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FEBRUARY 1963

HEAT TRANSFER, BARREL TEMPERATURES AND THERMAL STRAINS IN GUNS

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RDT & E Project No. 1M010501A004

BALLISTIC RESEARCH LABORATORIES

ABERDEEN PROVING GROUND, MARYLAND

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A B E R D E E N   P R O V I N G   G R O U N D,   M A R Y L A N D

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ELBannister/RNJones/DWBagwell/jdk
Aberdeen Proving Ground, Md.
February 1963

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ABSTRACT

Data on temperature distribution and heat input in a 37mm gun during firing are presented. These results are discussed and compared with theoretical values obtained by using the Nordheim, Soodak and Nordheim method of calculation.

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I. INTRODUCTION

Many text books on Interior Ballistics contain theories which permit the calculation of heat transfer rates, barrel temperatures and thermal strains in gun barrels. These theories are developments from the extant theories of heat transfer in tubes or to flat plates and are valid strictly for fully developed, steady flow. The situation in a gun barrel during operation of the gun is by no means steady. The boundary layer is very thin and the surface is not smooth. The validity of the theories and the accuracy of the derived results are open to some question. There does not exist much published experimental data by which one can evaluate the theories in question or arrive at empirical correlations among the parameters of importance to the phenomena in question.

The Ballistic Research Laboratories have for some years been interested in the problem of the measurement and control of heat transfer in guns and have made many firings on a laboratory scale in rather well instrumented 37mm guns under various conditions of propellant type, projectile mass, muzzle velocity, etc. Measurements were made, not only of the usual pressure vs. time and muzzle velocity, but also of external barrel surface temperatures, internal bore surface temperatures and external barrel thermal strains, and in some cases erosion rates. The temperature and strain measurements make it possible to calculate the overall heat input and thermal dilatation coefficient of the barrel. Comparison can also be made with the values calculated by the extant theories and estimates made as to the accuracy and utility of the latter.

II. EXPERIMENTAL PROCEDURES

The temperature of the external surface of the barrel was measured with gages known as RdF Stikons. These gages (manufactured by Arthur C. Ruge Associates, Inc., Hudson, New Hampshire) essentially are resistance thermometers and are very convenient for the purpose, and yield consistent results. They are similar in appearance to cemented strain gages and are applied in a similar manner. The model used is designated as type PN-1 and has a resistance of 50 ohms at 70°F. As used in heat experiments, the gage forms one arm of a Wheatstone bridge circuit. When the gage is heated the resulting unbalance of the bridge is indicated by the current in a microammeter and is a measure of the temperature rise of the gage.

The following procedure was used to calibrate the PN-1 gages. The Wheatstone bridge circuits were all constructed with 0.1% precision resistors of the same value. A precision decade resistor is substituted for the PN-1 gage, and with constant voltage in the bridge, the resistance is changed by one ohm steps, and a plot of resistance vs. current on the microammeter is made. Each bridge was calibrated in this manner, and the slopes were compared. All of the bridges used were exactly alike. The PN-1 gage manufacturer supplies a calibration chart showing resistance change as a function of temperature change. Now if one of these gages is waterproofed and placed in a water bath the temperature of the gage can be determined, and when it is connected into one of the bridge circuits, the change in resistance as a function of temperature can be measured as a change in the current in the microammeter. From this, a plot of temperature vs. current can be made with the slope being the gage calibration factor to be used with that particular bridge. Since all of the bridges are alike, only one gage need be completely calibrated.

To protect the gages from the effects of changes in temperature of the environment they were covered with a layer of plastic foam fastened down with several layers of electrician's tape.

The thermal strain measurements were made with Baldwin Southwark SR-4 cemented on strain gages, type C10, 1000-ohm resistance. They were first applied in the usual way using Duco cement. It was found, however, that their indications were quite variable from round to round. The thermal strains are very small and are preceded by relatively very large strains due to the gas pressure and the pressure due to the rotating band. It is probable that this large pressure causes some slight slippage of the gage which affects the subsequent measurements of the small thermal strain. It appeared to be necessary to cement the gages more securely to the barrel. This was done in the following way. A thin layer of epoxy resin (Araldite GN 502 manufactured by Ciba Co.,Inc., Kimberton, Pa.) was applied and the gage pressed firmly into the cement until any excess was removed along with any air bubbles. The gage was then covered with a piece of waxed paper upon which was placed a piece of sponge rubber. This assembly was bound tightly to the gun barrel with several turns of No. 14 wire and was allowed to cure for several days under an infrared lamp. After curing was completed, the gage was coated with several layers of insulating varnish. Finally, as a protection, several layers of electrician's tape were wrapped over the gages. Gages applied in this manner gave consistent readings for several hundred rounds. Two active gages were used in a Wheatstone bridge circuit so as to enhance the signal.

A typical record is shown in Figure 1. The signal from the gage was displayed on a cathode ray oscilloscope and photographed with a Polaroid Land Camera. The sweep time is 1.3 sec/cm measured on the record. The record shows that the full strain is reached in about 0.4 seconds and is effectively constant as the heat diffuses through the barrel. This is to be expected since the strain depends on the total heat input and is independent of its distribution. The "hash" on the early part of the record is due to the shock of firing and the hash near the center of the record occurs when the gun comes back into battery. The dots at either end of the record are calibration steps taken at the beginning and end of the cycle. Each step is due to a voltage change of 0.4 millivolts.

Usually the strains were measured circumferentially. In some cases longitudinal strains were measured simultaneously but were found to be equal to the circumferential strains.

The internal or bore surface temperature measurements were made with surface thermocouples of the Hackemann type. These thermocouples were first designed by P. Hackemann in Germany during World War II and have been extensively used and are described in several references.[1-2] Several models were used in the work reported here. The earlier ones were supplied by the Midwest Research Institute, Kansas City, Missouri. Subsequently others were fabricated at the BRL using essentially the same techniques as were used by Midwest. The main features of this model are shown in Figure 2. The thermojunction is the interface between the nickel plate and the steel base. This is essentially a Ni-Fe couple. The temperature is measured a few microns (the thickness of the plate) below the actual surface. The average thickness of the plate on the couples used was two microns. Since the junction has essentially zero heat capacity, the response of the couple is very rapid. A typical record is shown in Figure 3.

Figure 1. Typical Strain Record. The Oscilloscope Sweep is Toward the Viewer's Right

NICKLE - IRON THERMOCOUPLE

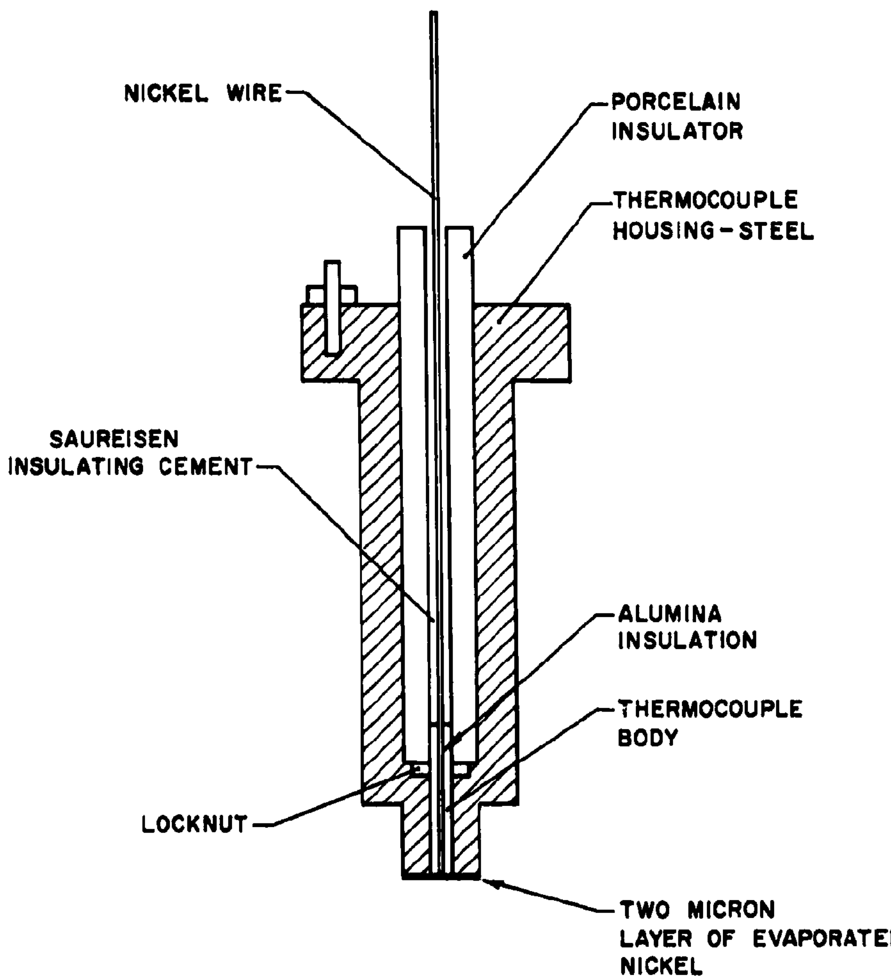

FIG. 2

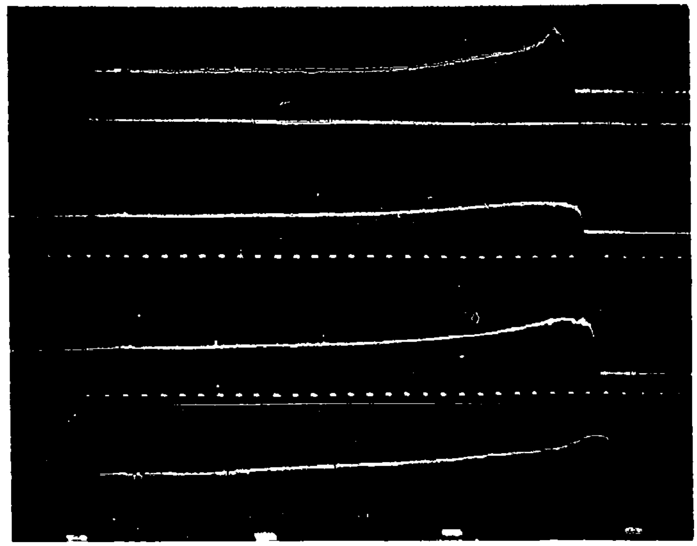

Figure [illegible]. [illegible] thermocouple record. Sweep is to viewer's left, with the close timing dots [illegible] millisecond separation. The traces represent thermocouples 1 to 4 from bottom to top on the figure with 1 being closest to the gun breech.

The four traces are from four thermocouples at different positions along the barrel. Timing marks also appear on the record so that the variation of temperature with time can be followed. The record shows that in this case the maximum temperature is reached in about 100 microseconds.

Unfortunately, these thermocouples are very fragile and must be renewed frequently if conditions are severe as they were in the work described here. Considerable effort was expended in trying to improve their ruggedness, particularly in the matter of adhesion of the plate. Although improvement was attained they remained too fragile for extensive use under the severe conditions to which they were subjected. The thermocouples were mounted in plugs which could be screwed into the barrel in the desired locations until they made contact with a specially designed instrument inserted down the bore. The contact was indicated by the completion of an electrical circuit. The thermocouple was then retracted three turns of the plug (about 3.8mm). It was observed that the lack of close match between the surface of the thermocouple and the bore did not alter the measured temperature significantly. Such behavior has also been reported in the literature by other users of similar thermocouples.

The model shown in Figure 2 was the most rugged and effective of those tested. The nickel plate was deposited with the thermocouple element mounted in the plug so that the plate covers not only the thermo element but also the full surface of the plug. In fabrication, the entire surface to be plated was ground and polished on a metallurgical polishing wheel, then cleaned with alcohol. The unit was then mounted in the evaporator and heated by a specially designed furnace to a temperature of 700 to 750°F. The surface was further cleaned by an electric discharge and then the plate deposited while the thermocouple element was still hot. The apparatus was so designed that six units could be fabricated simultaneously. The general procedures for determining plate thickness were those given in Reference 5. In some cases the thickness of the plate was checked by microscopic examination and found to agree with the predicted thickness.

Part of the improvement of the ruggedness of these thermo elements may be due to the fact that the plate extends over the end surface of the housing as well as the end of the thermo element itself. The plate is continuous and the larger area and circumference may make it more difficult for the gas to remove the plate.

Because of the difficulty in keeping the plate in place when these thermocouples are used in guns, it has been the practice in several laboratories to establish and maintain the thermocouple operation by simply abrading the surface by filing or rubbing on fine abrasive paper instead of plating. Enough metal is swaged across the gap between the nickel wire and the steel tube to make the junction. This technique was used in this work also, but, although such junctions are very simple to produce (and will successfully record temperature and are often more durable than plated junctions), the results of a large number of firings showed that the recorded maximum temperatures are much more variable than those produced by the plated thermocouples. Their use was, therefore, not continued.

In all, four different 37mm gun tubes (Model M-3) were instrumented. Two had the rifling removed for another program which made it simpler to mount the thermocouples as it obviated the problem of matching the thermocouple with the bore surface. For the earlier work only one station was provided but later five stations were instrumented on three of the tubes. Only one of these had provisions for employing the bore surface thermocouples. The locations of the different instrumentation are given in Table I. Chamber pressure also was recorded by a gage located five inches from the breech face. Muzzle velocities were measured by velocity coils in most cases but for some firings by lumiline screens.

Firings were made with M-1 and M-2 propellants and the charges were adjusted to several velocity levels. In some cases mixtures of M-1 and M-2 were used to study the affect of the propellant gas temperature upon the heat input and barrel temperatures. In these cases the lots of propellants used were such that the burning times were approximately the same. It was not possible to make the times equal using the available lots (standard production lots).

TABLE I

DISTANCES OF INSTRUMENTATION FROM THE BREECH FACE

(Inches)

Tube No.	Instrumentation	STATIONS 1	2	3	4	5
109961	PN-1 and Strain Gages Thermocouple	22-3/4 17-3/4				
109974	PN-1 and Strain Gages Thermocouples	20-1/4 16-1/4	29-1/4 23-1/4	37-1/4 32-1/4	51-3/4 58-1/4	64-1/4 82-1/4
97551	PN-1 and Strain Gages	18	30	46	59	72
120023	PN-1 and Strain Gages	18	30	46	59	72

III. DATA AND RESULTS

The data and derived results are given in Table II. The column headings are in general self-explanatory. The values recorded for the measured quantities are average values; in most cases for numerous firings and, in all cases, for not less than six. The muzzle velocities are nominal values rounded off from the average values and are presented to classify the firings.

The easiest to measure and the most accurately determined quantity is the external barrel temperature rise. On firing, the indicator (the microammeter) rapidly reaches a maximum value and then slowly declines as the barrel cools. The rise time is about five seconds from the time the heat pulse reaches the outer surface and the rate of cooling is about 0.1° per second. The section of the barrel on which the gage is mounted and the gage itself are well insulated from the surroundings. Also the rise in temperature is never more than $5^{\circ}C$. It was therefore assumed that the temperature gradients through the barrel wall were small, as were the heat losses, and that the gage recorded what was, in effect, the average temperature rise produced by the amount of heat supplied. From the density and specific heat of the steel, it is possible to estimate the total heat input per round. These estimates are given in Table II, the values of the specific heat and density are those given in Metals Handbook[3] for the type of steel used for the barrels.

A measurement of the temperature rise and the simultaneous strain allows a calculation of the thermal dilatation coefficient of the barrel. The strains are much less well determined than the rises in temperature. The precision, estimated from the average deviation from the average values is, for the temperature rise, about 5 per cent, and for the strains, about 15 per cent. The average estimated dilatation coefficient for the barrels is considerably higher than that (about $1.1 \times 10^{-5}/^{\circ}C$) given in the Metals Handbook for barrel steels. Assuming that the strains are properly measured, this may be due to the fact that the measured temperature rise is too small because of thermal gradients to the surface, or the thermal expansion of the barrel may depend on the method of fabrication. That the latter alternative may be valid is supported by information from Watertown Arsenal[4], which states that the thermal dilatation coefficient of similar barrels was measured to be about $1.5 \times 10^{-5}/^{\circ}C$. This value

TABLE II

CONSOLIDATED DATA AND DERIVED RESULTS OF TYPICAL FIRINGS

Tube No.	Station	Muzzle Velocity ft/sec	Charge M-1 gm	Charge M-2 gm	Flame Temp. °K	Barrel Temp.Rise °C	Total Heat Input Cal./cm²	Strain X10⁵	Dilatation Coeff. X10⁵/°C	Bore Temp. °C
109961	1	1800	130	0	2480	1.44	6.31	2.82	1.95	658
			104	21	2670	1.51	6.61	2.91	1.93	610
			78	41	2845	1.62	7.10	3.07	1.90	734
			52	61	3055	1.69	7.40	3.07	1.82	718
			26	81	3280	1.90	8.14	3.34	1.76	454
			0	101	3540	1.94	8.50	3.37	1.74	766
109961	1	2400	218	0	2480	2.31	10.1	4.20	1.82	587
			175	33	2650	2.26	9.90	4.26	1.88	564
			131	66	2835	2.53	11.1	4.62	1.83	693
			87	99	3045	2.59	11.3	4.65	1.80	618
			43	132	3270	2.71	11.9	4.85	1.79	686
			0	165	3540	3.10	13.6	5.38	1.74	774
109974	1	2400	0	165	3540	3.30	12.8	4.45	1.35	810
	2					2.99	10.8	3.95	1.35	680
	3					3.16	10.3	4.60	1.45	628
	4					4.61	8.90	6.59	1.43	434
	5					6.45	8.77	9.75	1.51	--
	1	2400	218	0	2480	2.42	9.37	3.93	1.62	634
	2					2.22	7.99	3.48	1.57	670
	3					2.36	7.74	3.81	1.61	470
	4					3.43	6.62	6.70	1.95	325
	5					4.86	6.61	9.10	1.87	--
97551	1	2340	215	0	2480		12.7			
	2						9.6			
	3						6.5			
	4						6.6			
	5						6.6			
97551	1	3000	0	220	3540		15.9			
	2						13.2			
	3						8.66			
	4						8.46			
	5						9.24			
120023	1	3700	0	235	3540		15.7			
	2						12.7			
	3						10.1			
	4						12.0			
	5						10.9			
97551	1	2700	255	0	2480		12.4			
	2						9.55			
	3						6.72			
	4						6.97			
	5						7.17			
97551	1	2700	190	50	2710		13.8			
	2						10.2			
	3						7.00			
	4						7.35			
	5						7.35			
97551	1	2700	125	100	2955		14.4			
	2						10.6			
	3						7.47			
	4						7.80			
	5						7.47			
97551	1	2700	60	150	3240		15.0			
	2						--			
	3						7.77			
	4						8.00			
	5						7.88			
97551	1	2700	0	200	3540		16.0			
	2						--			
	3						8.88			
	4						8.90			
	5						9.17			

was not determined from the thermal strains due to firing but rather by uniformly heating the tubes. The BRL values are still greater than the Watertown values, on the average, so that there still may be some discrepancy in the assumption that the measured temperature rise is equal to the average temperature through the barrel wall.

The total heat input per round falls rapidly with distance down the barrel as shown in Figures 4 and 5. The data are not extensive enough to determine the functional dependence accurately. It is probable, that as the measurements are taken closer to the muzzle, the functional dependence will change because as the muzzle is approached the heating is more and more that due to the gas flowing out at the muzzle as the tube empties, after exit of the projectile. After exit, the character of the gas flow is different and depends on different parameters. The graphs show that the heat input reaches a minimum at about six-tenths of the travel and subsequently rises somewhat toward the muzzle.

In all cases for constant ballistics (i.e. constant peak pressure and muzzle velocity) for which measurements were made the total heat input turns out to follow closely a linear dependence on the adiabatic flame temperature of the propellant. The data for one series (muzzle velocity 2700 ft/sec) are plotted in Figure 6. The figure shows also that the slopes of the graph for the different stations are equal within the precision of the data. The same is true for data taken at 1800 ft/sec. In this case, only one station was instrumented. The dependence on flame temperature is not very strong. This is due to the fact that as a cooler propellant is used it is necessary to use a larger charge. This increases the mean density of the gas which increases the heat transfer coefficient, thereby tending to compensate for the decreased gas temperature.

The bore surface temperatures indicated by the thermocouples are subject to large round to round variation and to obtain consistency in the readings an average over a number of firings must be taken. Figure 7 is a plot of the average maximum bore surface temperature for four of the five stations. It was found to be not possible to make measurements at the fifth station. The thermocouples were too fragile to withstand the shock of firing. The plotted points are averaged over sixteen rounds and, except for station 2 for M-1

HEAT INPUT (Q) VS. DISTANCE FROM BREECH FACE
FOR THE SAME BALLISTICS BUT DIFFERENT PROPELLANTS

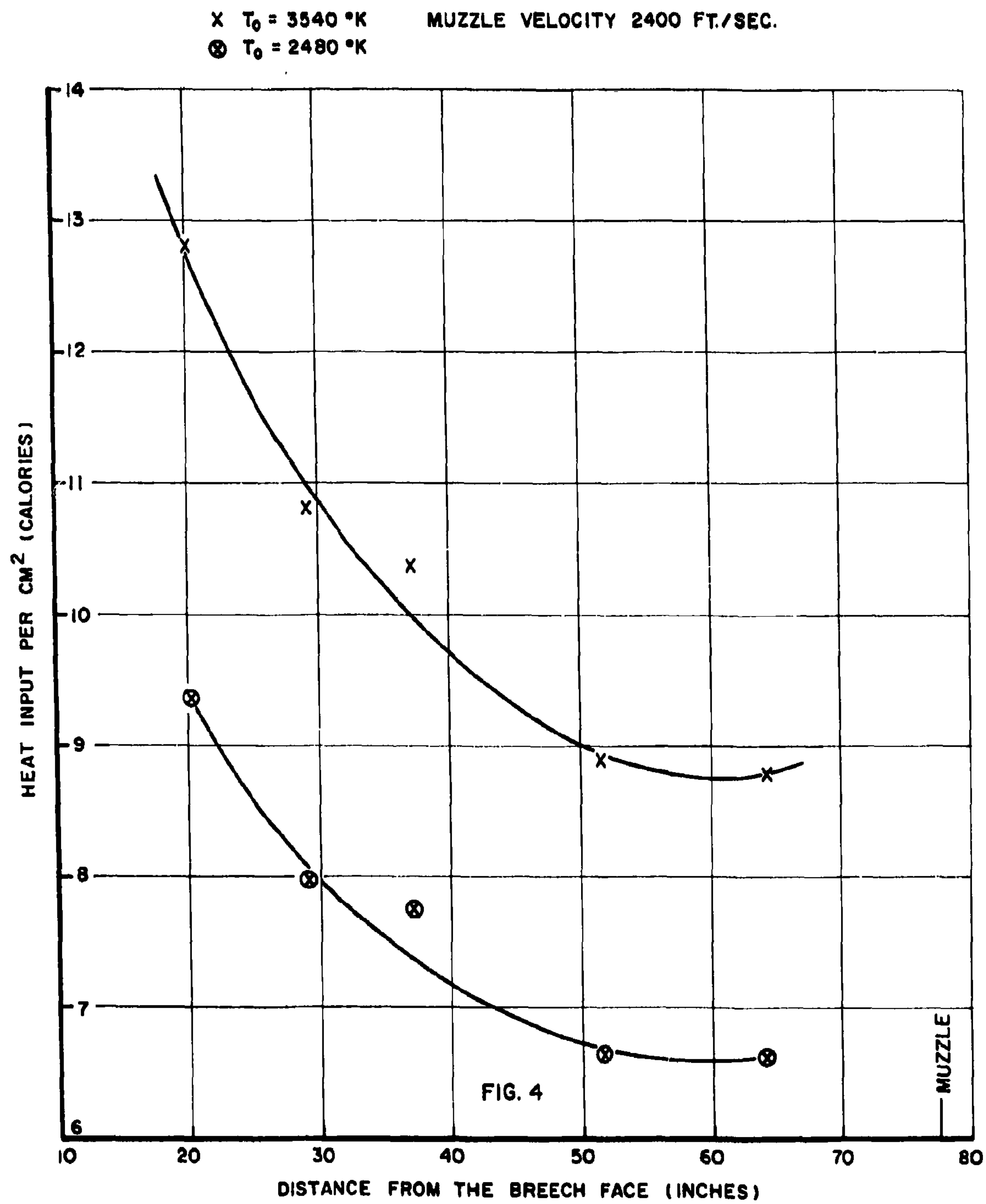

FIG. 4

HEAT INPUT (Q) VS. DISTANCE FROM THE BREECH FACE FOR SAME BALLISTICS BUT FOR DIFFERENT PROPELLANTS

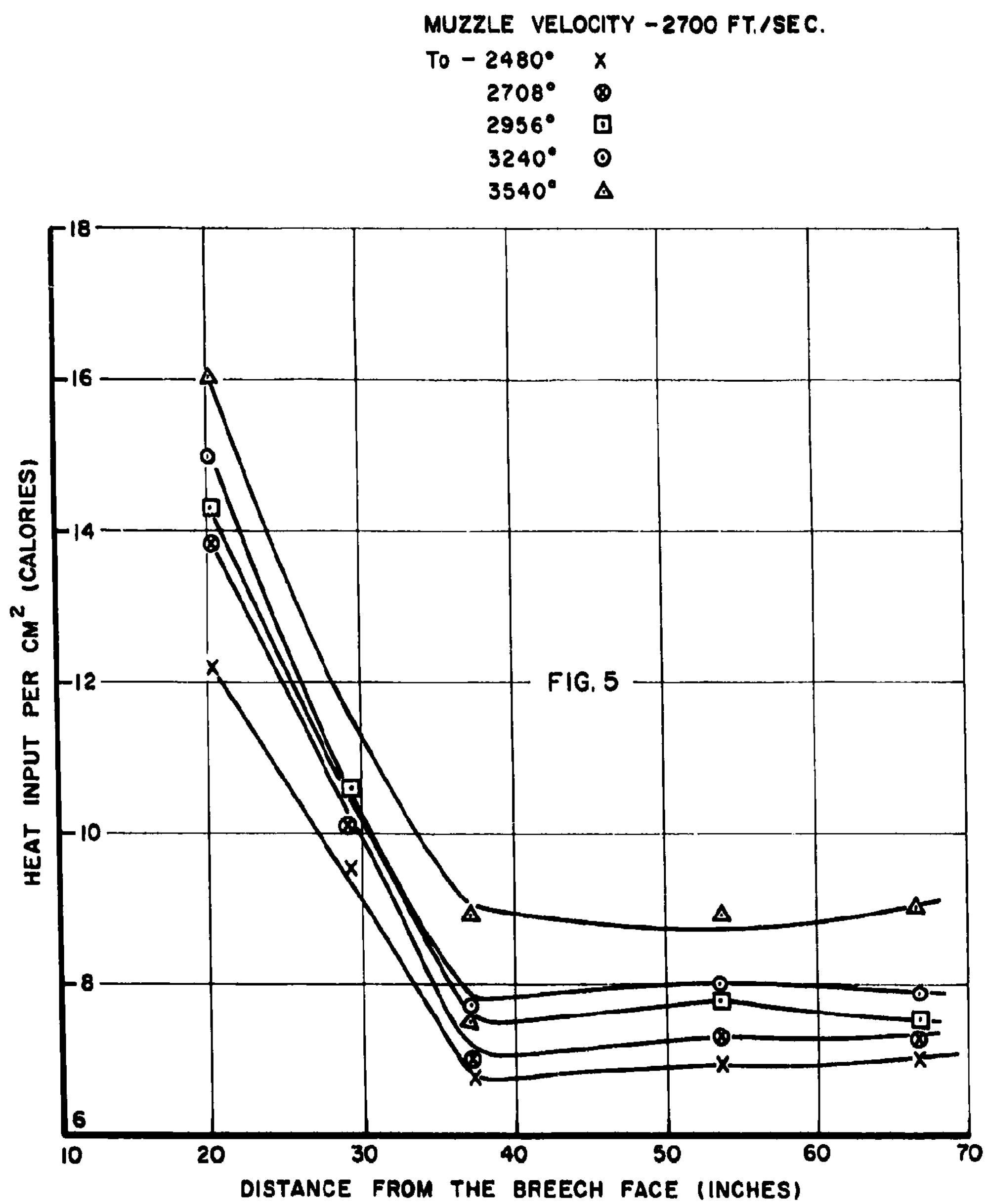

FIG. 5

HEAT INPUT (Q) vs. PROPELLANT FLAME TEMPERATURE
FOR SAME BALLISTICS

MUZZLE VELOCITY 2700 FT./SEC.

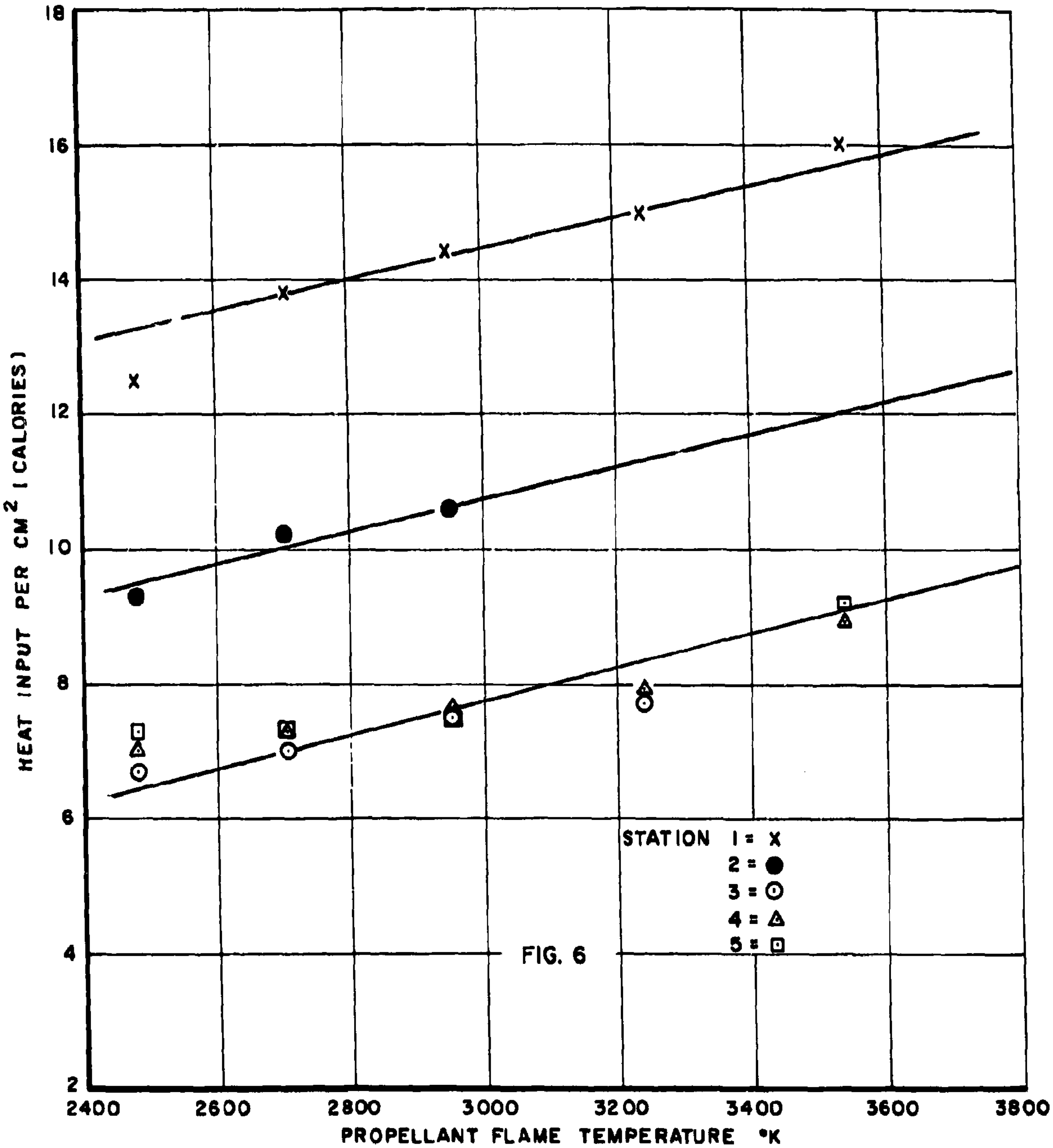

FIG. 6

MAXIMUM BORE SURFACE TEMPERATURE RISE VS. DISTANCE FROM THE FORCING CONE

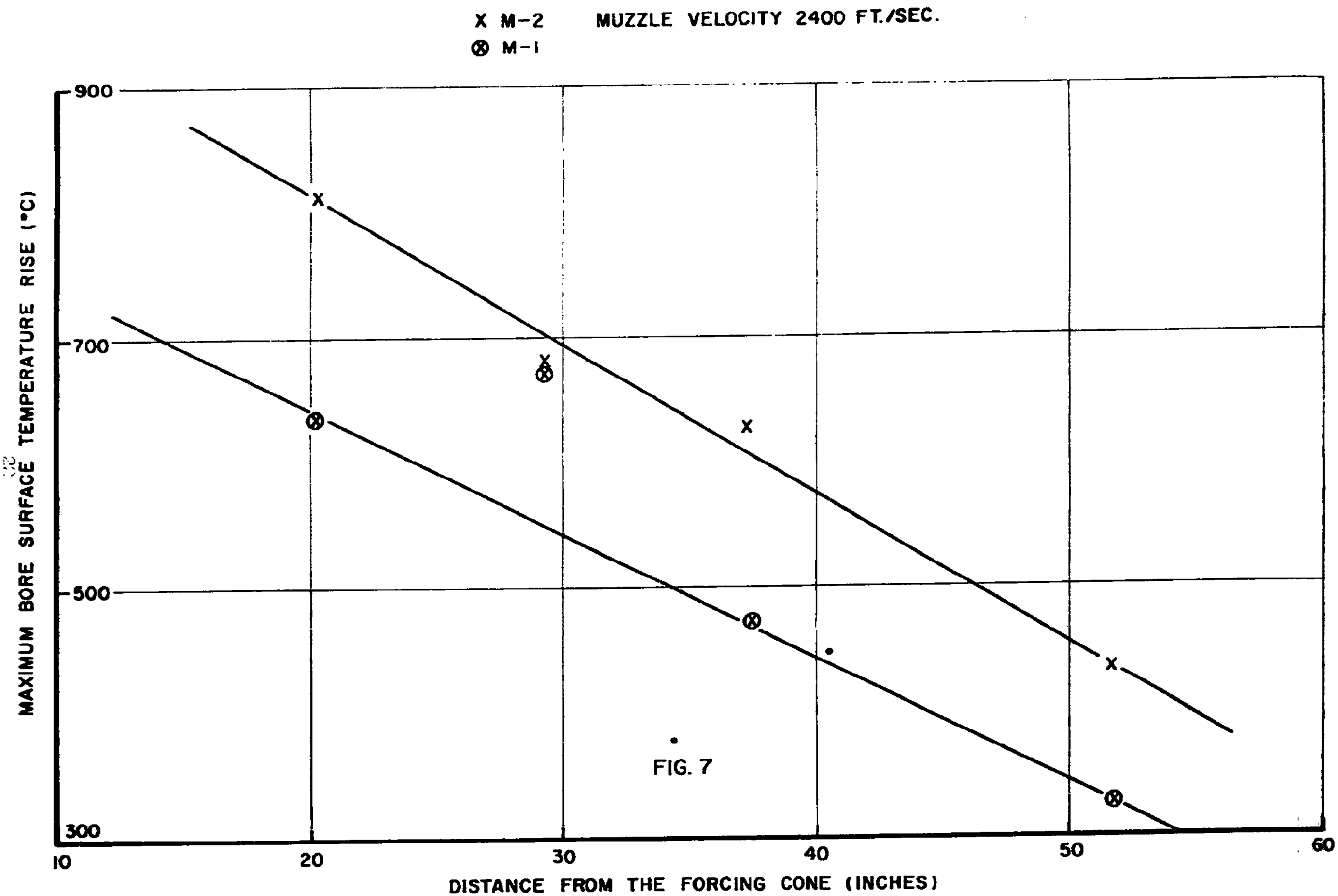

FIG. 7

propellant, they follow a consistent pattern. The data for the exceptional point have been checked. The rounds from which this point was derived were not all fired consecutively. They were distributed over a period of more than a year and any one thermocouple rarely lasted more than five rounds. It would seem that the deviation of the point from its appropriate value is statistical. The precision, as measured by the average deviation from the mean value, averages about $\pm 150^{\circ}C$ for the different stations so that the point is not outside the indicated precision of the data. The results indicate that one should use these thermocouples for bore surface temperature measurements wit . considerable discretion.

It is the opinion of the writers that the wide fluctuations in the thermocouple indications are real. There is considerable contamination of the thermocouples due to deposits from the propellant gases and also copper from the rotating band. It is also doubtful that a uniform surface temperature exists. The gas is not all at the same temperature. The firing cycle is so short and the mixing process relatively so slow, that the thermocouple may sense quite different maximum temperatures from round to round. The roughness of the surface, which varies from round to round, will also influence the rate of heat transfer locally, so that, in effect, a uniform surface temperature does not exist.

The data taken for the mixed propellants M-1 and M-2 bear out these ideas. The thermocouple readings do not follow a consistent pattern as do the data on total heat input. In this case, the variations in the gas temperature during firing will be much more drastic than for a single propellant and one would expect the data to show wider and more irregular fluctuations than for a single propellant.

IV. COMPARISON WITH THE THEORY OF NORDHEIM, SOODAK AND NORDHEIM*[6]

It has been pointed out earlier that the extant theories of gun barrel heating assume that the existing theories of heat transfer by forced convection between a solid boundary and a steady turbulent fluid flow can be applied to

* In this report abbreviated NSN

the case of the gun. The problem is to calculate the rate of heat transfer to the barrel wall as a function of the interior ballistic parameters which specifies the boundary condition on the solution of the Fourier Heat Conduction equation. It is assumed that the rate of heat transfer H(t) is given by:

$$H(t) = h(T_g - T_w) \tag{1}$$

where T_g is the gas temperature in the gas away from the wall, T_w is the wall temperature and "h" a heat transfer coefficient. In the case of the gun, h, T_g and T_w are all functions of the time but it is assumed that equation (1) is "quasi steady" that is, it holds over each small interval of the integration.

There exists an extensive literature on the specification of h. Nordheim, et al , adopt a very simple formula for h, namely:

$$h = \frac{\lambda}{2} C_p \rho V \tag{2}$$

where C_p is the specific heat of the gas at constant pressure, ρ the density of gas and V its velocity. The quantity λ, which they call the friction factor, is, in their theory, essentially a "fudge" factor to be determined experimentally. Corner[7] presents an estimation of h from boundary layer theory but finds that the results have to be corrected empirically in any case.

The usefulness of the theory will depend, obviously on how well λ can be estimated for a particular situation. This will depend on a sufficiently extensive accumulation of experimental determinations of λ, for a wide variety of cases so that, for a particular case, a reasonably reliable value of λ can be specified. Unfortunately, at this time, very few data have been published which permits an adequate evaluation.

A rather detailed discussion of NSN theory as applied to guns is included in an attached appendix. NSN solve the Fourier Equation in one dimension, that is, they assume that the curvature of the bore can be neglected. Following their procedure we have coded the problem for machine computation. The values of λ were adjusted to make the theoretical values of Q agree with the measured values to within the precision of the latter. These values of λ, as well as the measured values of Q, for a series of firings at 2400 ft/sec are plotted in Figure 8 for both M-1 and M-2 propellant. They decline steadily with distance down the bore but not so rapidly as the measured values of Q. Near the muzzle, they tend to decline less rapidly than does Q. This effect may be real in part. The bore is more eroded toward the breech and muzzle and one would expect λ to be larger for a rough surface. λ does not depend on the surface condition only however, as is evident from the treatment of Corner given in Reference 7.

From Figure 8, one can estimate the values of λ at the positions of the thermocouples and calculate the bore surface temperatures at these locations for comparison with experiment. When this is done, it is found that the maximum bore surface temperatures calculated are, in general, quite close to the measured values. In no case is the difference greater than the estimated error in the measured values and, in most cases, it is considerably less than this. The values are plotted in Figure 9.

The average observed bore surface temperature is plotted as a function of time along with the calculated values for some typical cases in Figures 10 through 17. The agreement is satisfactory since the divergencies are not greater than the estimated precision of the observations.

It should be noted that the maximum temperature plotted in Figures 10 through 17 does not correspond exactly with the maximum temperature shown in Figure 7. The reason for this is the following: The temperatures determined by the thermocouples were recorded by means of a 35mm moving film oscilloscope recorder. After the records were developed, the peak temperature only was read. The records were then stored in a record file for future reference. Sometime during storage about one half of the total records, some 15 rounds, were either lost or destroyed. Hence, when further study of the records was started, the average values of the temperatures were different than those shown in Figure 7. Unfortunately, there was no requirement to determine temperature as a function of time when the firings were first made.

However, in view of the rather wide scatter in the bore surface temperature as measured by the thermocouples, it is the opinion of the authors that data as shown in Figure 7 and Figures 10 through 17 are compatible and are of considerable value.

HEAT INPUT (Q) VS. DISTANCE FROM BREECH FACE
FRICTION FACTOR (λ) VS. DISTANCE FROM BREECH FACE

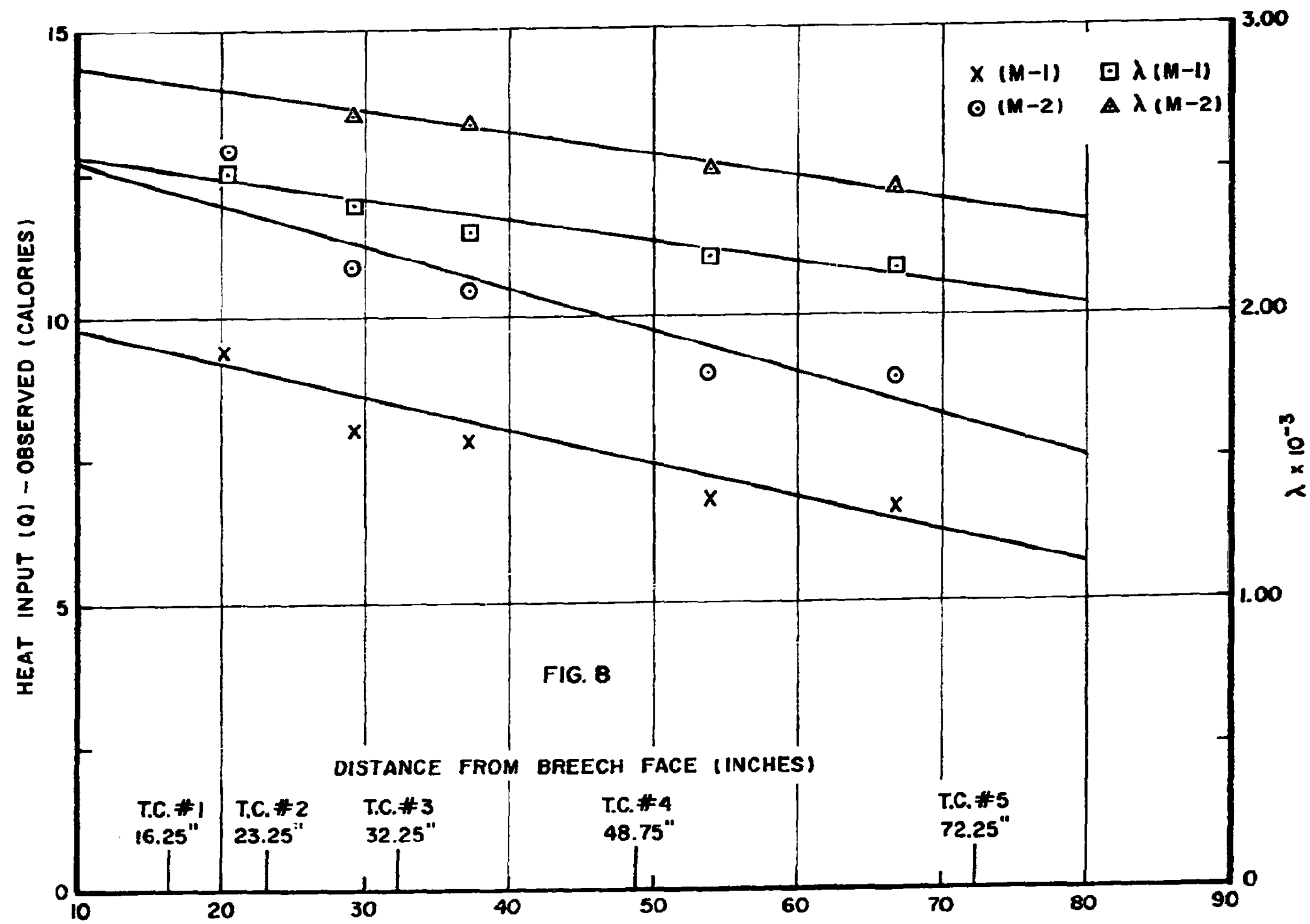

FIG. 8

MAXIMUM BORE TEMPERATURE VS. DISTANCE FROM THE BREECH FACE

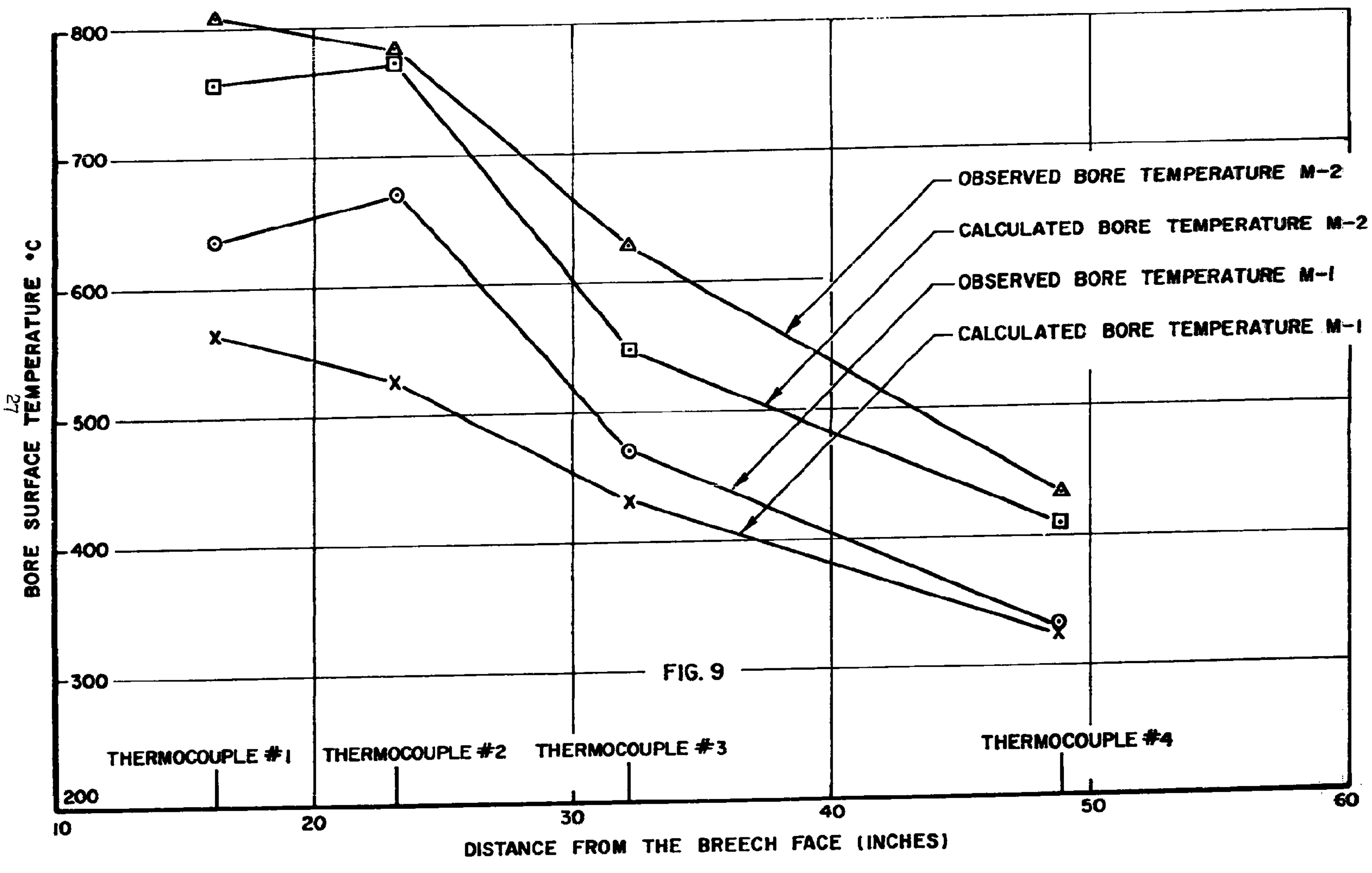

FIG. 9

In Figures 10 through 17 the solid line indicates the average bore surface temperature as measured by the thermocouples, and the dashed line, the surface temperatures as computed by NSN theory using a value of λ estimated from Figure 8. The time scales were adjusted arbitrarily so that maximum temperature occurred at zero time.

In view of the difficulty of making accurate bore temperature measurements, it would appear from the present work that one could make external barrel temperature measurements and use the value of λ (adjusted to match the external temperature) to calculate barrel temperature distribution. This would not require the drilling of holes in the barrel and would probably give as reliable a determination of the barrel temperature distribution as one could obtain from thermocouples.

CENTERED ON T_{MAX}

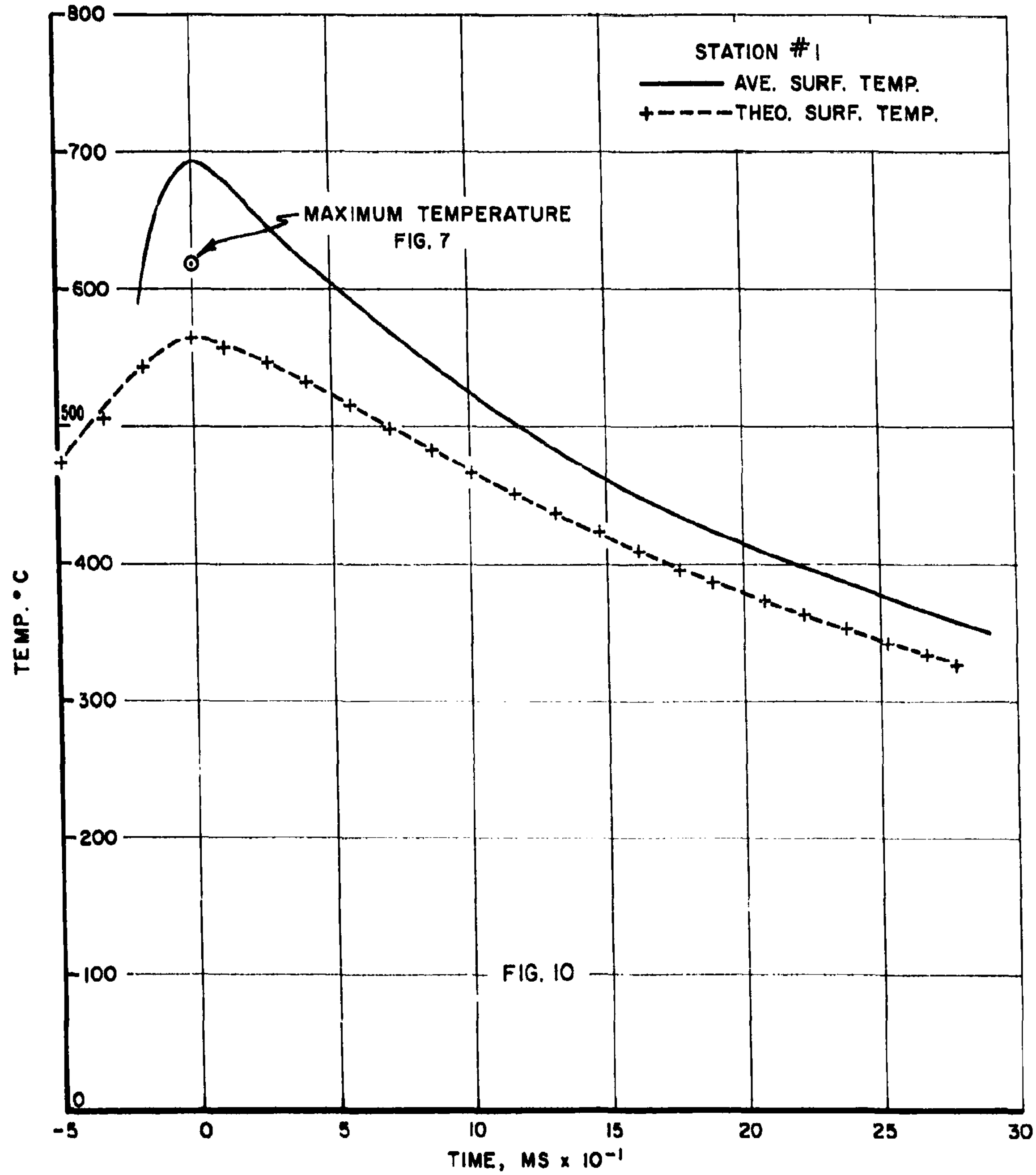

FIG. 10

CENTERED ON T_{MAX}

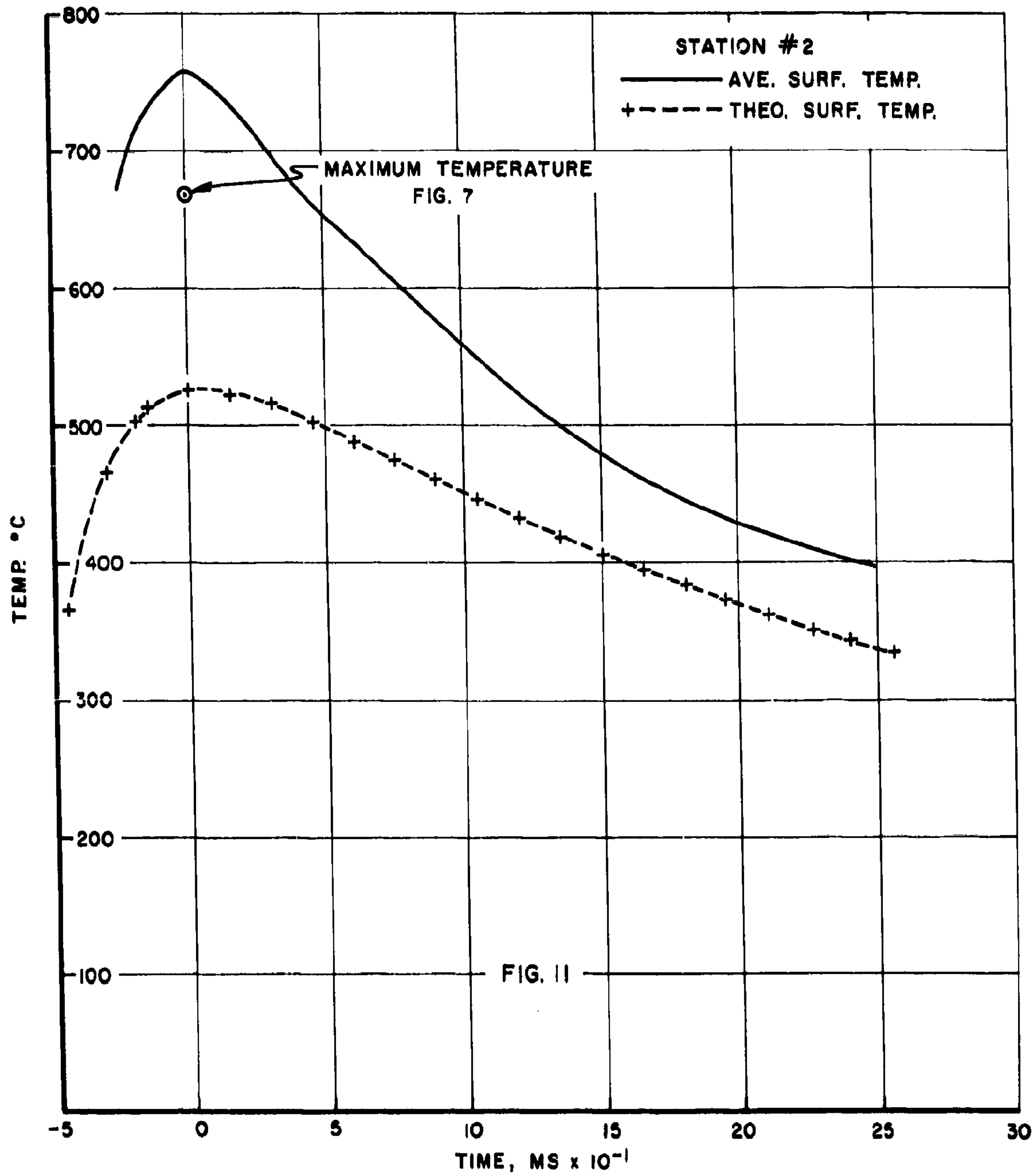

FIG. 11

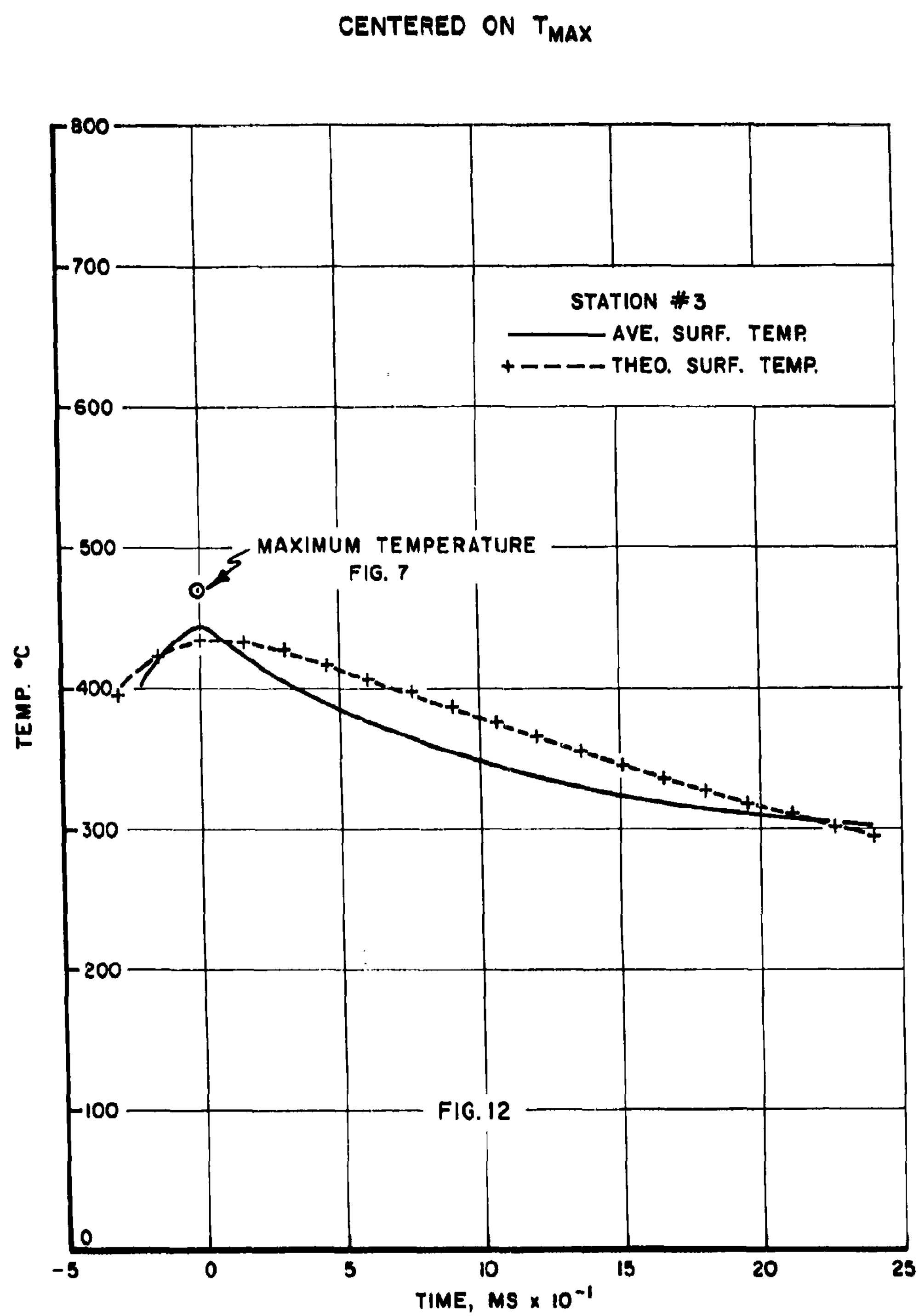
CENTERED ON T_{MAX}
STATION #3
AVE. SURF. TEMP.
THEO. SURF. TEMP.
MAXIMUM TEMPERATURE
FIG. 7
TEMP. °C
800
700
600
500
400
300
200
100
0
-5
0
5
10
15
20
25
TIME, MS x 10^{-1}

FIG. 12

CENTERED ON T_{MAX}

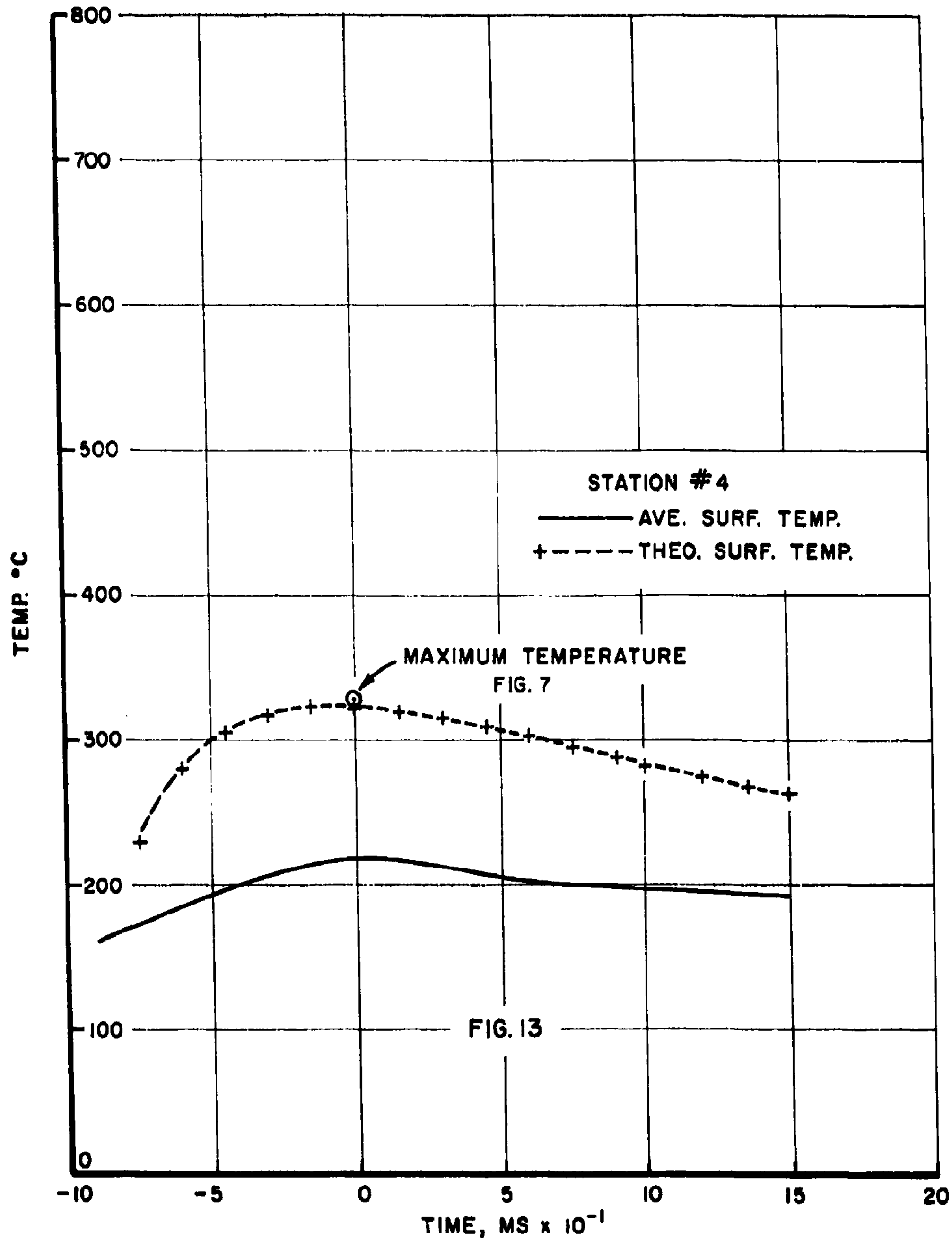

FIG. 13

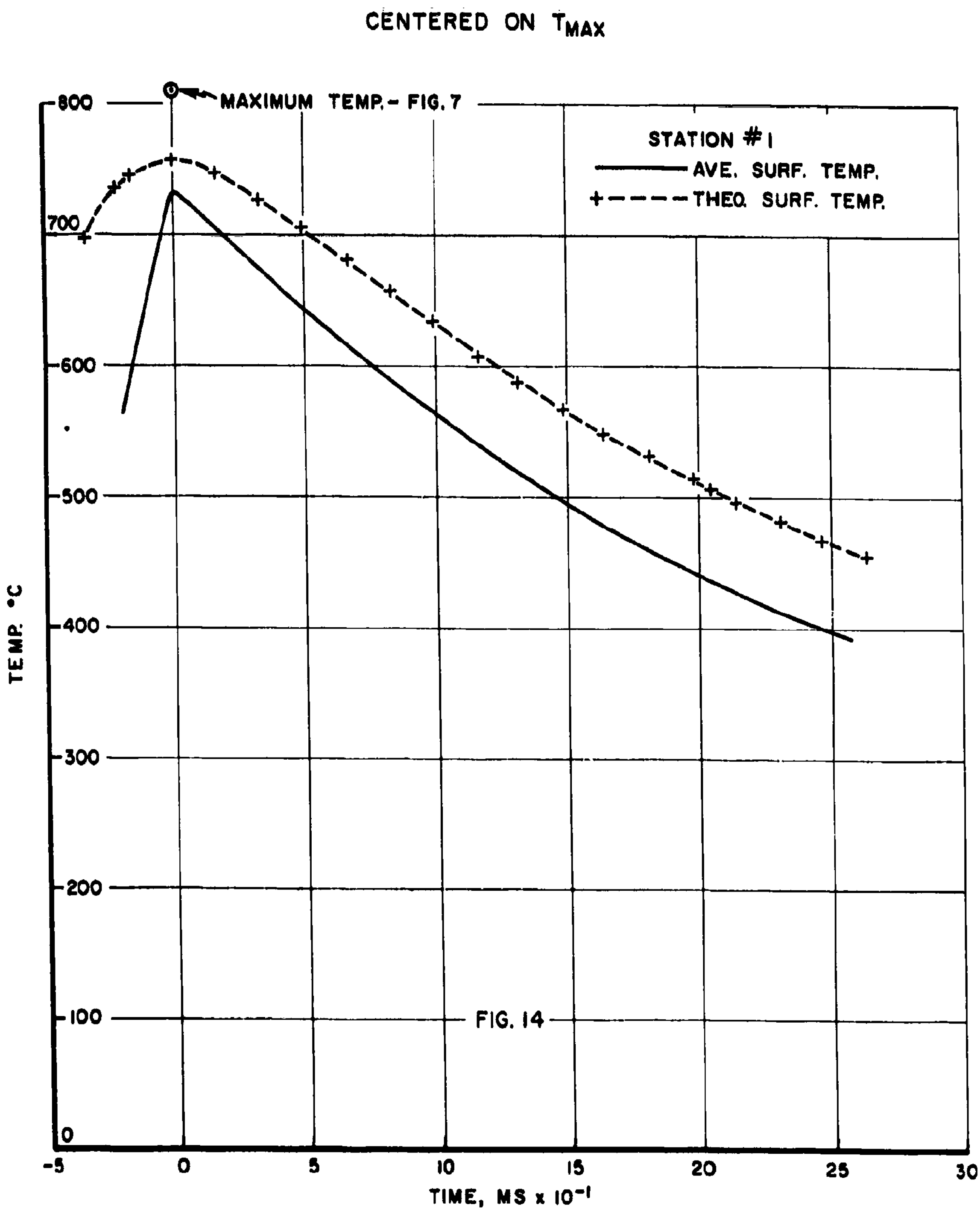
CENTERED ON T_{MAX}
MAXIMUM TEMP. - FIG. 7
STATION #1
AVE. SURF. TEMP.
THEO. SURF. TEMP.
TEMP. °C
800
700
600
500
400
300
200
100
0
-5
0
5
10
15
20
25
30
TIME, MS x 10^{-1}

FIG. 14

CENTERED ON T_{MAX}

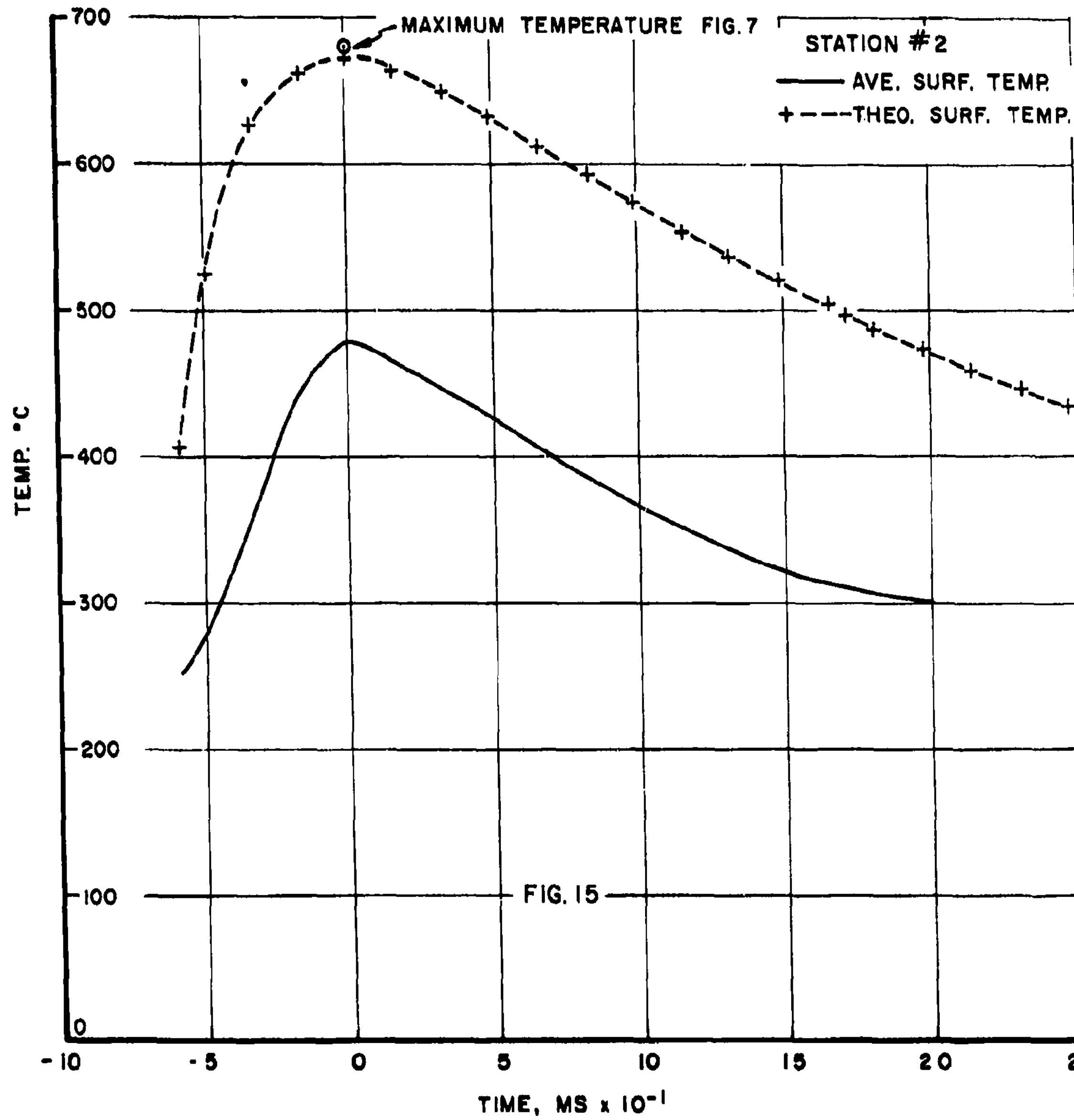

FIG. 15

CENTERED ON T_{MAX}

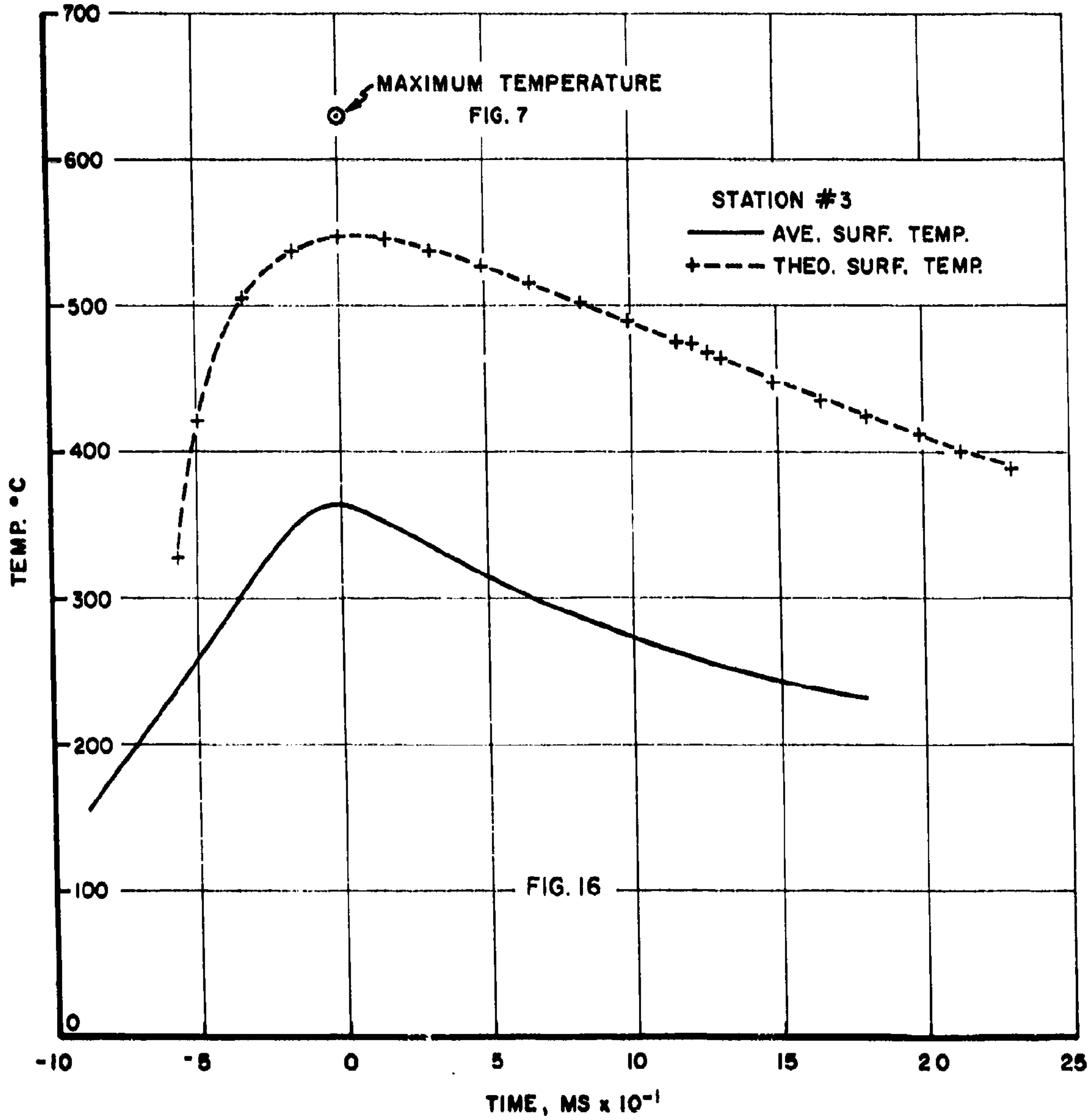

FIG. 16

CENTERED ON T_{MAX}

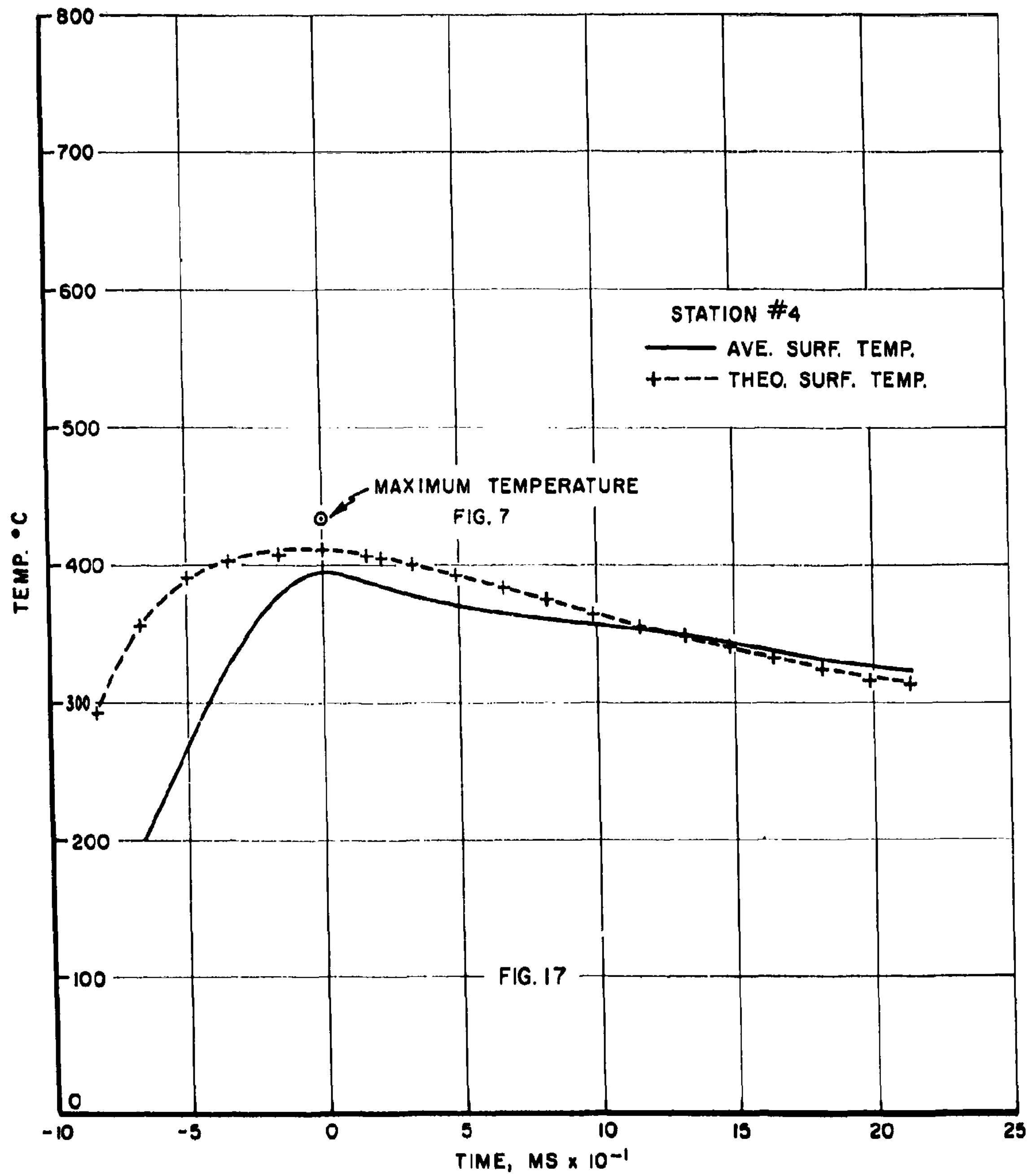

FIG. 17

ACKNOWLEDGMENT

A number of the experimental procedures and techniques used in this program were developed by military personnel assigned to the Interior Ballistics Laboratory. In addition, large amounts of the data were taken by military members of the Laboratory. With this in mind, the authors wish to express their appreciation of the support and cooperation given by Lt. David Wells, Pfc. Norbert Edlebach and Pfc. Donald E. Benz during their assignments to the research program. The work of Mr. Homer Murray in developing the technique for improving the nickel plating of the thermocouples, and the work of Mr. Fredrick McIntosh of the Ballistic Research Laboratories' Computing Laboratory in developing and programming a system for machine computation of the heat input data is greatly appreciated.

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REFERENCES

1. Hackemann, P. A Method for the Measurement of Rapidly Changing Surface Temperatures and its Application to Bores of Guns; Aircraft Research Center, Hermann Goering, Brunswick Germany, 27 January 1941 (In German).

2. Midwest Research Institute, Kansas City, Missouri.

3. The Metals Handbook, 1948 Edition, The American Society of Metals, Cleveland, Ohio.

4. Gruzdis, M.B. and Kosting, P.R. Dilation of Centrifugal Castings, C414, C696, C769; Report No. 314/22; 22 June 1937, Watertown Arsenal, Watertown, Mass.

5. Strong, J. Procedures in Experimental Physics, Prentice-Hall, Inc. (1938)

6. Nordheim, L.W., Soodak, H., and Nordheim, G.; Thermal Effects of Propellant Gases in Erosion Vents and in Guns, NDRC Report No. A262.

7. Corner, J. Theory of the Interior Ballistics of Guns, John Wiley & Sons (1950).

APPENDIX A

NORDHEIM, SOODAK, AND NORDHEIM THEORY OF HEAT TRANSFER IN GUNS

I. SYMBOLS USED IN THIS APPENDIX

SYMBOL	UNIT	DEFINITION
Propellant Constant		
F	(Cm^2/Sec^2)	$\left[= nRT \right]$, the "impetus" or force of the propellant.
n	mole/gm	Number of moles of propellant gas formed per gram of propellant.
R	erg/°K	Gas constant
T_o	°K	Adiabatic Gas Temperature
γ		True ratio of specific heat C_p/C_v
$\bar{\gamma}$		Effective ratio of specific heats which corrects for heat loss
C_p	Cal/gm°C	Specific heat at constant pressure
C_v	Cal/gm°C	Specific heat at constant volume
ρ_p	gm/Cm^3	Density of Solid Propellant
Gun Constants		
m_o	grams	True mass of projectile
m	grams	Effective Mass of Projectile
C	grams	Mass of Propellant Charge
U_s	Cm^3	Volume of the gun from the breech to a specified point s
A	Cm^2	Cross sectional area of the bore
x_s	Cm	$\left[U_s/A\right]$ Coordinate along the bore
Y_s	Cm	$\left[x_s - C/A\rho_p\right]$ Reduced coordinate along the bore

SYMBOL	UNIT	DEFINITION
Ballistic Notations		
V	cm/sec	$\left[dx/dt - dy/dt\right]$ Projectile Velocity
P	dynes/cm^2	Pressure
$N(t)$	gm	Mass of Propellant burned up to twice $\underline{t}$
B	cm^2 sec/gm	Burning constant
W	cm	Web thickness of propellant
Reduced Ballistic Quantities		
J	-	$\left[M/c\right]$ The ratio of projectile effective mass to charge mass
Δ	gm/cm^3	$\left[C/v_o\right] = C/Ax_o$ Density of loading
Φ	cm/sec	$\left[\frac{CB}{W}\frac{F}{A}\right]$ Reduced burning constant
E	-	$\frac{\gamma - 1}{2}$ $\frac{F}{J\Phi}2$ Burning parameter
y_m/y_o	-	Reduced length of gun
y_o	-	$\left[\frac{U_o}{A} (1 - \frac{D}{\rho_p})\right]$

II. ASSUMPTIONS

In order to apply the theory of heat transfer to guns, the following assumptions are necessary:

1. The equation of state for the propellant gases is assumed to be a simplified Van der Waals equation,

$$P = \left(\frac{1}{\rho} - \eta\right) = nRT \tag{1}$$

where P is the pressure, ρ the gas density, η the covolume, R the gas constant, n the number of moles per gram of propellant gas, and T the absolute temperature of the propellant gas, in degrees of Kelvin. The "force" of the propellant gas is determined by

$$F = nRT_o \tag{2}$$

where T_o is the adiabatic flame temperature. The specific heat C_p and C_v and their ratio γ are assumed to be independent of temperature.

2. The steady state formula,

$$H = h\,(\theta_o - \theta_w) \tag{3}$$

holds for the unsteady situation in a gun, that is:

$$H(t) = h(t)\left[\theta_g(t) - \theta_w(t)\right] \tag{4}$$

where θ is the temperature relative to some initial temperature.

3. That the heat transfer coefficient h(t), is given by

$$H(t) = \frac{\lambda}{2}\, C_p\, (\rho_v)\, (t) \tag{5}$$

which expresses the heat transfer in terms of the mean specific flux (ρ_v) of the gas, the specific heat C_p and the hydrodynamic factor λ. This factor is called the Fanning Coefficient in the literature, and is a dimensionless constant that connects the pressure loss dp/dx along a pipe due to friction with the dynamic pressure in the fluid according to the formula

$$\lambda = \frac{1}{r_o} \frac{dp}{dx} \Big/ \frac{1}{2} \rho u^2 \qquad (6)$$

where r_o is the so called hydraulic radius, that is, the cross sectional area of the pipe divided by one half of its circumference. However, in the case of a gun, λ is essentially a "fudge" factor introduced to account for the differences in heat transfer due to undetermined roughnesses on the surface of the gun bore. A method for determining a value of λ from heat input data is discussed in the body of this report.

4. The next assumption is that the curvature of the bore can be neglected and the solution of the problem is that of solving the Fourier Heat Conduction Equation in one dimension.

$$\frac{\partial \theta(zt)}{\partial t} = \frac{k}{C\rho} \frac{\partial^2 \theta(zt)}{\partial z^2} \qquad (7)$$

with the boundary conditions

$$H(t) + k \frac{\partial \theta(zt)}{\partial z} = 0 \qquad (8)$$

and $\theta(z,0) = 0$ as an initial condition.

III. FLUX OF THE PROPELLANT GASES

The basic problem to be solved prior to a calculation of gun temperature is the determination of the heat-transfer coefficient for a given position along the bore as a function of time.

In section II, equation (5) is the basic formula for the heat transfer coefficient.

$$h(t) = \frac{\lambda}{2} C_p \rho u \qquad (9)$$

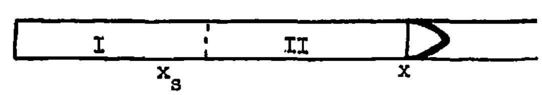
Where ρ_u is the specific flux of the propellant gases, the friction factor λ is a constant for a particular gun, and C_p is the constant specific heat of the propellant gas. It is assumed that the unburned propellant does not contribute measurably to the heat transfer.

Let us assume that the density of the propellant gases remain constant throughout the whole gun. However, the flux, and with it, the heat transfer will depend on the distribution of the unburned propellant during the firing. Therefore, to describe the distribution, we introduce a form function $G(x_s t)$ that gives the fraction of unburned propellant between the breechblock and the point x_s in the gun. While G may be a function of x_s and time, it also must satisfy the following conditions

$$\left[G\right]_{x_s = o} = 0, \left[G\right]_{x_s = x} = 1, \qquad (10)$$

where x is the coordinate of the base of the projectile, the fractional density of the solid at x_s will be $\frac{\partial G}{\partial x_s}$.

We can determine the flux by the following consideration. Let us divide the volume of the gun up to the base of the projectile to point x into two parts -- I back of, and II ahead of the point x_s in the following sketch.



The specific flux through a cross section at x_s, that is, the mass per unit area and time, will be given the excess of propellant burned per unit time in I over the rate of change of gas mass in I divided by the effective cross sectional area A* for the gas at x_s. A* is defined as the volume per unit length available for the gas.

By definition of G, the amount of propellant burned in I per unit time is GdN/dt, where N is the amount of propellant burned as a function of t; the total mass of gas in I equals its density $N/\left[Ax-(C-N)/\rho_p\right]$, times the available volume, $Ax_s - G(C-N/\rho_p$. This expression is obtained by subtracting the space occupied by the propellant in I from the volume in I. We obtain thus

$$iA^* = G\frac{dN}{dt} - \frac{d}{dt}\left[N\,\frac{Ax_s - G(C-N)/\rho_p}{Ax - (C-N)/\rho_p}\right] \tag{11}$$

which with $G\,\frac{dN}{dt} = \frac{d}{dt}(GN) - N\,\frac{dG}{dt}$ can also be written as

$$iA^* = \frac{d}{dt}\left[N\,\frac{A(Gx-x_s)}{Ax - (C-N)/\rho_p}\right] - N\,\frac{dG}{dt}\,. \tag{12}$$

Since the effective cross sectional area is defined as the volume per unit length available for the gas, that is the true volume per unit length minus the volume of solid propellant per unit length, we have

$$A^* = A - \left[(C-N)/\rho_p\ \frac{dG}{dx_s}\right] = A\left(1 - \frac{C-N}{A\rho_p}\ \frac{dG}{dx_s}\right) \tag{13}$$

These formulas allow us to write down the flux and thus the heat transfer coefficient under any assumption regarding the distribution of the unburned propellant if the ballistics are known; that is, if we have the functional dependence of x and N on the time t.

We shall consider only the assumption that the unburned propellant is always equally distributed over the whole available space. In this case G(a) is the ratio of the volume I to the total volume, or

$$G(a) = \frac{x_s}{x}\ ;\quad \frac{dG}{dx_s} = \frac{1}{x} \tag{14}$$

Using equations (14) and (13) in (12) we obtain

$$i_{(a)} = -\frac{N}{A*}\frac{dG}{dt} = -\frac{N}{A-(C-N)/\rho_p\, x}\frac{d}{dt} = \frac{x_s N}{x\left[Ax-(C-N)/\rho_p\right]} \qquad (15)$$

This expression can be directly interpreted as the density of the gas $N/\left[Ax - (C-N)/\rho_p\right]$ times a local velocity that stands in the ratio x_s/x to the velocity of the projectile.

IV. NORDHEIM, SOODAK, AND NORDHEIM (NSN) REDUCED BALLISTIC SYSTEM

The calculation of bore temperature and heat transfer for a gun becomes the problem of integration of the Fourier equation as soon as a definite ballistic system has been developed and definite values have been assigned to the parameters involved - e.g., the friction factor, and the thermal constant of the steel. Since some of the gun parameters are uncertain, it would be rather impractical to solve the problem of gun heating and temperature by treating each gun individually, that is, working out a separate ballistic system for each gun, and then solving the heat conduction equation under all possible assumptions in regard to the parameter concerned.

In order to get around this situation, it is desirable to use a slightly simplified ballistic system in which similarity relations can be used to reduce the number of necessary integrations. This becomes possible because the ballistic curves, (velocity - travel - pressure - and temperature - time curves) are of a similar type for all guns.

The assumptions that are necessary to allow the simplification are the following:

1. The burning rate is proportional to the pressure and the burning surface of the propellant remains constant until it is all burned.

2. In the equation of motion, the covolume of the gas is assumed to be equal to the volume of the same weight of unburned propellant.

3. The starting pressure is assumed to be zero.

4. The effects of mechanical friction and the kinetic energy of the propellant gases can be accounted for by introduction of an effective mass of the projectile in place of the true mass.

5. The effect on ballistics of heat loss by the propellant gases can be accounted for by the use of an effective ratio for the specific heat of the propellant gases in place of the true value.

In this resume the subscript o (v_o, x_o, y_o) refers to the base of the projectile at rest; the subscript s to points along the bore surface; coordinate without subscript refer to the position of the base of the projectile during firing; subscript m (x_m, y_m, etc.) to the muzzle of the gun; the subscript b, to the position of the base of the projectile when all of the propellant is burned. The symbols and definition appear at the beginning of this appendix.

According to assumption (1) the burning law is expressed as

$$\frac{dN}{dt} = C \frac{B}{W} \rho \qquad (16)$$

In setting up the ballistic equations, three time intervals must be treated separately. The first ends at the time all of the propellant is burned, the second extends from all burnt to the time the projectile leaves the gun, and the third, (need for heat transfer purposes) begins after the projectile leaves the gun.

The position at which the propellant is all burned is determined by the following equation:

$$\frac{y_b}{y_o} = \left[\frac{1}{1-E} \right]^{\frac{2}{\bar{\gamma}-1}} \qquad (17)$$

The velocity of the projectile as a function of travel is given by

$$V = \frac{2\Phi}{\bar{\gamma}-1}\left[1 - \left(\frac{y_o}{y}\right)^{\frac{\bar{\gamma}-1}{2}}\right] \text{ - before burning} \tag{18}$$

$$V = \left(\frac{2FC}{J(\gamma-1)}\right)^{1/2}\left[1 - \left(\frac{y_b}{y_o}\right)^{(\bar{\gamma}-1)/2}\left(\frac{y_o}{y}\right)^{\bar{\gamma}-1}\right]^{1/2} \tag{19}$$

after burning.

The travel time curves can be obtained by integration of (18) and (19).

The amount of propellant burned, as a function of projectile position is given by

$$N = \frac{m\Phi V}{F} = \frac{m}{F}\;\frac{2\Phi^2}{\gamma - 1}\left[1 - \left(\frac{y_o}{y}\right)^{\frac{\bar{\gamma}-1}{2}}\right] y < y_b \tag{20}$$

The pressure as a function of travel is given by

$$P = \frac{F\Delta}{1-\Delta/\rho_p}\;\frac{1}{E}\left(\frac{y_o}{y}\right)^{(\bar{\gamma}+1)/2}\left[1 - \left(\frac{y_o}{y}\right)^{(\bar{\gamma}-1)/2}\right] \text{ - before} \tag{21}$$

burning.

$$P = \frac{F\Delta}{1-\Delta/\rho_p}\left(\frac{y_b}{y_o}\right)^{(\bar{\gamma}-1)/2}\left(\frac{y_o}{y}\right)^{\bar{\gamma}} \quad \text{after burning.} \tag{22}$$

The maximum pressure occurs at

$$\frac{y_{p\,max}}{y_o} = \left(\frac{2\bar{\gamma}}{\bar{\gamma}+1}\right)^{\frac{2}{(\bar{\gamma}-1)}}$$

independent of all other conditions, provided $y_{p\,max} < y_b$. Otherwise the maximum pressure occurs when the propellant is all burned. The value of the maximum pressure is

$$P_{max} = \frac{j\Delta}{1 - \Delta/\rho_p} \;\Phi^2\; \frac{1}{\bar\gamma} \left(\frac{\bar\gamma+1}{2\bar\gamma}\right)^{\frac{(\bar\gamma+1)}{(\bar\gamma-1)}} \quad E \geq (\bar\gamma-1)/2\bar\gamma \tag{23}$$

$$P_{max} = P_b = \frac{\Delta F}{1-\Delta/\rho_p} \left[1-E\right]^{(\bar\gamma+1)/(\bar\gamma-1)}, \; E > \bar\gamma-1/2\,\bar\gamma\,. \tag{24}$$

The temperature T^oK of the gas is given by

$$T = T_o \left(\frac{y_o}{y}\right)^{(\bar\gamma-1)/2} \quad \text{before burning} \tag{25}$$

$$T = T_o \left(\frac{y_b}{y_o}\right)^{(\gamma-1)/2} \left(\frac{y_o}{y}\right)^{(\bar\gamma-1)} \quad \text{after burning.} \tag{26}$$

In order to adapt the system to any particular gun the following procedure is followed. For the connection between $\underline{m}$ and m_o, the relation

$$m = 1.04\,(m_o + C/3) \text{ is used.} \tag{27}$$

For the effective ratio of the specific heats, an average value $\bar\gamma = 1.36$ is used. The values of m_o, V_o, $\underline{j}$, $\underline{\Delta}$ and F are known gun constants. The value of Φ and $\underline{E}$ are determined from observed maximum pressure according to equation (21) or (22). Finally a check on the ballistics is obtained by comparing the muzzle velocity as calculated from equation (19) with the observed value.

The absolute size of the gun has been completely eliminated in the ballistic formulas given by equations (16) through (26). This system contains only one parameter, namely y_b/y_o, or the constant $\underline{E}$. If this quantity is kept constant, the ballistic curves (travel, velocity, temperature, and pressure - time) will be similar no matter what values are given to the other parameters. Thus by using ... 1 through 5, a one parameter group of possible travel-time curves under variation of all conditions such as ratio of projectile mass to charge mass, loading density, web thickness, and composition of the propellant are possible.

As already pointed out, guns of different absolute size will have similar ballistic curves. Then by rewriting equation (19)

$$\frac{1}{y_o} V = \frac{d}{dt}\left(\frac{y}{y_o}\right) = \frac{2\Phi}{y_o(\bar{\gamma}-1)}\left[1 - \left(\frac{y_o}{y}\right)^{(\bar{\gamma}-1)/2}\right] \tag{28}$$

and introducing a new reduced time

$$\tau = \frac{t}{t_o};\ \frac{1}{t_o} = \alpha = \frac{2\Phi}{(\bar{\gamma}-1)y_o} = \frac{2\Phi}{(\bar{\gamma}-1)(1-\Delta/\rho_p)}\ \frac{A}{U_o} \tag{29}$$

We get

$$\frac{d}{d\tau}\left(\frac{y}{y_o}\right)^{(\bar{\gamma}-1)/2} \tag{30}$$

which states that the velocity-time curves are the same for all guns if described in the reduced time scale τ.

V. REDUCED EQUATIONS OF HEAT CONDUCTION

It is assumed that the thermal constants can be considered as independent of temperature that is, suitable average values for the temperature range in question can be introduced. The equation for heat conduction is

$$\frac{\partial\theta}{\partial t} = \frac{k}{C\rho_s}\ \frac{\partial^2\theta}{\partial Z^2} \tag{31}$$

The boundary conditions are

$$h(\theta_g-\theta_s) + k\frac{\partial\theta}{\partial Z} = 0 \,. \tag{32}$$

The heat transfer coefficient h contains the friction factor λ as a multiplying constant, $h = \lambda h_o$.

If we consider a gun with definite ballistics, then h_o and θ_g are universal functions of the reduced time variable

$$\tau = \alpha t; \quad \alpha = \frac{1}{t_o} = \frac{2\Phi}{(\gamma-1)y_o} \tag{33}$$

In order to make use of this correspondence, a new system of time and space variables is introduced

$$\tau = \alpha t, \text{ and } \zeta = \beta_z \tag{34}$$

where the relation of β to α is given by

$$\frac{k}{C\rho_s} \quad \frac{\beta^2}{\alpha} = \frac{1}{2} \text{ or } \beta = \left(\frac{C\rho_s\,\alpha}{2k}\right)^{1/2} \,. \tag{34a}$$

Then equations (31), (32), and (33) become

$$\frac{\partial\theta}{\partial\tau} = \frac{1}{2}\,\frac{\partial^2\theta}{\partial\zeta^2} \tag{35}$$

$$H(\theta_g-\theta_s) + \frac{\partial\theta}{\partial\zeta} = 0 \tag{36}$$

$$\theta\;(0_1\;\zeta) = 0 \tag{37}$$

Where the reduced heat transfer coefficient $\underline{H}$ is

$$\underline{H} = \frac{h}{(\alpha k C\rho_s/2)^{1/2}} = \frac{\lambda h o}{(\alpha k C\rho_s/2)^{1/2}} \tag{38}$$

These equations are now universal except for the multiplying factor $\frac{\lambda}{(\alpha k C\rho_s/2)}$. This factor contains the friction factor, the thermal constants of the steel, and the time parameter α which is determined by the size of the gun. All combinations that give the same h_o factor will give the same temperatures in the $\tau\zeta$ system and the results in the actual xt system can then be obtained by simple conversion of scales.

Now we can collect the complete formula for the heat transfer coefficient as functions of the reduced travel variable y, which in turn is a universal function of τ. The assumption of equal distribution of unburned propellant is used.

By use of equations (5), (13), (18), (20), and (33) we find

$$H(\tau) = \frac{x_s}{x_o} \; Lf(\tau) \tag{39}$$

where

$$L = \frac{\lambda C_p}{(2kC\rho_s)} \left(\frac{C}{A}\right)^{1/2} \frac{1}{E}\left(\frac{\Delta}{1 - \Delta/\rho_p} \; \frac{2\Phi}{\gamma - 1}\right)^{1/2} \tag{40}$$

$$f(\tau) = \frac{\left[1 - \left(\frac{y_o}{y}\right)^{2-1/2s}\right]}{\left(1 - \frac{\Delta}{\rho_p}\right)\left[\frac{y}{y_o} + \frac{\Delta/\rho_p}{E(1-\Delta/\rho_p)}\left\{1 - \left(\frac{y_o}{y}\right)^{2-1/2s}\right\}\right]\left[\left(\frac{y}{y_o}\right) + \frac{\Delta/\rho_p}{1-\Delta/\rho_p}\right]} \tag{41}$$

before burning.

$$f(\tau) = \frac{E^{3/2}\left[1 - \frac{1}{1-E}\left(\frac{y_o}{y}\right)^{(\bar{\gamma}-1)}\right]^{1/2}}{\left(1 - \Delta/\rho_p\right)\left(\frac{y}{y_o} + \frac{\Delta/\rho_p}{1-\Delta/\rho_p}\right)^{s}} \quad \text{after burning.} \tag{42}$$

Using equation (25) and 26) we find that the gas temperature θ_g referred to an initial temperature of the gun of 300°K is given by

$$\theta_g + 300 = T_o\left(\frac{y_o}{y}\right)^{1/2\,(\bar{\gamma}-1)} \quad \text{before burning,} \tag{43}$$

$$\theta_g + 300 = T_o\left(\frac{y_b}{y_o}\right)^{1/2(\bar{\gamma}-1)}\left(\frac{y_o}{y}\right)^{\gamma-1} = T_o\,\frac{1}{1-E}\left(\frac{y_o}{y}\right)^{\bar{\gamma}-1} \tag{44}$$

after burning.

In $H(\tau)$ the factor x_s/x_o gives the location along the bore for which the gun temperature is to be determined. The factor $\underline{L}$ which is called the heating parameter depends on the friction factor, thermal conductivity of the gun steel and ballistic constant of the gun. The absolute size of the gun is contained only in the factor $(C/\Lambda)^{1/2}$, while the other quantities -- Δ, Φ, E, have the character of specific quantities. The integration must start at the value of τ corresponding to the selected x_s (where $y_s = x_s - C/A\rho_p$) and must continue using equation (43) and (44) until the time when the projectile is at the muzzle or to point y_m.

VI. THE CHOICE OF THE FRICTION FACTOR λ

The friction factor is theoretically calculable only for certain relatively simple cases of steady state fluid flow over smooth walls. It is obvious that these conditions do not hold in guns. However, as a first approximation, formulas based on analogy to those obtained for steady flow over smooth walls are used by NSN.

NSN used two methods for determination of a value of λ. The first used the equation[1]

$$\lambda = (3.46 + 4 \log_{10} ro/r)^{-2} \tag{45}$$

where thickness of the boundary layer δ for smooth surfaces is replaced by a roughness parameter r that measures the size of the irregularities. The exact meaning of r is not well defined, since the friction factor will depend also on the form and arrangement of the irregularities. NSN made an attempt to determine a value of λ for guns from data on the erosion of nozzles. They were not successful in making a satisfactory transition.

The other method NSN used was to determine values of λ from heat transfer data obtained from .50 caliber machine gun firings. A value of λ was computed from heat input data obtained at the origin of rifling. The values vary widely because of imperfection in the insulation of the calorimetric ring used. Corrections were made for these variations, but values of λ still varied from round to round.

Workers at Catholic University[2] applied the NSN theory to the calculation of the friction factor for several guns of different calibers. They used heat input data derived from bore surface and embedded barrel thermocouples. The results indicate that λ varies over the bore surface from breech to muzzle and varies for different guns of the same caliber.

It is the opinion of the writers that λ is a "fudge" factor that must be experimentally determined for the gun in question, and at the desired locations along the gun barrel. A discussion of our technique for determining λ from heat input data is contained in the body of this report.

VII . THE CALCULATION OF THE HEAT INPUT. Q

If one has knowledge of the temperature distribution in a gun barrel, he also can determine the total heat input Q to that section of the bore surface by either one of the following formulas:

$$Q = \int_0^t h(\theta_g - \theta_s)\, dt \tag{46}$$

$$Q = \int_0^\infty C\rho_s \theta(Z\tau) dZ \; . \tag{47}$$

Since equation (47) is the easier of the two to integrate, no further use of (46) will be made.

Equation (47) can be expressed in the form

$$Q(\tau) = C\rho_s \int_0^\infty \theta(Z\tau)dZ = C\rho_s(2k/C\rho_s\gamma)^{1/2} \int_0^\infty \theta(\zeta\tau)d\zeta$$

$$= 2kC\rho_s/\gamma I(\tau, y_s/y_o) \; . \tag{48}$$

The integral $I(\tau, y_s/y_o)$ should be evaluated at a time when any later heat transfer can be neglected. When this is done a value of all of the heat transferred from the propellant gases to the gun is obtained. These integrals are not sensitive to changes in the length of the gun, y_m/y_o, and they can be evaluated for all guns.

REFERENCES

1. Goldstein, S. Modern Developments in Fluid Dynamics, Oxford University Press (1938) p. 338

2. Boyd, Marjorie; Dooling, J.S.; Griffing, Virginia and Maller, R. Heating and Erosion Studies in Guns, A Final Report on Task 3, Contract NOord 10,260, Chemistry Department, The Catholic University of America, Washington, D.C. pp. 18-103.

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