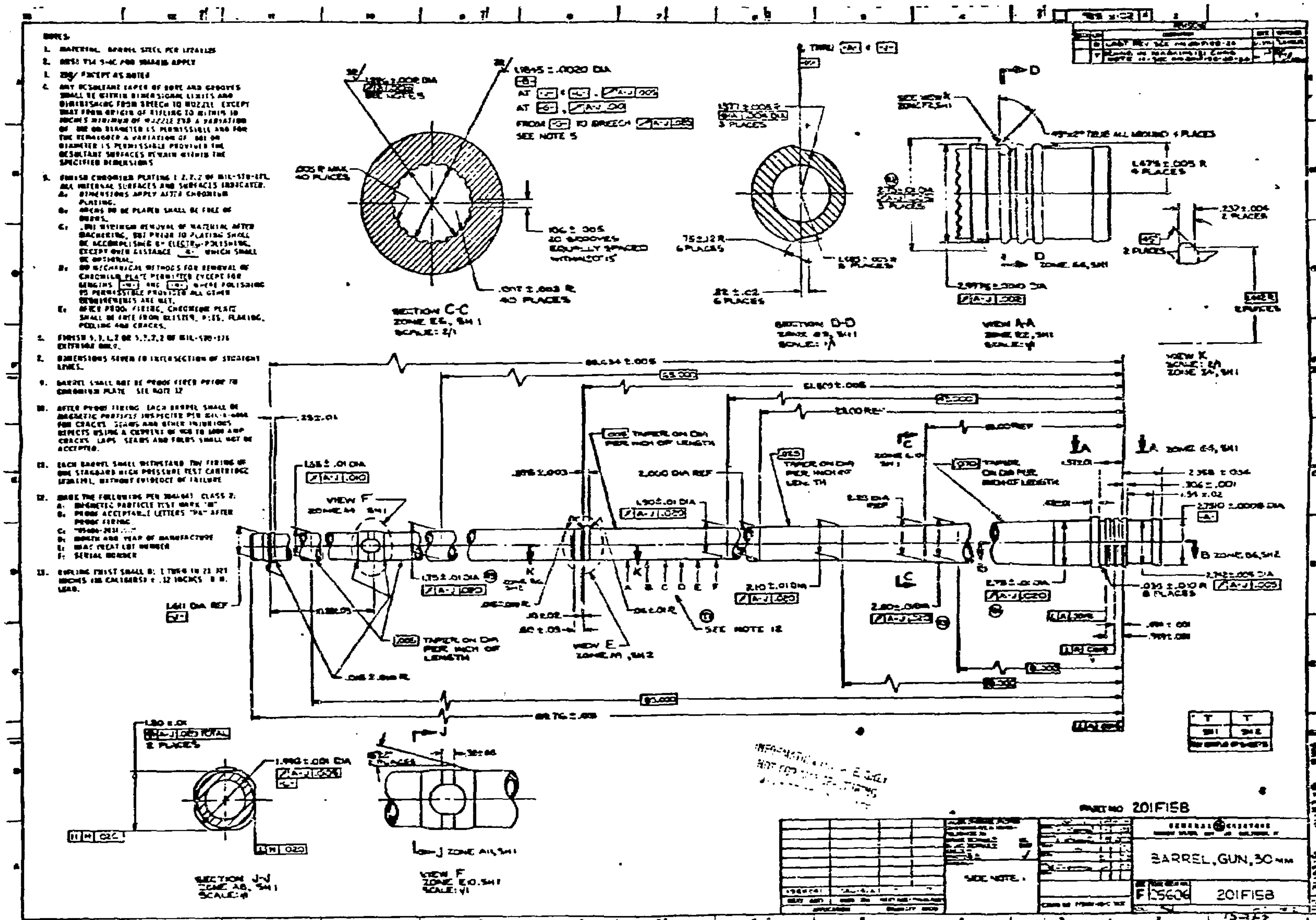
In conclusion I would like to mention another potential advantage of the plastic rotating band. Preliminary tests indicate that the strike velocity of the 30mm APIT round can be increased by 150 f/s if the band is sheared off even with the bourrelet. We have been achieving this by diverting the last six to eight inches of rifling 5° in the spin direction. To date these tests have only been conducted "single-shot" through a Mann barrel. The next step, which is currently underway, is to fire these barrels in the Gatling gun, determine barrel wear and effects, if any, of plastic pieces ingested by the aircraft engines.

References

1. Technical Report ADTC-TR-73-66, "COMPARATIVE EVALUATION OF TWO GAU-8/A GUN SYSTEM CAMDODATES". September 1973.

2. Technical Report ADTC-TR-74-103, "GAU-8/A 30MM AMMUNITION TEST, VOLUME II", December 1975.

3. Technical Report ADTC-TR-73-130 and ADTC-TR-73-131 "GAU-8/A GUN SYSTEM DEVELOPMENT, PHASE I (U) VOLUME II. AMMUNITION SYSTEM", June 1973.



SECTION C-C
SECTION D-D
VIEW A-A
VIEW K
VIEW F
VIEW E
SECTION J-J
SEE NOTE 12
PART NO 201F158
BARREL, GUN, 30 MM
201F158

Figure 1

FIGURE 2
V-626

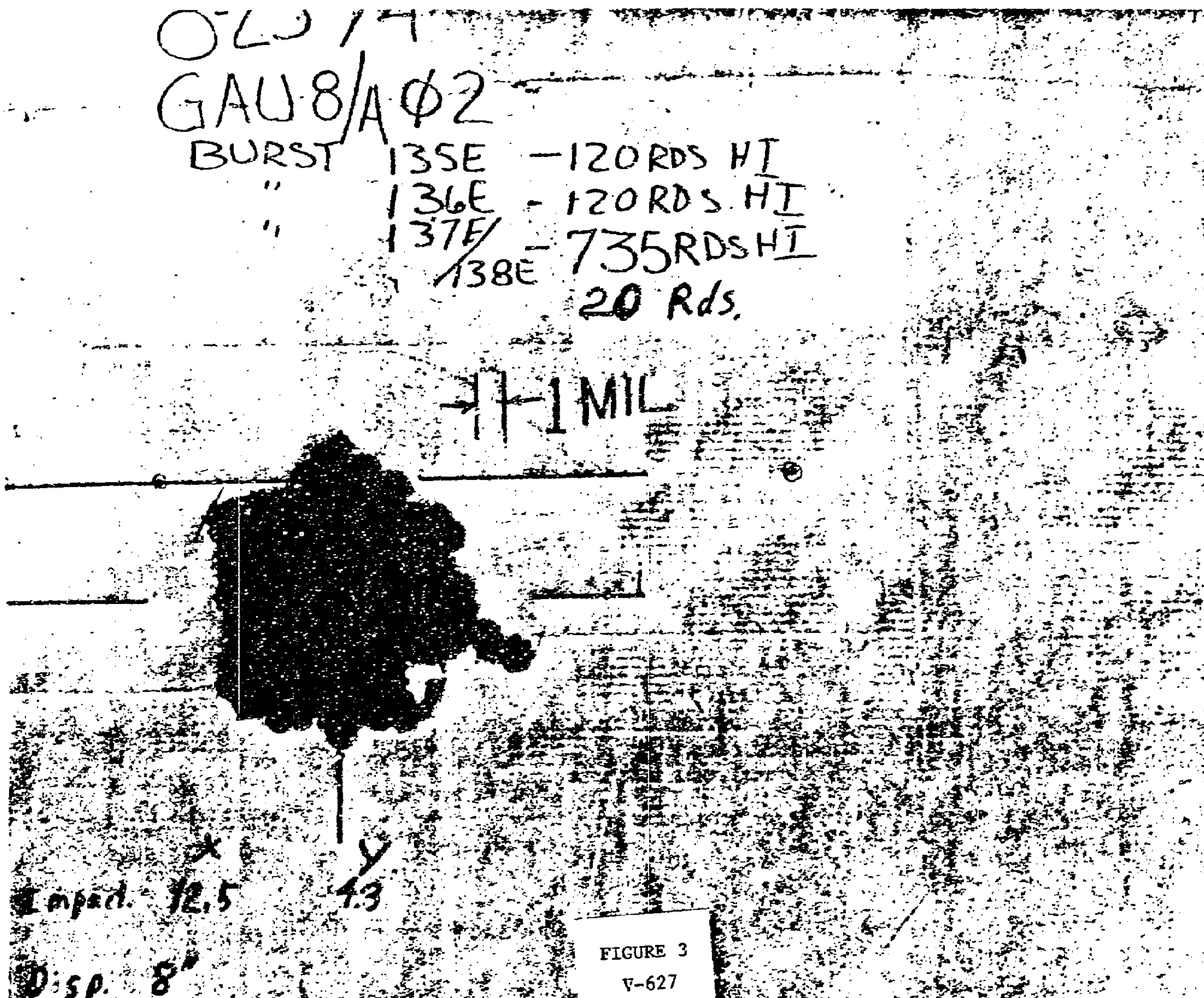


FIGURE 3
V-627

TABLE 1

COMPARISON OF PLASTIC - AND COPPER BANDED PROJECTILES

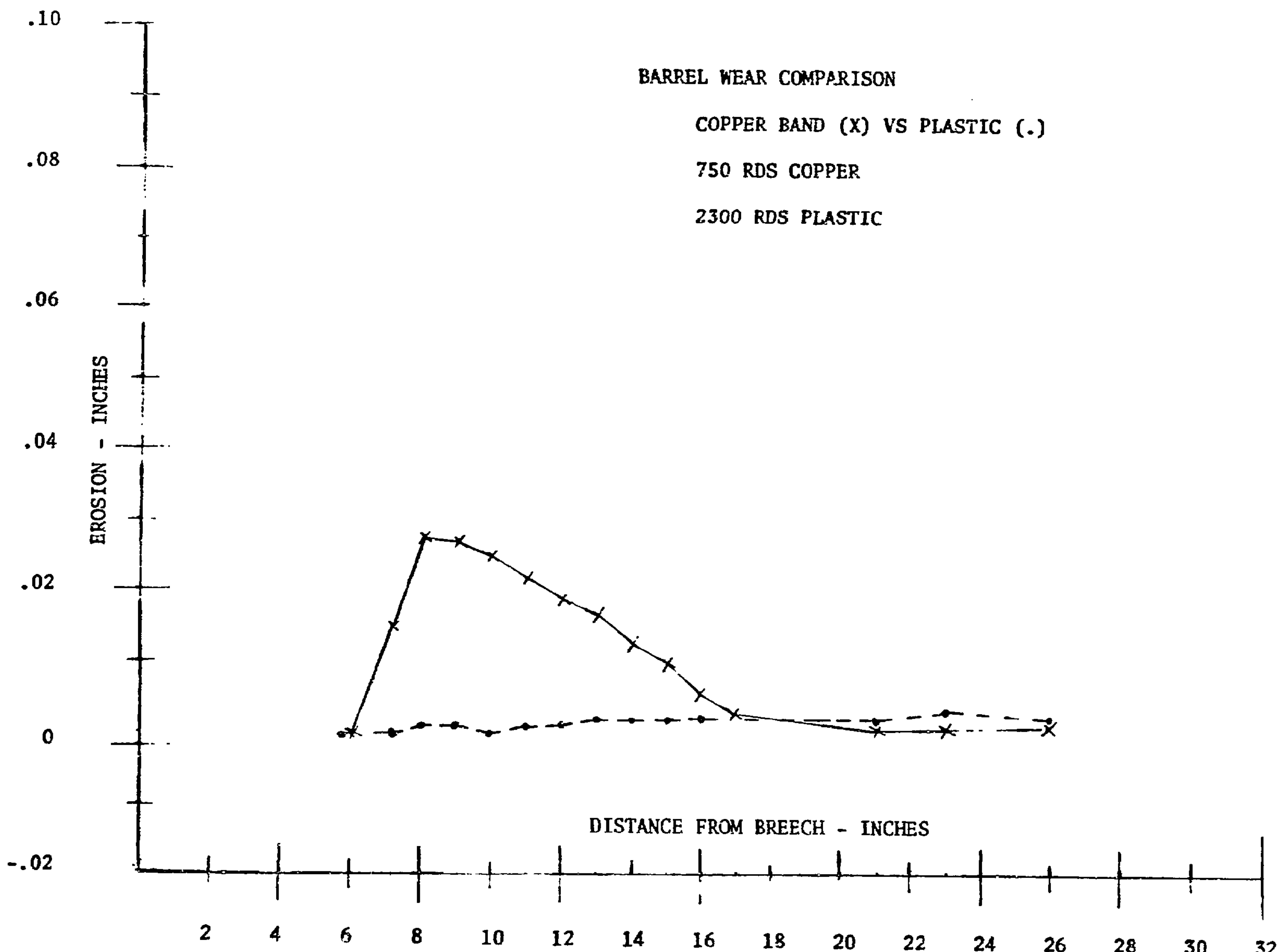
Projectile /Condition	Propellant Type	Charge Weight	Temp. ^oF	No.of Rounds	Chamber Press (PSI)			Muzzle Velocity(FPS)			Action Time (MS)		
					Mean		Range	Mean		Range	Mean		Range
Target Practice	Lot 5444 WECOM Tube	152 GM	Amb.	10	50,850	1949	6000	3355	14.6	45	4.85	0.130	0.4
Plastic Band	Lot 5444 Wecom Tube	152 GM	Amb.	10	47,350	2274	7000	3350	51.0	157	4.70	0.227	0.7
Plastic Band	Lot 5444 WECOM Tube	152 GM	-65^oF	10	39,833	3898	11,500	3228	65.6	202	5.04	0.237	0.7
Plastic Band	Lot 5444 WECOM Tube	152 GM	$+160^oF$	10	50,250	1300	4000	3413	16.6	51	4.65	0.130	0.4

TABLE 2

BARREL WEAR COMPARISION DATA

Distance from muzzle (in)	Barrel diameter wear (inches X 0.001)						
	Barrel 1	Barrel 2	Barrel 3	Barrel 4	Barrel 5	Barrel 6	Barrel 7
80.0	2	1	1	0	1	1	1
79.5	0	0	1	0	1	0	1
79.0	0	0	12	13	11	15	15
78.5	0	14	23	19	35	23	25
78.0	1	21	26	23	18	23	29
77.5	1	20	27	20	17	22	28
77.0	1	22	27	19	19	19	27
76.5	1	23	24	20	19	20	27
76.0	-1	24	24	19	17	23	25
75.5	0	20	18	18	16	22	23
75.0	0	16	7	18	13	20	22
74.0	1	16	3	13	6	18	19
73.0	2	12	11	9	2	12	17
72.0	2	10	8	5	2	11	13
71.0	2	4	6	5	1	5	10
70.0	2	2.5	2	2	2	3	6
69.0	2	1.5	2	2	2	1	3
65.0	2	-2	0	2	-2	1	1
63.0	3	1	-1	0	0	1	1
60.0	2	0	1	1	2	1	1
50.0	2	2	2	1	2	1	1
40.0	1	1	2	1	1	1	1
30.0	2	2	3	2	1	2	1

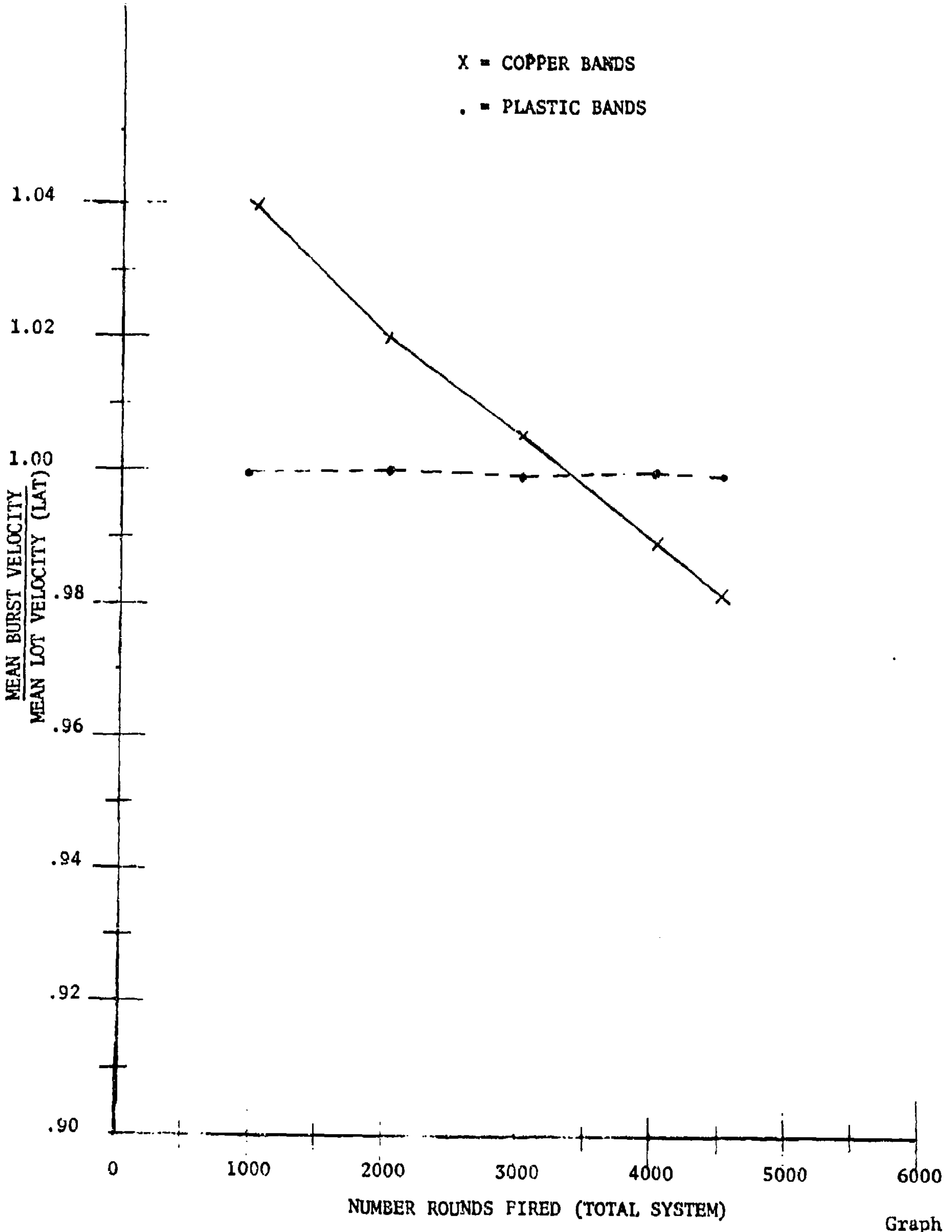
Diameter measurements were made across barrel lands at specified intervals along the barrel length. Negative numbers indicate possible buildup of metal due to rounds traveling through ba. rels.
Barrel 1, plastic rotating bands; barrels 2 through 7, copper rotating bands.



BARREL WEAR COMPARISON
COPPER BAND (X) VS PLASTIC (.)
750 RDS COPPER
2300 RDS PLASTIC
EROSION - INCHES
.10
.08
.06
.04
.02
0
-.02
DISTANCE FROM BREECH - INCHES
2
4
6
8
10
12
14
16
18
20
22
24
26
28
30
32

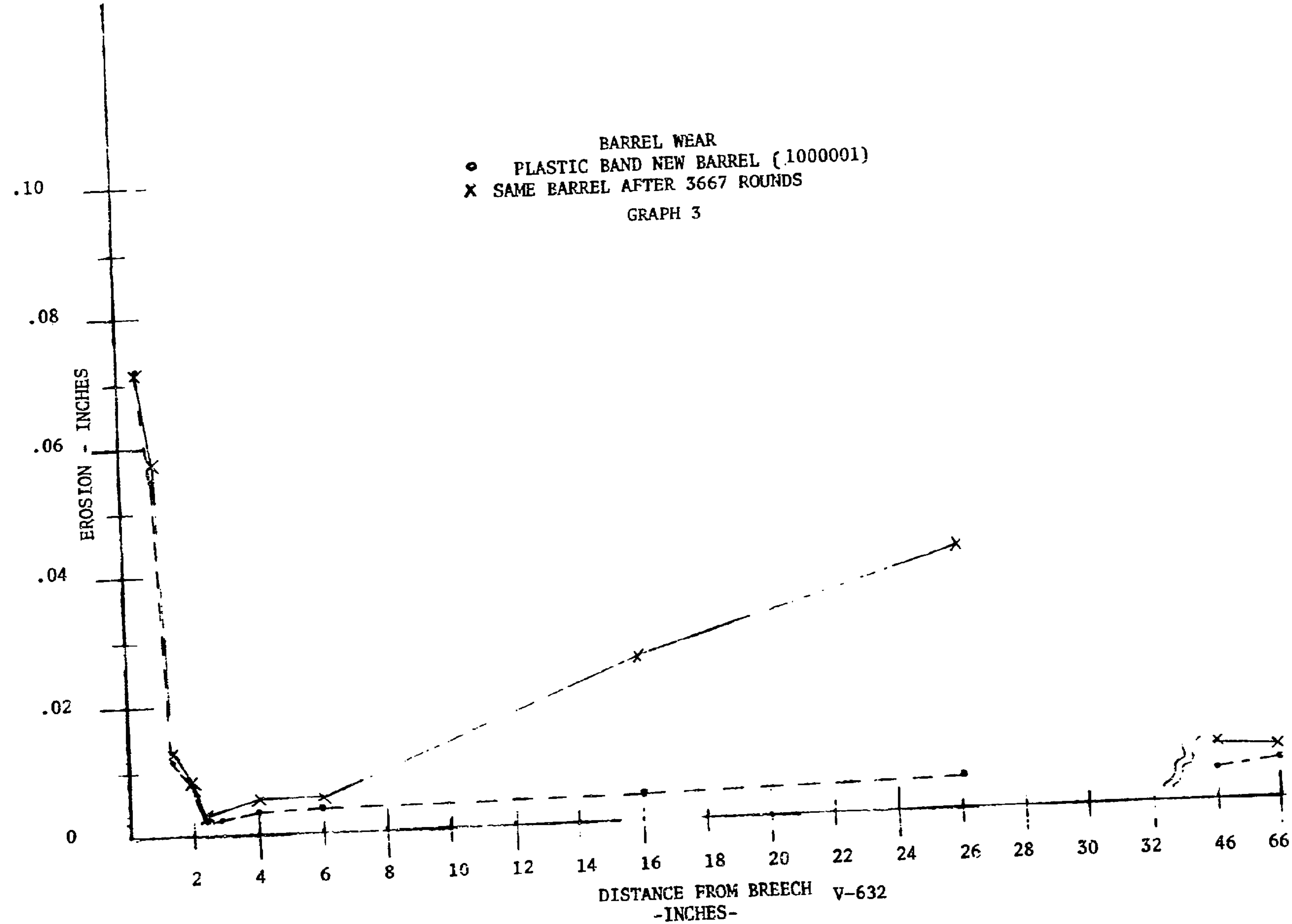
V-630

Graph 1

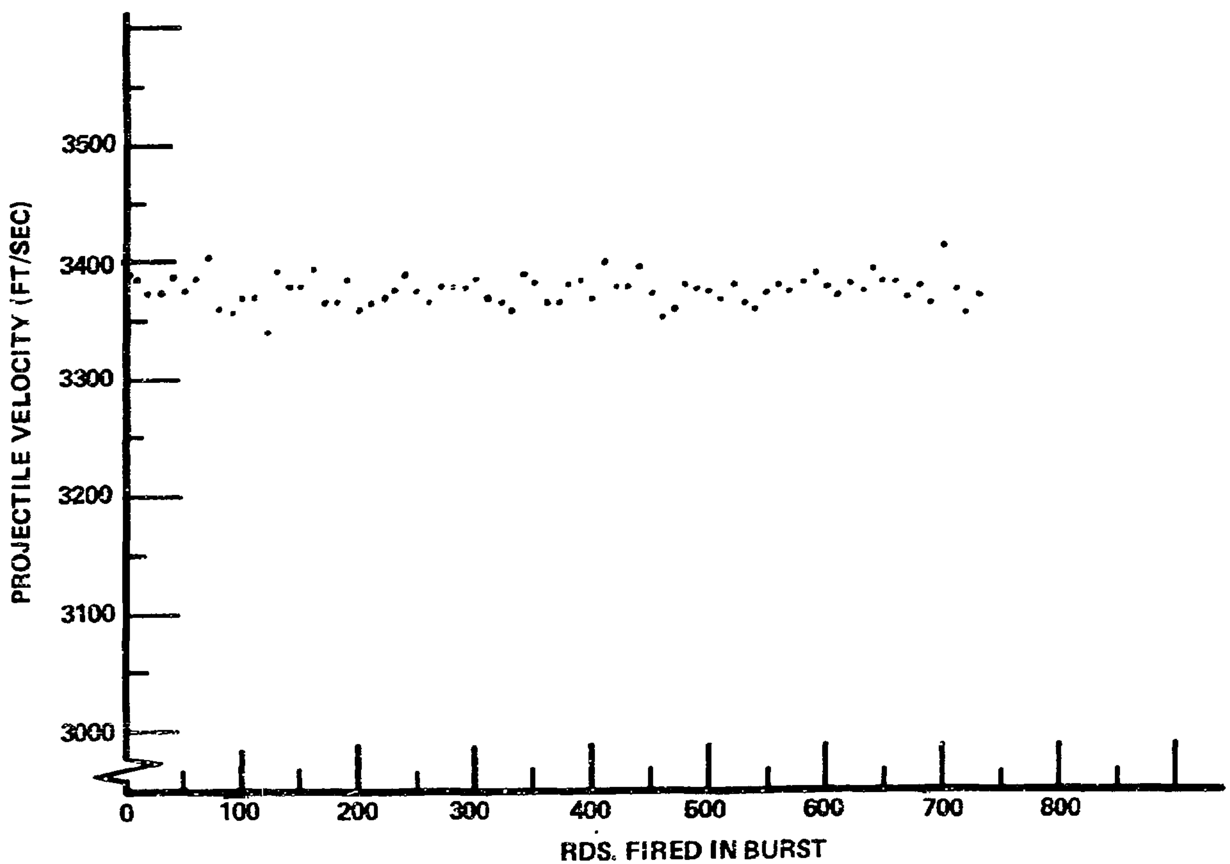


X = COPPER BANDS
. = PLASTIC BANDS
1.04
1.02
1.00
.98
.96
.94
.92
.90
MEAN BURST VELOCITY / MEAN LOT VELOCITY (LAT)
0
1000
2000
3000
4000
5000
6000
NUMBER ROUNDS FIRED (TOTAL SYSTEM)

Graph 2


BARREL WEAR
PLASTIC BAND NEW BARREL (1000001)
SAME BARREL AFTER 3667 ROUNDS
GRAPH 3
EROSION - INCHES
.10
.08
.06
.04
.02
0
2
4
6
8
10
12
14
16
18
20
22
24
26
28
30
32
46
66
DISTANCE FROM BREECH
-INCHES-
V-632

PROJECTILE VELOCITY VS BURST LENGTH FOR BURST # 137 MEASURED AT 912 IN. FROM MUZZLE EVERY 10th ROUND PLOTTED



V-633

Graph 4

BARREL EROSION MEASUREMENT

TABLE 3

NOTE: Before Start of 50,000 Rounds Test Date 13 December 1974

Rounds Accumulated Endurance None Rounds Accumulated Barrels 2118

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.072	0.073	0.077	0.071	0.075	0.075	0.074
85.0	0.055	0.070	0.063	0.051	0.063	0.064	0.059
84.5	0.012	0.015	0.015	0.013	0.013	0.013	0.013
84.0	0.008	0.013	0.013	0.011	0.010	0.010	0.011
83.5	0.002	0.005	0.004	0.003	0.003	0.003	0.003
83.0	0.002	0.006	0.004	0.004	0.004	0.003	0.004
82.0	0.003	0.005	0.005	0.004	0.005	0.004	0.004
80.0	0.004	0.007	0.007	0.005	0.006	0.005	0.005
70.0	0.004	0.008	0.007	0.006	0.006	0.006	0.006
60.0	0.005	0.007	0.006	0.005	0.006	0.008	0.007
40.0	0.005	0.008	0.008	0.005	0.005	0.006	0.006
20.0	0.006	0.008	0.007	0.006	0.007	0.006	0.007
1.0	0.005	0.005	0.005	0.005	0.006	0.006	0.006
75.0							
65.0							
55.0							
50.0							
45.0							

BARREL EROSION MEASUREMENT

TABLE 4

After Complement Number 5 Date 10 January 1975

Rounds Accumulated Endurance 6734 Rounds Accumulated Barrels 8852

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.072	0.075	0.075	0.071	0.075	0.073	0.073
85.0	0.048	0.055	0.054	0.048	0.059	0.052	0.057
84.5	0.011	0.013	0.014	0.013	0.014	0.012	0.013
84.0	0.009	0.012	0.012	0.011	0.012	0.011	0.012
83.5	0.002	0.004	0.004	0.003	0.005	0.002	0.003
83.0	0.003	0.005	0.004	0.003	0.005	0.003	0.003
82.0	0.003	0.005	0.004	0.003	0.006	0.003	0.004
80.0	0.003	0.006	0.005	0.004	0.007	0.004	0.005
70.0	0.005	0.007	0.007	0.006	0.007	0.005	0.006
60.0	0.005	0.007	0.007	0.005	0.007	0.005	0.007
40.0	0.005	0.007	0.007	0.005	0.007	0.006	0.007
20.0	0.006	0.007	0.007	0.006	0.005	0.006	0.007
1.0	0.005	0.005	0.005	0.005	0.006	0.005	0.006
75.0							
65.0							
55.0							
50.0							
45.0							

BARREL EROSION MEASUREMENT

TABLE 5

After Complement Number 6 Date 14 January 1975

Rounds Accumulated Endurance 8582 Rounds Accumulated Barrels 10,700

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.075	0.073	0.073	0.075	0.074	0.073	0.075
85.0	0.061	0.061	0.057	0.057	0.062	0.054	0.062
84.5	0.014	0.013	0.013	0.013	0.012	0.013	0.013
84.0	0.010	0.012	0.010	0.012	0.010	0.010	0.012
83.5	0.006	0.004	0.003	0.004	0.005	0.003	0.004
83.0	0.003	0.004	0.003	0.003	0.003	0.002	0.003
82.0	0.003	0.004	0.003	0.003	0.004	0.003	0.003
80.0	0.006	0.009	0.005	0.006	0.005	0.005	0.006
70.0	0.008	0.008	0.007	0.007	0.007	0.007	0.009
60.0	0.008	0.008	0.007	0.008	0.007	0.008	0.008
40.0	0.007	0.008	0.006	0.006	0.007	0.006	0.007
20.0	0.007	0.003	0.007	0.007	0.007	0.004	0.010
1.0	0.007	0.005	0.006	0.006	0.006	0.006	0.008
75.0							
65.0							
55.0							
50.0							
45.0							

BARREL EROSION MEASUREMENT

TABLE 6

After Complement Number 10 Date 21 January 1975

Rounds Accumulated Endurance 13,535 Rounds Accumulated Barrels 15,653

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.071	0.075	0.074	0.071	0.074	0.071	0.073
85.0	0.048	0.052	0.050	0.053	0.052	0.047	0.050
84.5	0.012	0.013	0.014	0.014	0.012	0.013	0.013
84.0	0.010	0.012	0.011	0.012	0.011	0.010	0.010
83.5	0.003	0.004	0.003	0.004	0.003	0.003	0.003
83.0	0.003	0.004	0.004	0.004	0.003	0.002	0.003
82.0	0.003	0.004	0.004	0.004	0.003	0.003	0.003
80.0	0.004	0.005	0.005	0.004	0.005	0.005	0.004
70.0	0.006	0.008	0.009	0.007	0.008	0.006	0.008
60.0	0.008	0.010	0.010	0.008	0.009	0.008	0.006
40.0	0.006	0.008	0.008	0.006	0.008	0.007	0.006
20.0	0.006	0.008	0.008	0.006	0.008	0.007	0.007
1.0	0.006	0.006	[illegible]06	0.006	0.006	0.006	0.006
75.0							
65.0							
55.0							
40.0							
45.0							

BARREL EROSION MEASUREMENT

TABLE 7

After Complement Number 15 Date 29 January 1975

Rounds Accumulated Endurance 20,279 Rounds Accumulated Barrels 22,397

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.071	0.074	0.075	0.072	0.072	0.071	0.072
85.0	0.049	0.052	0.051	0.050	0.053	0.049	0.052
84.5	0.011	0.012	0.013	0.012	0.011	0.011	0.011
84.0	0.010	0.012	0.013	0.011	0.011	0.010	0.011
83.5	0.002	0.004	0.003	0.003	0.003	0.002	0.003
83.0	0.003	0.004	0.004	0.003	0.003	0.002	0.003
82.0	0.003	0.004	0.004	0.003	0.003	0.003	0.003
80.0	0.004	0.005	0.005	0.004	0.004	0.004	0.004
70.0	0.008	0.011	0.010	0.008	0.010	0.009	0.008
60.0	0.012	0.020	0.017	0.011	0.020	0.015	0.009
40.0	0.008	0.010	0.009	0.007	0.009	0.008	0.008
20.0	0.006	0.008	0.008	0.008	0.008	0.008	0.007
1.0	0.007	0.006	0.006	0.007	0.006	0.006	0.007
75.0							
65.0							
55.0							
50.0							
45.0							

BARREL EROSION MEASUREMENT

TABLE 8

After Complement Number 17 Date 1 February 1975

Rounds Accumulated Endurance 22,971 Rounds Accumulated Barrels 25,089

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.070	0.074	0.074	0.072	0.075	0.071	0.073
85.0	0.051	0.052	0.057	0.049	0.052	0.052	0.050
84.5	0.011	0.012	0.013	0.011	0.012	0.012	0.011
84.0	0.008	0.010	0.010	0.010	0.009	0.009	0.009
83.5	0.002	0.003	0.004	0.003	0.002	0.002	0.003
83.0	0.001	0.003	0.004	0.003	0.002	0.002	0.002
82.0	0.002	0.004	0.005	0.005	0.003	0.003	0.003
80.0	0.004	0.006	0.007	0.005	0.005	0.004	0.005
70.0	0.015	0.020	0.020	0.016	0.025	0.022	0.012
60.0	0.029	0.044	0.042	0.022	0.038	0.035	0.016
40.0	0.008	0.010	0.010	0.009	0.010	0.009	0.008
20.0	0.007	0.009	0.009	0.008	0.009	0.008	0.008
1.0	0.007	0.008	0.008	0.008	0.007	0.008	0.008
75.0	0.007	0.008	0.008	0.008	0.007	0.007	0.008
65.0							
55.0	0.020	0.038	0.033	0.015	0.028	0.025	0.011
50.0	0.014	0.026	0.021	0.012	0.020	0.012	0.011
45.0	0.010	0.014	0.013	0.010	0.012	0.010	0.009

BARREL EROSION MEASUREMENT

TABLE 9

After Complement Number 19 Date 5 February 1975

Rounds Accumulated Endurance 25,669 Rounds Accumulated Barrels 27,787

INCHES FROM MUZZLE	BARREL SERIAL NUMBERS						
	1000001	1000002	1000004	1000005	1000006	1000007	1000023
85.5	0.071	0.073	0.073	0.073	0.074	0.072	0.075
85.0	0.051	0.057	0.053	0.053	0.053	0.051	0.057
84.5	0.012	0.013	0.013	0.013	0.012	0.013	0.011
84.0	0.009	0.011	0.012	0.010	0.011	0.010	0.010
83.5	0.002	0,002	0.003	0.003	0.003	0.003	0.002
83.0	0.002	0.004	0.004	0.003	0.003	0.002	0.003
82.0	0.003	0.005	0.004	0.003	0.004	0.002	0.003
80.0	0.004	0.006	0.006	0.004	0.005	0.005	0.004
70.0	0.024	0.030	0.028	0.017	0.033	0.025	0.016
60.0	0.041	0.051	0.053	0.036	0.047	0.045	0.033
40.0	0.009	0.012	0.009	0.009	0.010	0.010	0.009
20.0	0.008	0.010	0.007	0.008	0.007	0.008	0.008
1.0	0.008	0.007	0.007	0.008	0.007	0.008	0.008
75.0	0.007	0.009	0.010	0.007	0.009	0.008	0.008
65.0	0.042	0.049	0.048	0.036	0.045	0.045	0.029
55.0	0.037	0.046	0.036	0.027	0.040	0.037	0.023
50.0	0.020	0.037	0.031	0.018	0.032	0.029	0.013
45.0	0.013	0.025	0.012	0.010	0.010	0.012	0.011

GUN BARREL RIFLING DESIGNS FOR PLASTIC BANDED PROJECTILES

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Approximately four years ago, the Air Force began work on the development of a plastic rotating band for improved 20mm ammunition. Because the improved ammunition had to function in existing M61 gun and feed systems, the physical dimensions of the plastic rotating band had to be compatible with these systems and the rotating band width was constrained to be only slightly wider than that of the standard copper rotating band. The replacement of the standard copper rotating bands with plastic rotating bands was not as straightforward as it was originally thought that it would be and a large percentage of plastic rotating band failures resulted during the initial development testing. It was thought that possibly the gun barrel rifling configuration was contributing to the plastic rotating band failures and that optimizing the rifling design for plastic rotating bands would result in improved rotating band performance.

In October 1974, the Air Force Armament Laboratory awarded a contract to Aeronutronic Ford Corporation of Newport Beach, California, to develop an "Optimum Rifling Configuration for Plastic Rotating Bands". Since very little previous work had been done on designing gun barrel rifling for various rotating band materials (and plastic in particular), essentially no technology base existed and our work had to begin from scratch. The logical approach to optimizing the rifling configuration seemed to be to design the rifling to minimize the stresses induced in the rotating bands while still providing the projectile spin-up required for stability. A baseline 20mm projectile/plastic rotating band configuration was selected which exemplified the then current state-of-the-art in bonded plastic rotating band technology. This configuration was held constant throughout the program. It was felt that if a rifling design was developed which was optimum for this projectile/rotating band configuration, that this rifling design would also be optimum for other projectile/rotating band configurations of similar size and performance levels.

The first task was to determine the optimum rifling twist rate. This was a fairly straightforward mathematical task in which the torque applied to the rotating band was calculated as a function of twist rate, projectile travel, and muzzle velocity. Three twist configurations were evaluated: (a) constant twist with a 7° 3' exit angle; (b) exponential gain twist, which is standard for the M61 barrel; and (c) increasing gain twist with an initial rifling angle at the breech of 2° 30' increasing to a 7° 3' exit angle. As can be seen in Figure 1, a gain twist rifling configuration provides a significantly lower peak torque and more uniform torque loading than does constant twist rifling. Therefore, gain twist rifling is recommended for gun barrels which will have projectiles with plastic rotating bands fired through them.

Once a twist rate was established, the next task was to determine the effect that various rifling parameters (such as land and groove configuration, number of grooves, and choked rifling) had on rotating band performance. Again, the goal was to optimize the rifling by minimizing the stresses induced in the rotating band. Six different land and groove geometries were selected for evaluation. These configurations are illustrated in Figure 2. "Configuration A" is the conventional 9-groove M61 rifling currently in use. "Configuration B" represents a modified conventional rifling in which larger radii were employed to reduce stress concentrations, and the number of rifling grooves was increased from 9 to 12 in order to more evenly distribute the stresses within the rotating band. "Configuration C" represents a choked version of "Configuration B" in which the depth of the rifling decreases from the breech to the muzzle. The idea behind this configuration was to provide improved barrel obturation and ballistic performance as the plastic bands are worn or eroded as the result of travel through the gun barrel. "Configurations D, E, and F" represent new concepts in land and groove geometries. Again, more gradual contours were employed and the number of grooves was increased to 18 in an attempt to reduce stress concentrations and more evenly distribute the stresses within the rotating bands.

An evaluation approach was selected which employed extensive finite element computer analyses of the rifling configurations, low and high strain rate engraving and torsion laboratory testing, and actual single shot firing tests with microflash photography of the fired projectiles. A detailed description of this program and the final results are contained in AFATL-TR-75-153, "Optimum Rifling Configuration for Plastic Rotating Bands", dated November 1975. To summarize the results of this program, the 18-groove modified sawtooth configuration (Figure 2, Configuration F) appeared to be the best overall rifling configuration. The 18-groove modified sawtooth configuration was significantly better, in the overall analysis, than all other configurations evaluated and consistently finished at or near the top of each individual performance category. All other rifling configurations were essentially equal, some

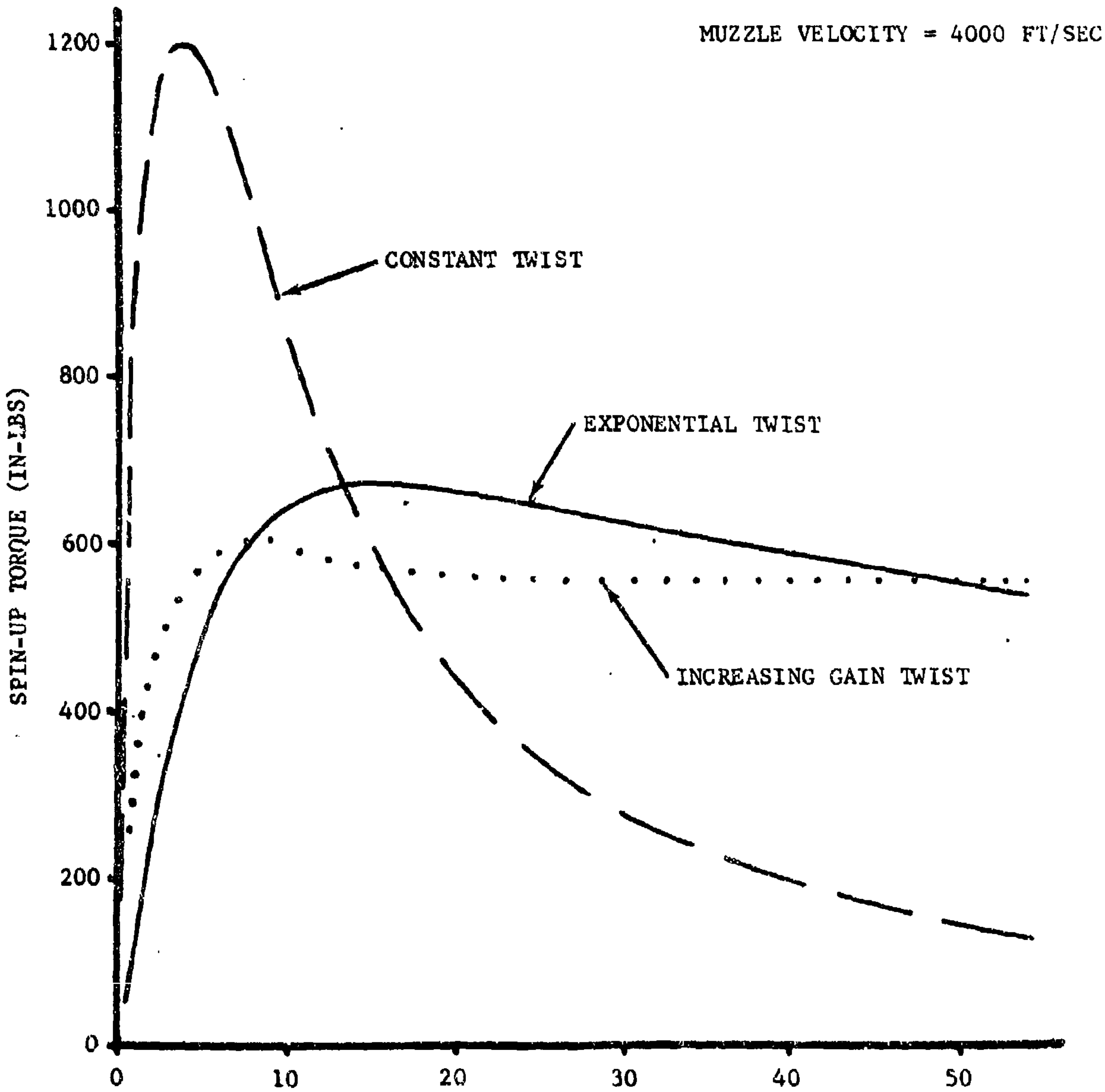

Figure 1. 20mm Spin-Up Torque for High Velocity Launch

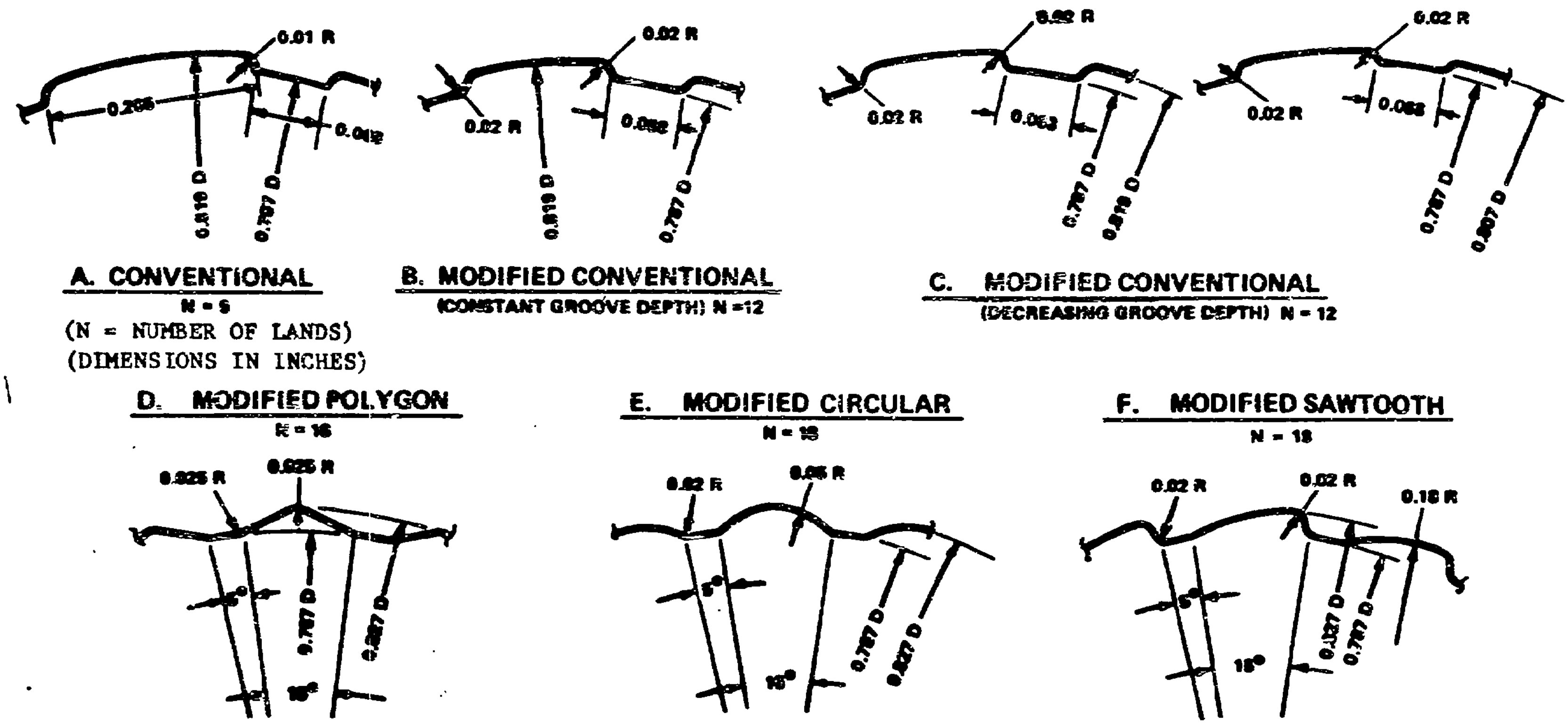

Figure 2. Candidate Rifling Profile Configurations

configurations ranking high in certain performance categories with other configurations ranking high in other performance categories.

The completion of this initial program resulted in the following conclusions. First of all, the standard rifling currently used in the M61 gun barrels (and probably most other gun barrel rifling as well) is far from optimum for plastic banded projectiles. Secondly, it was not clear whether the sawtooth groove configuration or the fact that this rifling had 18-grooves was the main factor contributing to the improved performance of this rifling configuration. Third, no quantitative predictions could be made of what a change in rifling would actually mean in the way of improved rotating band performance (i.e., would it be possible to fire plastic banded projectiles at higher muzzle velocities before band failures occurred). And last, it was obvious that a huge void existed in gun barrel rifling design technology.

A second Air Force Armament Laboratory contract was issued to Aeronutronic Ford Corporation in April 1976 to further investigate optimized gun barrel rifling and answer some of the above questions. This program is discussed in detail in AFATL-TR-76-152, "Optimum Rifling for Plastic Bands", dated December 1976. The basic approach of this program was to fire a large number of 20mm projectiles with plastic rotating bands through gun barrels with the following rifling configurations: (a) conventional 9-groove rectangular M61 rifling; (b) modified conventional 12-groove rifling; (c) choked modified conventional 12-groove rifling; (d) modified conventional 18-groove rifling; and (e) modified sawtooth 18-groove rifling. For each round fired, measurements of chamber pressure, muzzle velocity, accuracy, and projectile yaw were made. In addition, microflash photographs of each projectile were taken to evaluate the condition of the plastic rotating bands as they exited the barrels. The desirable approach for evaluating the above rifling configurations would have been to incrementally increase the muzzle velocities of the projectiles fired through each barrel until band failures resulted. This approach would have been difficult from the standpoint that extensive interior ballistic experimentation would have been required to establish the propellant loads for each velocity increment. An alternative approach was selected in which the 0.280-inch plastic rotating band width was incrementally decreased until band failures occurred as witnessed in the microflash photographs.

The results of this testing were as follows: (a) the microflash photographs indicated that rotating band smearing or wiping did not occur in the modified conventional 18-groove barrel and modified sawtooth 18-groove barrel until band widths were reduced t

0.160-inch, all other rifling configurations resulted in band smearing and wiping at rotating band widths of 0.240-inch; (b) the microflash photographs indicated slightly improved performance with the modified conventional 18-groove rifling as opposed to the modified sawtooth 18-groove rifling; and (c) all other measurements (muzzle velocity, chamber pressure, accuracy, and projectile yaw) indicated essentially equal performance of all the rifling configurations tested.

From the results of this program, it can be concluded that: (a) the number of rifling grooves, and not the groove configuration, is the more important variable in gun barrel rifling design; and (b) choked rifling offers no advantages over constant groove depth rifling.

As can be seen from all of the above, we have just begun to scratch the surface in optimizing gun barrel rifling design. We plan on continuing work in this area as well as interfacing it with a plastic rotating band design program to establish the optimum rifling/rotating band systems for our future gun/ammunition systems.

ELECTROCHEMICAL RIFLING OF GUN BARRELS

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I. INTRODUCTION

Increasing requirements for improved gun barrel materials have become apparent with the development of new high performance weapon systems. With these new systems, gun barrels must withstand higher muzzle velocities, higher firing rates, and more severe firing schedules, which not only increase barrel erosion rates, but also cause potential barrel overheating and possible structural failure. The Army and Air Force have actively pursued solutions to the high performance barrel problem for several years by funding several research and development programs with the objective of developing new barrel concepts, materials, and fabrication procedures. These programs, as well as internally funded barrel technology programs, have involved high strength steels and stainless steels, superalloys, and refractory metals. The work has indicated that generally the more erosion-resistant materials are also the most difficult to fabricate by conventional techniques, and before improved materials can be incorporated into new barrel designs, nonconventional high production rate fabrication processes must be developed in order to maintain cost effectiveness.

A significant advancement in barrel fabrication technology was achieved under Air Force Contract F08635-71-C-0209, "Development of an Electrochemical Machining Technique for Rifling Lined Gun Barrels", December 1972. This contract resulted in development of an electrochemical rifling process for caliber .220 Swift barrels fabricated from gun steel, and iron-base, nickel-base, and cobalt-base superalloys. The process not only produced excellent surface finishes and good dimensional control but also proved to be very rapid and potentially inexpensive when compared with conventional rifling methods. Based on the success of this program, an extrapolation of the process to larger calibers and longer barrel lengths was recommended.

The state-of-the-art was increased by an Army program Contract Number DAAF03-72-C-0189, "Development of an Electrochemical Rifling Process for 30 mm Gun Barrel", July 1973. The objective of this program was to develop electrochemical rifling techniques for Udimet 700 and rifling four XM140E5 30 mm barrel blanks for delivery to the Army for fabrication and testing.

An advantage of electrochemical rifling is that it is well suited toward fabricating production quantities. Accordingly, a program was awarded by the U.S. Army Armament Command to demonstrate the feasibility of electrochemically rifling five Squad Automatic Weapon System (SAWS) barrel blanks and delivering them for further fabrication and testing, Contract Number DAAA09-74-C-2005, "Electrochemical Rifling of SAWS Barrels", December 1973.

As a follow-on to the Air Force small caliber electrochemical rifling work, the Air Force awarded Contract F08635-73-C-0091, "Electrochemical Machining of Automatic Gun Barrels," with the objective of developing an electrochemical rifling process for 25 mm, 7-foot long barrels with gain-twist rifling fabricated from high strength stainless steels and superalloys. Preliminary investigations of electrochemical drilling of gun barrels were also included in this contract, and this effort was subsequently increased by an added task under Contract F08635-73-C-0057, "Fabrication of Composite Test Barrels", July 1974.

II. POTENTIAL RIFLING PROCESSES

A. CHEMICAL MILLING

Chemical milling, chemical machining and chemical contouring are terms applied to the production of desired shapes and dimensions through selective or overall removal of metal by chemical attack or etching. Selective removal requires the use of masking to protect certain areas from the chemical solution. Utilizing this technique for rifling would require masking the land area of honed barrel blanks, thus allowing the groove area to be attacked by the chemical solution. Developing a masking material with excellent edge protection in flowing chemicals presents a major problem for this process.

B. ELECTRICAL DISCHARGE MACHINING (EDM)

Electrical Discharge Machining is a method for producing holes, slots or other cavities in electrically conductive material by means of the controlled removal of material through melting or vaporization by high-frequency electrical sparks. Each spark produces enough heat to melt or vaporize a small quantity of the workpiece, leaving a tiny pit or crater in the surface of the work. The as-cast structure, resulting from the method of material removal, may result in an extremely hard brittle surface layer with considerable roughness. The resulting surface finish and condition could be detrimental to rotating bands and cause erosion. Also, since the tool wear is very rapid, this process is not economically feasible for rifling or drilling gun barrels.

C. ELECTROCHEMICAL MACHINING (ECM)

Electrochemical machining is the controlled removal of metal by anodic dissolution in an electrolytic cell in which the workpiece is the anode and the tool is the cathode. The electrolyte is pumped through the gap between the tool and the workpiece, while direct current is passed through the cell at low voltage, to dissolve metal from the workpiece.

Electrochemical machining has the following advantages over chemical machining and electrical discharge machining:

1. Masking for selective material removal is an electrical insulation applied to the external surface of the tool instead of the workpiece. Costs are reduced since the tooling has almost unlimited life. Small bore rifling is also possible.

2. The electrolyte is normally noncorrosive, thus making handling of the electrolyte easy compared to the chemicals necessary for chemical machining.

3. The low metal temperature encountered in electrochemical machining, compared to electrical discharge machining, does not alter the surface condition of the remaining material. Proper selection of electrochemical machining parameters produces a very smooth electropolished surface.

III. DEVELOPMENT OF AN ELECTROCHEMICAL MACHINING PROCESS FOR RIFLING LINED GUN BARRELS. CONTRACT NO. F08635-71-C-0209.

A. OBJECTIVE

The objective of this program was to develop an electrochemical machining process for rifling high performance barrel liner materials. Twelve .220 Swift barrel liners were delivered to the Air Force.

B. LINER MATERIALS

Pure metals or alloys representing several classes of potential barrel liner materials were considered. These included: (1) CG-27, an iron-nickel base intermediate temperature superalloy; (2) Alloy 718, a nickel-base intermediate temperature superalloy; (3) L-605 and VM-103, high temperature cobalt-base superalloys; (4) WC-3015 and Cb-752, columbium-base alloys; (5) TZM, a molybdenum-base alloy; (6) T-111, a high strength tantalum alloy; and finally (7) unalloyed tungsten. The intent was to determine which classes of materials could be electrochemically machined, and then to select those alloys with the highest predicted erosion resistance for development of rifling parameters adequate for fabrication of deliverable test barrels. Although the superalloys can be rified by conventional techniques, the process is comparatively slow and costly due to their generally low machinability. Conventional rifling of Cb, Ta, and Mo alloys is extremely difficult and results in poor surface finishes. Pure tungsten has also proven very difficult to machine, although it potentially offers excellent erosion resistance.

C. ELECTROCHEMICAL MACHINABILITY STUDIES

The initial machinability tests consisted of electrochemical machining 0.075-inch wide by 0.003-inch deep by 2-inch long grooves on the outside diameter surface of the test specimens. These tests were designed to determine the basic electrochemical machinability of the materials, and, in addition, permit evaluation of surface finishes obtained under electrochemical machining conditions similar to those which exist during rifling using the stationary electrode technique. The tooling for these tests, shown in Figure 1, consisted of a slotted fiberglass epoxy block containing an adjustable copper shim. The shim could be moved up or down within the slot to obtain any desired electrode-workpiece distance. The electrical cable attachment to the shim and the electrolyte flow passage are also shown.

A total of fifteen electrolyte systems (Table I) were evaluated in attempting to electrochemically machine the seven materials being considered as candidate barrel liner materials. These electrolytes ranged from the standard sodium chloride solution to nonsludging acidic and basic electrolytes and included some special compositions that had not been previously evaluated. The special compositions were formulated in an effort to machine the alloys which did not respond to the more common systems.

Visual and metallographic examination of electrochemically machined surfaces of L-605, TZM, and VM-103 indicated that these materials could be satisfactorily machined with sodium chloride electrolyte and WC-3015 with sodium bromide/sodium nitrite/sodium nitrate/sodium fluoride electrolyte. Pure tungsten could be machined with sodium hydroxide electrolyte, but a satisfactory surface finish could not be consistently obtained. Cb-752 could be machined with the sodium bromide/sodium nitrite electrolyte. Again, a satisfactory surface finish could not be obtained consistently. T-111 could not be machined with any of the fifteen electrolyte systems evaluated. No intergranular attack was observed on the L-605-TZM, VM-103 or WC-3015.

The radius of curvature at the surface intersection and the bottom of the groove was about 0.006 to 0.010 inch, whereas, at the groove-bore intersection the edge still appeared sharp at 250X. This geometry probably does not differ enough from conventionally machined radii to significantly change the projectile spin-up and obturation characteristics of the gun barrel.

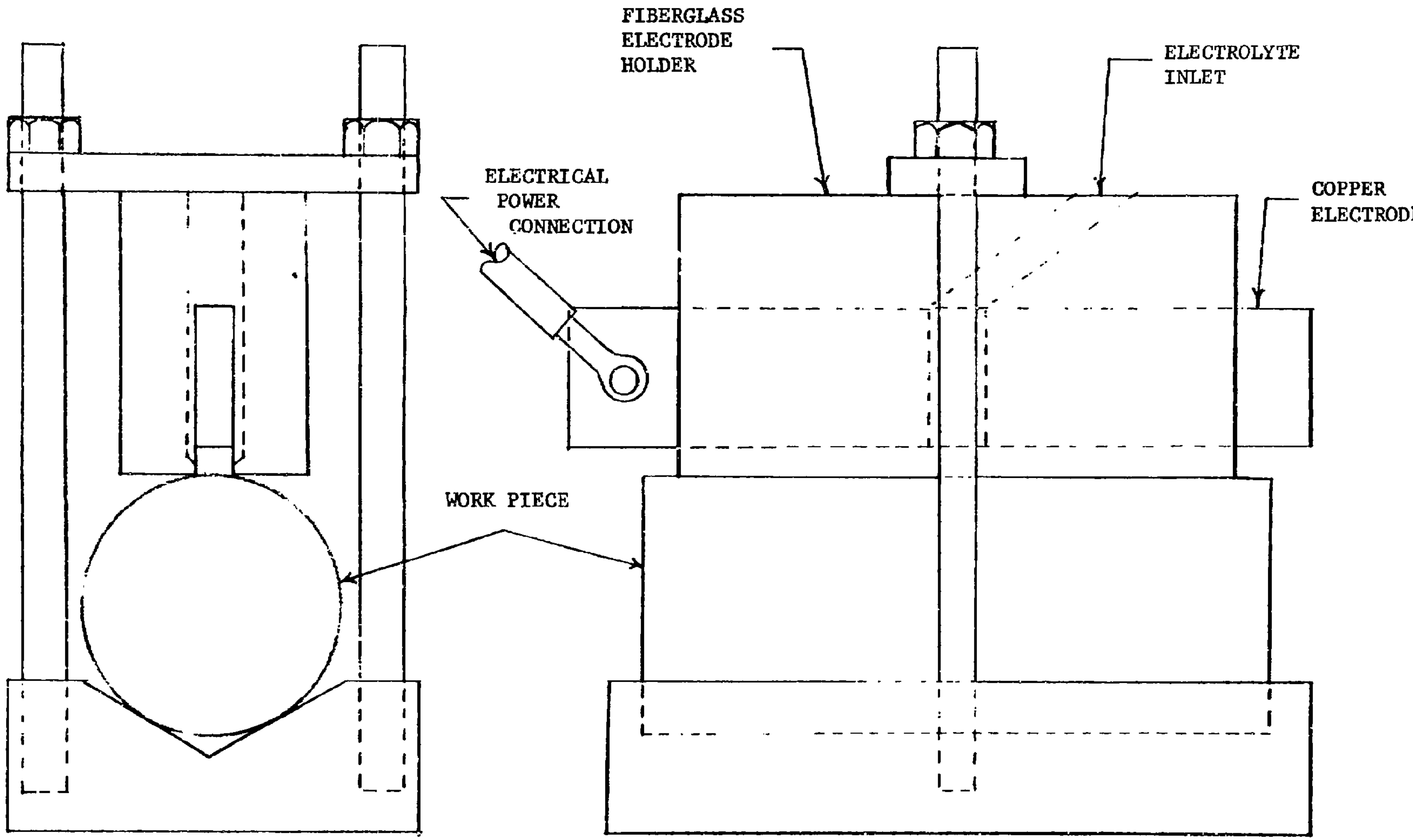


FIGURE 1. EXTERNAL RIFLING FIXTURE

TABLE I. COMPOSITIONS OF THE ELECTROLYTES EVALUATED

COMPOSITION	
Sodium Chloride	1 lb/gal
Sodium Chloride Sodium Nitrate Sodium Citrate Rochelle Salt	1 lb/gal 3 oz/gal 6 oz/gal 4 oz/gal
Sodium Chloride	0.55 lb/gal
Sodium Nitrate	2 lb/gal
Sodium Chlorate	2 lb/gal
Sulfuric Acid	5 percent
Sulfuric Acid	10 percent
Sodium Bromide Sodium Nitrite Sodium Nitrate Sodium Fluoride	1.5 lb/gal 1.5 lb/gal 0.1 lb/gal 0.1 lb/gal
Sodium Hydroxide	2 percent
Sodium Hydroxide	6 percent
Sodium Chloride Sodium Hydroxide Rochelle Salt	0.5 lb/gal 0.5 lb/gal 0.5 lb/gal
Sodium Bromide Sodium Nitrite Sodium Nitrate Sodium Fluoride Sodium Hydroxide	1.5 lb/gal 1.5 lb/gal 0.1 lb/gal 0.1 lb/gal 0.15 lb/gal
Nitric/Hydrofluoric Acid (65/35)	12-1/2 percent
Nitric/Hydrofluoric Acid (65/35)	25 percent
Nitric/Hydrofluoric Acid (65/35)	50 percent

D. ELECTROCHEMICAL MACHINING RIFLING TESTS

Two different methods were considered for electrochemical rifling; i.e. rifling with either a moving or a stationary electrode. The moving electrode would be similar to an electrical discharge machining (EDM) electrode, which is translated and rotated simultaneously to achieve the desired rifling twist. This method was considered undesirable due to anticipated tool chatter. The tooling design, fabrication complexity and higher cost of this method made it appear less attractive than the stationary electrode, described below.

The stationary electrode approach for electrochemically rifling gun barrels uses a metal electrode insulated on the outside diameter which extends through the length of the barrel to be rifled. This electrode has grooves machined through the insulation which correspond to the dimensions and configuration of the desired rifling in the gun barrel. This electrode is placed inside the barrel to be rifled, electrolyte is pumped through the grooves in the electrode, and when current is applied, corresponding rifling grooves are electrochemically machined into the barrel (see Figures 2 and 3). This approach has the advantage of much greater economy, since the entire barrel is rifled at one time. The moving electrode travel would be limited by the electrochemical machining removal rate, and rifling would be much more time consuming and costly.

E. STATIONARY ELECTRODE DEVELOPMENT

The initial objective of this portion of the program was to develop a satisfactory stationary electrode. Rifling tests were conducted using relatively short (approximately 6-inch) length 4130 steel tubes to determine optimum groove shape, evaluate various types of insulation materials and application techniques, and to establish electrochemical machining parameters. Machining tests conducted with aluminum electrodes revealed that the aluminum was eroded during machining operations, leaving rough edges at the corners of the electrode groove. Changing the electrode material to a tellurium-copper alloy eliminated this effect.

These initial tests were conducted with electrodes having a thin (0.002-inch) coating of epoxy insulation, which produced a smooth groove with a rough radius adjacent to the land area. As a result, electrodes were fabricated having a thick (0.025-inch) coating of insulation which produced grooves with a smooth radius. Therefore, all subsequent electrochemical machining rifling was accomplished with electrodes having the thick insulation coating.

In parallel to the above epoxy insulation thickness evaluation, tests were conducted using electrodes insulated with the thin (0.002-inch) coating of epoxy which had four different groove shapes

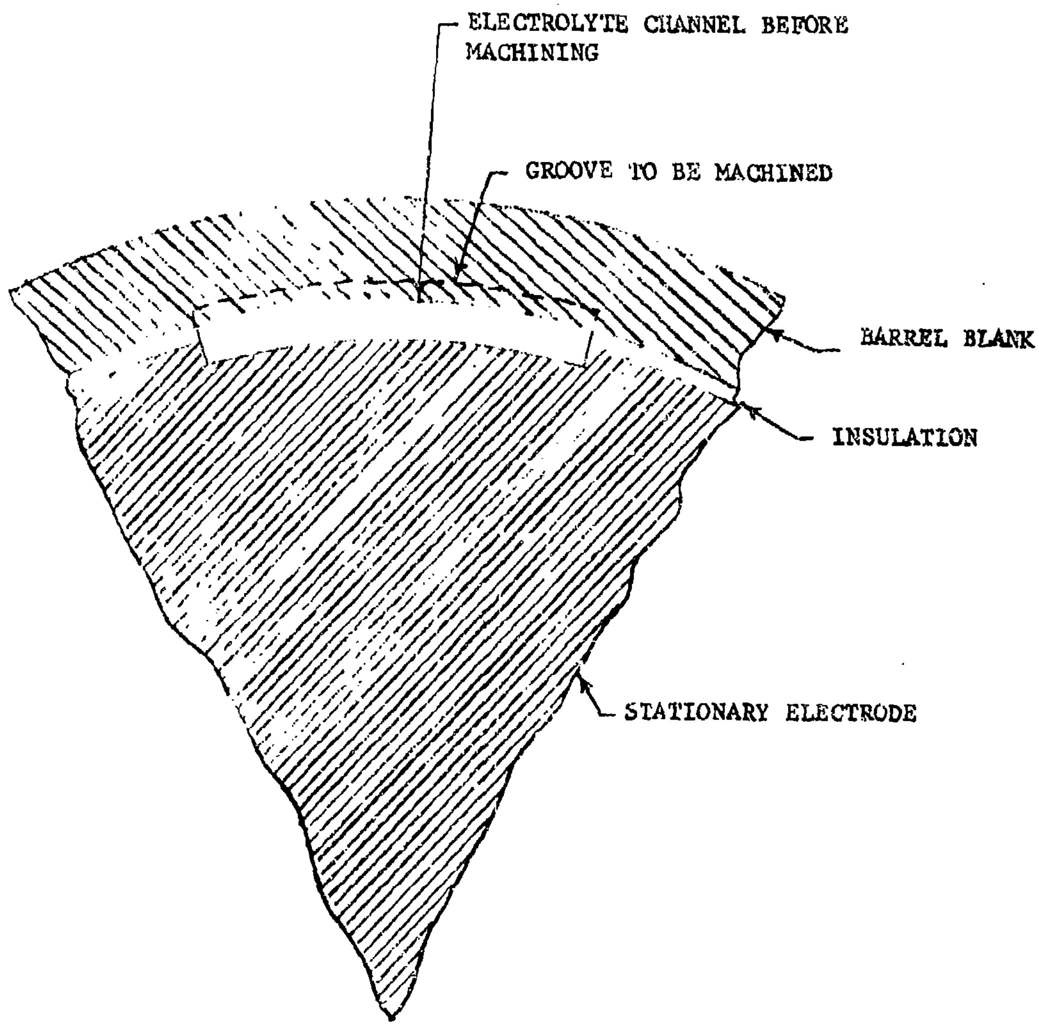


FIGURE 2. SKETCH OF ELECTROCHEMICAL RIFLING SET-UP WITH STATIONARY ELECTRODE

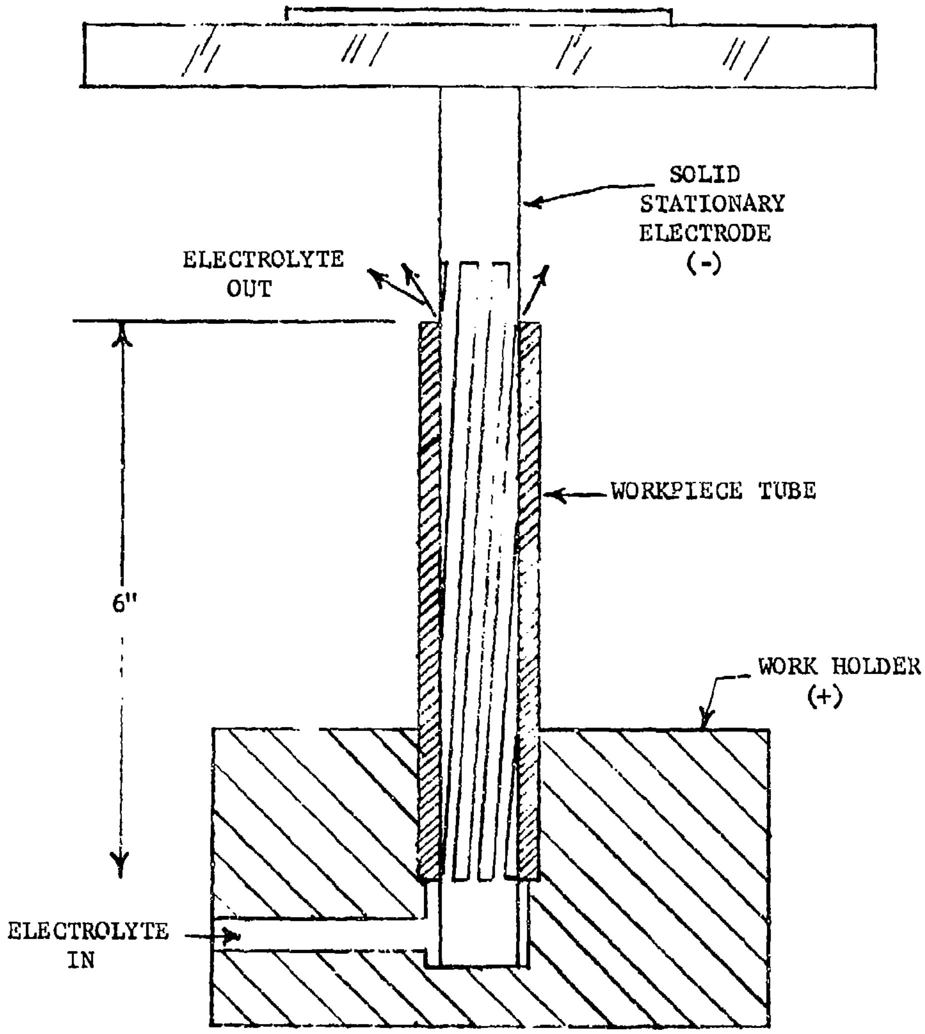


FIGURE 3. SKETCH OF SHORT-LENGTH ELECTROCHEMICAL RIFLING TOOLING UTILIZING THE STATIONARY ELECTRODE TECHNIQUE

(Figure 4), the objective being to determine the effect of groove shape on machining characteristics. No difference in machining characteristics among groove shapes was observed. Because groove shape "B" was most like conventional rifling, it was selected for all subsequent rifling.

A second parallel effort consisted of evaluating various electrode insulation materials to determine which was most suitable. Epoxy, acrylic, phenolic, polycarbonate, and acetal plastics were investigated. Of these, the epoxy and polycarbonate materials performed best. The final electrode design used a 0.169-inch diameter tellurium-copper rod which was coated with epoxy using a fluidized-bed application technique. The electrode was coated oversize and then centerless-ground to the final 0.219-inch diameter. Rifling grooves 0.070-inch wide by 0.025-inch deep were then machined in the electrode. A 0.220 Swift barrel has six grooves which are 0.074 +0.002/-0.000 inch wide.

F. ELECTROCHEMICAL MACHINING RIFLING SHORT LENGTH TRIALS

Having obtained a satisfactory stationary electrode, the next objective was to use the outside diameter electrochemical rifling results previously described, to develop electrochemical machining parameters for internal rifling of the candidate liner materials.

Initially, four materials — L-605, VM-103, TZM and WC-3015 — were tentatively selected as the deliverable barrel liner materials. In the course of the program, CG-27 and Inconel 718 were substituted for TZM and WC-3105 by mutual agreement with the contract monitor. TZM was dropped because of the poor erosion resistance it demonstrated during testing under Air Force Contract F08635-71-C-0181. WC-3015 was dropped because procurement of acceptable WC-3015 tubing could not be accomplished within the schedule of this program. However, WC-3015 is still an attractive future candidate since it was shown to be electrochemically machinable. CG-27 and Inconel 718 were selected as substitutes because of their known good electrochemical machinability, good erosion resistance (CG-27 was used successfully as a barrel material for the XM-140), and the fact that both were candidates for the GAU-8/A and other high performance weapons.

Rifling tests using short length barrels were run on five materials: 4130, L-605, VM-103, CG-27 and Inconel 718. The electrochemical parameters developed in these tests were then used as a guide to fabricate the full length liners for the deliverable barrels.

G. ELECTROCHEMICAL MACHINING FULL LENGTH LINERS

The goal of this program was to fabricate and deliver to the Air Force twelve barrels with electrochemically rifled liners.

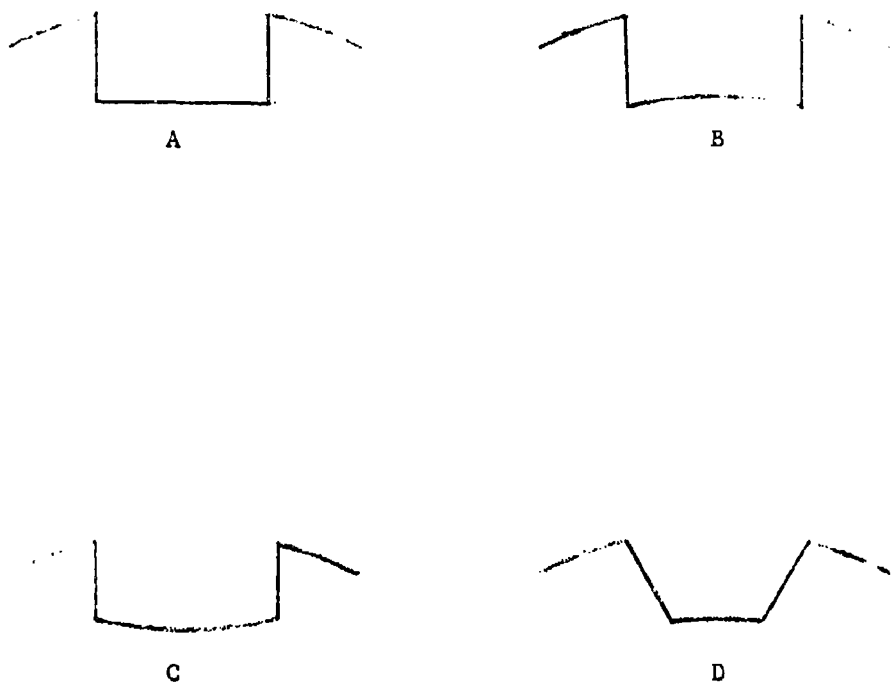


FIGURE 4. THE FOUR GROOVE SHAPES EVALUATED

The design of the full-length (24-inch) tooling for electrochemical rifling is sketched in Figure 5. Initial tests were conducted using a three-groove electrode which was indexed once to produce the six required rifle grooves. However, difficulty was encountered with insulation breakage, and a new electrode was fabricated having only two grooves. This electrode was indexed twice to produce the six grooves and was found to perform satisfactorily.

Using the successful two-groove electrode, two full-length 4130 steel barrels were satisfactorily electrochemically rifled prior to fabrication of the remaining liners.

Using conventional techniques, the gun barrel liners were gun-drilled, honed, and final machined on the outside diameter. They were then electrochemically rifled. A total of four barrels of L-605 and three barrels each of VM-103, CG-27, and Inconel 718 were electrochemically rifled. The bore data were very consistent, as expected, because the bore was honed to a constant inside diameter before electrochemical rifling. All bore diameters were on the low side of the dimension tolerance, 0.219 to 0.220 inch, the vast majority being between 0.2187 inch and 0.2193 inch. The groove diameters were either in tolerance or within 0.0010-inch undersize. A tendency of the electrochemical rifling process to produce shallower grooves at each end of the barrel was readily observed. An attempt to counteract this effect was made by trimming one inch of length from each end; however, the data show this did not eliminate the effect completely. All barrels met the groove depth tolerance of 0.002 to 0.003 inch, except two which were too deep by 0.0001 inch and 0.0004 inch at same location.

All VM-103 liners were gun-drilled using the electrical discharge machining process because conventional machining has been proven to be too slow to be economically feasible. The bores were drilled with a half-length electrode—one-half was drilled starting from one end and the other half was drilled from the other end. This resulted in a mismatch at the middle of the barrel length where the two holes met. Final honing did not eliminate this mismatch entirely, which existed in all of the VM-103 barrels delivered.

The outside diameters of the barrels were conventionally machined, chambered, and inspected. They were then subjected to a ten-round proof test firing burst on an MG-3 machine gun at approximately 1150 spm. Borescope examination and black oxide treatments followed. A sealing arrangement was constructed to prevent the black oxide solution from reaching the bore or the insulation between the liner and jacket. Finally, the barrels were air gauged, coated with a preservative oil, packaged, and shipped to the Air Force.

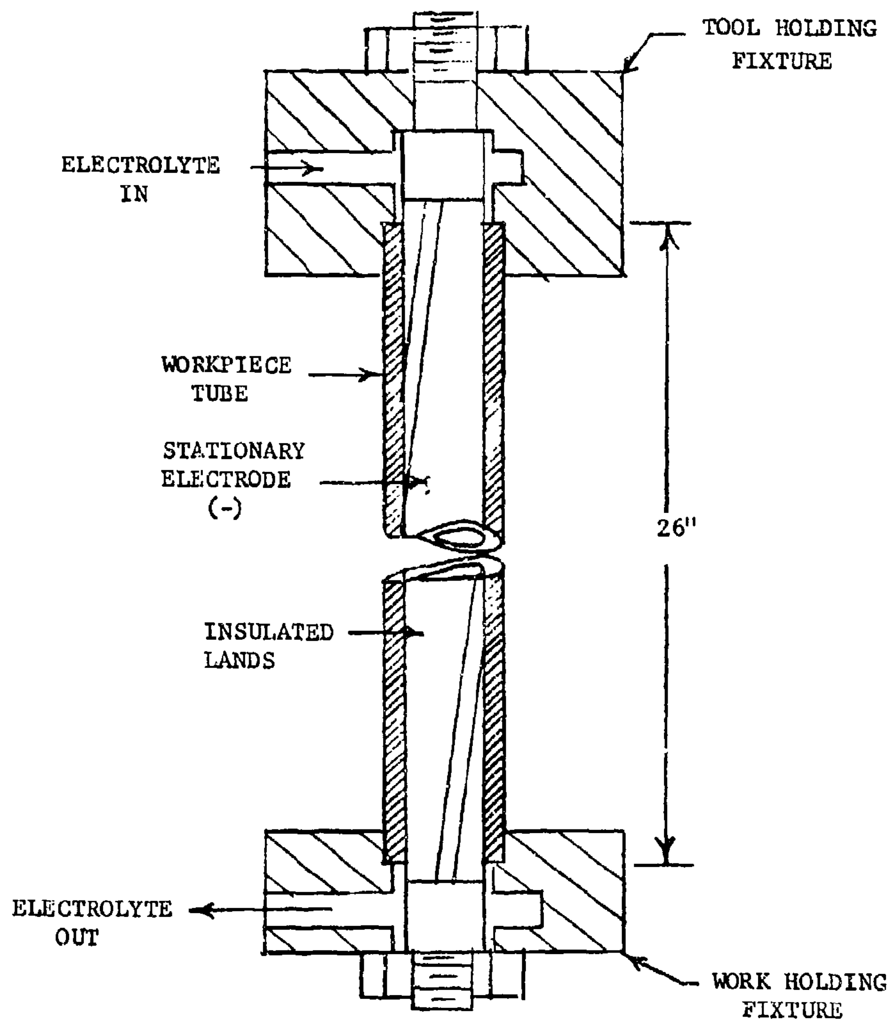


FIGURE 5. SKETCH OF FULL-LENGTH ELECTROCHEMICAL RIFLING TOOLING UTILIZING THE STATIONARY ELECTRODE TECHNIQUE

IV. DEVELOPMENT OF AN ELECTROCHEMICAL RIFLING PROCESS FOR 30 MM GUN BARRELS. CONTRACT NO. DAAF0-72-C-0189

A. OBJECTIVE

The objective of this program was to develop an electrochemical rifling process for 30 mm, 52-inch long Udimet 700 superalloy barrels for the XM 140 ES and deliver them to the Army for fabrication and testing. The approach was to scale-up the electrochemical rifling process previously developed for the .220 Swift, 24-inch long barrels to the XM 140 30 mm size.

B. ELECTROCHEMICAL RIFLING PROCESS DESCRIPTION

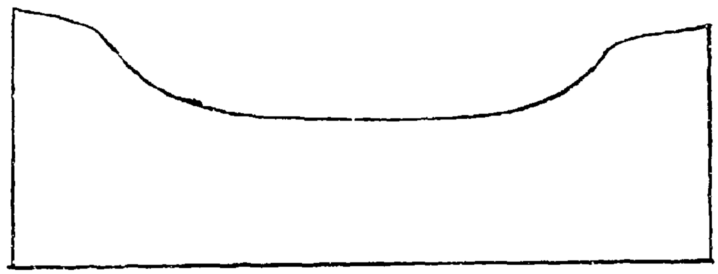
The stationary electrode utilized for this program contained four grooves, which required indexing three times until the 16 grooves were machined. The selection of only four grooves instead of the full 16 was based solely on the electrical power capability of the machine used for this development work; a 16-groove electrode would obviously be utilized with a larger power supply for production electrochemical rifling.

Short-length (12-inch) 4130 steel and Udimet 700 tubes were electrochemically rifled first in order to obtain operating parameters and groove geometry needed for the full-length tubes. The 4130 steel tubes were prepared by conventionally gun-drilling and honing to the required bore diameter. The Udimet 700 tubes were trim stock from the deliverable pieces.

The short-length steel specimens were electrochemically rifled easily. The width of the electrode groove was varied until the approximate rifling groove width was obtained. This dimension was then used as a starting point for the Udimet 700 trials.

Initial trials with the Udimet 700 short-length tubes revealed that the cross-section of an electrochemically machined groove sufficiently approximated that required by the XM 140 barrel. Comparing the scale drawing and the actual profile of an electrochemically machined groove shown in Figure 6, reveals that the electrochemically machined groove has a somewhat larger radius at the base of the groove than specified on the drawing. However, this is not considered detrimental to the performance of the barrel, and in fact, some weapons such as GAU-7/A require a generous radius to minimize the notch effect and to be more compatible with plastic rotating bands.

The land edge radius was much closer to that specified. This was controlled by the clearance between the electrode insulation and bore surface. Therefore, every effort was made to minimize this clearance and achieve a sharp land edge radius.



ELECTROCHEMICALLY MACHINED GROOVE PROFILE

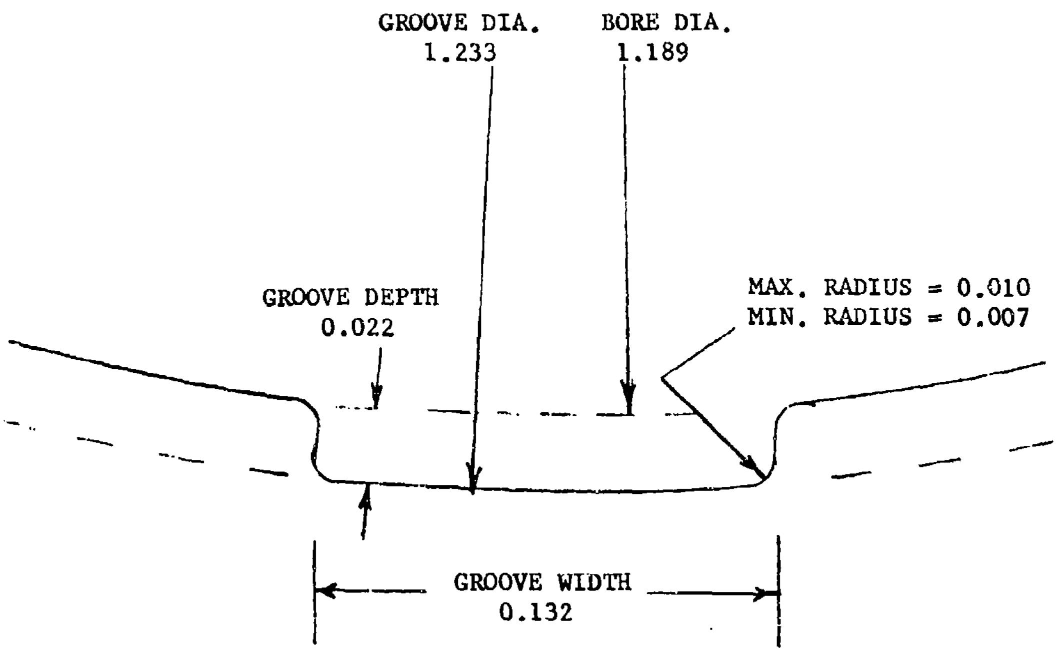


FIGURE 6. TYPICAL ELECTROCHEMICALLY MACHINED GROOVE IN UDIMET 700

In order to allow for the change in groove configuration, the width of the groove was increased until the groove cross-sectional area obtained electrochemically was approximately the same as that obtained with the dimensions specified on the drawing. It was considered more desirable to increase the groove width rather than the groove depth to minimize possibility of blow-by. An experiment was performed in which increasingly wider electrode grooves were used to generate increasingly wider rifling grooves. Based on these tests, an electrode groove width of 0.113 inch was selected for the full length tooling to machine a 0.132-inch wide groove.

The steel and Udimet 700 electrochemically machined tubes reveal a very smooth surface, approximately RMS 32. The surface is not shiny, but has an etched appearance. The grain structure of the material along the rifling grooves is usually revealed even though no preferential grain boundary attack occurs.

C. FULL-LENGTH STEEL BARREL RIFLING

Both constant-twist and gain-twist full-length electrodes were checked out on 4130 steel tubes before attempting to use them with the Udimet 700. Rifling was accomplished along the full length as expected; however, a larger variation in groove depth with respect to length was obtained than was anticipated. A maximum occurs at the tube center and tapers off toward each end. The total variation obtained on the 24-inch long .220 Swift barrels was about 0.0005 inch on a 0.074-inch wide by 0.0025-inch deep groove, whereas, the variation on the 60-inch long XM-140 barrels was as large as 0.004 inch on a 0.132-inch wide by 0.022-inch deep groove.

D. FULL-LENGTH UDIMET 700 DELIVERABLE TUBES

Four Udimet 700 tubes were electrochemically rifled. The rifling in tubes 1 and 2 was constant twist while tubes 3 and 4 were gain twist. All four tubes were GFE (fabricated by Batelle Northwest). The tubes were received in the aged condition, Rc 39. The bore dimensions were spot checked and found to be within tolerance. Tubes 1 and 2 had been gun-drilled and honed conventionally while tubes 3 and 4 had been gun drilled and honed oversized and then cold swaged to size.

The difference in the two processing sequences was subsequently found to be significant with regard to straightening. Because of the critical fit-up needed with the electrode, a straightness requirement of 0.001 in./ft maximum bow was necessary which required that the tubes be restraightened. After restraightening, tubes 1 and 2 were satisfactory, but tubes 3 and 4 still caused considerable difficulty during insertion of the electrode.

As with the full length steel barrels, cutting rate varied along the length of the Udimet 700 tubes also. This is seen in Figure 7 in which groove diameter is plotted as a function of length to more clearly show this effect. Since only 48 inches of rifling was required, it was possible to choose the trim locations such that all the rifling was within drawing dimensional requirements. The recommended muzzle and breech ends were metal-stamped on all deliverable Udimet 700 tubes.

An evaluation of the groove shape in the muzzle trim on all deliverable tubes was made and compared to the desired groove shape, together with that obtained with the short length Udimet 700 specimens discussed above. Tubes 1 and 2 displayed better groove shapes than tubes 3 and 4. Final hand-sanding was required to achieve fit-up of the electrode due to lack of straightness of barrels 3 and 4. This caused more of a gap than desired which resulted in some slight electrochemical machining on the land surfaces.

This type of problem is easily eliminated by a very close fit between the electrode and the tube to be rifled. Its observation in this case emphasizes the importance of close fit-up which cannot be achieved unless barrel straightness is maintained. Based on this work, a maximum bow of 0.001 inch/foot is recommended.

V. ELECTROCHEMICAL RIFLING OF SAWS BARRELS, CONTRACT NO. DAAA09-74-C-2005

A. OBJECTIVE

The objective of the program was to electrochemically rifle five SAWS barrels to demonstrate the feasibility of this process for a 6 mm bore. These five barrels were to be delivered to the Armament Command for test firing and evaluation.

B. ELECTROCHEMICAL RIFLING PROCESS

The SAWS barrel material was D6AC quenched and tempered to Rc 32-35. After heat treatment, the blanks were conventionally gun-drilled and honed to an inside diameter of 0.2350 + 0.0005 inch. This was followed by electrochemical rifling.

The stationary electrode was inserted into the bore of the tube to be rifled, electrolyte pumped through the grooves and current passed through the electrolyte to initiate machining action. Two grooves were electrochemically machined at a time. After completing the first set, the electrode was indexed 90° and the second set was machined.

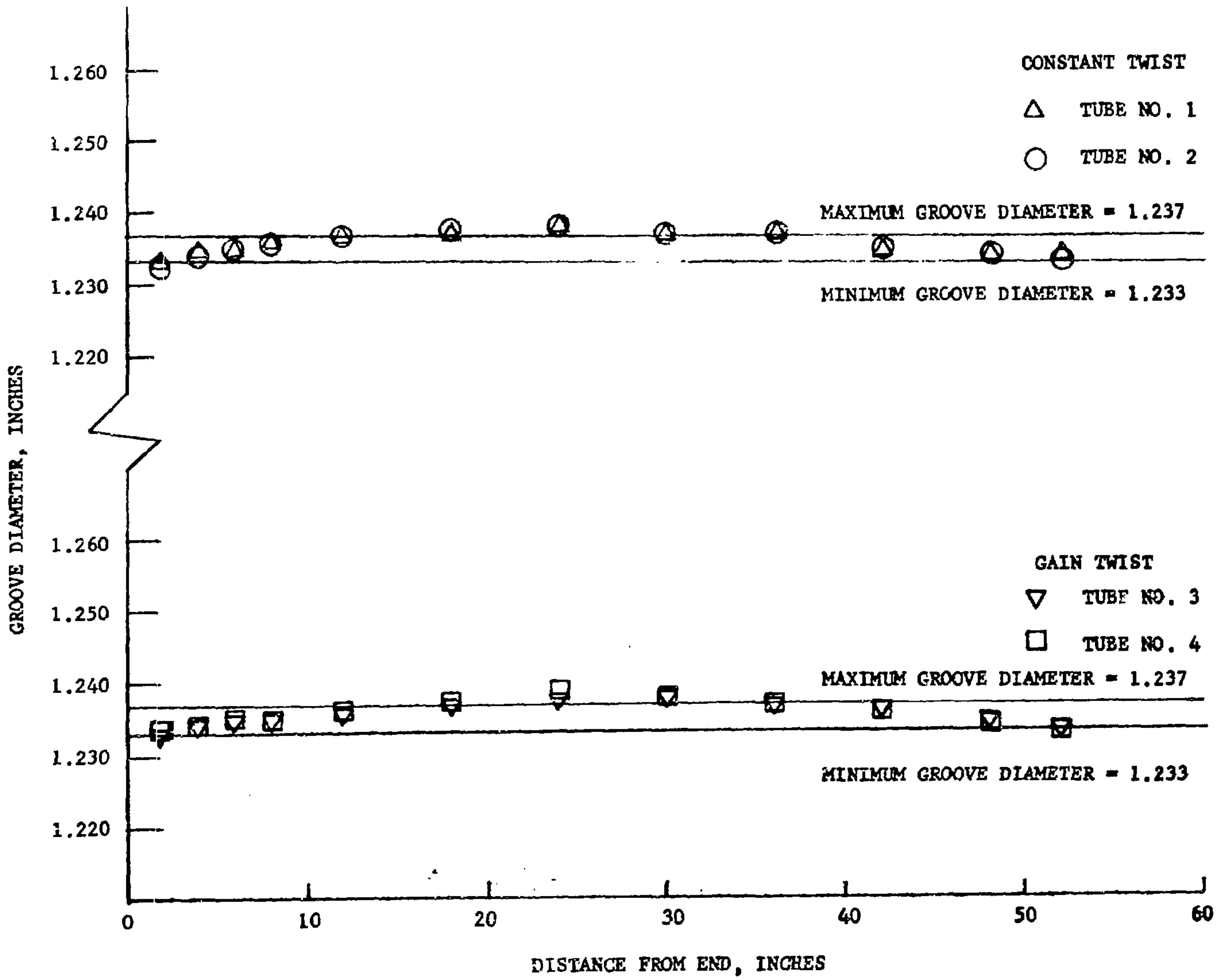


FIGURE 7. GROOVE DIAMETER AS A FUNCTION OF LENGTH FOR THE UDIMET 700 DELIVERABLE TUBES

Air gauge measurements of the groove diameter obtained from nine barrels showed that each barrel was nominally within drawing tolerance and that no single measurement was more than 0.0002-inch out of tolerance.

Silicone rubber replicas were made of the inside diameter of each deliverable barrel. Examination of each of these showed consistent rifling action along the entire length of each barrel with no major pitting or other defects.

Metallographic examination of the cross-section of the barrels (Figure 8) showed the groove edge radius, which cannot be controlled, to be somewhat larger than allowable. However, this is not considered detrimental to the performance of the barrel, since some other weapons require a generous groove edge radius to minimize the notch effect and to be more compatible with plastic rotating bands.

The land edge radius was also larger than the drawing callout. This is controllable and its lack of sharpness is due to too large a clearance between the electrode insulation and the bore. The lack of sharpness in the SAWS barrels arose from the fact that some hand sanding of the electrode was required to permit proper fit-up in the barrel. Hand sanding invariably rounds off edges which allows some electrolyte and, thus, machining action at the land-groove interface. This emphasizes the importance of minimal clearance between electrode and bore to achieve the required land edge radius.

After trimming to length and air gauging, the electrochemically rifled barrel blanks were coated with a preservative oil, packaged and shipped to the Armament Command.

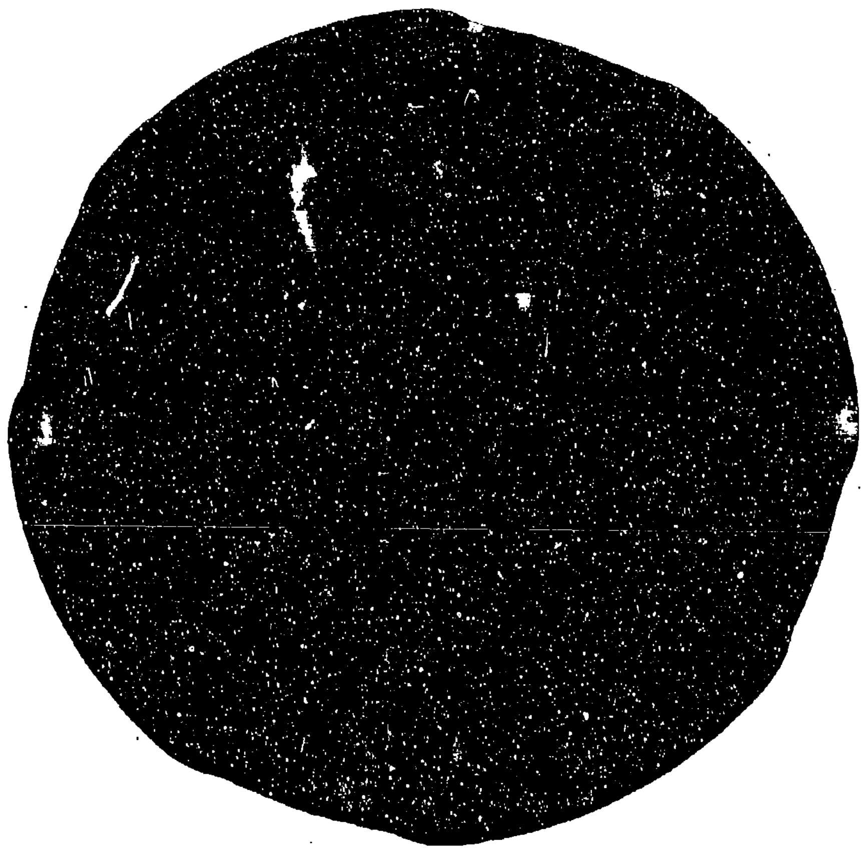
VI. ELECTROCHEMICAL MACHINING OF AUTOMATIC GUN BARRELS, CONTRACT NO. F08635-73-C-0091

A. OBJECTIVE

The objective of the program was to develop an electrochemical machining process for rifling medium caliber (20 to 400 mm) gun barrels made from high strength, erosion resistant materials. The intent was to develop a process that could ultimately be scaled up for use as a high volume production technique.

B. BARREL MATERIALS

The materials selected for this program were Pyromet X-15, Pyromet 860, CG-27, and Alloy 718, which are representative of high strength stainless steels and intermediate temperature iron-base and nickel-base superalloys with potential for use as high performance barrel materials. Also, 4340 steel was used as an inexpensive material



20X

FIGURE 8. METALLOGRAPHICALLY POLISHED CROSS-SECTION OF ELECTROCHEMICALLY RIFLED SAWS BARREL

for tooling checkout and is representative of a typical gun-barrel steel. The materials were purchased in bar stock form, heat treated, and conventionally gun barrel drilled and honed to an inside diameter of 0.984 +0.002/-0.000 inch with a surface finish of 32 rms or better, and straightened to a maximum of 0.010-inch TIR. The barrel blanks were approximately 90 inches long in order to allow trim stock for final machining into an 84-inch long barrel configuration.

C. ELECTROCHEMICAL RIFLING PROCESS

The rifling geometry was selected to be compatible with the GAU-7/A weapon and ammunition. The GAU-7/A rifling consists of 14 groove gain twist with 0.147-inch wide by 0.021-inch deep grooves. For electrode development purposes, only two grooves were utilized which required indexing six times to machine the 14 grooves.

Initial rifling tests were conducted using short length (12-inch) barrel blanks to establish optimum electrolyte and machining parameters for the different materials. Both sodium chloride (NaCl) and sodium nitrate ($NaNO_3$) electrolytes were evaluated with Pyromet 860, Alloy 718, and CG-27 barrels to determine the effect of electrolyte on surface finish and machining rate. Previous work had established that $NaNO_3$ electrolyte would be required to electrochemically rifle the Pyromet X-15 barrels.

In addition to the electrolyte studies, tests were run with short length barrels of 4340 steel to determine the effect of electrode groove depth on machining characteristics. During these tests, groove depth was varied from 0.065-inch to 0.125-inch deep. Satisfactory rifling grooves were obtained with all the different electrode grooves. As a result, an electrode groove depth of 0.125 inch was selected for subsequent tests since the greater groove area allows higher electrolyte flow rates. This results in faster removal of heat and reaction products from the barrel during the machining operation.

D. ELECTROCHEMICAL RIFLING OF FULL LENGTH BARRELS

The next step in this effort was to extrapolate the parameters and data developed on the 12-inch barrel blanks to electrochemical rifling of full length, 84-inch long blanks. The intent was to rifle at least two blanks each of Pyromet X-15 and CG-27, and one each of Pyromet 860 and Alloy 718.

Initially, in order to inexpensively confirm results obtained with short length tests, a full length barrel of 4340 steel was rifled using electrodes having various groove depths. The electrodes for these tests had straight grooves for the entire length of the barrel. The first test used an electrode having 0.075-inch deep grooves. The second test used an electrode having 0.075-inch deep grooves for 7 inches on each end and 0.085-inch grooves for the middle 76 inches of the groove. The four remaining tests used an electrode with 0.125-inch deep grooves to evaluate different operating parameters and electrolytes. The step electrode tended to reduce the excess machining in the center of the barrel. The best results on 4340 steel were obtained with a 0.125-inch deep grooved electrode and a sodium chloride electrolyte.

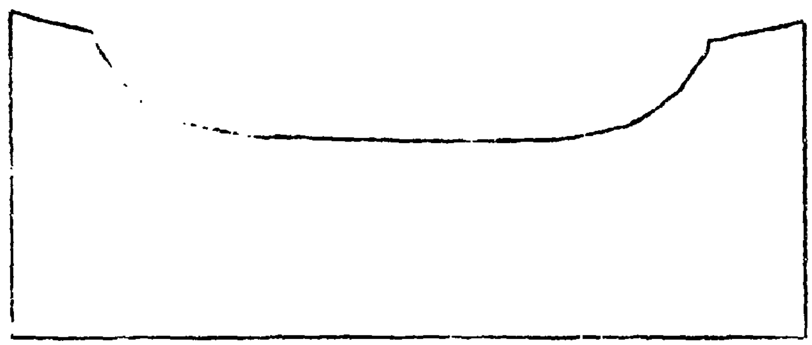
Based on tests with the full length, straight groove electrodes, barrels of 4340 steel, Pyromet X-15, Alloy 718, CG-27, and Pyromet 860 were rifled.

The groove geometries, typified by Figure 9, looked good for all materials. The radius at the base of the grooves was typically 0.060 inch compared to 0.025 inch which was specified for broach rifling. It is believed that such a configuration is entirely satisfactory and may offer advantages of minimizing the shear stresses on plastic rotating bands and of minimizing a potential stress riser (or notch effect) for barrel materials that exhibit marginal fracture toughness. The groove width was intentionally made wider than that specified in order to retain the same available groove volume for the projectile rotating bands.

Surface finish measurements taken at the base of the groove on each material were 32 rms or better for 4340 steel, Pyromet X-15, and Alloy 718 which meets drawing requirements. The CG-27 and Pyromet 860 barrels showed a surface finish of about 150 rms on the grooves. Metallographic examinations conducted on all materials did not reveal any indication of intergranular attack.

The CG-27 and Pyromet 860 showed nonuniform electrochemical machining, probably associated with microsegregation within the alloys, which accounts for their poorer surface finish. Additional optimization of electrolyte composition and electrochemical machine operating parameters would undoubtedly improve this condition and would be desirable prior to a production run.

The electrochemically rifled barrel blanks were air gauged to determine groove-to-groove and end-to-end variation in groove diameters. The desired tolerance of 1.027 ±0.001-inch groove depth was essentially achieved on one of the Pyromet X-15 barrels except for a few measurements that were 1.029. This is considered to be an exceptional achievement with the rather crude equipment and two-groove developmental electrode that was utilized. The other six barrel blanks were within groove diameter tolerance of $\geq\pm 0.004$, which is still considered highly successful, since only one or two barrels of each material were rifled with the developmental tooling. It is believed that the oversize condition in the mid-position of the barrels resulted from an increase in electrolyte temperature as it was pumped through alternately from each end,



ELECTROCHEMICALLY MACHINED GROOVE PROFILE

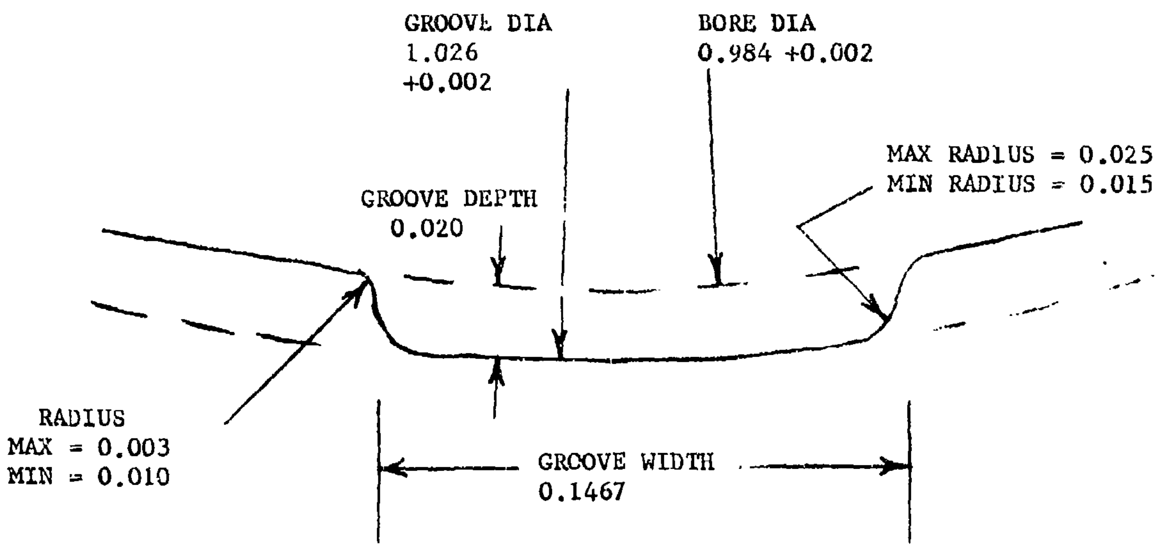


FIGURE 9. TYPICAL ELECTROCHEMICALLY RIFLED GROOVES, PYROMET X-15
EACH HAS BEEN MAGNIFIED 20X

thus increasing its machining rate. More work devoted to refinement of the electrode design and operating parameters would undoubtedly be effective in improving the attainable tolerance on a production basis. Two of the Pyromet X-15 barrel blanks were finish machined into the GAU-7/A configuration and delivered to the Air Force.

VII. PRODUCIBILITY STUDY

In order to predict production costs for electrochemical rifling of 25 mm, 7-foot long barrels, a cursory producibility study was conducted using Pyromet X-15 as a baseline material. Manhours were estimated for rifling quantities of 10, 100 or 1000 barrels per month for a period of one year. The estimates do not include cost of the electrochemical machining power supplies since this multipurpose equipment would necessarily be amortized over a longer period of time and for many other uses in addition to rifling. For comparison, estimates were prepared for conventional broaching utilizing previous data generated on 25 mm Pyromet X-15 barrels. Similarly, the broaching estimates do not include cost of the rifling machine.

The estimates, summarized in Table II show that the manhours required for electrochemical rifling are significantly lower than for broaching, particularly for large quantities. The tooling costs are also lower for electrochemical machining, since they can essentially be considered nonperishable, while the broaching cutters must be periodically sharpened and replaced. The electrochemical rifling tooling costs reflect costs of electrodes plus spares and associated plumbing and fixturing to machine one barrel at a time for all quantities considered. The higher tooling cost for 1000 barrels per month includes added development required to fabricate high integrity 14-groove electrodes. The broaching tooling costs include costs of sine bar, cutter heads, and cutters with adequate spares to machine 10 and 100 barrels per month on a single machine. Quantities of 1000 barrels per month would require 8 rifling machines or development of wafer broaching techniques to rifle all 14 grooves simultaneously. Wafer broaching would be less time consuming than broaching only two grooves at a time, but anticipated tool life would be very low and necessary development costs are difficult to predict. Without this information, the tooling costs required for 8 machines were included in the estimates for the 1000 barrels per month, recognizing that this approach is not realistic for large quantities.

Based on the above, the cost advantages of electrochemical rifling are readily apparent. These cost advantages are magnified tremendously when difficult-to-machine alloys, such as nickel- and cobalt-base superalloys are considered as barrel or liner materials. The electrochemical rifling costs for these materials would not be unlike those for gun steel or Pyromet X-15 while conventional broaching costs would be prohibitive for some of the more difficult-to-machine alloys primarily due to the extremely low expected tool life. In addition, some of these alloys probably cannot be broached due to their poor machinability.

TABLE II. COMPARATIVE COST ESTIMATES FOR PRODUCTION RIFLING

	10 Barrels/Mo		100 Barrels/Mo		1000 Barrels/Mo	
	Electro-chemical	Broaching	Electro-chemical	Broaching	Electro-chemical	Broaching
Machining Time (Man-hours)	1.0	4.0	1.0	4.0	0.4(a)	4.0
Set-up, Equipment Maintenance, and Cleaning (Man-hours)	2.2	1.0	1.5	0.1	0.6	0.1
TOTALS (Man-hours)	3.2	5.0	2.5	4.1	1.0	4.1
			Tooling Costs Per Barrel			
No. Grooves Machined Simultaneously	7	2	7	2	14	2
Tooling Costs/Barrel	$7.00	$45.00	$2.00	$19.00	$3.00	$18.00

NOTE: (a) Machining time decrease is due to simultaneous machining of 14 grooves vs 7.

DECOPPERING OF GUN TUBES BY LEAD

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ABSTRACT

During the firing of a gun, copper is deposited in the barrel from the copper coating of rotating band of the projectile. Metallic lead is commonly added to the charge to remove this copper. A mechanism for the decoppering action of metallic lead on gun tubes is proposed in which it is assumed that the lead melts, dissolves the deposited copper, and carries the copper out in the liquid. The proposed mechanism is shown to be reasonable based on the properties of lead and considering the lead-copper and lead-iron phase diagrams. Consideration of the impurities commonly present in lead indicates that iron, silver, and copper should not affect the decoppering action; antimony, arsenic, tin and zinc should decrease the effectiveness of lead; and bismuth should enhance the decoppering action of lead. Of other low melting metals, bismuth and bismuth-lead alloys should be more effective than lead, while all others should be less effective than lead in decoppering. This work was performed while the author was serving a temporary appointment with Headquarters, U.S. Army Armament Command, Rock Island, Illinois 61201.

INTRODUCTION

Most projectiles for large bore guns have rotating bands made of copper or gilding metal, a copper-zinc alloy. On firing the projectile through the tube, the rotating band is engraved by the rifling, contacting it throughout the length of the tube. During this contact, some of the copper is deposited on the rifling. These copper deposits can have a serious effect on the interior ballistics of the projectile, affecting muzzle velocity, precision and tube wear.

One method of reducing the amount of coppering of gun tubes is to add metallic lead to the propelling charge. This lead is usually added as a foil blanket around the forward end of the charge. The effect of lead foil additive in reducing coppering was discovered empirically and has been used with considerable success, though with no real understanding of why it works. A mechanism for the decoppering action of lead has been proposed[1] in which it is assumed that the lead forms a brittle alloy with the deposited copper, which is then carried out of the tube with the next round containing no decoppering additive. This mechanism is very unlikely since lead does not alloy with copper to any measureable extent unless the copper is also melted. The purpose of the present note is to propose an alternative mechanism for the decoppering action of metallic lead and to show that the proposed mechanism gives a reasonable explanation of the observed action.

Decoppering Mechanism

The mechanism proposed for the decoppering action of lead is as follows: (1) the lead melts and is deposited on the gun bore as liquid metal; (2) the liquid lead dissolves the deposited copper; (3) the liquid lead is then carried out of the tube as liquid droplets with the copper in solution. The process is visualized as one in which the copper-covered gun tube surface is wiped with liquid lead, allowing the lead to dissolve the copper and carry it out of the tube. To demonstrate that this mechanism is reasonable, several factors will be considered including the properties of lead, the lead-copper phase diagram and the lead-iron phase diagram.

Pure lead melts at $327^{\circ}C$ and has a low heat of melting of 5.9 cal/g. It has a very wide liquid range, with a boiling point of $1620^{\circ}C$. Thus, it is clear that lead is easily melted; any metallic lead present would be expected to melt under the conditions present in a gun tube and would remain molten during the time it travels the length of the tube. Because lead has a fairly high boiling point, it would remain as a liquid rather than vaporizing, particularly if it is present at the cool boundary layer along the gun tube surface.

The lead-copper phase diagram is very simple, Fig. 1[2]. Lead and copper are insoluble in each other in the solid state, with the solubility of lead in solid copper extremely small all the way to the copper melting point. No intermetallic compounds form between copper and lead. Copper has only a slight effect on the melting point of lead and has a very small solubility in liquid lead at the lead melting point. The solubility of copper in liquid lead increases with increasing temperature above the lead melting point, as given by curve AC, Fig. 1. At $955^{\circ}C$, a monotectic reaction occurs with two liquids above that temperature and one liquid below. Above the monotectic temperature, the solubility of copper in the liquid increases very rapidly with increasing temperature.

Based on the phase diagram, it is clear that, if there is a layer of copper on a surface, applications of liquid lead to the surface will allow some of the copper to dissolve in the liquid.

Removal of this liquid after dissolving copper will then remove some of the copper from the surface. By application of sufficient liquid lead, any desired amount of copper can be removed from the surface. The dissolution process is assumed to occur by diffusion and convection in the liquid. If an intermetallic compound layer formed at the solid-liquid interface, then further dissolution would be limited by the rate of diffusion of copper through this solid compound layer. This solid diffusion process would be much slower than liquid diffusion and would severely restrict the dissolution rate of copper into the liquid. In addition the formation of this compound layer would deplete the amount of liquid available for dissolving copper. Thus the absence of intermetallic compound formation between copper and lead is important in allowing rapid dissolution of copper in the lead.

It should be noted that, because lead is not soluble in solid copper at any temperature below the copper melting point, the application of liquid lead to the solid copper does not cause the metals to alloy. Any lead which remains on the copper surface will remain as a ductile lead layer and not as a brittle lead-copper alloy. The only way to form lead-copper alloys is to melt the copper in conjunction with the lead, which is unlikely to occur in the gun tube. Thus the decoppering mechanism quoted in the introduction is very unlikely to occur.

It is important to note that the proposed mechanism postulates that the lead removes the copper as a liquid, rather than as a vapor phase. If vaporization occurred, the lead would evaporate preferentially, leaving the copper behind. There is no evidence to suggest that lead and copper tend to associate in the vapor phase, as would be required for a vaporization process to be effective.

The interaction of liquid lead with the gun steel surface must also be considered. Liquid lead does not spread readily over an iron or steel surface[3]. Therefore, the liquid lead can deposit on the copper layer, dissolve it, and then be carried away very readily by the propellant gas. The lead-iron phase diagram[4] is similar to that of lead-copper except that the solubility of iron in the liquid is much lower than that of copper. Thus, the lead will dissolve copper very readily, but will dissolve very little iron.

We must now calculate how much copper can be removed by the amount of lead used in a typical artillery round. For purposes of calculation consider the 155mm M549 round as fired in the XM199 gun tube[5]. The XM203E1 propelling charge used with this round contains about 160 grams of lead foil (1.3% of the total charge weight of 11.8kg). As noted above, liquid lead at 900^{o}C will form a solution containing 6.7 weight percent copper. Thus, the weight of copper dissolved in 160 grams of lead is:

$$W_{dis} = \frac{0.067 \times 160}{0.933} = 11.5 \text{ grams.}$$

The rotating band on the M549 round is 5 cm long. The 48 rifling lands of the gun tube engrave grooves in the rotating band which are about 0.8 cm wide by 0.1 cm deep, removing a total metal volume of:

$$V_{rem} = 0.1 \times 0.8 \times 5 \times 48 = 19 \text{ cm}^3$$

from the rotating band. The total weight of copper removed from the rotating band is the (using the density of copper, 8.92 g/cm^3):

$$W_{rem} = 19 \times 8.92 = 170 \text{ g.}$$

A small fraction of this removed copper will be rubbed onto the rifling and will remain in the tube as a copper deposit. If 5 percent, or 8.5 g, of this copper remains as a deposit, then the lead in the charge is more than adequate to remove it. If as much as 20 percent, or 34 g, of this copper remains as a deposit, then the lead in the charge is probably not sufficient to remove all of it. The major portion of the copper will be removed as bulk pieces during the initial engraving process and only a small amount will rub onto the rifling bands. Thus, one could estimate that the amount of copper deposited would be nearer 5 percent than 20 percent of the total copper removed from the rotating band. Based on this estimate, it is concluded that the lead added to the charge is just about adequate to remove the deposited copper. If the amount of lead were reduced by a factor of 3, then it would almost certainly be insufficient to do the job; if the amount of lead were increased by a factor of 3, then it would certainly remove all deposited copper.

The conclusion reached from the above considerations is that the proposed mechanism of decoppering by lead is reasonable and that the amount of lead used in present charges is near the optimum amount to insure adequate copper removal.

Discussion

Accepting the conclusion of the previous section, that lead removes copper by dissolving it in the liquid, there are several points that should be discussed in order to obtain the best results in selecting decoppering additives. The items to be discussed are

(1) the selection of the amount of lead needed to be effective, (2) the effects of impurities in the lead on its decoppering effectiveness, (3) alternative low melting point metals for use as decoppering additives, and (4) the use of lead (or other) compounds for decoppering.

1. In order to select the amount of lead to include in the charge for decoppering, one must first determine how much copper must be removed. If there is full engraving of the round in the gun bore, then it is possible to determine how much copper is engraved from the band by knowing the extent of interference between the band and the rifling. It can then be estimated that from 5 to 20 percent of this copper remains in the gun tube and must be removed by lead. The amount of lead required is approximately 15 times the amount of copper which remains in the tube. For the example of the M549 projectile used above, the minimum lead addition needed was found to be about 170 g per round. If the rotating band were pre-engraved, then less copper would be stripped from the band but about the same amount would be deposited in the bore.

The position of the lead foil in the propelling charge could affect the amount of copper that would be removed per unit mass of lead. Lead foil or shot homogeneously mixed with the propellant would probably not be as effective as foil placed around the forward periphery of the charge.

2. Impurities in lead can affect its behavior as a decoppering agent. The common impurities are likely to be tin, antimony, arsenic, bismuth, iron, zinc, copper, and silver[6]. Iron and silver are just slightly soluble in lead and are very unlikely to affect the decoppering behavior. Copper would normally be present in an amount of 0.1 percent or less and this would not be deleterious. If, however, the lead had copper mixed in to an extent of several percent, then this copper would tend to saturate the liquid lead so it would not remove as much copper from the tube as expected.

Tin, antimony, arsenic, and zinc can go into solution in the copper or react with it to form intermetallic compounds. These processes do not remove copper from the tube, so that the portion of the lead taken up by the impurity is not effective. Thus, relatively more lead must be added to make up for the presence of the impurity. These elements can also react with the steel tube to form intermetallic compounds. The effect of this would be to form additional bore deposits, rather than remove the copper as desired.

Bismuth, as an impurity, would probably promote the decoppering action of lead as discussed in the next section. Bismuth has a higher solubility for copper than does lead and has a lower melting point than lead; both of these properties would tend to make bismuth more effective than lead in removing copper.

In conclusion of this section, tin, antimony, arsenic, and zinc as impurities in lead would decrease its decoppering action. Bismuth would help the decoppering action, while iron, silver, and copper would not have much effect.

3. It is interesting to consider other low melting metals which might have decoppering action similar to that of lead. In Table I are the elements which are candidate materials, listed according to their column in the periodic table. These elements all have relatively low melting points, so they would probably melt under the conditions in a gun tube.

Consider first the alkali metals, lithium, sodium, potassium, rubidium, and cesium. Lithium has a low melting point and a high boiling point. Also, there are no Li-Cu or Li-Fe intermetallic compounds, and copper is appreciably soluble in liquid lithium. Therefore, lithium would have good properties as a decoppering agent if it could be used in a charge. All the alkali metals, however, including lithium, react very strongly with air, water-containing materials and many other materials, so they would not be stable as part of a propellant charge. Therefore, the alkali metals must be eliminated as possible decoppering agents.

Zinc, cadmium, and mercury have low melting points, but they also have low boiling points, so they would tend to evaporate rather than remain as liquids. Thus they would not remove copper. In addition, zinc and cadmium react with copper to form solid solutions and intermetallic compounds, tending to remain in the tube rather than removing copper. Zinc also forms compounds with iron which would cause it to stay in the tube. Cadmium and mercury are undesirable because they have a tendency to cause embrittlement of steel. Therefore, zinc, cadmium, and mercury would not be useful as decoppering agents.

Selenium and tellurium have relatively low boiling points. In addition, they form quite stable compounds with copper and iron. Thus, selenium and tellurium would not be good decoppering agents.

Gallium, indium, and tin all have low melting points and high boiling points. All three of these elements, however, form solid alloys and intermetallic compounds with copper, so they would not remove the copper in the liquid unless a very large amount of the element were added to the charge. The low melting point of gallium would make it difficult to add to a charge since the metal would melt at temperatures only slightly above room temperature. In addition, gallium is a relatively rare material and would be very expensive if it were possible to obtain enough to add to all charges. Indium is also a relatively rare, expensive material. Thallium has properties similar to those of lead in its physical characteristics. It is, however, very toxic and would be difficult to handle with an adequate degree of safety. Based on these considerations, then, the metals gallium, indium, thallium, and tin must be eliminated as decoppering agents.

The only metal that is left in addition to lead is bismuth. This metal has melting and boiling points somewhat lower than those of lead. The copper-bismuth phase disgram is shown in Fig. 2[7]. There are no intermetallic compounds between copper and bismuth. There is extremely small solid solubility of copper in bismuth and of bismuth in copper. Copper is more soluble in liquid bismuth than in liquid lead, having a solubility[8] of about 45 weight percent copper in bismuth at $900^{\circ}C$ as given by point A, Fig. 2. Bismuth wets copper much better than does lead, having a tendency to penetrate copper grain boundaries; thus, copper would probably dissolve more rapidly in bismuth than in lead. Bismuth also does not interact strongly with steel. Based on these properties of bismuth, it appears that bismuth should be a more effective decoppering meterial than lead. The amount of bismuth required for effective action should be less than the amount of lead currently being used.

An alloy of 55 percent bismuth - 45 percent lead has a melting point of $124^{\circ}C$, which is considerably lower than that of either of the pure metals. Thus, an alloy of this composition or alloys for a considerable range around the 55Bi - 45Pb composition could be very effective decoppering agents.

In conclusion of this section, it appears that bismuth or bismuth-lead alloys would be good alternatives to lead for tube decoppering. All of the other available metals have shortcomings which eliminate them as candidate materials. The principal deterrent to the use of these metals is the fact that they form solid solution alloys or intermetallic compounds with copper or iron or both. Bismuth appears to be promising enough as an alternate material that it would be well worthwhile to prepare and fire rounds containing bismuth rather than lead.

TABLE I

Elements with Low Melting Points

Element	Melting Point (^{o}C)	Boiling Point (^{o}C)
Li	181	1330
Na	98	892
K	64	760
Rb	39	688
Cs	29	690
Zn	420	906
Cd	321	765
Hg	-39	357
Ga	30	2237
In	156	2000
Tl	303	1457
Sn	232	2270
Pb	327	1725
Bi	271	1560
Se	217	685
Te	450	990

4. Because metallic lead has some deleterious effects on steel, consideration can be given to the use of lead compounds or other compounds in the charge as decoppering agents. There are three possible mechanisms by which a compound could remove copper from the gun tube.

a. The compound could melt and dissolve the copper, as proposed in the mechanism for the action of lead in an earlier portion of this paper.

b. The compound could react with the copper deposit to form a copper compound which is easily removable from the tube.

c. A lead (or bismuth) compound could be reduced to metal by the propellant gases, and the metal could remove the copper by the mechanism proposed earlier in the paper.

With regard to mechanism (a) there are very few compounds which melt at low temperatures and at the same time will dissolve metallic copper. In particular, compounds such as PbO, PbO_2, or $PbCo_3$ will not melt at low enough temperatures to dissolve copper in a gun tube.

Mechanism (b) above would be unlikely to occur because copper is less active than steel. Any compound added to the charge to react with copper would react even more strongly with steel. Thus, at any spot where the copper was removed by the compound, the compound would then continue to attack the steel, causing damage to the gun tube rather than removing the copper.

Mechanism (c) could be limited by the kinetics of reaction of the propellant gas with the lead or bismuth compound. For this mechanism to work, it would be necessary for the compound to be very rapidly reduced by the gas and have the metal collect on the tube walls to dissolve the copper. The liquid metal would then carry the copper out of the tube. This mechanism would require a special combination of circumstances. The compound would be reduced most readily in the hotter portion of the flame, while the reduced metal is utilized on the cooler tube walls. This combination would not be easily achieved in a real propellant system. If the compound were placed toward the center of the charge, it would be readily reduced but could not find its way to the walls before the charge was carried out of the tube. If the compound were placed along the outside of the charge, it might not be reduced in sufficient time to deposit the lead on the walls. This mechanism might possibly work with compounds such as PbO or $PbCO_3$, but conditions would have to be selected very carefully to get the optimum results.

It is interesting to note that the effect of tin dioxide on tube decoppering has been investigated previously[8]. The conclusion of this study was that tin dioxide did not assist in decoppering. Very little tin was found in any of the barrel residues examined, but large amounts of coppering were found in every case, regardless of tin dioxide content. This result is in agreement with our conclusion that tin would not be an effective additive for decoppering.

In conclusion of this section it appears that compounds added to the propellant charge are not likely to be effective in removing copper from the tube in the same manner that metallic lead is effective.

There are, of course, so many organic and inorganic compounds available that it is not possible to state arbitrarily that none will work. Based on the discussion in this section, however, one can state with considerable confidence that almost no compounds are likely to be effective by these three mechanisms. Thus, an effective compound would be likely to act by some other mechanism.

Conclusions

1. A mechanism has been proposed for the decoppering action of metallic lead in gun charges. The mechanism assumes the lead melts, dissolves the deposited copper, and carries the copper out in the liquid.

2. The proposed mechanism is shown to be reasonable based on the known properties of lead and considering the copper-lead and iron-lead phase diagrams. Based on this mechanism, the amount of lead currently used in propellant charges is shown to be just about adequate. A method for calculating the estimated amount of lead required is demonstrated.

3. The impurities commonly present in lead were considered and it was indicated that (a) iron, silver, and copper will not affect the decoppering; (b) antimony, arsenic, tin, and zinc will decrease the decoppering effectiveness; (c) bismuth will increase the decoppering action.

4. A large number of low melting metals were considered as alternate materials to lead as decoppering agents. It was concluded that bismuth and bismuth-lead alloys might be even more effective in decoppering than is lead. All other low melting metals would probably not work as well as lead in decoppering.

5. It was indicated that additions of inorganic compounds to the charge for decoppering are unlikely to be effective.

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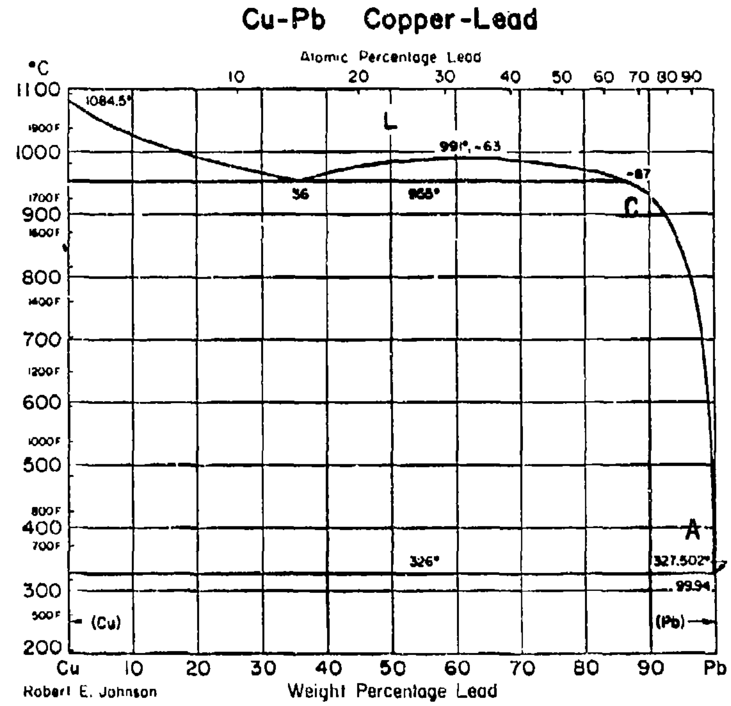

Figure 1. Copper-Lead Phase Diagram[2]. Curve AC shows the solubility of solid copper in liquid lead.

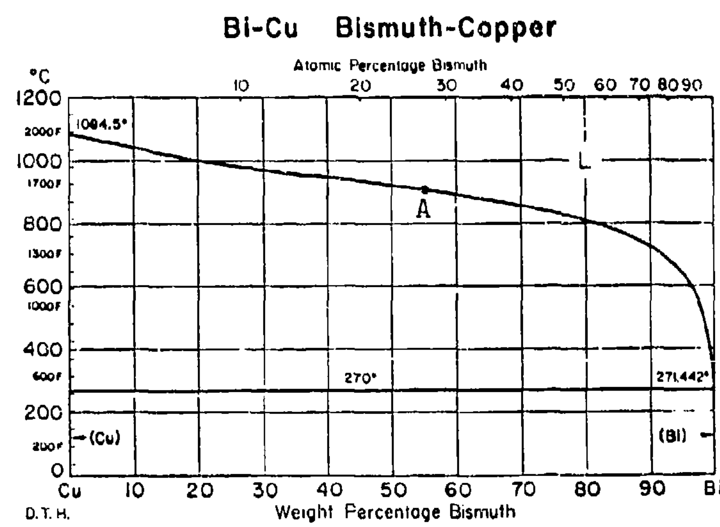

Figure 2. Copper-Bismuth Phase Diagram[7]. Point A shows the solubility of copper in liquid bismuth at 900°C.

WEAR RESISTANCE OF ELECTROLESS DEPOSITS

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Introduction

Electrodeposited metals have been applied to the interior of gun tubes in attempts to reduce wear and gas erosion during firing. The greatest attention has been directed to the use of chromium electrodeposits which provide the greatest hardness and wear resistance of the commonly plated metals. However, there are operational difficulties in application of chromium to gun tubes which become accentuated with decrease in tube diameter. For example, a centrally located anode is required which must be capable of passing relatively high currents. The plating configuration is such that the balance of bath constituents is broken, i.e., excessive trivalent chromium is produced, and corrective measures are required periodically. Also, chromium deposits have the unfortunate tendency to crack spontaneously as a result of internal stresses and could result in compromise of the protective value. In spite of these difficulties, the Army has apparently been successful in utilizing these deposits for acquiring improved wear and erosion resistance.

Electroless (autocatalytic) deposition has found widespread application where it is advantageous to obtain certain superior characteristics over electrodeposition, such as:

a. high degree of deposit uniformity
b. no need for electrical connections or electrodes
c. unique mechanical and chemical properties
d. less porous deposits
e. direct deposition on nonconductors

Electroless deposits would appear suitable for application to gun tubes as it would eliminate set-up difficulties with anodes and should provide deposits with exceptional freedom from porosity. Electroless nickel deposits can provide a high degree of hardness and wear resistance as a result of inclusion in the deposits of elements such as phosphorus (1) or boron (2) originating from the chemical reducing agents used.

The investigation described herein compares the performance of electroless nickel or chromium deposits in a laboratory wear resistance test using the Falex apparatus. High unit pressures are applied to moving parts and high temperatures are generated as a result of frictional forces. A second objective was to ascertain whether wear resistance of the electroless deposits were essentially a function of hardness or whether dependent upon specific effects related to the presence of phosphorus or boron in the deposits.

Experimental Procedure

Four electroless nickel plating baths, selected for this study were prepared. The bath compositions are shown in Table I. The baths were heated and maintained within ± 2°C of the desired temperature using a constant temperature water bath.

Falex specimens (journals AISI 3135 steel, Rockwell B87 to 91, surface finish 0.25 µm RMS; V-blocks, AISI C-1137 steel, Rockwell C20 to 24, surface finish 0.25 µm RMS) were prepared for electroless nickel plating as follows:

Alkaline Clean - Ten minutes immersion in 60 g/l sodium orthosilicate - 3 g/l Nacconol 40F* at 85°C; cold water rinse.

Acid Activation - one minute immersion in 50% (vol) hydrochloric acid (38%) at 25°C; cold water rinse.

The specimens (journals and blocks) were then immersed into an electroless nickel bath (see Table I) for sufficient time to produce 25 to 28 µm deposit thickness. The bath volume to surface area plated was about 30 ml/cm^2.

The following heat treatments were applied to Falex specimens plated in each of the electroless baths:

(a) Three hours at 200°C
(b) Sixteen hours at 280°C
(c) One hour at 400°C

Knoop hardness measurements were made on the deposits with 50 gram load applied.

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*Stepan Chemical Co., Northfield, Ill.

Wear resistance of the deposits was determined using the Falex Lubricant Tester (3) (See Figure 1). The journal is rotated at 290 RPM between the two V-blocks positioned in the jaws of a load applying mechanism. The load applied to the blocks was increased from zero to 22.7 kg for a 30 sec period. At the end of that time, and each 30 sec period thereafter the load was increased by 22.7 kg until a total of 182 kg was applied. The load was then maintained at the 182 kg level until catastrophic failure occurred (i.e. seizure and fracture of the journal). The plated journals were weighed before and after the Falex test to provide a quantitative measure of the degree of wear involved. No lubricants were used in any of the tests.

Results and Discussion

Electroless nickel deposits ranged from bright (baths 1 & 2) to matte (bath 4). Deposits from bath 3 were semi-bright but had a finely cracked structure visible at 7X magnification; adhesion was considered inadequate as particles of the deposit could be easily dislodged by probing with a blade.

Heating of the plated specimens for three hours at 200°C is intended for release of any occluded hydrogen and for relieving internal stresses within the deposit. Heat treatments at 200°C had no visible effects on the appearance of the deposits. Interference colors from oxide films were evident on deposits heat treated at 400°C. The nickel-phosphorus deposits acquired a yellow cast along with blue iridescence. The nickel-boron deposits were pale yellow. Only the nickel-boron deposit from bath #3 changed in surface appearance by heating at 280°C; a light yellow film was formed.

Hardness - The hardnesses of electroless nickel deposits are shown in Table II and Figure 2. The hardness of deposits from a given bath increased with increase of heat-treatment temperature. The nickel-boron deposits were significantly harder than the nickel-phosphorus deposits exposed to the same heat-treatment temperature. It is noteworthy that the nickel-boron deposit from the acid DMAB bath (see Figure 2), heat treated at 400C, acquired the greatest hardness, i.e., > 1300 Knoop; of those heated at only 200C, the nickel-boron deposit from the borohydride bath was considerably harder than any of the others.

Wear Resistance - There was definite improvement in Falex wear resistance, with increase in heat-treatment temperature, for all of the deposits except those from bath #3. See Table II. All deposits from bath #3 provided poor wear resistance apparently owing to poor deposit adhesion as noted above. Adhesion improved somewhat with heating at 280 or 400C but was still decidedly inferior. Since the hardest deposits obtained in these studies were those from the acid DMAB bath, it is evident that hardness alone is not indicative of wear resistance and deposit adhesion is an important factor. Ductility of deposits may also influence wear resistance (1). Modification of bath #3 composition or operating conditions may be necessary in order to improve deposit adhesion and prevent the formation of cracked deposits.

With the exception of deposits from bath #3, the Falex wear resistance of deposits appeared to closely parallel the hardness value. It did not appear from these studies that the presence of boron or phosphorus had any significant effect on wear resistance independent of hardness. However, it is recognized that specific effects of phosphorus or boron in the deposits might well exert themselves if other types of wear resistance tests were conducted.

In general, deposits with hardness values below 1000 Knoop were not capable of reaching the highest Falex load applied (182 kg) before seizure and fracture of the journal occurred.

Electrodeposited chromium* (as-plated) on Falex specimens was found to have a hardness of 1300 Knoop which is greater than that of most of the nickel deposits tested. However, the chromium plated specimens were incapable of preventing seizure and journal-fracture before maximum load (182 kg) was applied. Therefore, the electroless nickel-phosphorus deposits heat treated at 400C and the nickel-boron deposits (from bath #4, Table II), heat-treated at 200, 280 or 400C were superior in wear resistance to electrodeposited chromium. These results may be significant since the superior hardness of Cr deposits was not indicative of Falex wear resistance and thus the electroless deposits appear to be potentially useful candidates as replacement for chromium plating of gun tubes.

It was reported earlier (4) that whereas Falex specimens plated with electroless nickel-phosphorus are subject to almost immediate failure during Falex testing, a thin deposit of silver on the nickel deposit provided much improved wear resistance and Falex performance was superior to that of any of the deposits described above. The silver apparently functioned as a dry-film lubricant.

*SRHS, M&T Chemicals, Inc., Rahway, N.J. 07065

Conclusions

The hardness of electroless nickel-boron deposits is greater than that of nickel-phosphorus deposits subjected to the same heat treatments (3 hours at 200C, 16 hours at 280C or 1 hour at 400C). Hardness of a given deposit increases with increasing heat-treatment temperature.

The Falex wear resistance of deposits from hypophosphite or borohydride baths improves with heat-treatment temperature and is largely related to hardness rather than to whether deposits contain phosphorus or boron. Deposits from the DMAB bath were cracked and poorly adherent and performed poorly in the wear resistance tests.

Electroless nickel deposits from hypophosphite or borohydride-based baths, heat treated to Knoop hardness values of about 1000 kg/mm^2 or higher, outperformed chromium electrodeposits of the same thickness at Falex wear test conditions.

Recommendations

Electroless nickel deposits should be evaluated for effectiveness in providing wear and erosion resistance to gun-tube interiors under firing-test conditions.

Additional Studies

Falex wear tests are being continued on electroless nickel-phosphorus alloys containing cobalt or tungsten in order to determine whether the additional metal component can provide wear resistance independent of deposit hardness. In addition, several double-layer electroless deposits are being applied to Falex specimens to determine whether synergistic wear resistance may be attained thereby.

Table I. Bath Compositions for the Electroless Deposition of Nickel

Bath Constituents, g/l	1	2	3	4
Nickel sulfate ($6H_2O$)	25	32	25	-
Nickel chloride ($6H_2O$)	-	-	-	30
Sodium citrate ($2H_2O$)	-	84	-	-
Sodium acetate ($3H_2O$)	15	-	15	-
Sodium hydroxide	-	-	-	40
Ammonium chloride	-	50	-	-
Ethylenediamine, 98%	-	-	-	52
Sodium borohydride, 98%	-	-	-	1
Dimethylamine borane (DMAB)	-	-	4	-
Sodium hypophosphite (H_2O)	22.5	15	-	-
Lead acetate ($3H_2O$)	0.001	-	0.002	-
Thallous nitrate	-	-	-	0.02
pH	4.8	9.5*	6	14
Bath temperature, °C	90 ± 2	90 ± 2	55 ± 2	96 ± 2
Deposition rate, μm	15	7	10	10
Reference	(5)	(6)	-	(7)

*Adjusted with NH_4OH

Table II. Heat Treatments, Hardness and Wear Resistance of Plated Falex Specimens

Bath #	Bath Type	Falex Specimen Group	Heat Treatment	Knoop Hardness, kg/mm^2	Journal Wt Loss, mg	Max Load Attained Prior to Failure, kg	Time to Failure at 182 kg, minutes
1	(Ni-P) Acid Hypophosphite	A	3 hrs 200°C	650	-	< 22.7	-
		B	16 hrs 280°C	1010	51	159 → 182	-
		C	1 hr 400°C	1110	64	182	6
2	(Ni-P) Alkaline Hypophosphite	A	3 hrs 200°C	710	555	< 22.7	-
		B	16 hrs 280°C	960	63	159 → 182	-
		C	1 hr 400°C	1010	16	182	3
3	(Ni-B) Acid DMAB	A	3 hrs 200°C	760	234	< 22.7	-
		B	16 hrs 280°C	1180	427	< 22.7	-
		C	1 hr 400°C	1310	419	< 22.7	-
4	(Ni-B) Alkaline Borohydride	A	3 hrs 200°C	980	43	182	0.2
		B	16 hrs 280°C	1160	104	182	6
		C	1 hr 400°C	1220	68	182	7

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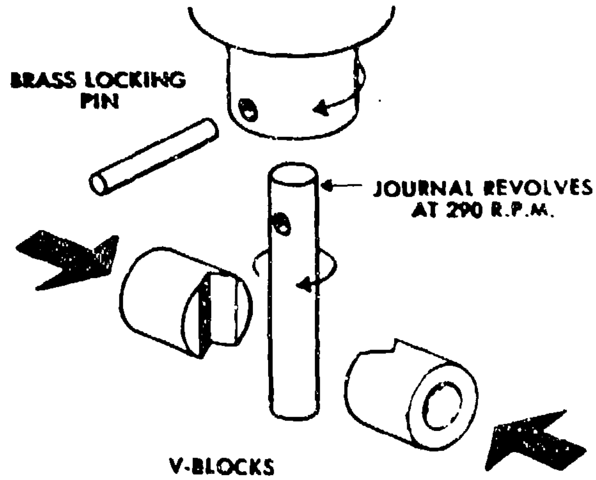

Fig. 1. Exploded view of V-blocks and journal arrangement, Falex lubricant tester.

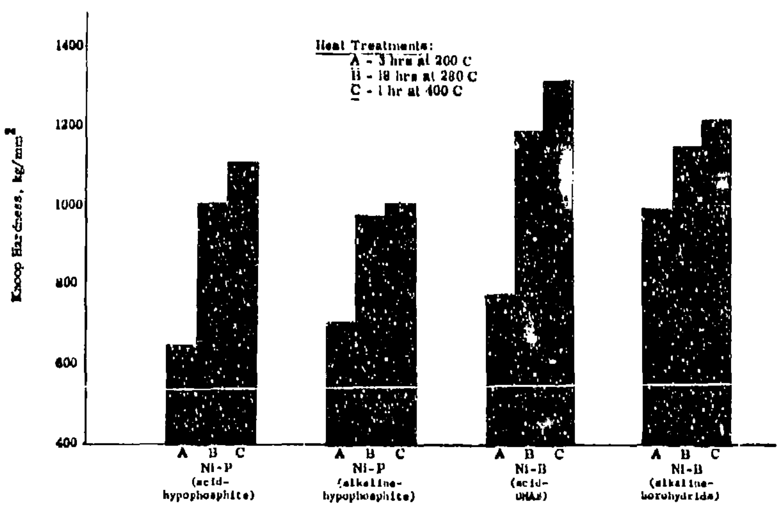

Fig. 2. Effect of heat treatment on hardness of electroless nickel deposits.

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