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LIQUID PROPELLANT EXPLOSIVE HAZARDS

Final Report - December 1968

VOLUME 1 - TECHNICAL DOCUMENTARY REPORT

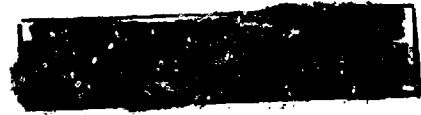


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UNITED STATES AIR FORCE
EDWARDS, CALIFORNIA

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LIQUID PROPELLANT EXPLOSIVE HAZARDS

Final Report - December 1968

VOLUME 1 - TECHNICAL DOCUMENTARY REPORT

by

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This report has been reviewed and approved.



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ABSTRACT

Project PYRO consisted of a comprehensive program to determine the blast and thermal characteristics of the three liquid propellant combinations in most common use in military missiles and space vehicles; liquid oxygen-RP-1 ($\text{LO}_2/\text{RP-1}$), liquid oxygen-liquid hydrogen (LO_2/LH_2), and nitrogen tetroxide/50% unsymmetrical dimethylhydrazine-50% hydrazine ($\text{N}_2\text{O}_4/50\% \text{UDMH}-50\% \text{N}_2\text{H}_4$). During the course of the program some 270 tests were conducted with these propellant combinations on weight scales ranging from 200 lb to 100,000 lb. This basic explosive test program was supplemented by analytical and statistical studies, laboratory-scale experimental studies, simulation tests with inert propellant combinations and a series of high-explosive tests for calibration and evaluation purposes.

The basic test program was designed to investigate the explosive characteristics of the three propellant combinations for the most credible ways that the propellants might accidentally come into contact with each other and result in a significant explosion.

The results of the basic test program in conjunction with the analytical studies and prior information regarding liquid propellant explosive behavior were used as the basis for developing methods for predicting the blast and thermal environment that would be expected for any given missile or space vehicle system and any specified failure mode.

In the prediction method the thermal environment is given only as a function of propellant type, while the blast environment is given as a function of a number of controlling parameters. A failure mode analysis is required to select the appropriate values of the parameters needed to predict the blast environment for a specific system.

FOREWORD

This report was prepared by URS Systems Corporation, Burlingame, California, under Air Force Contract AF 04(611)-10739, which supported the NASA/USAF Liquid Propellant Blast Hazards Program (Project PYRO) conducted at the Air Force Rocket Propulsion Laboratory, Edwards Air Force Base, California. The URS support included establishing the overall design of the program; analyzing the test data; developing mathematical models relating the pertinent blast characteristics to the missile failure conditions; conducting exploratory studies; designing and constructing test articles; providing statistical, ordnance, and instrumentation consulting; and the writing of the technical reports.

The report is in three volumes: Volume 1, which is the Technical Documentary Report on the basic program; Volume 2, which contains the tabular blast, thermal and fragmentation data obtained during the program; and Volume 3, which presents prediction methods and recommendations for use of the PYRO data in predicting the explosive potential of full-scale missile failures.

ACKNOWLEDGEMENTS

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Our particular thanks are extended to the past and present members of the Steering Committee for their dedication in providing overall technical guidance to the program. These members were:

Mr. W. A. Riehl, Present Chairman, NASA, Marshall Space Flight Center, Alabama

Mr. H. C. Dyer, NASA, Marshall Space Flight Center, Alabama

Mr. John Atkins, NASA, Kennedy Space Center, Florida

Mr. L. Ullian, USAF, Patrick Air Force Base, Florida

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Dr. J. B. Gayle, Past Chairman, NASA, Kennedy Space Center, Florida

Mr. P. King, Past Member, General Electric Co., Mississippi Test Facility

Our sincere thanks go to personnel at EAFB, Edwards, California, for their fine administrative and technical contributions to the project. Noted for their participation were:

Robert Thomas, Current Test
Program Director

John Marshall, Past Test Program
Director

Capt. Karl W. Joffs

Charles Cooke

Austin A. Dickinson

James Wanchek

Richard McQuown

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The authors also wish to gratefully acknowledge the technical contributions made to the program by the following URS staff members and consultants:

Mr. T. C. Goodale, Principal Chemist

Mr. J. V. Zaccor, Principal Physicist

Mr. J. Boyes, Test Engineer

Mr. Allen Saltzman, Technical Editor

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Section 1

INTRODUCTION AND BACKGROUND

Section 1
INTRODUCTION AND BACKGROUND

In the early 1950's there was relatively little need for accuracy in predicting the yield potential from liquid propellants used in missiles and aerospace vehicles. A conservative approach could be used to define the yield because the relatively small amounts of propellants involved did not require that total safety distances be very great. Launch vehicles contained no more than 100,000 to 300,000 lb of propellants, so that yield percentages up to 50% or greater resulted in clearance distances no greater than 2,000 to 3,000 ft. It was a common practice at that time to treat the explosive behavior of liquid propellants in the same fashion as that of standard solid high explosives. A given propellant combination was considered to have a fixed yield value and the same distribution of blast wave characteristics with distance as TNT. Thus the explosive potential could be represented by a single yield number relative to TNT.

It was recognized that this was only a crude approximation, since unlike normal high explosives, liquid propellants are not premixed and the explosive characteristics can vary widely depending on the degree of mixing, which in turn, depends on factors such as tank configuration, specific failure mode, and time of ignition. However, there was insufficient information available concerning the effect of these factors on explosive yield to provide the basis for deriving a better prediction method.

Until 1960 the quantities of propellants of interest remained relatively small, so that use of conservative and fixed yield values was not too much of a problem. Within the last few years, however, tremendous increases have occurred in vehicle propellant weights. Saturn-V contains almost 6 million lb of liquid propellants. These order-of-magnitude increases in propellant weights with their associated increases in required clearance distances no longer permit the luxury of ultraconservatism in determining propellant yields. Clearance distances for pre-launch, launch, and post-launch safety have become

critical because of their effects on other programs located nearby. Personnel must be evacuated or given adequate protection. Launch vehicles and complexes must be located at distances great enough to provide low hit probabilities in case destruct action is required. All of these requirements add to costs in time delays, required real estate, precount and countdown interference, and facilities. These increased costs dictated the need for development of more precise means of determining realistic and most probable yields in case of pre-launch and launch aborts.

In addition to siting and general operating criteria, the ability to predict propellant explosive characteristics is required to define hazard envelopes. These requirements generated the need for more detailed data concerning the blast and thermal environments resulting from propellant explosions, e.g., peak overpressure, positive-phase impulse and duration, fireball temperature and duration, and heat flux-time histories.

The strong requirement for an improved and more realistic method for predicting the environment from liquid propellant explosions and the lack of information on which to base such an improved method led the Air Force Eastern Test Range and the NASA, who jointly had the prime concern for launching missile systems and space vehicles, to look to a new test program to better define the problem. This resulted in Project PYRO, a combined experimental and analytical program to define the propellant blast environment for the three common propellant combinations, liquid oxygen-RP-1, liquid oxygen-liquid hydrogen, and nitrogen tetroxide/50% unsymmetrical dimethylhydrazine-50% hydrazine. The end result of the program is a method for predicting the blast and thermal environment that would be expected for any given missile or space vehicle for a wide range of failure modes.

This program was under the overall direction and sponsorship of the PYRO Steering Committee, which consisted of representatives from NASA Marshall Space Flight Center, NASA Kennedy Space Center, the Air Force Eastern Test range, and the Sandia Corporation.

This is the final report of this program and consists of three volumes: Volume 1, the comprehensive technical report on the basic program; Volume 2, Test Data; and Volume 3, Prediction Methods. Volume 1 (herein) contains an introduction, objectives, and general approach as the first three sections. These are followed by the presentations of the hypergolic and cryogenic test program, including test design, test conditions, results, and discussion of results in Sections 4 and 5, respectively. These sections also show the derivation of the methods for predicting blast yields. The results of the thermal program, laboratory supporting programs, and a summary of the findings and conclusions are given in Sections 6, 7, and 8.

A glossary and appendices follow, including descriptions of the blast instrumentation system; the statistical procedures used in the analysis of the blast data; and the thermal instrumentation system. The final appendix lists a number of "ride along" R&D programs, which although not a part of Project PYRO, used the blast and thermal environment provided by the liquid propellant explosions, particularly on the large-scale tests, for a variety of experiments, including structural response, ground shock measurement, cloud tracking, and evaluation of instrumentation.



Section 2

OBJECTIVES

Section 2
OBJECTIVES

The overall objective of this program was to establish data and methods for predicting the credible damage potential which would be experienced from the accidental explosion of the liquid propellant combinations $N_2O_4/50\%$ UDMH - 50% N_2H_4 , $LO_2/$ RP-1, and LO_2/LH_2 .

Based on experimentally derived data, analytical and statistical work, and laboratory studies, the end product would be a method for predicting the surface blast and thermal environment that would be expected for any given missile or space vehicle system for a wide range of failure modes.



Section 3

GENERAL APPROACH

Section 3
GENERAL APPROACH

This section of the report describes the general approach used in the selection of the test conditions for the PYRO program. It starts with a discussion of the fundamental differences expected in explosive behavior of hypergolic and cryogenic propellants and why these led to somewhat different approaches being used for the two classes of propellants. This is followed by a description of the general approach used for the hypergolic propellants. The last and major portion of this section covers in detail the rationale for the selection of the test conditions and parameter variations for the cryogenic propellant combinations.

The information available at the time the basic test design philosophy was being established indicated that hypergolic propellants tend to have significantly different explosive characteristics from cryogenic propellants. For cryogenic propellants, scaled model tests had indicated that significant explosive yields were possible for a large variety of missile and failure conditions (Refs. 3-1 to 3-6). Thus, it was clear that a comprehensive testing program would be necessary to evaluate the effect of the various important parameters of the process. It was less clear, however, that a comprehensive test program would be required for the hypergolic propellant combination since these propellants ignite on contact, and within a few milliseconds or tens of milliseconds (at ambient temperature), the reaction has proceeded far enough to produce visible flames. This spontaneous ignition behavior tends to limit the explosive potential of hypergolic propellants. In order for a propellant mixture to produce a large explosive yield, it is necessary for one of the propellants to be in a state of fine subdivision or solution and dispersed uniformly throughout the volume of the other propellant at the time of ignition. This permits the majority of the available reaction energy to be released fast enough to contribute to explosive effects. With hypergolic propellants, reaction starts on

initial contact and continues throughout the mixing process. Since the time involved in mixing the two propellant masses together to create the state of subdivision of one in the other required for explosive effects is usually long compared with the time interval required for their reaction to proceed to its completion spontaneously, it is difficult to get significant explosive effects from hypergolic propellants. Test results by other investigations (Refs. 3-7 to 3-9) tended to confirm this.

For this reason, the philosophy used for the testing of the $N_2O_4/50\%$ UDMH- $50\% N_2H_4$ propellant combination was to concentrate the initial testing on certain limiting conditions which tend to maximize the explosive yield. If under these conditions no significant yields were obtained, further testing with other failure conditions could be minimized.

From the foregoing it is evident that the most serious mixing conditions (with regard to explosive effects) for hypergolic propellants are those which:

1. Lead to extremely rapid mixing, or
2. Lead to confinement of the reacting products. (In this case, although the reaction itself need not be explosive, confinement of the reaction can build up pressures which can create significant blast effects if suddenly released.)

From a practical point of view, the specific failure modes which would lead to these conditions are high-velocity impact or an explosive-donor situation, which might occur if both cryogenic and hypergolic propellants are used in the same vehicle.

Accordingly, in the initial test program, the major effort was placed on these two failure modes. Two other conditions were selected for minor testing, command-destruct and confinement-by-the missile (the rapid removal of a diaphragm between the compartments containing the fuel and oxidizer). A 100-ft tower drop series was added to the program later.

This relatively simple upper limit approach was not practical for the cryogenic propellant combinations because of the anticipated large

number of conditions under which significant yields would be obtained and the corresponding large number of potentially important variables.

The basic objective in the cryogenic test program was to test with a limited number of generalized test configurations selected so that the results from the tests would be generally applicable to any tankage configuration and failure mode combination. The selection of the test configurations was based on the generalized concept of the missile failure and explosive process shown in Fig. 3-1 and described below.

Five basic variables of the initial configuration were considered of prime concern with regard to the ultimate explosive yield. These were the propellant type, the tank configuration, the failure mode, the launch-pad geometry, and the ignition-source configuration. The tank configuration, failure mode, and the launch-pad geometry, in turn, largely determine the gross space-time history of the propellants, although the propellant type may also play an important role. The gross space-time history of the propellants, controlled by the boundary and initial conditions, determines how the two propellants come into contact with each other and thereby determines the explosive effects as a function of time. The time of ignition depends on the ignition source configuration, the space time history of the propellants and the propellant type.

The design approach used, which was termed the Generalized Propellant Interaction Approach, was based on the assumption that although the detailed course of propellant mixing may be too complicated to admit to detailed analysis, propellants undergoing similar mixing conditions will lead to mixtures having similar explosive properties. In other words, any time two propellant masses interact or come into contact with each other in the same fashion (and with the same constraints), it was assumed that the explosive yield as a function of time after initial contact will be the same. Further, it was assumed that all propellant spillage configurations can be analyzed in terms of a few basic types of interactions, called basic mixing modes.

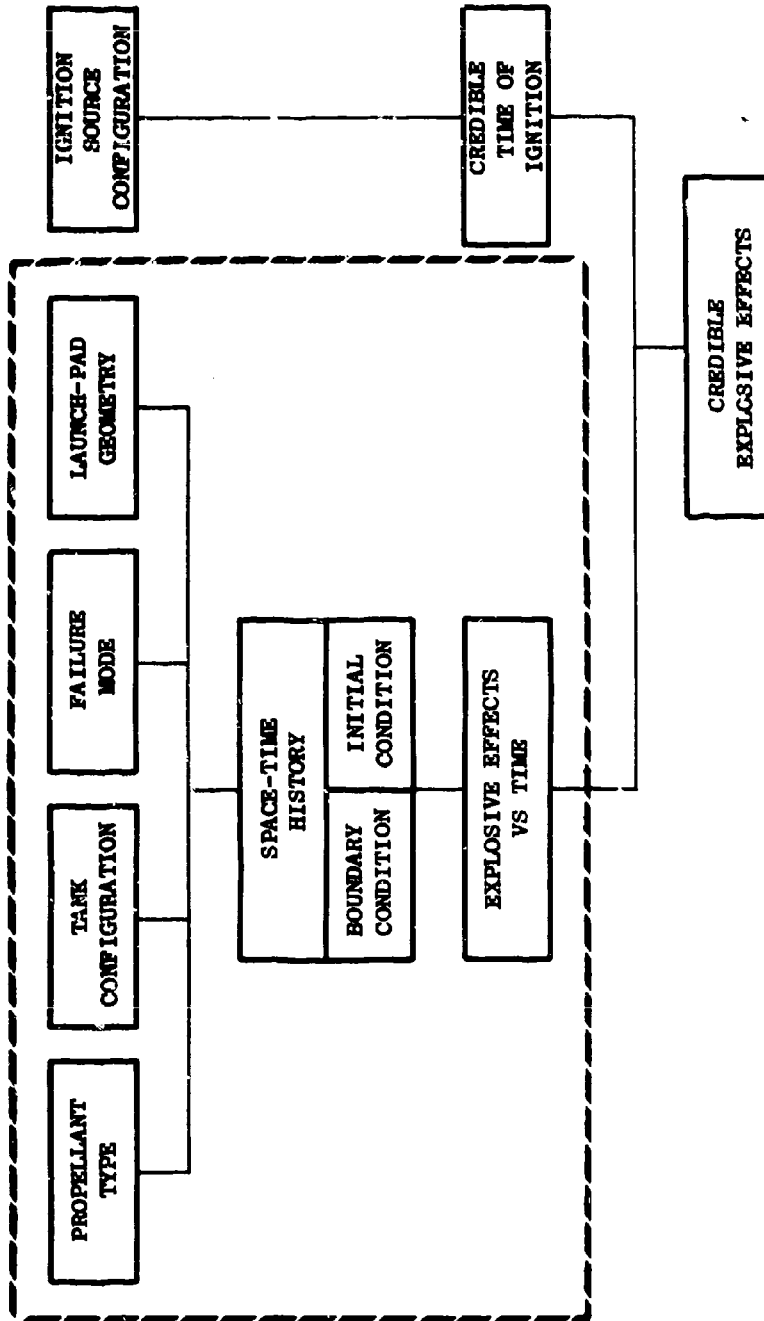


Fig. 3-1. Generalized Concept of the Missile Failure and Explosive Process

The objectives of the explosive testing were, first, to determine which parameters of potential interest in the interaction of two propellant masses have a significant influence on the explosive yield time relationship and, second, to determine the quantitative nature of the influence.

The main tasks in the overall design of the test program were to identify the parameters of potential interest, to establish the number of levels of each parameter for testing, and insofar as possible, to determine how best to scale the parameters for testing.

Since the basic mixing modes are really just convenient groupings of interaction types controlled by the same general parameters, it was appropriate to give general consideration to all the parameters of potential interest in the interaction of two propellant masses prior to selecting the basic mixing modes. For this, it was convenient to divide the parameters into the following general classes:

1. Specific properties of propellants
2. Initial conditions of propellants
3. Boundary conditions (nature of environment)
4. Ignition conditions

Although many of the specific properties of the propellants, such as density, viscosity, heat capacity, conductivity, boiling and freezing points, and heats of fusion and vaporization, were expected to be important in the mixing process, they were not subject to variation except in large discrete steps (that occur when propellant type changes), i.e., they were fixed once the propellant type and initial conditions were specified. Accordingly, the only parameter in Class 1 that entered directly into the test program design was the propellant type.

The initial conditions, i.e., the conditions at the time of first contact of one propellant with the other, consist of:

1. State of propellants
2. Spatial distribution of propellants
3. Velocity distribution of propellants

The boundary conditions consist of:

1. Spatial distribution of surfaces that control the flow of propellants during mixing
2. Characteristics of the air and surfaces through and along which the propellants fall or flow during mixing

The ignition conditions consist of:

1. Time of ignition (from first contact of propellants)
2. Nature of ignition source

Each class of conditions is discussed in the following, leading to the selection of the variables investigated.

INITIAL CONDITIONS

State of Propellants

In theory, the propellants could be in any of three states (gas, liquid, solid), or a mixture thereof. They also could be mixed with ambient gas (assumed to be air) in any proportion. In practice, however, for any given propellant, only certain states appeared reasonable.

These are listed below:

1. RP-1: liquid state, negligible mixing with ambient air
2. LO₂: liquid state, negligible mixing with ambient air (Although LO₂ boils rapidly in ambient air or on contact with surfaces, primary concern is with large quantities and with massive spills, so that the fraction of LO₂ vaporized at the time of first contact is assumed negligible.)
3. LH₂: (a) liquid state, negligible mixing with ambient air, (b) gaseous state, mixed with ambient air, or (c) combination of (a) and (b)

Because of the low density of the gaseous H_2 (GH_2), it is not likely to react significantly with LO_2 but only with the gaseous oxygen of the air or that produced by vaporization of LO_2 . Thus, the mixing process for GH_2 , is entirely different from that for LO_2 /RP-1 or LO_2 / LH_2 . Sufficient effort was not available during this program to conduct an investigation into GH_2 /air mixture, however, some work has been done in this area by other investigators.

Spatial Distribution

With the foregoing assumptions, it would appear possible to describe the initial distribution of LO_2 , LH_2 , and RP-1 in terms of (1) the total weight (W), (2) the shape (with one or more variables), and (3) the position of the mass (with one or more variables). There were innumerable ways of characterizing shape; however, considering the generalized nature of the basic approach, it appeared adequate to use a single variable, L/D, which specifies the ratio of the average height (or length), L, of the propellant mass to a characteristic dimension, D, of a cross section normal to the height. In essence, this was equivalent to approximating all shapes by cylinders. Because gravitational potential varies with height, greater emphasis was given to it than to the other dimensions.

Since the initial conditions are defined as those which exist when the propellant masses first contact each other, and since the shape and weight are already specified, the only position variable necessary is orientation, i.e., which propellant is on top. This assumes that primary concern is with initial interfaces that are horizontal and, furthermore, that the cylinders are concentric. Considering initial vertical interfaces to be of secondary importance does not seem unreasonable, because it was unlikely that they could be very large and they would very likely be unstable (not persisting very long).* The justification for assuming concentricity is that this was a credible condition which tended to maximize mixing.

* This point is discussed in more detail later.

Velocity Distribution

Although it is possible to conceive of a wide variety of velocity distributions at the time of initial contact, when consideration is given to the mechanisms by which the propellant masses achieve their velocity (primarily, gravitational or pressure forces), it did not seem unreasonable to assume that the entire mass of one propellant will have the same net translational velocity and that the rotational velocity will be small compared to it.

BOUNDARY CONDITIONS

Space Distribution of Rigid Surfaces

The major types of rigid surfaces in the vicinity of launch sites that can modify the flow of propellants during the mixing process are:

1. The vehicle itself
2. The basic launch-site geometry
3. Miscellaneous close-in launch facilities and structures, such as service towers, cribs, and flame deflectors.

In general, the sizes of surfaces in Item 3 tend to be much smaller than those in 1 or 2, so that their effects were not included in the basic program. Obviously, there are some exceptions. Perhaps most important is the silo case, where the crib, platforms, and miscellaneous equipment may significantly modify the flow. It is believed that these surfaces were of too special a nature to be included as a generalized boundary condition and that estimation of the blast effects from these special cases would have to be made by considering various portions of the flow separately (so that it would be possible to use a generalized boundary condition for each), and combining results to get the overall effect.

The vehicle itself would affect the flow of fluid during the mixing process in various ways, perhaps the most important of which occurs when,

due to internal failure, mixing starts inside the tank walls. Once the propellants are outside the vehicle tankage, its role would tend to be secondary, compared with the ground surface, in modifying the flow.*

For a flat-pad geometry, the ground surface is expected to be generally flat or, at most, have small curvature since it is unlikely to have large slopes over large areas.

Accordingly, we can conceive of two basic types of boundary conditions which depend on the extent of the rigid walls:

1. Confinement by the missile
2. Confinement by the ground surface

Actually, however, within the framework described above, there are two other cases which were not included in the basic program. These are confinement by the ground surface and vertical walls and no confinement, i.e., where mixing occurs in the absence of any confining surface.

The confinement-by-the-ground-surface-and-vertical-wall case, which would most typically be the silo geometry was originally planned to be included in the program but with less emphasis than the other cases. However, as the program proceeded and the full extent of the testing needed for the other cases became evident this case was essentially eliminated from the program because it was of much less practical interest than the others. The $\text{LO}_2/\text{RP-1}$ propellant combination in a silo geometry had been studied for propellant weights up to 200-lb in previous URS programs (Refs. 3-3 and 3-5). Information from these programs combined with the few tests with the LO_2/LH_2 propellant combination which were left in this program would provide at least a minimum data base for this geometry.

* How the vehicle fails is very important in determining the space-time history of the propellants; however, these effects were considered under initial rather than boundary conditions.

The no-confinement case was not included because of the very small explosive yields anticipated for this condition. Unless there is a large velocity difference between the two propellants (which is unlikely when both are in free air), there are no significant forces holding the two masses together. Thus, even a small pressure generated by vaporization of a small amount of one of the propellants at the boundary between the two masses would be sufficient to separate them and minimize mixing. Confirmation of this concept has been obtained by experimental testing of propellant quantities up to 250 lb (Ref. 3-4).

SUMMARY OF PARAMETERS

A summary of important parameters is presented in Table 3-1. Examination of this list suggests that the basic mixing modes are identified primarily by the boundary conditions, although within each of the latter further subdivision was necessary, particularly with regard to flow direction. Using a breakdown by boundary conditions, it was possible to determine which initial-condition parameters, and ranges in value, were appropriate for each.

BASIC MIXING MODES

Confinement by the Missile

In this case an internal failure is assumed to occur, and one propellant falls down into the other. This failure condition could occur in either the static test stand, launch, or in-flight mode. Cause of failure could be bulkhead rupture from overpressurization of a propellant compartment, engine blowup sending fragments through the bulkheads, etc. The position and velocity distributions of the lower propellant at the moment of first contact are fairly well specified, since it is assumed to be in its original configuration and to have zero velocity. Those of the upper propellant, however, may have a large range of values, depending on how full the tanks are and how large an opening is created between them. If a relatively small opening is produced, the L/D ratio of the top propellant will be

Table 3-1
SUMMARY OF PARAMETERS

Propellant Properties

P.T. (propellant type)

Initial Conditions

W (weight)

L/D (length-to-diameter ratio)

P.O. (propellant orientation)

V_H (velocity, horizontal)

V_V (velocity, vertical)

Boundary Conditions

Confinement by the missile (CBM)

Confinement by the ground surface (CBGS)

Ignition Conditions

T (time)

I.T. (type)

effectively very much greater than that of the bottom one, and its total weight (entering into the mixing at an early enough stage to be a matter of concern) will be much smaller than that for the bottom one. In addition, the top propellant will have an initial velocity given by the fluid head in the top tank and the pressure differential between the two tanks. As the opening between the tanks becomes larger, the L/D ratio and effective weight of the upper propellant more nearly approaches the values they originally had in the missile. In addition, at the time the opening between the tanks is the full cross section of the original tank, the velocity would reach the value given by the acceleration of gravity through the distance of the ullage space in the lower tank, provided there was no pressure difference between the two tanks.

In the case where the opening between the tanks is significantly less than the full cross section of the vehicle, the different initial L/D values for each propellant can be treated by assuming that the L/D ratio refers to the over-all vehicle geometry and by defining a D_o/D_t ratio, where D_o is the opening diameter and D_t the vehicle diameter. A sketch of the CBM test configuration is presented in Fig. 3-2.

Duration of the confinement by the missile case is limited to the time that the propellants remain confined by the walls of the vehicle. This time is determined by the strength of the tankage, the rate of vaporization of the cryogenic materials, the initial pressure in the tanks, and the initial ullage space.

The parameters considered to be of primary interest for this case and the initial ranges in values were as follows:

- Propellant type: two cases: $LO_2/ RP-1$ and LO_2/ LH_2
- Propellant weight: three values: 200-lb, 1,000-lb and 25,000-lb
- L/D ratio: two values: 5 and 1.8 (selected to span the range of credible missile geometries)
- D_o/D_t ratio: two values: 1 and 0.45
- Time of ignition: minimum of three values
- Type of ignition: two cases: detonator and squib

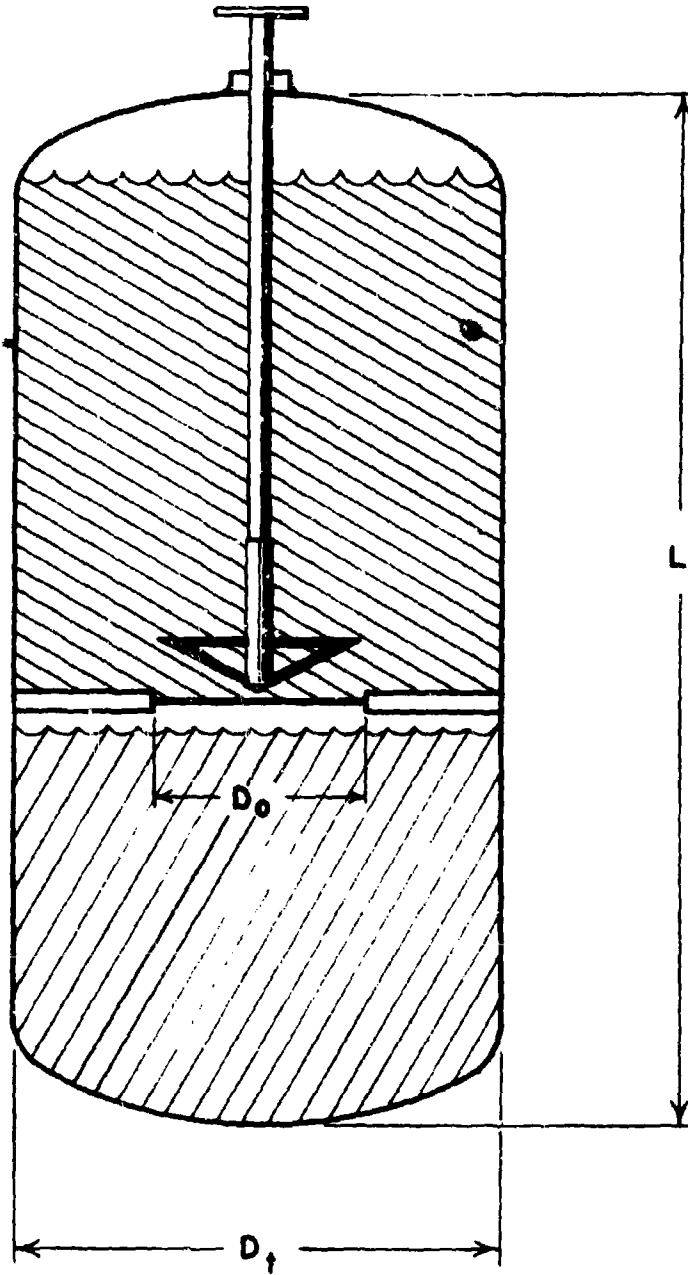


Fig. 3-2. Confinement-by-the-Missile Test Configuration

Confinement by the Ground Surface

A relatively large range in initial conditions was appropriate for this boundary condition because of the large variety of credible failure modes. For example, this condition could occur in static test stand operations from overpressurizing the tanks, where the tank could be split open, and the propellants pour out on the test stand or ground surface. It could also occur at the launch pad both by the overpressurization of tanks described above or by the fallback or topple-over of the entire missile. Probably the most severe failure mode under this category would be fallback from a high altitude or powered flight impact at a high velocity. Certain of the parameters selected for study were quite similar to those for the previous case, although specific values were not identical. These include: the propellant type, weight, and orientation and the time of ignition.

The major problem for this case was in establishing the appropriate L/D and velocity conditions. This was complicated because, theoretically, each of the two propellant masses may have a different L/D and magnitude and direction of flow. Thus, there really were six parameters, and if each were permitted to take on two values, 64 different combinations would be obtained. Fortunately, certain of these combinations seemed so unlikely they could be neglected. Considering first each parameter separately, the following conclusions were reached:

- The reasonable L/D conditions were that both propellant masses may have high or low ratios or that the bottom propellant mass may have a low and the top one a high ratio. The converse of the latter condition was not assumed reasonable.
- The reasonable flow direction conditions were for both propellants to be moving either vertically or horizontally or for the top propellant to be moving vertically and the bottom propellant horizontally. The converse of the latter conditions was not assumed reasonable.
- The reasonable flow magnitude conditions were for both propellants to have either high or low velocity or for the top propellant to have a high and the bottom propellant to have low velocity. The converse of the latter condition was not assumed reasonable.

Considering next the interaction between parameters, divided into the three flow direction cases, the following conclusions were reached:

When both propellants were vertical:

Although all three flow-magnitude cases were credible, it is unlikely that the velocity of the top propellant would exceed that of the bottom by more than a factor of about two, so that it seemed reasonable to drop the case where the propellants had differing velocities.

Although all three L/D cases were possible, the credible ratios were all likely to be equal to or greater than typical missile values and the differences in ratios between the two propellants did not seem likely to be large. It appeared reasonable, therefore, to drop the case where the propellants had differing L/D ratios.

When both propellants were horizontal:

Only the relative velocity between propellants seemed important, so that two cases should suffice: the top propellant with a velocity high or low relative to the bottom propellant.

All three L/D cases are credible, except that it seemed possible to cover the cases of interest with only two values of L/D, since it is likely that all L/D ratios will be much less than typical missile values.

For both propellants flowing horizontally, the earlier assumptions that the initial interface between the two propellants was horizontal and that the propellant masses have concentric axes actually did not appear to correspond very closely to real situations. The most likely way to have both propellant masses flowing horizontally at the time of the first contact was for them to impact the ground separately and to flow into one another. This results in nonconcentric propellant masses, and at the moment of first contact, the interface was likely to be primarily vertical. However, this initial interface area would be quite small and because of density differences, it was anticipated that as the pools overlap, the interface would be primarily horizontal.

In spite of these differences, however, it was believed that the major features of this case were adequately simulated by the original assumptions of only horizontal interfaces and of concentric masses.

By using concentric masses, radial symmetry could be preserved, which simplified determination of the gross overlap area as a function of time and which, perhaps even more important, simplified interpretation of the resulting explosive phenomena.

In summary, the parameters considered of primary interest for the "confinement-by-the-ground-surface" case, and the initial ranges in values were as follows:

Basic Program: Vertical Flow Direction Case

Propellant type: two cases: $LO_2/RP-1$ and LO_2/LH_2

Propellant weight: 200, 1,000 and 25,000 lb

L/D ratio: two values 5 and 1.8 (selected to span the range of credible missile geometries)

Propellant orientation: two cases

Velocity: four values (~15, ~44, ~100, ~600 ft/sec)

Time of ignition: three values

Basic Program: Horizontal Direction Case

Propellant type: two cases: $LO_2/RP-1$ and LO_2/LH_2

Propellant weight: 200 lb

L/D ratio: two values (top propellant fixed at a typical in-tank value, bottom propellant variable)

Propellant orientation: one case: $LO_2/RP-1$, LH_2/LO_2

Velocity: two values (bottom propellant as near zero as practical, top propellant variable)

Time of ignition: three values

TEST PROGRAM MODIFICATIONS

Continually throughout the test program minor modifications were made to the outlined test series and a number of additional tests were added to investigate

in more detail those parameters showing a large effect on explosive yield. Type of ignition, for example, was dropped as a significant parameter early in the testing because it did not have a significant effect on yield, while much greater emphasis had to be given to time of ignition to properly evaluate its effect on yield. The full scope of the test program as actually conducted is described in Sections 4 and 5.

Section 3

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- 3-7 Pesante, R. E., R. D. Erickson, D. G. Frutchery, and W. J. Helm, Blast and Fireball Comparison of Cryogenic and Hypergolic Propellants with Simulated Tankage, Report No. 0822-01(02)FP, Aerojet-General Corporation for NASA, June 1964
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Section 4

HYPERGOLIC PROPELLANT PROGRAM

Section 4
HYPERGOLIC PROPELLANT PROGRAM

This section of the report summarizes the existing information concerning the explosive potential of the hypergolic propellant combination nitrogen tetroxide/50% unsymmetrical dimethylhydrazine - 50% hydrazine ($N_2O_4/50\% UDMH - 50\% N_2H_4$). The summary is divided into three parts. Part 1 describes the tests conducted under the PYRO Program, Part 2 presents the information available from other sources, and Part 3 contains a summary of all the data and the conclusions.

Part 1 - PYRO TEST PROGRAM

The rationale behind the test design of the PYRO Hypergolic Test Program was presented in Section 3, the discussion of the general approach. One of the important points made in this discussion was that since it should be relatively difficult to obtain significant yields with hypergolic propellants because they ignite on contact, the major effort during the test program would be concentrated on the more severe mixing conditions.

From a practical point of view, the specific failure modes which could lead to these conditions are high-velocity impact, resulting from either powered impact or fallback from a high altitude, or an explosive-donor situation which might occur if both cryogenic and hypergolic propellants are used in the same vehicle.

Accordingly, in the initial test program, the major effort was placed on these two failure modes. Three other conditions were selected for minor testing: command-destruct in which the two propellant compartments were opened by explosive shaped charges; confinement-by-the-missile (the rapid removal of a diaphragm between the compartments containing the fuel and oxidizer); and tower drop, which simulated low-altitude fallback or seam rip.

HIGH-VELOCITY IMPACT TESTS

The high-velocity impact test series consisted of 15 tests in which 200- and 1000-lb quantities of the hypergolic propellant combination were propelled down a sled track at speeds ranging from 340 to 580 fps and allowed to impact on various target configurations.

The test parameters which were considered important for the high-velocity impact test condition were:

- Orientation of propellant tanks with respect to the impacted surface
- Velocity of impact
- Strength of propellant tanks
- Strength of impacted surface
- Geometry of impacted surface

Although there are innumerable tankage configurations that can be visualized for future hypergolic military and space vehicles, it was felt that the main features of the impact process could be studied using the conventional tankage configuration for existing vehicles, i.e., two cylindrical tanks, one above the other, with the resultant length greater than the diameter. With this tankage configuration, two impacting geometries were of concern: impact on a surface normal to the cylindrical axis (nose-on impact) and impact on a surface parallel with the cylindrical axis (side-on impact).

Information furnished by AFRPL staff members indicated that a reasonable upper limit for nose-on impact of a missile (either from a high-altitude fallback or from turn-around and power impact) is about 500 to 600 fps. A value of 330 fps was selected as an upper limit for side-on impact.

The strength of the tank walls could influence the impact process under some circumstances. However, at the upper end of the impact velocity ranges of interest, pressures generated in the fluids by the impact significantly exceed the design pressure of the tanks. For this reason, tankage strengths were not considered as a significant parameter in the program.

The strength of the impacted surface could influence the impact process in two ways. For an ideal rigid surface, maximum pressures would be developed in the fluids at the moment of impact, which would lead to maximum initial velocities of the fluids after tank failure. For softer surfaces, the tank will be decelerated more slowly, and lower pressures will be generated in the impact process. However, some cratering of the surface would be expected, which could lead to confinement of the propellants after tank rupture. Since confinement and also high mixing velocity should tend to increase explosive effects, both of the above situations were of interest. For the soft-surface-cratering condition, it was necessary to decide on the type or types of surfaces to use or simulate. (In order to avoid simulation of the strength characteristics of real vehicles, it seemed easier to impact on a rigid surface with a preformed crater rather than attempting to actually create the crater during the impact process.) In a true fluid-fluid impact case with a roughly spherical impactor, a hemispherical crater is formed. Although few data are available regarding fluid-fluid impact for the case of impactors significantly longer than their diameters, it was anticipated that this would lead to deeper craters. Accordingly, for nose-on impact, two craters were used, a shallow crater, roughly hemispherical (depth equal to radius), and a deep crater with a depth three times the radius.

The initial geometry of the impacted surface is perhaps the most difficult of all the parameters to generalize. Simple depressions would act in a manner similar to craters, whose effects have already been discussed. Of perhaps more importance are sharp protuberances from the surface, which might increase the breakup of large fluid masses on impact, particularly those protuberances which might tend to force the propellants together. Accordingly, the geometry selected for the shallow crater, nose-

on impact test consisted of a hemispherical depression with a cylindrical rod protruding from the center, the rod diameter being equal to one-third the tank diameter and the crater diameter equal to twice the tank diameter. For side-on impact, a flat surface with two parallel walls protruding from the surface was selected. The parallel walls were normal to the tank axes and approximately at the midpoints of the two tanks. This ensured that at least half of each propellant mass was directed into the space between the walls.

Hardware and Test Design

The tanks required for this test condition were designed to be used on the K-2 sled track located at the Naval Weapons Center, (formerly the Naval Ordnance Test Station) China Lake, California. They were propelled down this track by solid-motor propulsion units at speeds ranging from 340 to 580 fps and allowed to impact the various target configurations.

The criteria for the design of these tanks were determined by: (1) the requirements to approximately simulate conventional vehicles with regard to shape; (2) the desirability of using minimum weight, strength, and length-to-diameter ratios consistent with present and expected usage; (3) compliance with the following operations restrictions imposed by the Naval Weapons Center:

- Tanks should be capable of withstanding a 30-g load in any direction
- Tanks must be tested to 20 psi in either compartment with the other vented to the atmosphere.

Sketches of the aluminum tanks, designed to meet these requirements are presented in Figs. 4-1, 4-2, and 4-3. The tanks were designed, in general, in accordance with the ASME code procedures outlined in Ref. 4-1, except that a working stress of 8,000 to 9,000 psi, instead of 6,000 psi, was utilized because of the nature of the vessel use. The tanks were cylindrical with 2:1 ellipsoidal domed ends. The domed ends and the side walls were 1/8 in. thick.

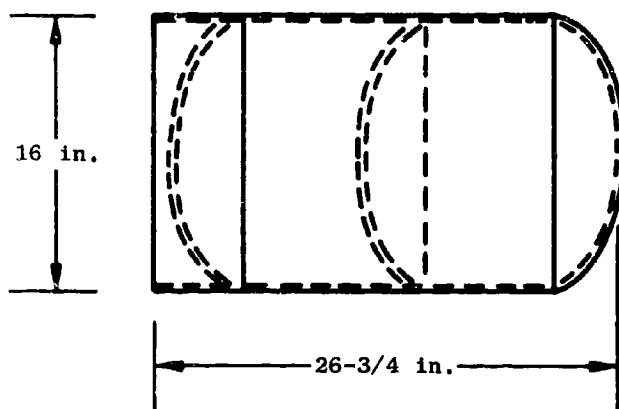


Fig. 4-1. Nose-on 200-lb, High-Velocity Impact Tank

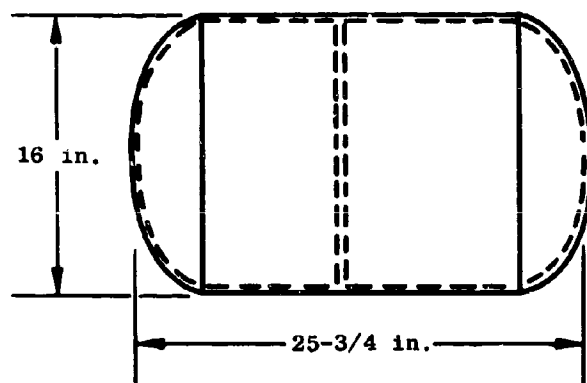


Fig. 4-2. Side-on, 200-lb, High-Velocity Impact Tank

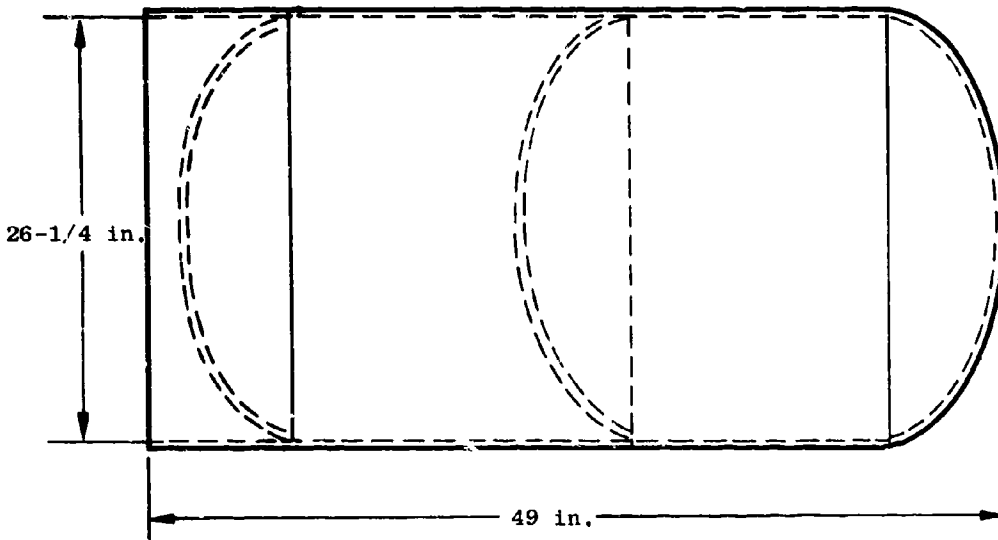


Fig. 4-3. Nose-on, 1000-lb, High-Velocity Impact Tank

The diaphragm design was of special interest. For the nose-on impact tests, the critical design load condition for the diaphragm of the 16-in. tank was 20 psi, imposed by both the static proof-test load and the 30-g acceleration, and for the 26-in. tank, 37 psi, imposed by the 20-g acceleration. The required thickness of an elastic flat plate for these conditions was approximately 3/16 in. and 9/16 in. for the 16- and 26-in.-diameter tanks, respectively. This was considered unrealistic for the nose-on impact configuration, since diaphragms of these thicknesses might be expected to inhibit hypergolic impact mixing. Therefore, it was decided to utilize an ellipsoidal diaphragm which permitted the use of the more realistic material thicknesses of 1/16 in. for the 16- and 3/32 in. for the 26-in. tanks. For the side-on impact test tank, it was decided to use the flat-plate diaphragm. In this configuration, it would serve as a tank stiffener and yet have negligible effect on fuel mixing at impact.

Four different target geometries were used: flat-wall, shallow-hole, deep-hole, and parallel-wall. For the flat-wall target, a massive concrete block was faced with a 1-in.-thick steel plate, 8 ft high, and 16 ft wide, with a 12-in. splash shield around the edge. For the shallow-hole and deep-hole test targets, the base was faced with concrete blocks 6 ft in cross section and having cylindrical cavities. For the parallel-wall target, the massive concrete base was faced with a steel plate from which two vertically oriented walls protruded perpendicular to the plate. A diagonal brace was placed between the vertical walls at the planned impact point of the tank and a splash shield placed across the top of the vertical walls. Sketches and photos of these target geometries are shown in Figs. 4-4 through 4-8.

In addition to the propellant tests, a series of high-explosive calibration tests were conducted. The purpose of these tests was to obtain suitable reference curves using the same basic geometry and height of burst as in the propellant impact tests.

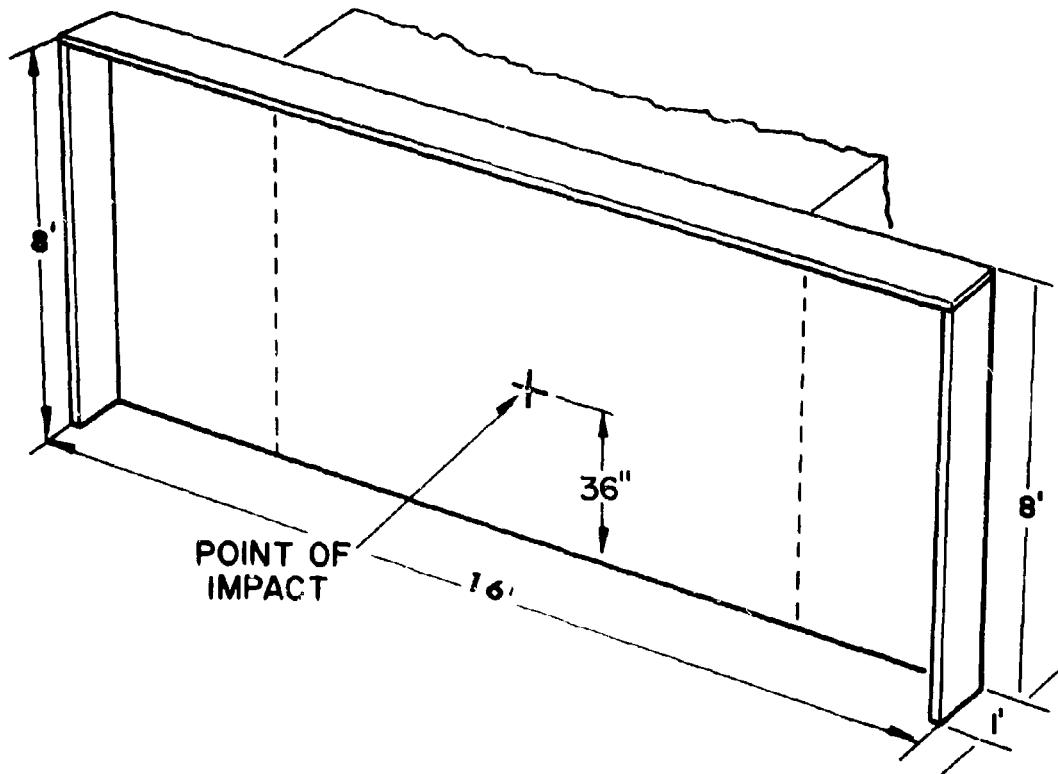


Fig. 4-4. Flat-Wall Target

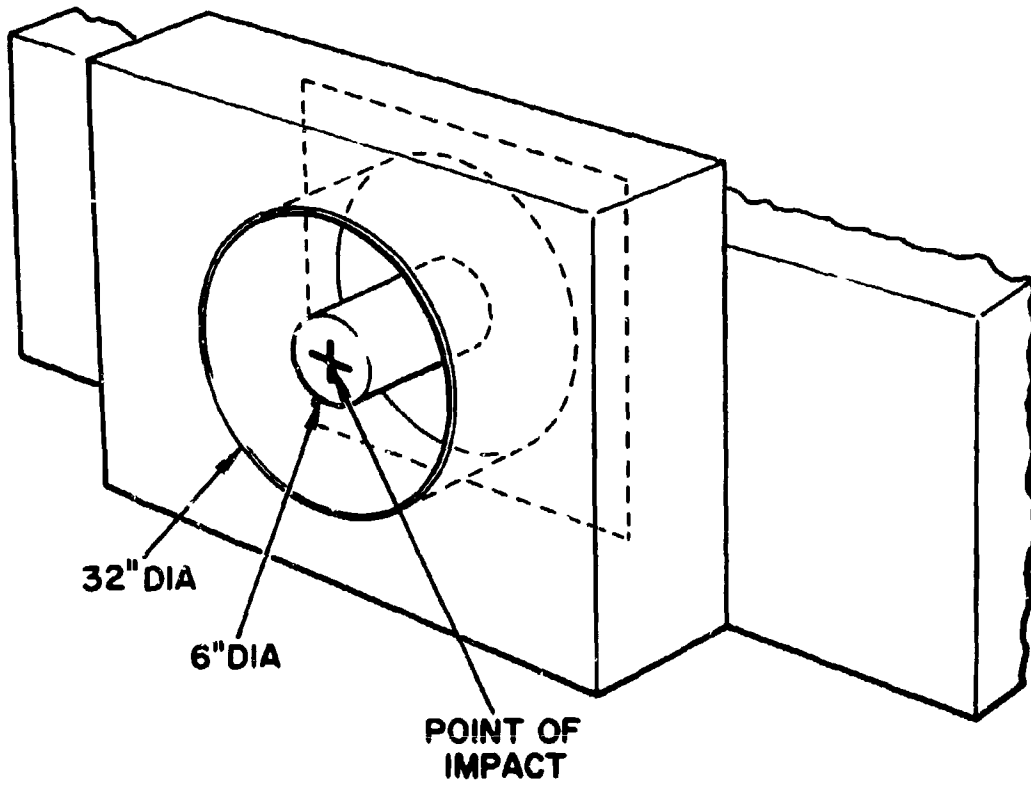


Fig. 4-5. Shallow-Hole Target

It can be seen that, in general, the calibration results were as expected. The transition is evident for peak pressure, being apparently well underway at the 38-ft gauge station ($d/W^{1/3}$ of about 15). The impulse curve falls between the two reference curves and, furthermore, has approximately the same shape, suggesting either that the transition is complete at a distance closer than the nearest station or that it commences at distances greater than the remote stations. Since rarefactions from the target edges should commence decreasing the impulse at smaller distances than peak pressure, and since the peak pressure is apparently affected at close distances, presumably completing its transition near the 38-ft gauge station, the impulse calibration curve is probably that which would be obtained without the presence of the target. The fact that its magnitude is approximately 25 percent greater than that for the 18-lb TNT reference curve is not surprising, because of the differences between TNT and pentolite and between a surface burst and a 3-ft HOB.

No significant differences in the gauge line magnitudes were apparent for either pressure or impulse. The possibility of peak-pressure differences occurring over the transition region might be anticipated but were not detectable.

Impact Test Results

The individual peak overpressure and positive-phase-impulse data are presented in the data bank in Volume 2. The yield values which have been computed from these data using the basic reference curves in Figs. 4-11 and 4-12 and multiplying by a factor of 1.18 to correct for the difference between pentolite and TNT are presented in Table 4-2. From the data in Table 4-2 it is evident that some of the computed yields tend to be distance-dependent. Yields computed from the overpressure for all gauge lines typically increase with increasing distance. Impulse yields for the 30-deg and 60-deg lines tend to decrease with increasing distance, while impulse yields for the 90- and 180-deg lines tend to remain relatively constant with distance or in some cases show a slight increase.

In addition, the data indicate that large shock wave asymmetries were present, even at the outer gauge stations. This is particularly evident for the deep-hole-target tests.

Table 4-2
EXPLOSIVE YIELDS FROM HYPERGOLIC IMPACT TESTS

| TARGET GEOMETRY | TEST NO. | PROP. WEIGHT (lb) | IMPACT VELOCITY (ft/sec) | GAUGE LINE (deg) | YIELD (%) AT INDICATED NOMINAL DISTANCES (ft) | | | | | | | | | | | |
|-----------------|----------|-------------------|--------------------------|------------------|---|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----|--|
| | | | | | 23 | | 37 | | 67 | | 117 | | 200 | | | |
| | | | | | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | | |
| Flat Wall | 001 | 200 | 570 | 30 | 8 | 24 | 13 | 17 | - | - | - | - | - | - | - | |
| | | | | 60 | - | - | 12 | 16 | - | - | - | - | - | - | - | |
| | 007 | 200 | 340 | 30 | 2 | 6 | - | - | - | - | - | - | - | - | - | |
| | | | | 60 | - | - | 2 | 8 | 4 | 6 | 6 | 7 | - | - | - | |
| | 068 | 200 | 566 | 30 | 7 | 35 | 12 | 25 | 12 | 15 | 18 | 14 | - | - | - | |
| | | | | 90 | - | - | 7 | 15 | 8 | 14 | 9 | - | - | - | | |
| | | | | 180 | - | - | - | - | - | - | - | - | - | 16 | 18 | |
| | 012 | 1000 | 570 | 30 | - | - | 6 | 21 | - | - | - | - | - | - | - | |
| | | | | 60 | - | - | 11 | 39 | 17 | 22 | 22 | 30 | 16 | 22 | | |
| | 072 | 1000 | 585 | 30 | - | - | 14 | 40 | 23 | 31 | - | - | - | 30 | 31 | |
| | | | | 90 | - | - | 5 | 18 | 10 | 17 | 8 | 13 | 12 | 19 | | |
| | | | | 180 | - | - | - | - | 9 | 14 | 13 | 12 | 15 | 14 | | |
| 073 | 1000 | 410 | 30 | 30 | 100 | 28 | 88 | 38 | 75 | 49 | 69 | - | - | | | |
| | | | 90 | - | - | 16 | 15 | 13 | 17 | 17 | 17 | 23 | - | | | |
| | | | 180 | - | - | - | - | 4 | 15 | 6 | 33 | 5 | 20 | | | |
| Shallow Hole | 002 | 200 | 575 | 30 | 26 | 143 | 44 | 67 | - | - | - | - | - | | | |
| | | | | 60 | - | - | - | - | - | - | - | - | - | | | |

* Non-classical
Y_P = yield by pressure
Y_I = yield by impulse

Table 4-2 (Cont.)

| TARGET GEOMETRY | TEST NO. | PROP. WEIGHT (lb) | IMPACT VELOCITY (ft./sec) | GAUGE LINE (deg) | YIELD (%) AT INDICATED NOMINAL DISTANCES (ft) | | | | | | | | | | | |
|--------------------|-------------|-------------------------|---------------------------------|------------------------|---|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----|---|
| | | | | | 23 | | 37 | | 67 | | 117 | | 200 | | | |
| | | | | | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | | |
| Deep Hole | 003 | 200 | 575 | 30 | 9 | 24 | 13 | 38 | - | - | - | - | 27 | 33 | - | - |
| | | | | | 8 | 17 | 13 | 27 | - | - | - | - | - | - | - | - |
| | 008 | 200 | 340 | 30 | 14 | 107 | 22 | 145 | 49 | 76 | 56 | 64 | - | - | - | - |
| | | | | | 6 | 9 | - | - | - | - | - | - | - | - | - | - |
| | 009 | 200 | 580 | 30 | 23 | 122 | 51 | 133 | 98 | 82 | 89 | 89 | - | - | - | - |
| | | | | | 11 | 14 | - | - | - | - | - | - | - | - | - | - |
| 013 | 1000 | 570 | 30 | 30 | 100 | 28 | 88 | 38 | 76 | 49 | 69 | - | - | - | - | |
| | | | | - | - | 16 | 15 | 13 | 17 | 17 | 23 | 6 | 33 | 5 | 20 | |
| 074 | 1000 | 557 | 180 | - | - | - | - | 4 | 15 | 6 | 33 | - | - | - | - | |
| | | | | - | - | 16 | - | 41 | - | 51 | - | 28 | - | - | | |
| 004 | 200 | 340 | 30 | 17 | 59 | 26 | 57 | 30 | 46 | 41 | 43 | - | - | - | - | |
| | | | | 10 | 46 | 15 | 41 | - | - | - | - | - | - | - | | |
| 010 | 200 | 340 | 30 | 10 | 38 | 15 | 42 | 24 | 32 | 19 | 30 | - | - | - | - | |
| | | | | 7 | 26 | - | - | - | - | - | - | - | - | - | | |

Table 4-2 (Cont.)

| TARGET GEOMETRY | TEST NO. | PROP. WEIGHT (lb) | IMPACT VELOCITY (ft./sec) | GAUGE LINE (deg) | YIELD (%) AT INDICATED NOMINAL DISTANCES (ft) | | | | | | | | | | | |
|-----------------|----------|-------------------|---------------------------|------------------|---|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|----------------|---|--|
| | | | | | 23 | | 37 | | 67 | | 117 | | 200 | | | |
| | | | | | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | Y _P | Y _I | | |
| Deep Hole | 003 | 200 | 575 | 30 | 9 | 24 | 13 | 38 | - | - | - | 27 | 33 | - | - | |
| | | | | | 8 | 17 | 13 | 27 | - | - | - | - | - | - | | |
| | 008 | 200 | 340 | 30 | 14 | 107 | 22 | 145 | 49 | 76 | 56 | 64 | - | - | - | |
| | | | | | 6 | 9 | - | - | - | - | - | - | - | - | | |
| | 009 | 200 | 580 | 30 | 23 | 122 | 51 | 133 | 98 | 82 | 89 | 89 | - | - | | |
| | | | | | 11 | 14 | - | - | - | - | - | - | - | - | | |
| | 069 | 200 | 555 | 30 | 30 | 100 | 28 | 88 | 38 | 76 | 49 | 69 | - | - | | |
| | | | | | - | - | 16 | 15 | 13 | 17 | 17 | 23 | - | - | | |
| | | | | | - | - | - | - | 4 | 15 | 6 | 33 | 5 | 20 | | |
| 013 | 1000 | 570 | 30 | - | - | 16 | - | 41 | - | 51 | - | 28 | - | | | |
| | | | | - | - | 10 | - | - | - | - | - | - | - | | | |
| 074 | 1000 | 557 | 30 | - | - | 34 | 122 | 43 | 129 | 60 | 78 | 55 | 68 | | | |
| | | | | - | - | 5 | 23 | 16 | 31 | 20 | 39 | 24 | 42 | | | |
| | | | | - | - | - | - | 3 | 11 | 12 | 22 | 4 | 8 | | | |
| 004 | 200 | 340 | 30 | 17 | 59 | 28 | 57 | 30 | 46 | 41 | 43 | - | - | | | |
| | | | | 10 | 46 | 15 | 41 | - | - | - | - | - | - | | | |
| 010 | 200 | 340 | 30 | 10 | 38 | 15 | 42 | 24 | 32 | 19 | 30 | - | - | | | |
| | | | | 7 | 26 | - | - | - | - | - | - | - | - | | | |

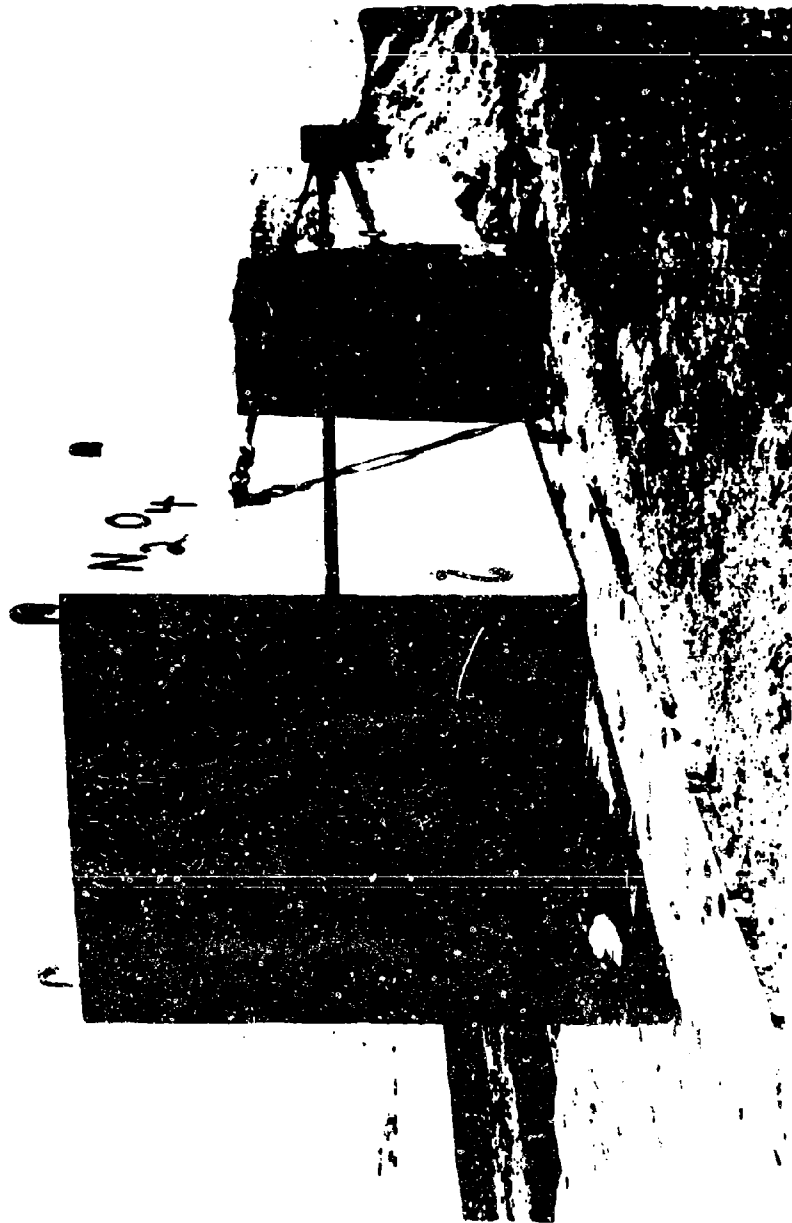


Fig. 4-7. Deep-Hole Target

The persistence of these blast asymmetries over the entire measuring range tends to complicate the selection of appropriate terminal yield values for these tests since the test geometry is not completely similar to the full-scale case of concern. It will be recalled that these tests were intended to simulate nose-on impact of a vehicle at high velocity onto the ground surface. The flat-wall target was selected to simulate a rigid ground surface, in which no impact cratering would occur, while the deep-hole target simulated a soft ground surface, in which significant impact cratering would occur.

In the full-scale case of concern, it would be anticipated that blast pressure would be radially symmetrical about the point of impact along the ground surface and that blast asymmetry, if it existed, would occur in a vertical plane, with the pressure directly above the impact point being the highest. Such asymmetries are not of much concern since pressure along the ground surface would be of most importance in the full-scale case.

In the test geometry, the tankage was accelerated along the ground surface on a sled track and allowed to impact on a massive vertical target since this was the only practical way to obtain the desired high velocities and the required control on the impact point.

The important differences between the real and test geometries can be understood by visualizing that the test geometry is created by rotating the line of flight of the test tankage and a section of the ground surface the size of the target through 90 deg, as shown in Fig. 4-13.

From Fig. 4-13, it appears that the pressures along the 90-deg gauge line in the test geometry case would most nearly correspond to the pressures along the ground surface in the real geometry. The pressures along the 30- and 60-deg lines in the test geometry tend to correspond to those at 30 deg and 60 deg from the vertical in the real geometry and thus would be higher than the ground-surface value. The pressures along the 180-deg gauge lines in the test geometry would tend to be lower than the ground-surface pressure in the real geometry.

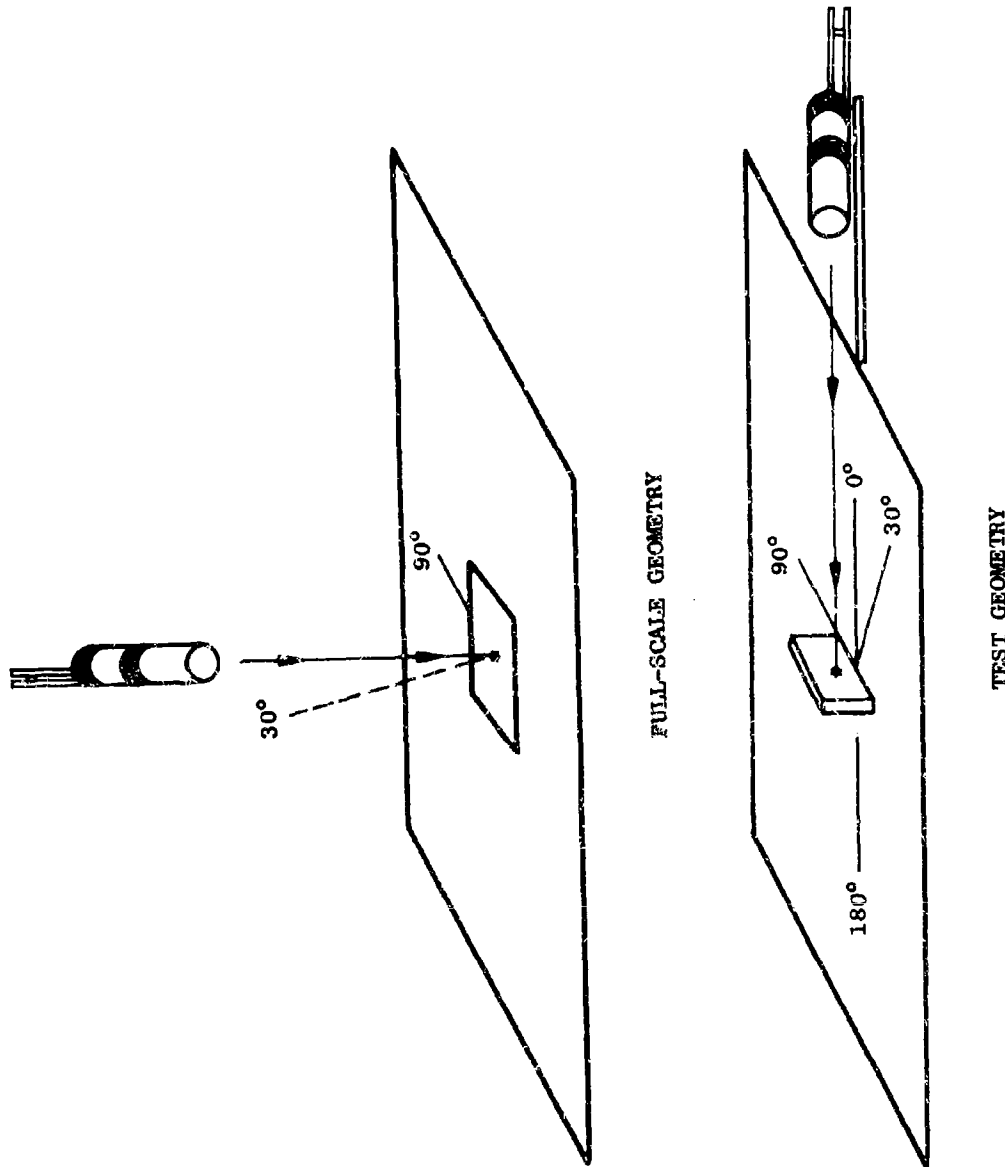


Fig. 4-13. Sketch Indicating Differences Between Full-Scale and Test Geometry

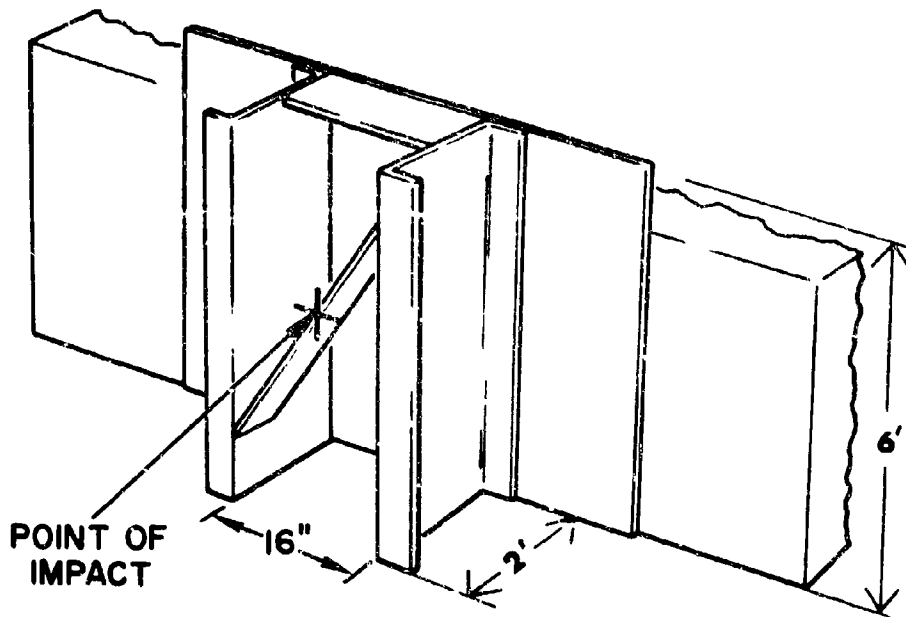


Fig. 4-8. Parallel-Wall Target

The test conditions for each of the propellant tests and the calibration tests are presented in Table 4-1.

These tests were conducted in two series. Test numbers 001 through 015 were conducted in May, June and July of 1964 and test numbers 065 through 081 were conducted in September and October of 1965. The tankage, test conditions and type of instrumentation were the same for both test series. The layout of the instrumentation system, however, was quite different.

The instrumentation layout used for the first series is shown in Fig. 4-9. The Kistler* gauges were installed in two gauge lines 30 and 60 deg from the track. For the second series of tests, the system was modified to obtain more information on the blast asymmetries. The 60-deg leg was moved to 90 deg and a third gauge line was added behind the target. This system is shown in Fig. 4-10. During this second series a gauge was installed on the target to obtain close-in blast information. The data from this gauge location are presented at the end of the high-velocity impact discussions.

During both test series, BRL** self-recording pressure-time gauges were also installed at all of the basic measuring stations and at several additional stations at longer distances. Since the pulse durations obtained from the tests were rather short for correct recording by the BRL gauges, particularly at the close-in stations, and since the Kistler system worked well, the major use of the BRL data was in checking blast asymmetry at long distances.

Calibration Tests

The calibration tests included eight with 18-lb pentolite spheres, one with a 105-lb sphere, and three with 216-lb spheres. The pentolite spheres in most cases were hung in fish nets at the horizontal center of a 5-ft-high, 13-ft-wide, 4-in.-thick backup plate; 3 ft above the ground surface and with the center of the charge approximately 1 ft in front of the plate.

* Quartz Piezoelectric Air Pressure gauges - Kistler Instrument Co. Models 601A.

** Ballistic Research Laboratory.

These considerations suggested two possible methods for estimating appropriate terminal yields:

1. Use value for 90-deg gauge line
2. Use average of maximum and minimum values where the minimum value corresponds to that for the 180-deg line and the maximum value to that along the line of flight (this value would have to be obtained by extrapolation)

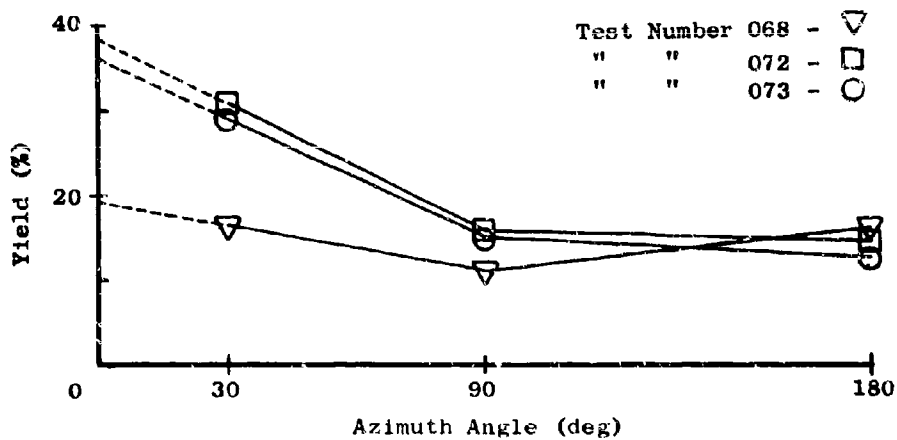
A combination of the two methods was finally selected for computing the yields for the tests in the second test series. This method involved averaging the computed yields from the 0-, 90-, and 180-deg direction, with the 0-deg value obtained by extrapolation. (Plots demonstrating the method for obtaining the extrapolated value are given in Fig. 4-14.)

When data were available, the same method was used to obtain terminal yield estimates for the tests conducted during the first test series; however, for tests 002 and 007 sufficient data were not available to make this computation and thus the yields quoted are estimated values. The computed yield values for both test series are presented in Table 4-3.

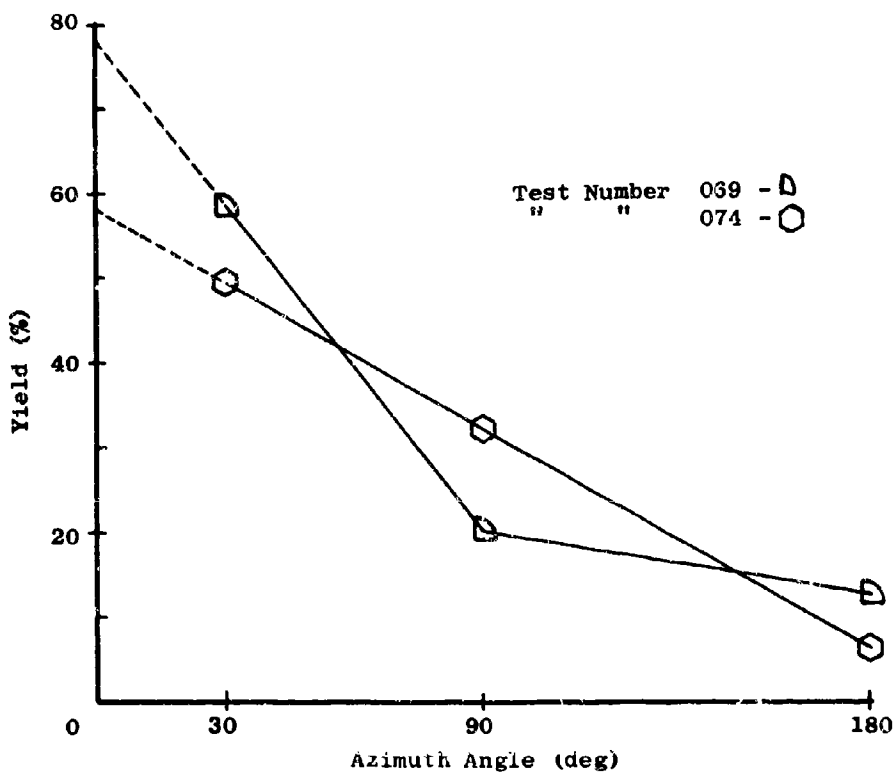
Close-In Data

During the second high-velocity test series, effort was devoted to measuring the close-in blast environment overpressure. Gauges with a 0- to 30,000-psi pressure range were flush-mounted 2 ft from the point of impact in the flat-wall targets and on the lip of the deep-hole targets. Data were obtained from two tests: 069, a 200-lb deep-hole-target test; and 074, a 1000-lb deep-hole-target test.

The analog traces for these two tests have been redrawn and are presented in Fig. 4-15. Included are the approximate impact times of the tanks and the ignition times, which were obtained from the film records and the maximum peak overpressure values.



a. Flat-Wall Target Tests



b. Deep-Hole Target Tests

Fig. 4-14. Plots of Yield as a Function of Azimuth Angle for Selected Hypergolic Impact Tests

Table 4-1
SUMMARY OF TEST CONDITIONS

| TEST NO. | WEIGHT (lb) | TYPE | IMPACT VELOCITY (fps) | TARGET CONFIGURATION |
|----------|-------------|--------------------------------------|-----------------------|----------------------|
| 01C | 18 | Pentolite | - | Flat Wall |
| 02C | 18 | Pentolite | - | Flat Wall |
| 03C | 18 | Pentolite | - | Flat Wall |
| 001 | 200 | N ₂ O ₄ /50-50 | 570 | Flat Wall |
| 002 | 200 | N ₂ O ₄ /50-50 | 575 | Shallow Hole |
| 003 | 200 | N ₂ O ₄ /50-50 | 575 | Deep Hole |
| 004 | 200 | N ₂ O ₄ /50-50 | 340 | Parallel Wall |
| 006 | 18 | Pentolite | - | Flat Wall |
| 007 | 200 | N ₂ O ₄ /50-50 | 340 | Flat Wall |
| 00E | 200 | N ₂ O ₄ /50-50 | 340 | Deep Hole |
| 009 | 200 | N ₂ O ₄ /50-50 | 580 | Deep Hole |
| 010 | 200 | N ₂ O ₄ /50-50 | 340 | Parallel Wall |
| 011 | 18 | Pentolite | - | Flat Wall |
| 012 | 1000 | N ₂ O ₄ /50-50 | 570 | Flat Wall |
| 013 | 1000 | N ₂ O ₄ /50-50 | 570 | Deep Hole |
| 014 | 216 | Pentolite | - | Flat Wall |
| 015 | 216 | Pentolite | - | Flat Wall |
| 065 | 18 | Pentolite | - | Flat Wall |
| 067 | 18 | Pentolite | - | Flat Wall |
| 068 | 200 | N ₂ O ₄ /50-50 | 566 | Flat Wall |
| 069 | 200 | N ₂ O ₄ /50-50 | 535 | Deep Hole |
| 070 | 18 | Pentolite | - | Flat Wall |
| 071 | 105 | Pentolite | - | Flat Wall |
| 072 | 1000 | N ₂ O ₄ /50-50 | 585 | Flat Wall |
| 073 | 1000 | N ₂ O ₄ /50-50 | 410 | Flat Wall |
| 074 | 1000 | N ₂ O ₄ /50-50 | 557 | Deep Hole |
| 081 | 216 | Pentolite | - | Flat Wall |

Table 4-3
 TERMINAL YIELDS FROM HYPERGOLIC IMPACT TESTS

| TARGET TYPE | PROPELLANT WEIGHT (lb) | VELOCITY | TEST NO. | TERMINAL YIELD (%) |
|---------------|------------------------|----------|----------|--------------------|
| Flat Wall | 200 | 340 | 007 | 5 |
| | | 570 | 001 | 13 |
| | | 566 | 068 | 15 |
| | | 410 | 073 | 21 |
| | 1000 | 570 | 012 | 15 |
| | | 585 | 072 | 23 |
| Shallow Hole | 200 | 575 | 002 | 53 |
| Deep Hole | 200 | 340 | 008 | 38 |
| | | 575 | 003 | 24 |
| | | 560 | 009 | 56 |
| | | 535 | 069 | 37 |
| | 1000 | 570 | 013 | 28 |
| | | 557 | 074 | 34 |
| Parallel Wall | 200 | 340 | 004 | 33 |
| | | 340 | 010 | 21 |

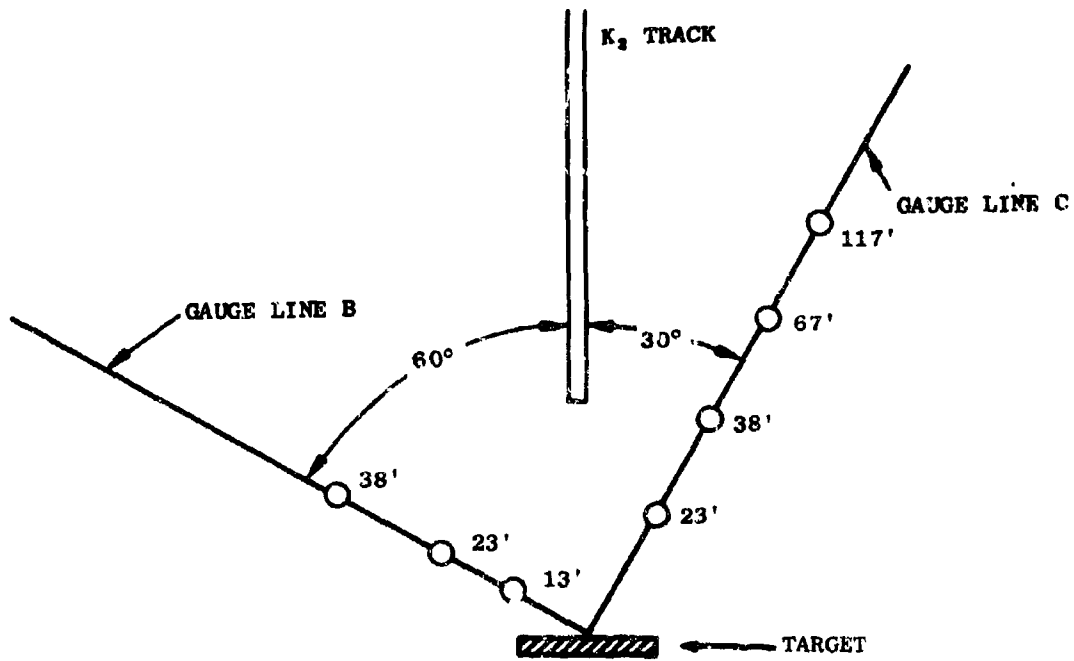


Fig. 4-9. Pressure Sensor Locations for First Hypergolic Impact Test Series

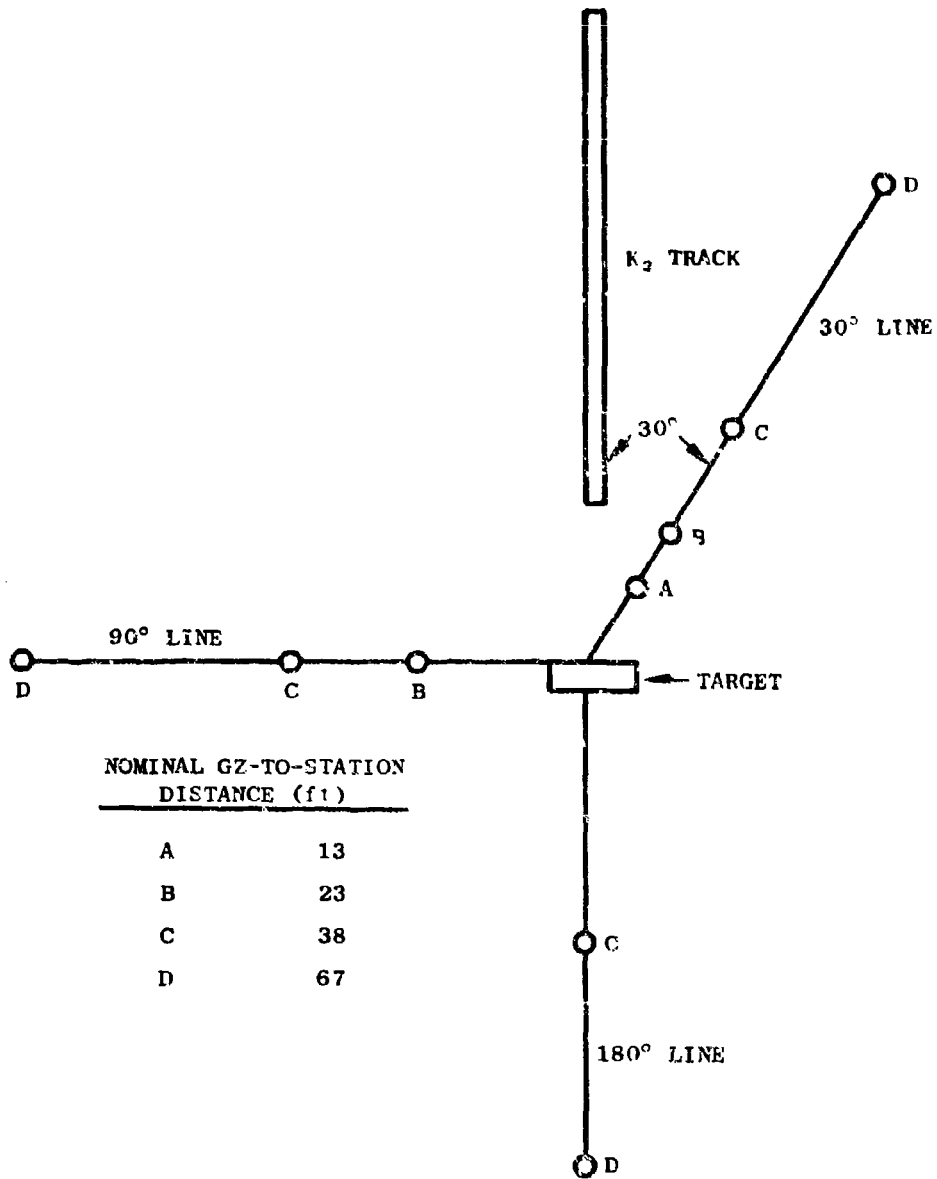
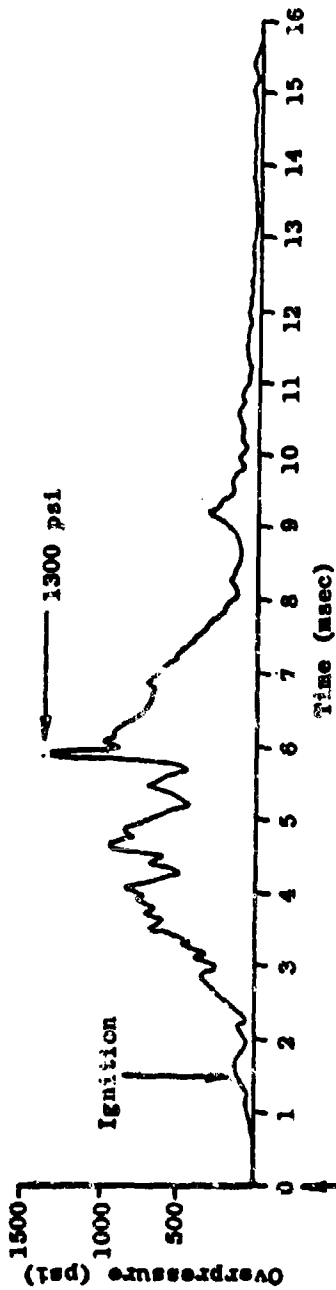
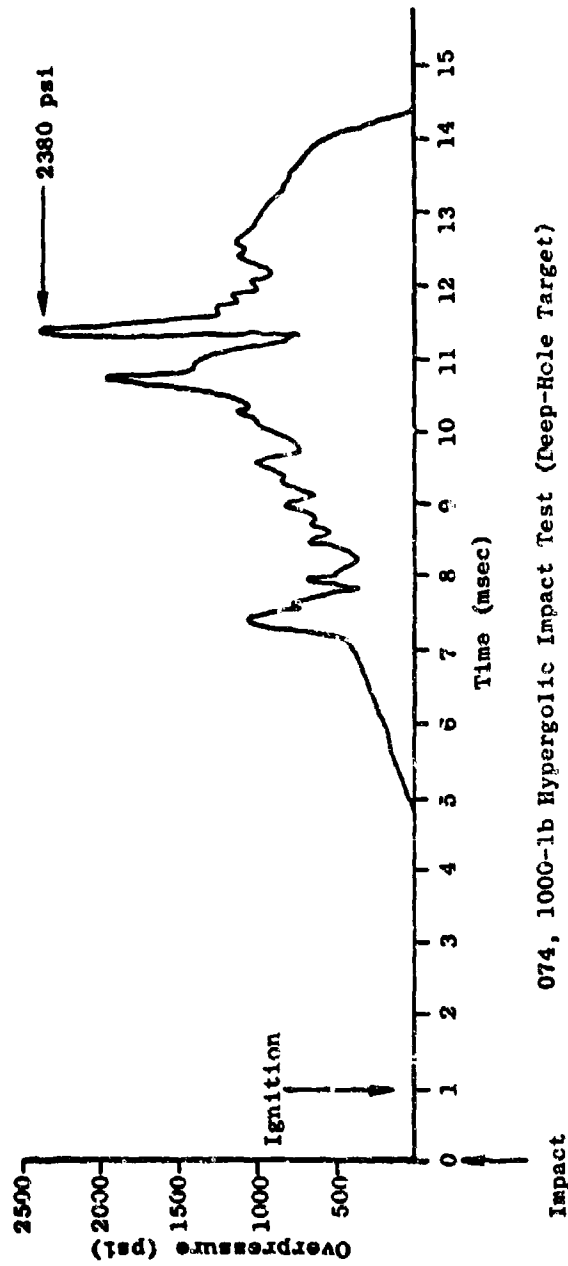


Fig. 4-10. Pressure Sensor Locations for Second Hypergolic Impact Test Series



Impact 069, 200-lb Hypergolic Impact Test (Deep-Hole Target)



Impact 074, 1000-lb Hypergolic Impact Test (Deep-Hole Target)

Fig. 4-15 Close-in Gauge Traces, Hypergolic Tests 069 and 074

The peak overpressure^{*} and positive-phase-impulse data from these calibration tests are plotted as a function of scaled distance in Figs. 4-11 and 4-12, respectively.^{**} It can be seen that the data points for each plot are internally consistent in that they tend to lie along a smooth curve. As a measure of the reproducibility of the results, a standard deviation was calculated from the percent deviation of each point from a smooth-curve fit through all of the data points. The standard deviation (of an individual observation) computed in this manner for pressure was 11.9 percent and for impulse, 7.6 percent.

Also included in Figs. 4-11 and 4-12 are two curves (dashed lines) illustrating expected values for 18 lb of a standard high explosive^{***} detonated under two conditions: one, in the absence of the target, the other with an infinite target. (The latter condition corresponds to 36 lb in the absence of the target.) It would be expected that the blast wave characteristics from the 18-lb pentolite spheres with the massive target would correspond to those from a 36-lb charge without the target at distances less than the minimum target dimensions, and that they would correspond to those from an 18-lb charge without the target at distances very much greater than the minimum target dimensions. Although there should be a gradual transition from one case to the other, the exact manner in which this occurs is not known.

* The terms "peak overpressure" and "overpressure" and "positive-phase-impulse" and "impulse" are used interchangeably throughout this report.

** These data as well as the propellant test data have not been corrected for the difference in ambient pressure between sea level (14.7 psi) and that existing at the test site (approximately 13.7 psi). The correction is small and unnecessary for later yield computations, because it is common to both the calibration and impact test data.

*** For overpressure, these curves are based on data given in Ref. 4-2 for composition A charges (95% RDX - 5% wax) detonated over a flat surface with a scaled HOB of $0.94 \text{ ft/lb}^{1/3}$. (This is equivalent to a 3-ft HOB for a 36-lb charge.) These data were used since suitable pentolite data for the proper HOB were not readily available, and composition A has explosive characteristics very close to those of pentolite. For impulse, these curves are based on data given in Ref. 4-3 for a surface burst of TNT. These data were used since neither suitable pentolite nor composition A data were readily available. (It is estimated that the 18- and 36-lb TNT curves would be obtained with about 15 to 30 lb of pentolite, respectively.)

ANALYSIS OF DATA

The results obtained from the high-velocity test series were analyzed to determine whether the observed variations in yield with the different test conditions could be considered significant in a statistical sense.

An analysis of variance was first performed on all the high-velocity data to test for significance of the scale and target geometry effects. This analysis was performed on the basic data without transformation, with a square root transformation, and with a \log_{10} transformation. In the first case, the variance is assumed independent of the mean value; in the second case, the variance is assumed to be proportional to the mean (or the standard deviation is proportional to the square root of the mean); and in the third case, the variance is assumed to be proportional to the mean squared (or the standard deviation is proportional to the mean). These two transformations were used since it is believed that the uncertainty in yield value as expressed by the standard deviation tends more to be proportional to the mean than independent of it. Except for cases where the mean value is quite small, the \log_{10} transformation seems most appropriate.

The results of the analysis on both the original and transformed data showed that:

1. The scale effect was not significant at the 10-percent level.*
2. The target geometry effect was significant at the 2.5-percent level, i.e., the deep-hole geometry gave significantly higher yields than the flat-wall target.
3. The interaction of the target geometry and scale effect was significant at the 10-percent level. In other words the effect of target geometry at the 200-lb scale tended to be somewhat greater than at the 1000-lb scale.

Because of the moderate interaction effect, the yield data were next

* Refers to level of significance used in the statistical test. A 10-percent level means there is a 10 percent probability of erroneously rejecting a valid null-hypothesis.

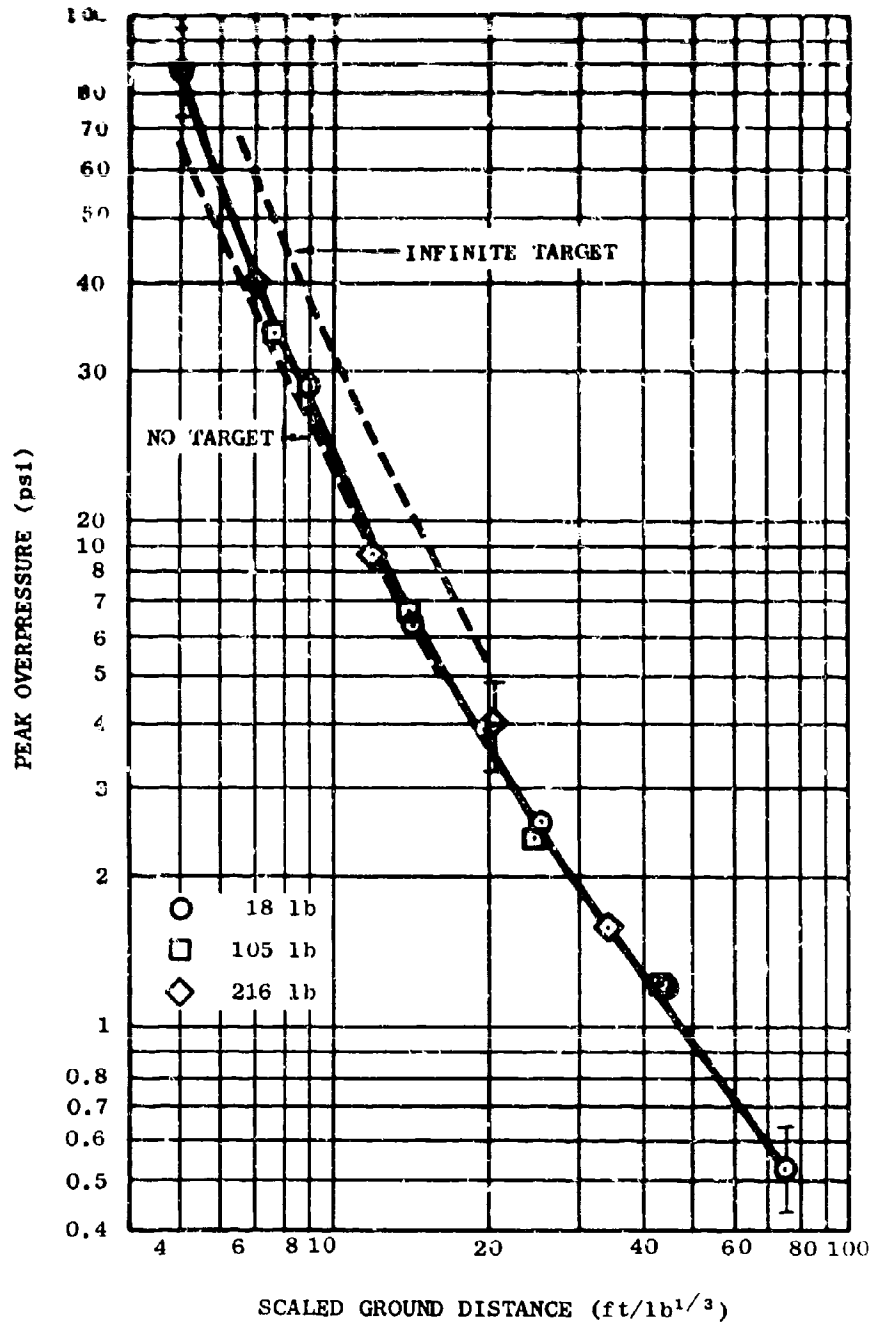


Fig. 4-11. Peak Overpressure vs Distance for Pentolite (flat-wall target, 3-ft HOB)

analyzed for scale effect separately for each target geometry. Again the original data and the two transformations were used. For both the flat-wall and deep-hole targets the data exhibited no significant scale effect (at the 10-percent level).

Next, an analysis was performed to determine whether there was a significant effect of velocity between the mean of the six high-velocity observations which had low-velocity counterparts and that of the three lower velocity observations: Equality of population variance was not assumed. The analysis was performed on both the original and transformed data. No significant effects were detected at the 10-percent level.

In summary, those statistical analyses indicate that:

1. The deep-hole target gives significantly larger explosive yields than the flat-wall target.
2. A variation in impact velocity from approximately 340 to approximately 570 ft/sec does not significantly affect the yield.
3. For the deep-hole target there is a small tendency for the yield to decrease with increasing propellant weight.

In addition an overall estimate of the experimental variability was computed using the sets of duplicate data that were available and assuming that the standard deviation is proportional to the mean yield. For this computation both the hypergolic and cryogenic high-velocity-impact data were used.* These computations indicated that the standard deviation in yield is approximately 25% of the mean yield. The uncertainty was estimated to be equal to two standard deviations.

EXPLOSIVE-DONOR TESTS

Test Conditions

The explosive-donor case is similar in some respects to the high-velocity-

* The cryogenic high-velocity-impact data are described in detail in Section 5.

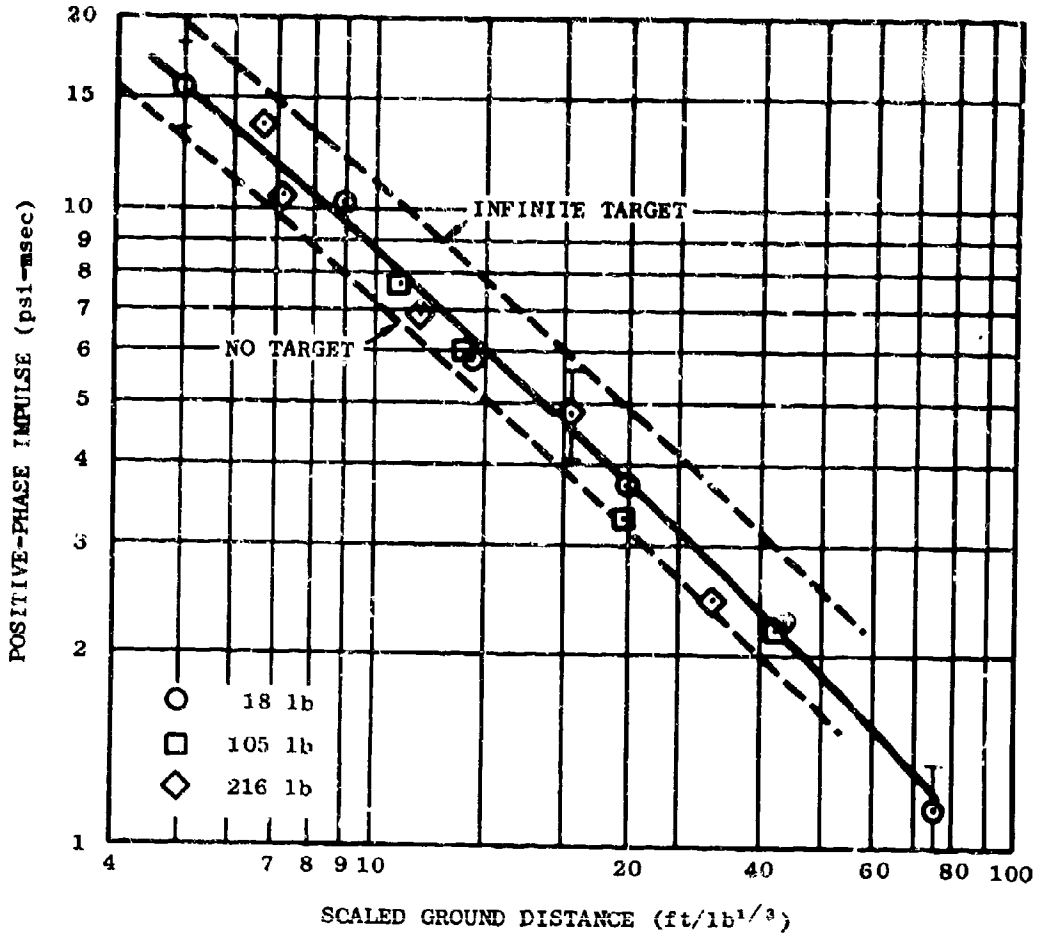


Fig. 4-12. Positive-Phase Impulse vs Distance for Pentolite (flat-wall target, 3-ft HOB)

impact case. The major difference is that in the explosive-donor case, the propellants are subjected to a short-duration, very-high-pressure loading rather than a longer duration, moderate-pressure loading, as in the high-velocity-impact case.

The parameters which appeared important for the explosive-donor situation were:

- Geometry of propellant tanks
- Weight of explosive donor
- Location of explosive donor
- Shape of explosive donor
- Geometry of neighboring surfaces

As in the impact tests, the most important geometry of the propellant tanks appeared to be that of a conventional vehicle, i.e., two cylindrical tanks, one on top of the other with the length-to-diameter ratio and tank strengths being the minimum values consistent with expected usage.

The weight of potential explosive donors could vary over wide limits. However, there are several factors which tend to narrow down the range of primary interest. First it can be shown that there is not too much concern with donors whose weight approaches that of the propellants. If a donor equal to the propellant weight is necessary to make the majority of the propellants react explosively, then the resulting explosion is not much worse than that given by the donor itself. (An increase in explosive weight by a factor of two only increases the distance at which a given peak pressure is obtained by a factor of $(2)^{1/3}$ or 1.26.)

On the other hand, too small a donor may not be able to cause the propellants to mix and explode (if in fact, they are capable of exploding under the action of an explosive donor). With this line of reasoning, it was concluded that the range of interest was from about 1 to 50 lb for 200 lb of propellant.

A variety of shapes and locations of donors were considered with regard to their ability to create extremely fast mixing. These included:

- A spherical charge on top of the tank
- A disk charge on top of the tank
- Longitudinal charges spaced at intervals around the tank sidewall
- A shaped charge on top of the tank with the jet directed along the axis of the tank.

With the assumption that rapid mixing can occur only with a rapid increase in interface area, it appeared that the spherical and disk charges would be more effective than the longitudinal charge. Furthermore, it did not appear that there would be much difference in effects between the spherical and disk charges, and since the former is simpler, the disk and longitudinal charges were dropped from further consideration.* For the spherical charge, it seemed appropriate to use a charge weight of 30 lb, which is near the upper end of the range of interest. The charge was placed on the top of the tank, and the tank was placed on the ground surface to take advantage of the confinement of the propellant between the explosion and the ground surface.

The shaped charge differs from the other types because it is a directed source of energy release. By vaporizing the propellants along the axis of the tank, a radial velocity should be given to the remaining propellants, which may lead to relatively fast mixing. Since the possibility of an actual shaped charge, particularly of large size, is relatively unlikely in an actual missile failure, it seemed appropriate in this case to use a charge weight of the order of a pound, which is at the lower end of the range of interest.

A 1-lb shaped RDX charge having a copper-lined reentrant cone, with a 60-deg apex angle, was selected for this purpose. Such a charge was reported to be capable of penetrating 14 in. of steel. This charge and its initiator were oriented so that a fan-shaped jet was directed down the axis of the propellant tanks.

* A command destruct test series using longitudinal charges was conducted later in the program.

For both types of tests, the tanks were approximately 29 in. long and 15 in. in diameter with 1/16-in.-thick aluminum walls. The oxidizer was in the upper part of the cylindrical tank and the fuel in the lower part, separated by a tempered glass diaphragm. The total propellant weight was 200 lb. The propellant length-to-diameter ratio was 1.8 and the oxidizer-to-fuel weight ratio 1.9. A sketch of this type of tank is shown in Fig. 4-16.

This test series was conducted at AFRPL, and the blast environment was measured using the basic PYRO instrumentation system, which consisted of 15 piezoelectric transducers distributed along 3 radial lines, 120 deg from each other, over a ground distance of approximately 7 to 67 ft. A complete discussion of this system is presented in Appendix A.

There were two propellant tests for each type of donor and one spherical donor test with both propellants replaced by water. This latter test was conducted because it was suspected that the contribution of peak pressure and impulse by this 30-lb donor charge might be large compared to the propellant. If this occurred, separation of the yield (effective charge weight) of the propellant from that of the donor would be extremely uncertain unless the results from the donor charge itself were known quite well.

Test Results

The peak overpressure and impulse data and the propellant yields computed from these data are presented in Volume 2. The computed terminal yield values for these tests are presented in Table 4-4. For the spherical-charge tests, the propellant yields were obtained by subtracting the yield values of the tests with water from those of the tests with propellants. For the shaped-charge tests, the propellant yields were obtained by subtracting the estimated yield of the donor (0.5 percent) from the total yield. This estimate is believed to be commensurate with the accuracy of the measurement for the outer measuring stations but is relatively uncertain close to the explosion because of the shape of the charge. For the spherical-donor tests, there is a large scatter in the yield results from station to station because the propellant yield was small compared to the donor yield. Accordingly, it was not practical to ascertain distance

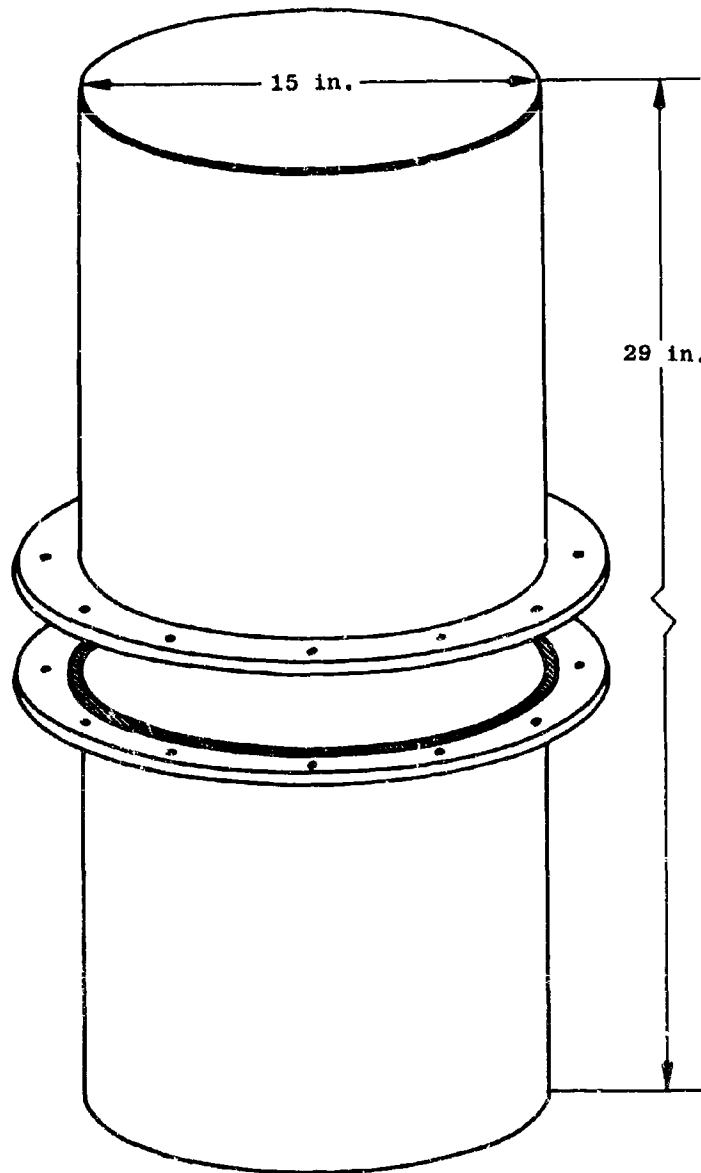


Fig. 4-16. Explosive-Donor Tank

effect on yield, and terminal yield values were computed by averaging the overpressure and impulse yield values at all stations. Although the results from the two tests showed remarkably good agreement, considering the variability in individual station results, it is estimated that the average terminal yield value of the two tests, 3.6 percent, is uncertain by a factor of about 2 to 3.

For the shaped-charge tests, both the overpressure and impulse yields are roughly constant beyond 22.5 ft, so that terminal yields were computed by averaging the pressure and impulse yields for the last three stations.

Considering the small estimated uncertainty in the instrumentation system and the relatively good agreement between the two tests, it is estimated that the average terminal yield, 1 percent, is uncertain to about a factor of 2.

The average terminal yield for the two tests with the 30-lb spherical donor was 3.6 percent, and since this is equivalent to only 7.2 lb of TNT (0.036×200) or about 25 percent of the donor weight, it is evident that this type of explosive donor is not very effective in creating mixing of the propellants. Since the uncertainty in the yield estimate for this test condition is about a factor of 2 to 3, it cannot be concluded that the propellant yield is completely negligible compared to the donor, but it represents a relatively small additional contribution.

The average terminal yield for the two tests with the shaped charge was 1 percent, indicating that this arrangement of the explosive was also not very effective in creating propellant mixing, although per pound of donor charge, it was more efficient than the spherical charge.

Table 4-4
TERMINAL YIELDS FROM EXPLOSIVE DONOR TESTS

| Test No. | Donor Type | Terminal Yield |
|----------|-----------------------|----------------|
| 019 | 30-lb spherical donor | 3.4 |
| 030 | 30-lb spherical donor | 3.7 |
| 028 | 1-lb conical donor | 1.2 |
| 029 | 1-lb conical donor | 0.8 |

HYPERGOLIC CONFINEMENT-BY-THE-MISSILE TESTS

Tear Conditions

In this test condition, an internal failure, such as bulkhead rupture, is assumed to occur, and one propellant falls down onto the other. As stated in the discussion of this failure mode (Section 3), the time of interest for the in-tank confinement case is limited to the time the propellants remain confined by the walls of the missile. This time is determined by the strength of the tankage, the rate of reaction of the propellants, the initial tank pressure, and the initial ullage space.

Thus the primary requirement on the tankage is to contain the propellants in the proper length-to-diameter ratio until the internal pressure created by the hypergolic reaction exceeds the normal missile tankage design burst pressure. From communications with AFRPL and various missile manufacturers, it was determined that most missile fuel tankage would fail at internal pressures below 100 psi. Accordingly, this value (100 psi) was chosen as the design burst pressure for the tank.

Although it was not anticipated that the exact manner in which the tank fails would have an important effect on the explosion and/or the burning geometry of the propellants, it appeared worthwhile to make some effort to control the failure mode as well as the burst pressure. The simplest way to accomplish this was by making the bolts holding the two flanges together the weak point in the system.

The tank designed with these criteria is sketched in Fig. 4-17. It is a cylindrical tank with 2:1 ellipsoidal domed ends and is divided into two separate propellant compartments by a tempered glass diaphragm. The top compartment contains N_2O_4 and the bottom 50% UDMH - 50% N_2H_4 .

The shell of the tank was fabricated from 1/16-in.-thick aluminum. The domes were spun on hardwood molds from 1/16-in.-thick aluminum, and were heat-treated, after forming. Bolted flanges, 3/8-in. thick, were provided for the placement and sealing of the tempered glass diaphragm.

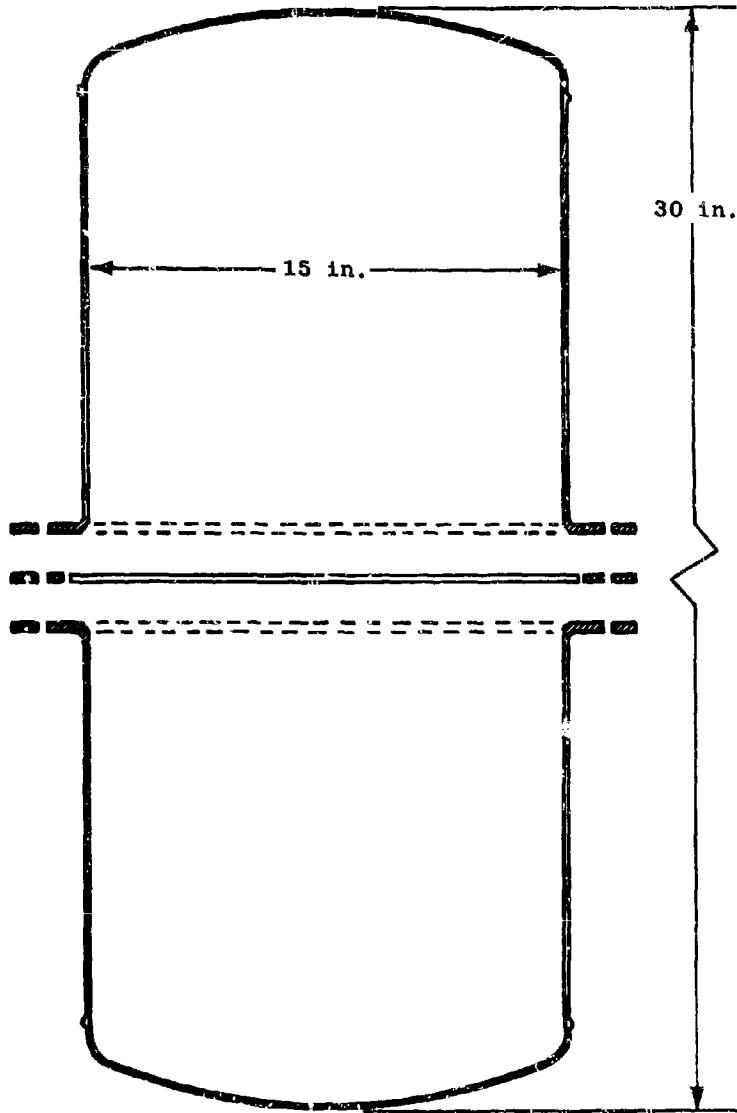


Fig. 4-17. Hypergolic Confinement-by-the-Missile Tank

This tempered glass diaphragm, 1/4 in. thick, was ruptured, by a six-bladed explosive-driven breaker ram, which is pictured in Fig. 4-18. This ram, which rests on the top surface of the tempered glass, is connected to a long drive shaft which extends up through the top of the tank through a gasket seal. An explosive charge (approximately 0.09 lb of C-4) is placed on a small plate affixed to the end of this drive shaft.

A total of three tests with 200 lb of total propellant were run. All tests had total propellant length-to-diameter ratios of 1.8 and oxidizer-to-fuel weight ratios of 1.9. The blast environment was measured using the basic PYRO instrumentation system, which is described in Appendix A.

Test Results

A total of three tests were conducted (Test Nos. 31, 32, and 35). The individual peak overpressure and positive-phase-impulse data, and the computed yield values from these data are presented in Volume 2. The computed terminal yield values were: Test No. 31 - 0.20 percent; Test No. 32 - 0.08 percent; and Test No. 35 - 0.15 percent.

Considering the relatively small uncertainty in individual readings obtained during the calibration tests of the instrumentation system (estimated to be 8 to 12 percent), it is clear that the major source of uncertainty in an average terminal yield value for this test condition arises from the variability in explosive yield from test to test. It is estimated that the average terminal yield for the three tests is uncertain by a factor of 2 to 3.

COMMAND DESTRUCT TESTS

Test Conditions

In this series of tests the propellants were contained in a cylindrical tank and separated by a tempered glass diaphragm. Failure of the tank was caused by two external linear-shaped charges (total weight of explosive 0.04 lb)

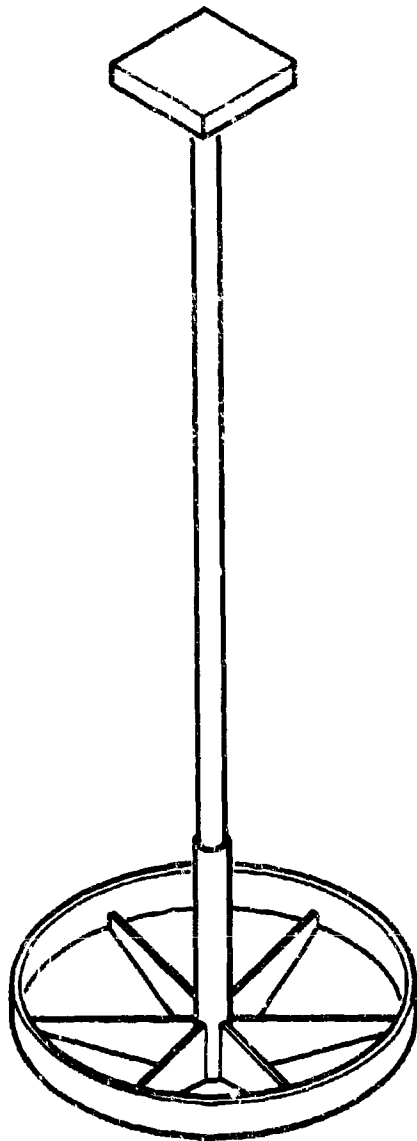


Fig. 4-18. Breaker Ram

vertically aligned against the tank wall. The charge on the lower tank was oriented 180 deg from that on the upper tank so that the propellants would spill simultaneously from openings on opposite sides of the container and initial propellant contact would be at or near the ground surface. The container was 29 in. in length and 15 in. in diameter with 1/16-in.-thick aluminum walls, as shown in Fig. 4-16.

The blast environment was measured as described above under Explosive-Donor Tests.

Two propellant tests were performed, each with a total propellant weight of 200 lb, a total propellant length-to-diameter ratio of 1.8, and an oxidizer-to-fuel weight ratio of 1.9.

Test Results

The individual peak overpressure and positive-phase-impulse data and the computed explosive yields obtained from this data are presented in Volume 2. The terminal yields from these tests were: Test No. 25 - 0.35 percent and Test No. 36 - 0.30 percent with an estimated uncertainty of 1.5 to 2.

TOWER DROP TESTS

Test Condition

In this series of tests the propellants were dropped from a 100-ft drop tower. This tower, which was designed and built for the PYRO Program, is shown in Figs. 4-19 and 4-20. The first 30 ft of this tower was a tripod constructed of 6-in.-diameter double-extra-strong pipes with cross bracing of high-strength cable. The remaining 71 ft of the tower was a high-strength version of a standard radio tower. The vertical members were fabricated from seamless mechanical tubing, and high-strength bolts were used throughout.

A T-shaped track was affixed down one side of the tower to guide the test tanks, which had thin walls and aluminum foil bottoms. These tanks were guided and held to the T-shaped track by a "skate" which clamped around the track by

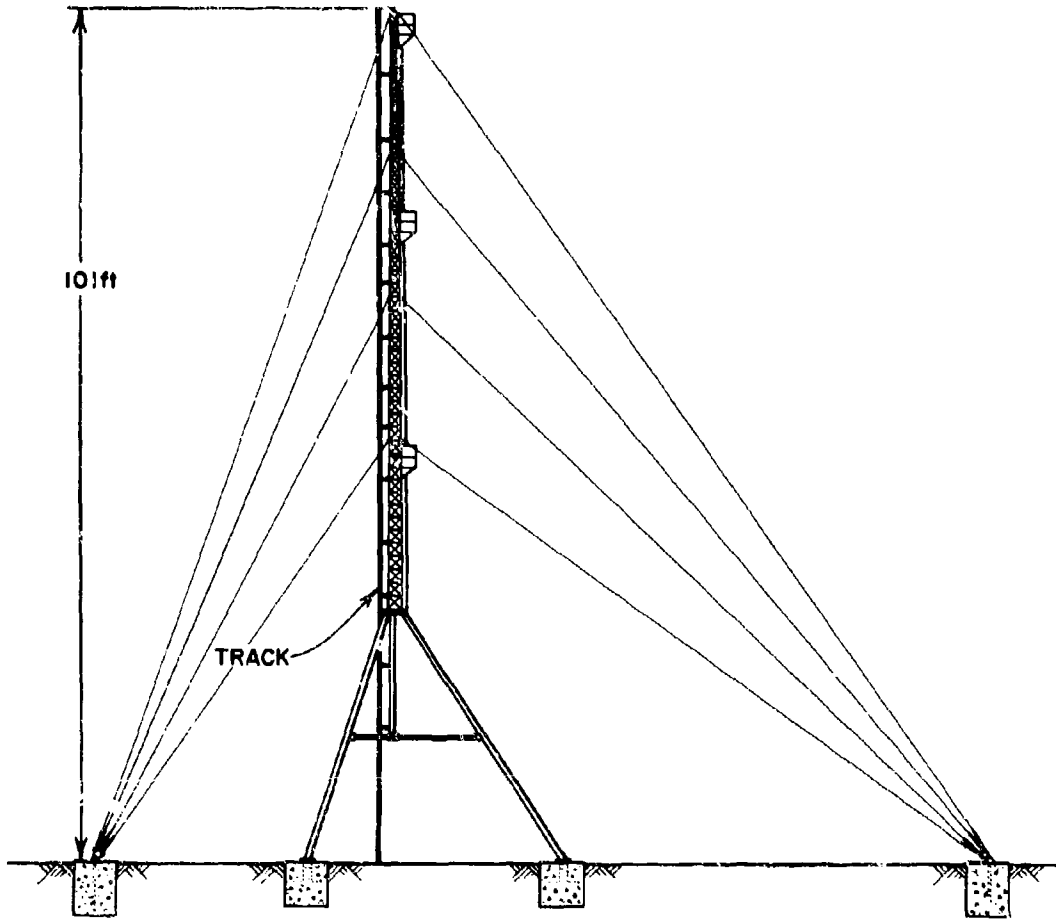


Fig. 4-19. Sketch of the Drop Tower

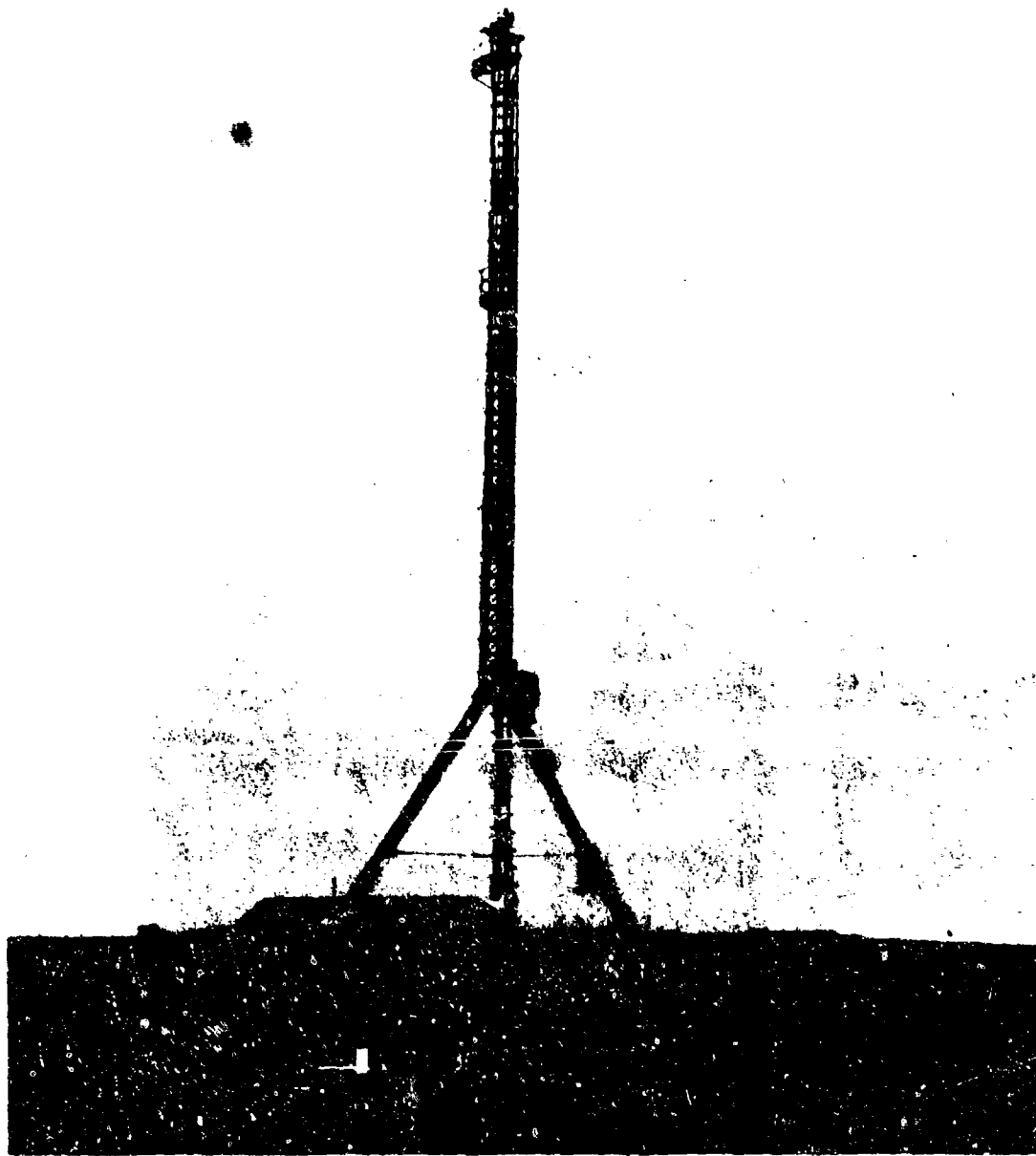


Fig. 4-20. Drop Tower

six roller-bearing wheels. Dropping of the tank was accomplished by an explosive cable-cutting device. A typical drop tank and guide assembly is shown in Fig. 4-21.

Diaphragm breaking was accomplished by a single six-pronged breaker installed on the pad at GZ. Immediately after the diaphragm was pierced, the tanks were abruptly stopped by a rigid frame at an elevation of approximately 2 ft above the test pad and the propellants allowed to flow out.

Three tests were performed with a propellant weight of 200 lb and three with a propellant weight of 1,000 lb. The propellant length-to-diameter ratios were 1.8 and the oxidizer-to-fuel weight ratios were 1.9.

Test Results

The individual peak overpressure and positive-phase-impulse data and the computed explosive yields obtained from these data are presented in Volume 2. The terminal yields from these tests are presented in Table 4-5. The data for these six tests was quite reproducible and there was no evidence of a scale effect between the 200- and 1000-lb tests. The estimated uncertainty for the case was estimated as 1.5 to 2.

COLD" PROPELLANT TESTS

The objective of this limited test series with hypergolic propellants was to determine whether a simple mixing technique could be used to obtain significant explosive yields when the propellants are cooled sufficiently to be nonhypergolic.

The oxidizer and fuel were cooled in separate open-topped containers by adding liquid nitrogen (LN_2) to each container. After a suitable cooling period the oxidizer and remaining LN_2 were poured into the fuel container. The intention was to initiate the mixture with a 32-lb TNT donor charge after the LN_2 had evaporated, approximately 2-1/2 min after the original mixing. In only one of the three tests, however, was this condition obtained. Brief descriptions of each test are given below:

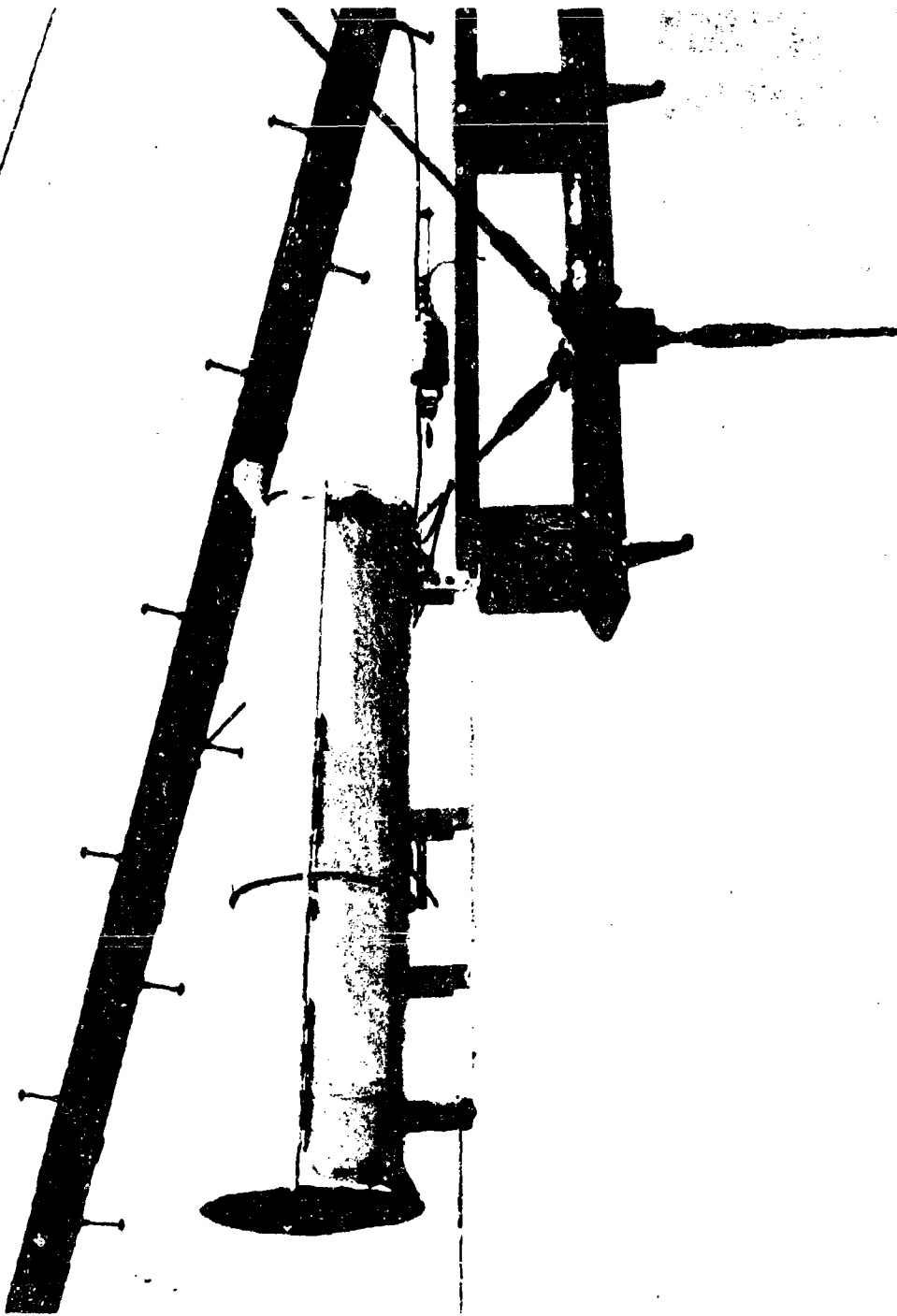


Fig. 4-21. Typical Drop Tank and Guide Assembly

Table 4-5
TERMINAL YIELDS FOR HYPERGOLIC DROP TESTS

| PROPELLANT WEIGHT (lb) | TEST NO. | TERMINAL YIELD (%) |
|------------------------------|-------------|--------------------------|
| 200 | 157 | 0.29 |
| | 158 | 0.20 |
| | 159 | 0.30 |
| 1000 | 189 | 0.35 |
| | 257 | 0.30 |
| | 258 | 0.25 |

- Test 033. Explosion of the propellants occurred 5 sec after the propellants were united (and before the donor charge was initiated) due to a fire on the ground surface from spilled propellants. A posttest survey revealed sufficiently numerous TNT fragments to suggest that little, if any, of the explosive-donor charge detonated.
- Test 034. Failure of the oxidizer transfer system prevented mixing of the propellants, and the 32-lb charge was detonated to provide reference data for the other tests.
- Test 037. The frozen propellant mixture was initiated by detonating the 32-lb TNT donor charge 2-1/2 min after uniting the propellants.

In all tests the TNT charge, which was rectangularly shaped, was located on the ground just beneath the fuel tank. This tank was cylindrical with a diameter of 22 in. and was made of 1/16-in.-thick aluminum.

The blast environment was measured as described above under Explosive-Donor Tests.

The individual peak overpressure and positive-phase-impulse data and the computed explosive yields obtained from these data are presented in Volume 2.

The terminal yields for these tests were: Test 033, 8.8 percent; and Test 037, 13.7 percent. The terminal yield for Test 033 was computed by assuming that the donor charge did not contribute to the blast. The yield for Test 034 (donor charge only) agreed with the expected value for 32 lb. Thus the yield for Test 037 (donor charge and propellants) was obtained by subtracting 32 lb (the weight of the donor) from the total effective charge weight of the explosion. Since the two tests were not identical, the uncertainty in terminal yields for both test conditions was estimated, on the basis of the test-to-test reproducibility of other tests, to be about a factor of 2.

PART 2 - SUMMARY OF DATA FROM OTHER SOURCES

This part of the report presents the available information concerning the explosive potential of this hypergolic propellant combination as obtained from a number of experimental programs other than Project PYRO.

The various experimental programs can be divided into two general categories: those in which the propellant mixing generally simulated that which might occur from failure of full-scale vehicles and those special tests in which the propellant mixing mode did not correspond to any credible full-scale failure mode but rather was selected to study the influence of some particular parameter of the mixture on the explosive yield.*

The simulated failure mode tests can be further subdivided into two categories by the manner in which the propellants come into contact with each other:

1. Diaphragm rupture. In this case the propellants are initially in separate cylindrical tanks, one on top of the other, with either a common bulkhead or closely adjacent bulkheads. The failure is created by rapidly removing all or most of the bulkhead (or bulkheads) separating the two propellants, allowing reaction to occur within the remainder of the tankage. Two test programs have been conducted which fit in this category, one by Aerojet General (Ref. 4-4) and one by Martin Marietta (Ref. 4-5).
2. Spill. In this case the propellants are spilled from tanks, and interaction of propellants occurs external to the tank, generally on the underlying surface. The tanks initially are near the surface, so that the impact velocity of the fluids is generally low. Confinement of the spilled propellants varies from shallow basins on the ground to model silos. Five series of tests have been conducted which fit in this category, four by Rocketdyne (Ref. 4-6) and one by the U.S. Army Chemical Research and Development Laboratories (CRDL) (Refs. 4-7 and 8).

Three test series were conducted which were not intended to simulate any credible full-scale failure mode.** These are listed below:

- a. Aerojet high-interface-area tests (Ref. 4-9). In this series the objective was to find the importance of interface area and oxidizer-to-fuel ratio on explosive yield.

* It should be noted that not all of the test series listed under the simulated failure mode category were specifically classified in that manner by the experimenters; however, it is the opinion of the authors of this report that they can be logically so considered.

** The initial interface area per unit volume of propellants used in series a and b is considered to be much greater than could be obtained in a credible full-scale failure and the quantities of propellants involved in series c were too small to be applicable.

- b. Aerojet detonation tests (Ref. 4-10). In this series, the objective was to determine whether steady-state detonation could be induced in well-mixed propellants.
- c. Atlantic Research Laboratory tests (Ref. 4-11). This series of tests was exploratory in nature and involved tests with milliliter quantities of propellants.

SIMULATED FAILURE MODE TESTS

Diaphragm Rupture

Aerojet Diaphragm Rupture Tests* (Ref. 4-4)

This program consisted of two series of tests in which the propellants were contained in a cylindrical tank separated by a diaphragm. The two sets of tests differed basically in the mechanism employed to rupture the diaphragm and in the propellant orientation.

For one set of tests, the propellant container was elevated to a height of 15 ft. The upper section, which in this case contained the oxidizer, also contained a vertically aligned rod or ram along the container axis. The entire container was permitted to fall, and upon impact with the ground surface, the impulse associated with the changing momentum of both the ram and the upper propellant fractured the diaphragm (tempered glass). Near-total removal of the diaphragm was achieved by a ring attached to the ram.

It is possible that continued motion of the ram influenced the propellant reaction. Further, it is likely that the upper propellant retained some fraction of its motion relative to the lower propellant upon diaphragm rupture.

For the other tests, in which the fuel was the upper propellant, diaphragm rupture was accomplished by the explosion of a linear charge that had been formed into a circular loop and placed immediately above the diaphragm (0.003-in. aluminum foil).

* In this discussion only the basic overpressure and impulse data were obtained from the original reference. All yield computations and error estimates were made for this report.

The arc of the charge loop was just less than one complete circumference. Consequently a small arc remained uncut and acted as a diaphragm hinge, thereby insuring against propellant reaction influence by the diaphragm (the correctness of these statements was tested using water in the upper tank).

Flexible plastic tubing was placed over the linear charge in order to contain explosive fragments and prevent initiation of the fuel. The tubing permitted transfer of the shock wave in sufficient intensity to rupture the diaphragm without rupture of the tubing wall or damage to the tank walls.

For both test types, the container was 0.10-in.-thick aluminum with a diameter of 17-3/4 in.

The blast wave environment was measured by means of an array of 12 piezoelectric gauges located 10, 25, and 40 ft from ground zero along four radial lines at 90 deg to each other.

Calibration tests were conducted with cylindrical, 5-lb TNT and composition C-4 charges and 25-lb TNT charges, which were positioned near the ground surface. The authors of Ref. 4-4 stated that a comparison of the calibration test results with the data of Ref. 4-2 indicated a negligible difference at all gauge distances.

Two tests for each failure mode were conducted, although for one of the linear charge tests, the results were of insufficient magnitude to record. All tests were with 300 lb total propellant, a propellant length-to-diameter ratio of 1.6:1, and an oxidizer-to-fuel weight ratio of 2:1.

Propellant test results in terms of peak overpressure and positive phase impulse are given in Table 4-6. Explosive yields relative to a TNT surface burst were computed from the average overpressure and impulse at each distance for each test and are listed in Table 4-7.

For the two drop tests it is evident that there was a slight but fairly consistent tendency for yield to increase with distance over the range from 10 to 25 ft. Accordingly, terminal yield values for each test were computed

Table 4-6
 PEAK OVERPRESSURE AND POSITIVE-PHASE-IMPULSE DATA
 FROM AEROJET DIAPHRAGM RUPTURE TESTS

| Test Type | Test No. | Gauge Line | Distance (ft) | | | | | |
|---------------|----------|------------|----------------|------------------|----------------|------------------|----------------|------------------|
| | | | 10 | | 25 | | 40 | |
| | | | Pressure (psi) | Impulse (psi-ms) | Pressure (psi) | Impulse (psi-ms) | Pressure (psi) | Impulse (psi-ms) |
| Drop | 7 | 1 | 1.8 | 5.4 | 0.8 | 2.0 | 0.5 | 1.6 |
| | | 2 | 9.7 | 12.1 | 2.1 | 3.6 | 1.0 | 2.1 |
| | | 3 | 9.2 | 9.3 | 2.5 | 5.8 | 1.1 | 2.2 |
| | | 4 | 2.0 | 6.2 | 1.0 | 2.1 | 0.6 | 1.3 |
| | | Avg | 5.4 | 8.3 | 1.6 | 3.6 | 0.8 | 1.8 |
| Drop | 8 | 1 | 3.0 | 10.4 | 1.3 | 5.4 | 0.8 | 3.3 |
| | | 2 | 10.1 | 13.0 | 3.6 | 6.6 | 1.9 | 4.2 |
| | | 3 | 17.0 | 21.7 | 4.8 | 7.7 | 2.5 | 4.7 |
| | | 4 | 5.0 | 10.2 | 2.8 | 5.0 | 1.5 | 2.9 |
| | | Avg | 8.8 | 13.8 | 3.1 | 6.2 | 1.7 | 3.8 |
| Linear Charge | 4 | 1 | 0.5 | * | 0.3 | * | 0.4 | * |
| | | 2 | 0.5 | * | 0.2 | * | 0.1 | * |
| | | 3 | 0.5 | * | 0.2 | * | 0.1 | * |
| | | 4 | 0.6 | * | 0.2 | * | 0.1 | * |
| | | Avg | 0.5 | * | 0.2 | * | 0.2 | * |

* No impulse values were obtained due to low overpressure values.

Table 4-7
 EXPLOSIVE YIELDS FROM
 AEROJET DIAPHRAGM RUPTURE TESTS

| Test Type | Test No. | Basis of Computation | Yield (%) at Indicated Distances (ft) | | | Terminal Yield (%) |
|---------------|----------|----------------------|---------------------------------------|-------|-------|--------------------|
| | | | 10 | 25 | 40 | |
| Drop | 7 | Pressure | 0.12 | 0.16 | 0.14 | 0.24 |
| | | Impulse | 0.33 | 0.37 | 0.31 | |
| Drop | 8 | Pressure | 0.37 | 0.65 | 0.74 | 0.80 |
| | | Impulse | 3.83 | 0.89 | 0.91 | |
| Linear Charge | 4 | Pressure | 0.001 | 0.002 | 0.007 | 0.007 |
| | | Impulse | - | - | - | |

by averaging the overpressure and impulse data from the 25- and 40-ft stations. The resulting values were 0.24 percent for Test 7 and 0.80 percent for Test 8. It may be noted that the overpressure and impulse values for Test 8 were within about 25 percent of each other, while for Test 7 the overpressure yield was less than one-half of the impulse yield.

Although there was considerable variability in the four gauge readings at each distance for the drop tests, the major source of error in yield estimation for this test condition again appears to be the test-to-test reproducibility of the blast phenomena. Based on the difference in terminal yield between the two tests, a factor of 3.3, it is estimated that the mean terminal yield for the two shots, 0.52 percent, is uncertain to a factor of about 3 or 4.

For the one linear charge test there was a tendency for yield to increase with distance over the entire range so that the terminal yield was taken to be the yield of the last station. The resulting terminal yield for this test condition, 0.007 percent, is estimated to be uncertain by a factor of about 5.

One-Half-Scale Titan II First-Stage Destruct Test* (Ref. 4-5)

The half-scale model tanks used in this test were 5-ft-diameter cylinders with domed ends. The upper tank, containing 21,500 lb of oxidizer, was approximately 15 ft long and the lower tank, containing 11,200 lb of fuel, was approximately 13 ft long. The separation between tanks was about 2 ft.

Failure of the tankage was induced by simultaneously rupturing the bottom dome of the top tank and the top dome of the bottom tank with a linear-shaped charge containing approximately 0.5 lb of explosive. The linear charge was arranged to rupture a minimum of 75 percent of the tank domes.

The blast environment was measured with eight pressure transducers ranging from approximately 10 to 300 ft from the test tankage. Five of the transducers were mounted on a frame surrounding the test tankage and were

*In this discussion only the basic overpressure values were obtained from the original reference. All correction factors, yield computations, and error estimates were made for this report.

within 20 ft of the tankage. The remaining three transducers were mounted at a height of 23 ft above the ground on one radial line at ground distances of 61, 141.9, and 300.5 ft from the center of the tankage.

The propellant reaction following rupture of the tankage produced two separate explosions, both of which were stated to have occurred in the vapor phase. One occurred at approximately 3.1 sec after the initial destruct action, and the other occurred at approximately 4.3 sec after the destruct action. The position of the explosions could not be determined very accurately. The center of the 3.1-sec explosion was estimated to be at a height of about 28 ft and about 20 ft in a horizontal direction from the centerline of the tank, and that for the 4.3-sec explosion, at a height of about 40 ft and about 5 ft horizontally from the centerline of the tank.

Because of the uncertainty in the location of the centers of the two explosions, the pressure data obtained from the five close-in transducers was not suitable for estimating the yields of the two explosions. The low magnitudes of the peak pressure values obtained at these stations, however, which ranged from 3 to 10 psi, certainly support the postulate of a low-energy-density reaction.

The pressures measured at the three long-distance stations for each explosion are given in Table 4-8.

Since the pressure measurements were made well above the ground surface and since the height of the explosion was also well above the ground surface, it was necessary to use two special reference curves in computing yields. From the assumed geometry it is concluded that Station 6 was at a distance where Mach reflection was just starting, so that the gauge (23 ft above the ground) was well above the triple point and in the region where the incident and reflected wave fronts were separate. At the distance of Station 7 the triple point had increased in height but still should have been below the gauge. Station 8 was well in the Mach reflection region, and the triple point should have been significantly higher than the gauge.

Table 4-8
 PEAK OVERPRESSURE DATA FROM ONE-KALF-SCALE
 TITAN II FIRST-STAGE DESTRUCT TEST

| Gauge Station | Nominal Ground Distance (ft) | Explosion ¹ | Estimated Actual Distance ² (ft) | Peak Over-Pressure (psi) | Yield (%) |
|---------------|------------------------------|------------------------|---|--------------------------|-----------|
| 6 | 61.0 | 3.1 | 48 | 3.0 | 0.1 |
| | | 4.2 | 60 | 5.0 | 0.4 |
| 7 | 141.9 | 3.1 | 128 | 1.0 | 0.2 |
| | | 4.2 | 140 | 1.1 | 0.3 |
| 8 | 300.5 | 3.1 | 287 | 0.5 | 0.1 |
| | | 4.2 | 297 | 1.3 | 0.9 |

1. Refers to 3.1-sec or 4.2-sec explosion.

2. For stations 6 and 7, value is slant distance from the point of explosion to the gauge; for station 8, it is the ground distance from the point of explosion to the gauge.

It is assumed that the peak overpressure values reported for Stations 6 and 7 corresponded more nearly to the incident or free-air values rather than the reflected values seen at ground level. Accordingly in computing yields, which are also given in Table 3-8, a free-air pressure-distance TNT curve (Ref. 4-12) was used for the data from Stations 6 and 7.

Station 8 was well below the triple point, so that the pressures from this station should correspond fairly well to those at ground level. The explosions, however, were too far off the ground surface to allow use of the TNT surface burst reference curve. Actually the explosions in the two previous diaphragm rupture cases also were not truly surface bursts, but in most cases they were estimated to be at a height of burst of less than 1 or 2 ft/lb^{1/3}. In the present case the best estimate of the scaled height of burst is of the order of 5 to 6 ft/lb^{1/3}. To maintain consistency in the analysis of these data with the two previous sets, the yields for Station 8 were first computed using the same surface burst reference curve and then reduced by a factor of 2.3 to account for the difference between a 5-ft/lb^{1/3} and a 1-ft/lb^{1/3} height of burst. This correction factor was based on data given in Fig. 3.67b of Ref. 4-13 for the pressure region of 1 psi.

Prior to computing yields it was also necessary to correct the overpressures to their equivalent sea level values by means of Sachs' scaling law (Ref. 4-15). (These tests were conducted at an atmospheric pressure of 11.7 psi and the reference data are for 14.7 psi.)

Because of the data scatter, it is difficult to be sure whether there is any significant tendency for the yield to increase with distance or not. However, since primary interest is in the 4.2-sec explosion (the largest), which has some of this tendency, the estimated terminal yield value for this test, 0.6 percent, was obtained in a conservative (high) fashion by averaging only the values from the last two stations.

Considering the small number of useful gauge readings (2), the small yield values, the lack of impulse data, the variability in results from gauge to gauge (factor of 3), and the fact that only one test was conducted, it is estimated that the terminal yield value has an uncertainty of a factor of 2 or 3.

Spill TestsRocketdyne Small-Scale Spill Tests* (Ref. 4-6)

This test program consisted of two separate series of tests, the first using straight hydrazine and the second a 50/50 mix of UDM²-hydrazine. In general, the experimental arrangement was quite similar for both. Approximately 2.5 lb of propellant were fed through separate 1/2-in. lines under a pressure of 30 to 50 psi into a concrete spill basin 6 in. deep.

In the first series, the basin had a 2- by 2-ft cross section and the spill lines discharged the propellants from about 1 ft above the surface so that they impinged on the basin within a radius of 2 in. The parameters varied in the tests were the nature of the surface of the basin (concrete, asphalt, dirt, or water) and the order in which the propellant flows were started (simultaneous or lead of one propellant by the other by about 1 sec).

In the second series of tests, the concrete spill basin was 4 by 4 ft in cross section and the spill lines were 8 in. above the surface, arranged so that the propellants impinged on the basin within a radius of 2 to 4 in. The parameter variation included the nature of the surface of the basin (concrete, dirt, or water) and the order in which propellant flows started (simultaneous or lead of one propellant by the other by 0.5 - 1.0 sec).

In both series of tests, pressures were measured with two Photocon microphone pickups, at distances of 10 and 15 ft, mounted face-on to the center of the basin at a height of 1 ft.

The propellant combinations ignited within a few milliseconds after contact under all conditions tested. A series of explosions was recorded in most cases. The pressure values for the largest explosion on each test of the first series are given in Table 4-9 along with the corresponding yields computed by means of

* In this discussion only the basic overpressure data were obtained from the original reference. All yield computations, statistical analyses, and error estimates were made for this report.

Table 4-9
PEAK OVERPRESSURE AND EXPLOSIVE YIELD DATA FROM THE FIRST
SMALL-SCALE SPILL TEST SERIES

| Test No. | Surface | Order of Spill | Propellant Wt (lb) ¹ | Pressure (psi) ² | | | Yield (%) | | Terminal Yield (%) |
|----------|----------|----------------|---------------------------------|-----------------------------|-------|-------|-----------|------|--------------------|
| | | | | 10 ft | 15 ft | 10 ft | 15 ft | | |
| 55 | Concrete | Simultaneous | 1.5/1.0 | 0.82 | 0.33 | 0.06 | 0.03 | 0.04 | |
| 56 | Concrete | Fuel lead | 1.5/1.3 | 2.0 | 1.81 | 0.38 | 1.0 | 0.69 | |
| 57 | Concrete | Oxidizer lead | 2.0/1.0 | 1.0 | 0.67 | 0.07 | 0.11 | 0.09 | |
| 58 | Asphalt | Simultaneous | | No detectable explosion | | | | | |
| 59 | Asphalt | Fuel lead | 1.5/1.3 | 1.54 | 0.67 | 0.21 | 0.11 | 0.16 | |
| 60 | Asphalt | Oxidizer lead | 2.0/1.0 | 1.23 | 0.71 | 0.12 | 0.12 | 0.12 | |
| 61 | Dirt | Simultaneous | 1.5/1.0 | 1.46 | 0.76 | 0.21 | 0.17 | 0.19 | |
| 62 | Dirt | Fuel lead | 1.5/1.3 | 1.91 | 1.62 | 0.34 | 0.80 | 0.57 | |
| 63 | Dirt | Oxidizer lead | 2.0/1.0 | 1.73 | 1.24 | 0.25 | 0.41 | 0.33 | |
| 64 | Water | Simultaneous | 1.5/1.9 | 1.82 | 1.23 | 0.35 | 0.53 | 0.44 | |
| 65 | Water | Fuel lead | 1.5/1.3 | 1.23 | 0.62 | 0.13 | 0.10 | 0.12 | |
| 66 | Water | Oxidizer lead | 4.0/2.0 | 1.05 | 0.43 | 0.04 | 0.02 | 0.03 | |

1. First number refers to oxidizer weight, second number to fuel weight.

2. Pressures are peak reflected overpressures.

a surface-burst TNT reference curve.* The results of the second test series are given in a similar fashion in Table 4-10. The investigators reported that (based on examination of high-speed photography) all explosions occurred in the vapor phase.

It is apparent from Tables 4-9 and 4-10 that there is no consistent trend for yields to increase from 10 to 15 ft, although the average yield at 15 ft is somewhat greater than that at 10 ft. Accordingly, terminal yield values were obtained by averaging the two data points.

The data in Table 4-9 suggest that a fuel lead or dirt surface tends to give the highest yields and that an oxidizer lead or asphalt surface tends to give the lowest yield. However, since there were no repeat tests or calibration data reported, it is difficult to be sure whether the observed differences are significant. In an effort to estimate the significance of the effects and the reproducibility of results, the data given in Table 4-9 were treated as a single replicate factorial design, and an analysis of variance was performed (with the type of surface and order of propellant release the two variables) on the assumption of no interaction between variables. The results, which were computed separately for each gauge station, indicated that statistically, with the above assumptions, there was no significant effect of either the type of surface or order of flow. The calculated remainder standard deviations (expressed as percentages of the mean values) were 70 and 110 percent for the 10- and 15-ft stations, respectively.

The data in Table 4-10 are consistent with those from Table 4-9 in regard to the effect of the order of flow; however, they suggest that a concrete surface gives higher yields than the dirt surface, which is in contrast to the trend indicated by Table 4-9. Because of the similarity of test conditions in the two test series, it is assumed that they both have the same uncertainty and that the

* The authors of Ref. 4-6 reported that photographic evidence indicated the explosions occurred from 2 to 4 ft off the surface. Thus the use of a surface burst reference curve may not be appropriate even though the liquid propellants were on the surface. It was used, however, to maintain consistency throughout all spill test series. (In the other series no comments were made regarding the explosion height of burst.)

Table 4-10
 PEAK OVERPRESSURE AND EXPLOSIVE YIELD DATA FROM THE
 SECOND SMALL-SCALE SPILL TEST SERIES

| Test No. | Surface | Order of Spill | Propellant Wt (lb) ¹ | Pressure (psi) ² | | Yield (%) | | Terminal Yield (%) |
|----------|------------------------|----------------|---------------------------------|-----------------------------|---------------------------|-----------|-------|--------------------|
| | | | | 10 ft | 15 ft | 10 ft | 15 ft | |
| 1 | Concrete | Simultaneous | 1.5/1.0 | 1.67 | - | 0.28 | - | 0.28 |
| 2 | Concrete | Simultaneous | 1.5/1.0 | 1.63 | 0.67 | 0.27 | 0.13 | 0.20 |
| 3 | Concrete | Oxidizer lead | 2.0/0.5 | | No overpressures recorded | | | |
| 4 | Concrete | Fuel lead | 1.0/1.5 | 1.67 | 1.41 | 0.28 | 0.66 | 0.47 |
| 5 | Dirt | Simultaneous | 1.5/1.0 | 1.17 | 0.60 | 0.13 | 0.10 | 0.12 |
| 6 | Dirt | Oxidizer lead | 2.0/0.5 | 0.55 | 0.40 | 0.02 | 0.04 | 0.03 |
| 7 | Dirt | Fuel lead | 1.0/1.5 | 0.77 | 0.52 | 0.05 | 0.07 | 0.06 |
| 8 | Water covered concrete | Simultaneous | 1.5/1.0 | 0.87 | 0.44 | 0.07 | 0.05 | 0.06 |
| 9 | Water covered concrete | Fuel lead | 1.0/1.5 | | No overpressures recorded | | | |

i. First number refers to oxidizer weight, second number to fuel weight.

2. Pressures are peak reflected overpressures.

observed differences in Table 4-10 are no more significant than those for Table 4-9. Accordingly, an average terminal yield was computed for all data for each test series. The result is 0.25 percent for Table 4-9 and 0.18 percent for Table 4-10. Considering the fact that only two data points were available for each test, that no calibration data were reported, and that no impulse data were available, it is estimated that these average yield values are uncertain to about a factor of 2 or 3.

CRDL Suppression Tests * (Refs. 4-7 and 4-8)

In this test series, the primary objective was to determine the effectiveness of water deluge and water fog systems in suppressing the fire and fumes resulting from "spilled" propellants. Blast data were of secondary concern, so that only a single pressure transducer, located near the ground 50 ft from the center of the reaction pan, was used.

The propellants were fed through separate 1-1/2-in. lines oriented 180 deg to each other, so that initial fuel and oxidizer contact occurred before the propellants had an opportunity to disperse on the surface. The pipe nozzles were slightly off the ground and 2 ft from the center of a 16- by 16- by 1-ft-deep reaction pan.

The water deluge system was located on the interior perimeter of the reaction pan and consisted of a 2-in. feed pipe with twelve 1-in. outlets equally spaced around the pan. These outlets could, for example, deliver water at a rate of 415 gal/min at an outlet pressure of 48 psig.

The water fog system consisted of thirty-six 1-in. fog nozzles arranged in three tiers at heights of 2, 10.5, and 17.5 ft above the outside perimeter of the reaction pan, each tier having twelve equally spaced nozzles. This system could, for example, deliver water at a rate of 800 gal/min at a nozzle pressure of 48 psig.

* In this discussion only the basic overpressure data were obtained from the original reference. All yield computations, statistical analyses, and error estimates were made for this report.

Tests were conducted for a range of total propellant weights from 90 to 300 lb and with and without the water deluge and fog suppression. The propellant flow durations ranged from about 6 sec for the 90-lb tests to about 20 sec for the 300-lb tests. It should be noted that the suppression systems were usually started about 6 to 7 sec after the propellant flows started, so that for an appreciable portion of the flow duration, the propellants were actually unsuppressed.

Because of the long durations of flow in this test series, it did not seem appropriate to compute yields with the total propellant quantity. Instead, yields were calculated by means of the estimated amount of propellant delivered in a 1-sec period, a time corresponding approximately to that used for the other spill test series. The best estimate of the amount of propellant delivered in 1 sec is 15 lb; however, this value is uncertain to about a factor of 1.5 to 2.0.

Peak overpressures and explosive yields (in both pounds and percent) for each test are listed in Table 4-11.

In an effort to estimate the significance of the variables (i.e., propellant weight and degree of suppression), average yields were computed for each test condition. These average yields were then treated as a single replicate factorial design and an analysis of variance was performed on the assumption of no interaction between variables. The results indicated that statistically, with the above assumption, there was no significant effect of propellant weight or degree of suppression. The calculated remainder standard deviation (expressed as a percent of the mean value) was 50 percent.

Considering the fact that only one data point was available from each test, that no impulse data or calibration data were reported, and that the yields were very small, it is estimated that the average yield for these tests, 0.02 percent, is uncertain to a factor of about 4 to 5.

Table 4-11

PEAK OVERPRESSURE AND EXPLOSIVE YIELD DATA FROM CRDL SUPPRESSION TESTS

| Prop Wt (lb) | Reaction Suppressed-Unsuppressed | Test No. | Pressure (psi) | Effective Charge Wt (10^{-3} lb) | Yield (%) |
|--------------|----------------------------------|----------|--------------------|-------------------------------------|-----------|
| 90 | U | 5 | 0.07 | 4.4 | 0.029 |
| | | 15 | 0.05 | 2.2 | 0.015 |
| | S | 4 | 0.045 | 1.7 | 0.011 |
| | | 6 | 0.045 | 1.7 | 0.011 |
| 120 | U | 7 | No results | | |
| | | 8 | 0.06 | 3.2 | 0.021 |
| | S | 9 | 0.065 | 3.8 | 0.025 |
| 150 | U | 10 | Instrument Failure | | |
| | | 11 | 0.04 | 1.3 | 0.009 |
| | S | 12 | 0.02 | 0.3 | 0.002 |
| 210 | U | 13 | 0.03 | 0.7 | 0.005 |
| | | 16 | 0.09 | 7.4 | 0.049 |
| | | 21 | 0.04 | 1.3 | 0.009 |
| | | 22 | 0.05 | 2.2 | 0.015 |
| | S | 17 | 0.065 | 3.6 | 0.024 |
| | | 18 | 0.035 | 1.0 | 0.007 |
| | | 19 | 0.05 | 2.3 | 0.015 |
| | | 20 | 0.035 | 1.1 | 0.007 |
| 240 | U | 23 | 0.06 | 2.9 | 0.019 |
| | | 27 | 0.04 | 1.3 | 0.009 |
| | S | 31 | 0.06 | 3.2 | 0.021 |
| | | 28 | 0.045 | 1.7 | 0.011 |
| 270 | U | 29 | 0.08 | 5.7 | 0.038 |
| | | 30 | 0.04 | 1.3 | 0.009 |
| | | 32 | 0.05 | 2.2 | 0.015 |
| | S | 33 | 0.06 | 3.2 | 0.021 |
| | | 34 | 0.09 | 7.4 | 0.049 |
| 300 | U | 35 | 0.045 | 1.7 | 0.011 |
| | | 36 | 0.06 | 3.2 | 0.021 |
| | | 37 | 0.12 | 13.0 | 0.087 |
| | | 38 | 0.05 | 2.2 | 0.015 |
| | S | 39 | 0.05 | 2.2 | 0.015 |
| | | 40 | 0.045 | 1.7 | 0.011 |
| | | 41 | 0.04 | 1.3 | 0.009 |
| | | 42 | 0.03 | 1.1 | 0.007 |

Rocketdyne Model-Missile Spill Tests* (Ref. 4-6)

This program consisted of scale-model missile spills in which the propellants were initially contained in separate cylindrical tanks with the oxidizer on top and allowed to fall to the underlying surface by pneumatically removing the bottom plate of the tank. The tank sizes used corresponded to 1/18 and 1/10 linear scale models of the Titan II (scaled by volume only). The 1/18-scale tank, which was 8.5 in. in diameter and 14 in. high, contained 50 lb of propellant with an oxidizer-to-fuel weight ratio of 2:1 and the 1/10-scale tank, which was 12 in. in diameter and 36 in. high, contained 300 lb of propellant at the same oxidizer-to-fuel ratio. On the 1/18 scale the tanks were separated by approximately 28 in. and on the 1/10 scale by 24 in. Neglecting this spacing, the effective L/D of the 1/18-scale tanks is about 3.3:1 and that for the 1/10 scale about 6:1.

The spill tests were performed in 1/18- and 1/10-scale silo models and aboveground in a steel tray approximately 20 by 20 by 2 ft deep. The silo dimensions were 3 ft in diameter by 9 ft deep and 5 ft in diameter by 15 ft deep for the 1/18-scale and 1/10-scale silos, respectively.

Pressure measurements were made at ground level on one gauge line at distances of 25, 35, 50, and 75 ft from the center line of the tanks.

Other variables included in the testing were the order in which propellants were released from the tanks (simultaneous as well as oxidizer and fuel leads) and the silo environment prior to spill (burning fuel, light oxidizer and fuel vapors, and water in the bottom).

The pressure data obtained from these tests are given in Table 4-12. In cases where multiple explosions occurred, only the data from the largest are given. Only data from the 25- and 75-ft stations are included since the authors of Ref. 4-6 indicate that the data from the 35- and 50-ft stations could not be trusted. Yield values computed using a surface burst TNT curve are also given in this table.

* In this discussion only the basic overpressure data were obtained from the original reference. All yield computations and error estimates were made for this report.

It can be seen from Table 4-12 that in all cases yield values were increasing up to the 75-ft station, so that the data from this station represent the best estimate of the terminal yield values.

Several TNT calibration shots were also performed as part of this test series. The overpressure values and yields computed by means of a surface burst reference curve are included in Table 4-12. The results from the one above-surface test indicate that the instrumentation system is reading high by almost a factor of 2.

Only one repeat TNT calibration shot and one repeat propellant test were available to estimate the reproducibility of the instrumentation system and the test-to-test blast phenomena. The repeat calibration shot was for the silo geometry. They showed a difference of 40 percent in yield. The repeat propellant tests (12 and 15) were also for a silo geometry. They showed a difference of about 15 percent in yield.

Considering that the terminal yield values are based on one gauge reading, that the yields are very small, that the results from the calibration shots differed by a factor of almost 2 from the standard curve, and that no impulse data were available, it is estimated that the absolute values of the terminal yields are uncertain to about a factor of 3 or 4. On a relative basis, the results are probably uncertain to a factor of 2 or 3.

Rocketdyne Large-Scale Spill Tests^{*} (Ref. 4-6)

In these two tests the propellants were contained in separate 150-gal cylindrical tanks, 30 in. in diameter and approximately 60 in. in length. The tanks were positioned adjacent to each other with their cylindrical axes tilted 20 deg from the vertical so that the propellants discharging from a 6-in. port at the bottom of the tank would impinge in the center of 20- by 20- by 2-ft deep spill tray.

* In this discussion only the basic overpressure data were obtained from the original reference. All yield computations and error estimates were made for this report.

Table 4-12
 PEAK OVERPRESSURE AND EXPLOSIVE YIELD DATA
 FROM THE MODEL-MISSILE TESTS

| Test No. | Scale | Order of Spill | Overpressure (psi) | | Yield (%) | |
|-------------|-------|--|--------------------|--------------|-----------|-------|
| | | | 25 ft | 75 ft | 25 ft | 75 ft |
| ABOVEGROUND | | | | | | |
| 7 | 1/10 | 60-sec fuel lead | | No explosion | | |
| 8 | 1/10 | 2-sec oxid lead | 0.5 | 0.16 | 0.01 | 0.03 |
| 9 | 1/10 | 60-sec oxid lead | | No explosion | | |
| 10 | 1/10 | 60-sec oxid lead | | No explosion | | |
| 11 | 1/10 | Simultaneous | 1.0 | 0.36 | 0.05 | 0.15 |
| | | 10 lb TNT | 11.0 | 1.92 | 180 | 190 |
| STEEL SILO | | | | | | |
| 12 | 1/18 | Simultaneous | 0.9 | 0.32 | 0.27 | 0.73 |
| 13 | 1/18 | 30-sec oxid lead | 0.85 | 0.28 | 0.24 | 0.55 |
| 14 | 1/18 | Simultaneous into burn fuel | 0.8 | - | 0.20 | - |
| 15 | 1/18 | Simultaneous | 0.85 | 0.3 | 0.24 | 0.63 |
| 16 | 1/18 | Simultaneous into 50 lb of water | 0.6 | 0.15 | 0.11 | 0.14 |
| 17 | 1/10 | 1-sec oxid lead | 2.0 | 2.0* | 0.26 | - |
| 18 | 1/10 | Simultaneous with light oxid vapor | 2.5 | 0.65 | 0.43 | 0.56 |
| 19 | 1/10 | 180-sec fuel lead | | No explosion | | |
| 20 | 1/10 | Simultaneous with light fuel vapors | 1.25 | 0.4 | 0.09 | 0.20 |
| | | 1.5 lb TNT | 2.70 | 0.6 | 98 | 93 |
| | | 2.0 lb TNT | 3.63 | 0.8 | 138 | 134 |

* Assumed to be spurious reading and not included in yield computation.

The propellants were released by rupturing a thin diaphragm in the 6-in. port. The tanks were pressured to 50 psia to assure rapid expulsion of the propellants.

Pressures were measured with gauges at ground level at distances of 25, 50, and 75 ft.

In the first test, 300 lb of fuel and 1300 lb of oxidizer were used. The diaphragms were ruptured simultaneously, and the fuel was emptied in less than 0.5 sec and the oxidizer in 1.5 sec. After 2 sec, a water deluge system was actuated which delivered 100 gal/min into the spill tray. Several weak explosions were audible at a distance of 1,000 ft; however, the blast instrumentation did not detect these small overpressures.

In the second test, 500 lb of fuel and 800 lb of oxidizer were used, and again the diaphragms were ruptured simultaneously. The fuel was emptied in approximately 0.625 sec and the oxidizer in 0.75 sec. Multiple explosions were obtained, the largest yielding peak overpressures of 2.2 and 0.67 psi at the 25- and 75-ft stations, respectively. These correspond to yields of 0.08 and 0.14, respectively. (The authors of Ref. 4-6 indicated that the data from the 50-ft station was not usable.)

As in the previous test series the data from the last station was taken as the best estimate of terminal yield. One 6-lb and one 12-lb TNT calibration shot were also performed as part of this test series. The pressure values for the 6-lb shot were 4.0 and 1.02 psi at the 25- and 75-ft stations, respectively, and those for the 12-lb shot, 6.0 and 1.2 psi. These pressure values correspond to yield values ranging from 53 to 77 percent computed by means of the surface-burst TNT reference curve.

Considering that the terminal yield estimate is based on one gauge reading, that the yields are very small, that the results for the calibration shots differed by almost a factor of 2 from the standard curve, and that no impulse data were available, it is estimated that the absolute value of the terminal yield is uncertain to a factor of about 3 or 4.

SPECIAL TESTS

Aerojet High-Interface-Area Tests* (Ref. 4-9)

The objective of this program was to determine the influence of the initial interface area between oxidizer and fuel and the oxidizer-to-fuel weight ratio on explosive yield.

In these tests the oxidizer was contained in a number of small cylindrical glass Dewars placed in an aluminum pan containing the fuel. Propellant mixing was accomplished by dropping the pan a distance of 10 to 15 ft onto a steel plate, thereby shattering the Dewars.

Use of various Dewar sizes permitted variation of oxidizer-to-fuel ratio and initial interface area (defined as that area obtained by using the mean radius of the double wall of the glass Dewars). Tests were performed using three different contact areas for each of three oxidizer-to-fuel weight ratios, 3:1, 2:1, and 1:1. The three contact areas were slightly different for each oxidizer-to-fuel ratio; however, in each case the three values were in the order of 1:2:4.

The blast environment was measured with an array of 12 piezo-electric gauges located at 10, 25, and 40 ft from ground zero along four radial lines at 90 deg to each other.

Calibration tests were conducted with cylindrical 5-lb TNT and composition C-4 charges and 25-lb TNT charges, which were positioned near the ground surface. The authors stated that a comparison of the calibration test results with the data of Ref. 4-2 indicated a negligible difference at all gauge distances.

Peak overpressures and positive-phase impulses from each of the propellant tests are given in Table 4-13 and the corresponding explosive yields computed by means of a surface-burst TNT reference curve in Table 4-14.

* In this discussion only the basic overpressure and impulse data were obtained from the original reference. All yield computations and error estimates were made for this report.

Table 4-13

PEAK OVERPRESSURE AND POSITIVE-PHASE-IMPULSE DATA
 FROM AEROJET HIGH-INTERFACE-AREA STUDY
 (Oxidizer-to-Fuel Weight Ratio = 1:1)

| Interface Area (sq ft) | Gauge Line | Distance (ft) | | | | | |
|------------------------|------------|---------------|-------|------|------|------|------|
| | | 10 | | 25 | | 40 | |
| | | P | I | P | I | P | I |
| 8.88 | 1 | 4.2 | 25.6 | 2.1 | 12.3 | 1.5 | 10.5 |
| | 2 | 3.3 | 23.0 | 2.5 | 17.4 | 1.1 | 8.0 |
| | 3 | 3.6 | 20.7 | 2.2 | 12.3 | 1.6 | 9.7 |
| | 4 | 5.0 | 33.6 | 3.0 | 13.9 | 2.1 | 9.3 |
| | Avg | 4.0 | 25.7 | 2.5 | 14.0 | 1.6 | 9.4 |
| 18.0 | 1 | 11.9 | 67.3 | 8.0 | 43.2 | 4.6 | 27.3 |
| | 2 | 13.5 | 91.5 | 6.7 | 32.4 | 3.1 | 17.3 |
| | 3 | 12.2 | 62.3 | 7.1 | 37.3 | 4.6 | 22.1 |
| | 4 | 11.5 | 69.7 | 8.9 | 44.7 | 4.3 | 19.3 |
| | Avg | 12.3 | 72.7 | 7.7 | 39.4 | 4.2 | 21.5 |
| 35.9 | 1 | 52.8 | 144.0 | 24.9 | 73.9 | 11.5 | 55.4 |
| | 2 | 75.8 | 172.0 | 34.2 | 81.5 | 10.9 | 39.8 |
| | 3 | 56.5 | 131.0 | 22.2 | 62.9 | 11.4 | 43.6 |
| | 4 | 75.0 | 147.0 | 28.5 | 82.4 | 13.7 | 52.1 |
| | Avg | 65.0 | 148.3 | 27.5 | 75.2 | 11.9 | 47.7 |

P - Peak Overpressure, psi

I - Positive-Phase Impulse, psi-msec

Table 4-13 (Cont.)
(Oxidizer-to-Fuel Weight Ratio = 3:1)

| Interface Area (sq ft) | Gauge Line | Distance (ft) | | | | | |
|------------------------|------------|---------------|-------|------|------|------|------|
| | | 10 | | 25 | | 40 | |
| | | P | I | P | I | P | I |
| 12.6 | 1 | 4.4 | 26.8 | 2.3 | 12.0 | 1.3 | 8.6 |
| | 2 | * | * | 2.5 | 12.0 | 1.4 | 7.3 |
| | 3 | 5.2 | 28.8 | 2.3 | 10.5 | 1.5 | 7.5 |
| | 4 | 5.0 | 26.9 | 2.5 | 14.0 | 1.8 | 7.5 |
| | Avg | 4.9 | 27.5 | 2.4 | 12.1 | 1.5 | 7.7 |
| 25.4 | 1 | 21.7 | 95.2 | 10.3 | 39.5 | 5.4 | 29.5 |
| | 2 | 18.3 | 85.5 | 13.2 | 52.5 | 4.9 | 23.1 |
| | 3 | 19.5 | 84.3 | 10.3 | 29.7 | 5.5 | 24.7 |
| | 4 | — | — | 11.0 | 34.5 | 6.7 | 25.5 |
| | Avg | 19.2 | 88.3 | 11.2 | 39.1 | 5.5 | 25.7 |
| 51.0 | 1 | 97.6 | * | 30.3 | 80.1 | 14.5 | 58.4 |
| | 2 | 85.0 | 186.0 | 36.0 | 89.9 | 10.8 | 43.7 |
| | 3 | 103.9 | * | 20.4 | 55.2 | 12.3 | 43.2 |
| | 4 | 103.1 | 147.0 | 26.3 | 72.2 | 15.0 | 51.6 |
| | Avg | 97.2 | 166.5 | 28.3 | 74.4 | 13.2 | 49.2 |

P - Peak Overpressure, psi

I - Positive-Phase Impulse, psi-msec

* - Gauge damaged by fragments

— - No Gauge

Table 4-13 (Cont.)
(Oxidizer-to-Fuel Weight Ratio = 2:1)

| Interface Area (sq ft) | Gauge Line | Distance (ft) | | | | | |
|------------------------|------------|---------------|-------|------|-------|------|------|
| | | 10 | | 25 | | 40 | |
| | | P | I | P | I | P | I |
| 11.4 | 1 | 2.4 | 21.5 | 1.7 | 12.7 | 1.2 | 3.7 |
| | 2 | 3.2 | 29.3 | 2.2 | 16.6 | 1.0 | 6.6 |
| | 3 | 4.0 | 25.4 | 1.9 | 12.3 | 1.1 | 7.3 |
| | 4 | 3.2 | 26.1 | 2.0 | 12.2 | 1.4 | 8.4 |
| | Avg | 3.3 | 25.6 | 2.0 | 13.5 | 1.2 | 7.8 |
| 22.9 | 1 | 22.9 | 103.1 | 10.5 | 45.8 | 5.8 | 33.2 |
| | 2 | 20.2 | 92.5 | 13.7 | 58.1 | 5.4 | 26.2 |
| | 3 | 19.4 | 80.6 | 9.8 | 41.8 | 5.7 | 28.1 |
| | 4 | * | * | 10.1 | 40.6 | 6.7 | 29.7 |
| | Avg | 20.8 | 92.1 | 11.0 | 46.6 | 5.9 | 29.3 |
| 45.5 | 1 | 52.6 | 163.0 | 23.9 | 77.9 | 10.7 | 54.4 |
| | 2 | 52.9 | 104.0 | 35.7 | 106.0 | 10.0 | 49.4 |
| | 3 | 81.3 | 133.0 | 19.1 | 55.8 | 11.1 | 50.3 |
| | 4 | 70.7 | * | 23.9 | * | 14.5 | 47.6 |
| | Avg | 64.4 | 133.3 | 25.9 | 79.7 | 11.6 | 50.4 |

P - Peak Overpressure, psi

I - Positive-Phase Impulse, psi-msec

* - Gauge damaged by fragments

Table 4-14
EXPLOSIVE YIELDS FROM AEROJET HIGH-INTERFACE-AREA STUDY

| Oxidizer- to-Fuel Weight Ratio | Oxidizer- Fuel Interface Area (ft ²) | Basis of Computation | Yield (%) at Indicated Distance (ft) | | | Terminal Yield Estimate (%) |
|---|---|----------------------------|---|------|------|--------------------------------------|
| | | | 10 | 25 | 40 | |
| 1:1 | 8.88 | Pressure | 0.07 | 0.42 | 0.65 | 2.0 |
| | | Impulse | 2.5 | 2.9 | 3.4 | |
| | 18.0 | Pressure | 0.47 | 3.5 | 4.8 | 8.5 |
| | | Impulse | 15.2 | 17.3 | 11.1 | |
| | 35.9 | Pressure | 5.0 | 25.0 | 29.3 | 34 |
| | | Impulse | 52.7 | 53.0 | 42.3 | |
| 2:1 | 11.4 | Pressure | 0.05 | 0.27 | 0.34 | 1.6 |
| | | Impulse | 2.5 | 2.6 | 2.5 | |
| | 22.9 | Pressure | 1.1 | 6.2 | 8.7 | 12.4 |
| | | Impulse | 22.5 | 23.0 | 17.2 | |
| | 45.5 | Pressure | 4.9 | 22.6 | 27.3 | 36 |
| | | Impulse | 43.7 | 56.6 | 47.4 | |
| 3:1 | 12.6 | Pressure | 0.10 | 0.39 | 0.56 | 1.5 |
| | | Impulse | 2.9 | 2.4 | 2.5 | |
| | 25.4 | Pressure | 0.93 | 6.5 | 7.8 | 12.0 |
| | | Impulse | 21.4 | 16.9 | 14.8 | |
| | 51.0 | Pressure | 8.2 | 25.2 | 33.0 | 40 |
| | | Impulse | 63.3 | 51.0 | 45.4 | |

From Table 4-14 it can be seen that the pressure yields are increasing with distance over the entire distance range, that the impulse yields are generally decreasing with increasing distance (except for the smallest-interface-area cases), and that although the pressure and impulse yields are converging towards each other, the impulse yields at the farthest station are still considerably greater than the pressure yields. This yield-distance behavior is similar to that obtained in the AFRPL impact tests, and the terminal yields given in Table 4-14 were estimated in the same fashion as described for the impact tests.

The lack of duplicate tests in this test series makes it difficult to estimate the uncertainty in the results. The stated good agreement between the expected and measured results from the calibration shots and the generally good agreement between results from the four gauges at a given distance suggest that errors due to the instrumentation system or blast asymmetries are small. The fact that terminal yield conditions were not reached within the measuring range and had to be estimated by extrapolation introduces some uncertainty in the terminal yield values. This uncertainty is particularly large for the smallest interface-area tests, since the impulse yields at the greatest measuring distance were still about a factor of 5 to 7 greater than the pressure yields. For the intermediate- and high-interface-area cases, the differences were about factors of 2 and 1.5, respectively. Based on these differences and the test-to-test reproducibility obtained in other test series, it is estimated that the terminal yields for the highest-interface-area cases are uncertain to about a factor of 1.5, those for the intermediate-area cases to about a factor of 2, and those for the smallest areas about a factor of 3 to 4.

Aerojet Detonability Tests* (Ref. 4-10)

This test series consisted of 11 detonability tests using 130 to 140 lb of propellant. The test setup consisted of a vertical Plexiglas tube, 9-13/16 in. ID by 42 in. long with a 1/8-in.-thick wall, containing 11 test tubes approximately 45 in. long fabricated from 47-mm ID Pyrex tubing of 2-mm wall thickness. The Plexiglas tube was pressed onto a ring of Presstite putty on a

* In this discussion the basic overpressure and explosive yield values were obtained from the original reference. Only the terminal yield and error estimates were made for this report.

24-in.-square by 1/8-in.-thick Plexiglas sheet which was placed on a 9-1/2-in.-diameter by 9-in.-long cylinder (approximately 45 lb) of composition B explosive. The Plexiglas sheet served both as the bottom of the N_2O_4 vessel and as a blast shield to prevent detonation products from the booster from obscuring the detonation wave in the hypergolic mixtures. A sketch of the test configuration is presented in Fig. 4-22.

Primacord (Type A-5 mild detonating fuse) was suspended along the entire length of each test tube to provide the shock stimulus to shatter the tubes and initiate mixing of the fuel and oxidizer. In order to ensure the generation of a reasonably planar shockwave at the booster-propellant interface, the composition B booster was initiated simultaneously at seven points on its bottom surface (by seven commonly initiated lengths of Primacord which terminated in cylindrical charges of composition C-4 explosive in precast cavities in the bottom surface of the booster).

Immediately before testing, the Pyrex tubes were filled to a depth of 40 in. with fuel; then, the oxidizer was added remotely to the outer cylinder to the same depth, to give a total propellant weight of 130 to 140 lb, with an oxidizer-fuel weight ratio of about 2.4:1.

The blast yield of the system was measured by four Atlantic Research Corporation Model LC-33 piezoelectric-type blast gauges located 15.0, 21.9, 34.7, and 60.3 ft from the center of the charge. The TNT equivalence was determined by comparing the overpressure from the propellant system with a shock overpressure vs distance curve for the test area previously obtained with the same gauge array, using various weights of composition B and composition C-4 explosive.

The only parameter varied in these tests was the delay time between initiation of the mild detonating fuse (MDF) and initiation of the composition B booster.

The results of the tests are summarized in Table 4-15. It can be seen that in the majority of cases the pressure from the first station was significantly lower than those from the others. Accordingly, terminal yields were estimated by averaging the results from the last three stations. Duplicate

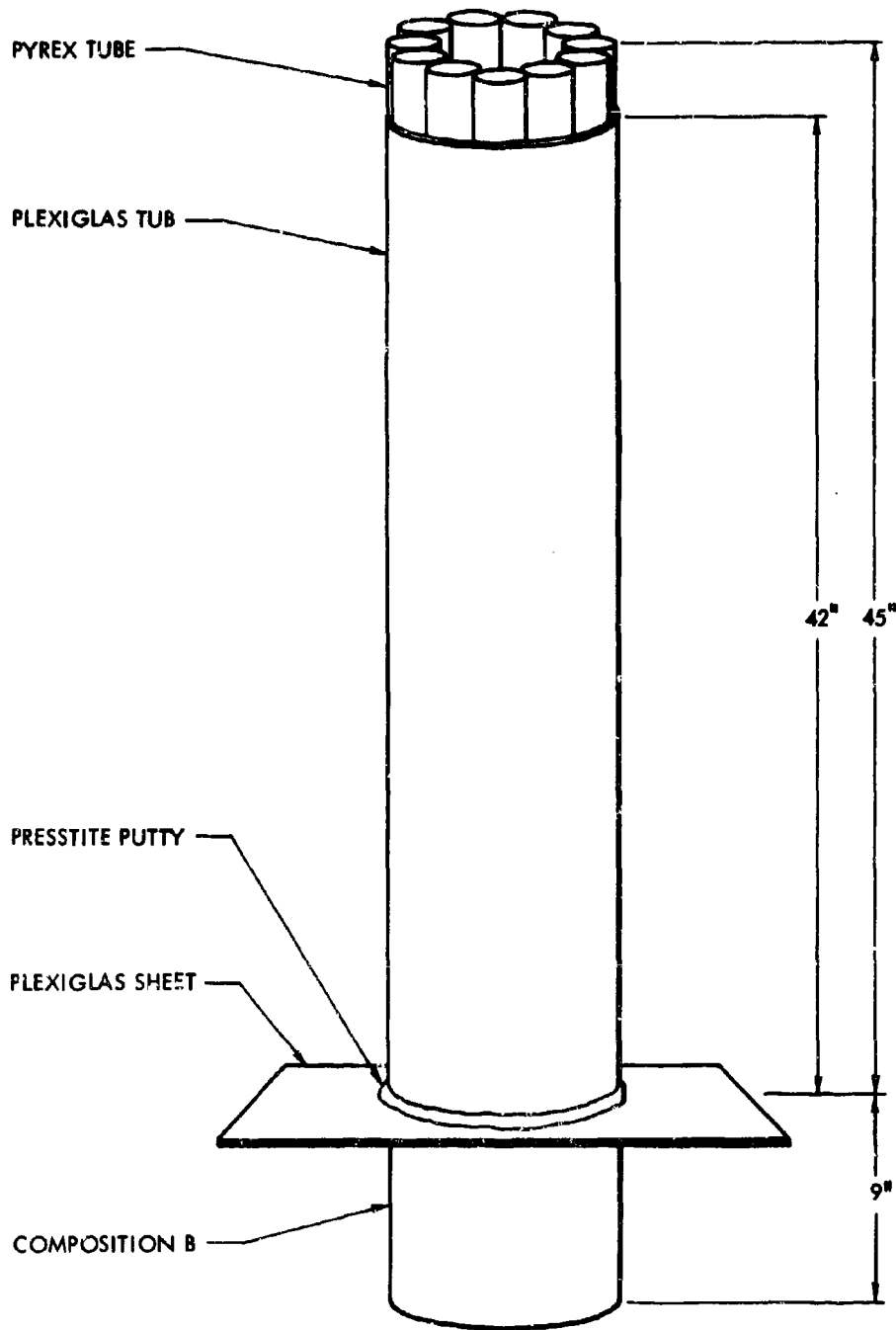


Fig. 4-22. Aerojet Detonability Test Configuration

Table 4-15
PEAK OVERPRESSURE AND EXPLOSIVE YIELDS FROM AEROJET DETONABILITY TESTS

| Test No. | Delay Time* (msec) | Gauge No.** | Overpressure (psi) | Yield (%) | Terminal Yield (%) |
|----------|--------------------|-------------|--------------------|-----------|--------------------|
| N/A-2 | 1 | 1 | 180 | 10.4 | 52 |
| | | 2 | 85 | 43.3 | |
| | | 3 | 29.1 | 66.8 | |
| | | 4 | 8.1 | 46.4 | |
| N/A-3 | 3 | 1 | 231 | 27.8 | 35 |
| | | 2 | 57 | 28.1 | |
| | | 3 | 23.4 | 34.7 | |
| | | 4 | 7.9 | 42.9 | |
| N/A-4 | 0.6 | 1 | 237 | 25.9 | 51 |
| | | 2 | 74.8 | 56.1 | |
| | | 3 | No data | - | |
| | | 4 | 8.0 | 45.0 | |
| N/A-5 | 0.3 | 1 | 135 | <0 | 44 |
| | | 2 | No data | - | |
| | | 3 | 29.1 | 63.4 | |
| | | 4 | 6.6 | 23.7 | |
| N/A-6 | 0.1 | 1 | 113 | <0 | 16 |
| | | 2 | 48.5 | 12.6 | |
| | | 3 | 20.8 | 24.2 | |
| | | 4 | 5.8 | 11.1 | |
| N/A-7 | - | 1 | 28.9 | 6.2 | 20 |
| | | 2 | 14.6 | 9.9 | |
| | | 3 | 8.0 | 15.8 | |
| | | 4 | 3.3 | 23.5 | |
| N/A-8 | 6 | 1*** | 23.9 | 5.5 | 16 |
| | | 1 | 117 | <0 | |
| | | 2 | 50.7 | 19.2 | |
| | | 3 | 17.1 | 11.0 | |
| N/A-9 | 6 | 1*** | 26.4 | 6.3 | 14 |
| | | 1 | 122 | <0 | |
| | | 2 | 49.6 | 18.0 | |
| | | 3 | 12.3 | <0 | |
| N/A-10 | 0.6 | 1 | 226 | 29.9 | 41 |
| | | 2 | 72.0 | 49.0 | |
| | | 3 | 19.3 | 29.5 | |
| | | 4 | 7.7 | 44.0 | |
| N/A-11 | 0.1 | 1 | 151 | 0.3 | 28 |
| | | 2 | 49.7 | 18.4 | |
| | | 3 | No data | - | |
| | | 4 | 7.5 | 38.6 | |
| N/A-12 | - | 1 | No data | - | 18 |
| | | 2 | 16.4 | 12.0 | |
| | | 3 | 8.3 | 17.8 | |
| | | 4 | 3.4 | 24.9 | |

* Delay time = time between initiation of MDF train and initiation of Composition B booster.

** Gauge 1, 2, 3, and 4 located 15.0, 21.9, 34.7, and 60.3 ft from the charge.

*** Initial shock of fireball engulfing Gauge No. 1 before initiation on booster.

tests were run in four of the test conditions. In two of these conditions the two values agreed within a factor of 1.1 of each other; in one, a factor of 1.2; and in the last, a factor of about 1.5. Considering this degree of reproducibility and the lack of impulse data, it is estimated that the terminal yields listed are uncertain to about a factor of 1.5.

In addition to the instrumentation described above, pressure probes were mounted on the Plexiglas cylinders, and streak-camera photographs were taken to measure detonation velocity in the propellant mixture.

No evidence of a propagating detonation was noted in any of the tests; however, rapidly fading shock waves were noted on tests N/A-4 and N/A-5. These were attributed to the decaying shock from the booster with slight contribution by the propellant mixture. The authors of Ref. 4-10 concluded that with this configuration the propellant mixture is incapable of steady-state detonation.

Atlantic Research Laboratory Tests (Ref. 4-11)

This test series included several hundred experiments with milliliter quantities of propellants and used a variety of test geometries. Because of the small quantities of propellants used, no actual pressure measurements were attempted. The tests were documented by high-speed photographs and visual observations. The number and variety of these tests makes it impractical to summarize the results in this report, and the reader is referred to the original work for details. For the purpose of this report, perhaps the most important conclusion of the experimenters was that the full body of their results is inconsistent with the explosion being a hydrazine vapor-air explosion, as suggested in the small-scale spill test results of Ref. 4-6.

Part 3
SUMMARY OF DATA AND CONCLUSIONS

Parts 1 and 2 of this section presented the data from the 32 tests conducted during the basic PYRO program and an additional 100 tests conducted during recent years by a number of other agencies using the hypergolic propellant combination $N_2O_4/50\%$ UDMH - 50% N_2H_4 .

From the results of these tests, it is clear that the explosive yield of the $N_2O_4/50\%$ UDMH - 50% N_2H_4 propellant combination is very dependent on the failure mode and tankage configuration as well as on the inherent properties of the propellants. Thus the process of selecting the maximum credible explosive yield for this propellant combination involves first establishing the failure modes and tankage configurations considered credible, then obtaining estimates of the explosive yields for each case, and finally selecting the largest of these explosive yields.

Since not all configurations using this propellant combination appear to have the same credible failure modes, use of a single explosive yield value (determined in the fashion described above) will be overly conservative for many cases. Accordingly, it seems desirable to use not one, but a number of explosive yield values for this propellant combination, each value corresponding to the worst credible failure mode for the designated configuration.

The following generalized configurations seem to provide a reasonable grouping for this purpose:

1. Static test stand
2. Launch pad
 - a. Pre launch
 - b. Launch
3. Post launch
 - a. In flight
 - b. Ground impact

Static Test Stand

For the static test stand case it is assumed that the tanks, and in particular the tank support systems, are strong enough so that failure modes which involve the entire tank structure falling off its supports are not credible. It is further assumed that there will be no large explosive sources in the vicinity that can act as explosive donors.* This leaves as credible failure modes those involving bulkhead (diaphragm) ruptures, tank wall failure (resulting in propellant spills) or possible small explosive donors (e.g., rupture of high-pressure gas tanks). The scale-model tests which correspond to those failure modes are the diaphragm rupture tests, confinement by the missile tests, spill tests, 100-ft tower drop tests and the small explosive donor tests.

The data presented in Parts 1 and 2 indicated that the range of terminal explosive yields and the estimated upper limit yield for each of these modes is as follows:

| | <u>Terminal Yield Range (%)</u> | <u>Estimated Upper Limit</u> |
|---|-------------------------------------|----------------------------------|
| Diaphragm rupture (confinement by the missile) | 0.01 - 0.8 | 1.5 |
| Spill | 0.02 - 0.3 | 0.5 |
| Small explosive donor | 0.8 - 1.2 | 2.0 |
| Tower drop | 0.2 - 0.4 | 0.6 |

Based on the largest of these, the small explosive donor, the maximum credible yield selected for the static test stand case is 2%. It is possible, however, that in many static test stand situations an explosive donor will not be present. For these cases, the next highest failure mode could be used, i.e., diaphragm rupture, and the maximum credible yield would be 1.5%.

* The only source of large explosive yield considered credible for any situation is another stage of the vehicle. In the static test stand case, it is assumed that only one stage is tested at a time.



Section 5

CRYOGENIC PROPELLANT PROGRAM

Section 5
CRYOGENIC PROPELLANT PROGRAM

During the program a total of 212 tests using the cryogenic propellant combinations liquid oxygen-RP-1 ($LO_2/RP-1$) and liquid oxygen-liquid hydrogen (LO_2/LH_2) were conducted. For purposes of discussing and presenting the results from these tests, this section of the report has been organized as follows:

- Part 1 - Confinement-by-the-Missile, Confinement-by-the-Ground-Surface, and High-Velocity-Impact test procedures and test hardware.
- Part 2 - Test results from the basic program.
- Part 3 - Discussion of results from the basic program.
- Part 4 - Full-Scale Tests, includes a failure test of a Saturn S-IV with ~100,000 lb LO_2/LH_2 and a Titan with ~100,000 lb $LO_2/RP-1$.
- Part 5 - Discussion of yield and ignition time scaling.

Launch Pad

This failure category has been divided into two sub-categories; pre-launch and launch.

The pre-launch category is very similar to the static test stand case and the same failure modes apply, i.e., diaphragm rupture, spill, small explosive donor, and tower drop. Thus, the maximum credible yield for the pre-launch category is 2%. The launch category includes the static test stand failure modes as well as command destruct and large explosive donor. This category, however, does not include the high-velocity-impact case (either from a high-altitude fallback or from turn-around and powered impact) since, in these cases, the point of impact on the ground surface is almost sure to be at a considerable distance from its launch pad.

Although the separation between low- and high-velocity impact is somewhat arbitrary, it is assumed that the low-velocity case is limited to a velocity on the order of 140 ft/sec (corresponding to free fall from a height of about 300 ft).^{*} The high-velocity case is assumed to have an upper limit of about 600 ft/sec. It is also assumed that for a silo geometry, the maximum impact velocity would be significantly less than that for the flat-pad case (say by a factor of 2) because of the lower probability of the vehicle falling directly back into the silo from the greater heights.

The terminal explosive yields for each of the failure modes included under the launch pad failure category are as follows:

| | TERMINAL YIELD RANGE (%) | ESTIMATED UPPER LIMIT |
|-------------------------|--------------------------|-----------------------|
| Diaphragm rupture (CBM) | 0.01 - 0.8 | 1.5 |
| Spill (CBGS) | 0.02 - 0.3 | 0.5 |
| Small explosive donor | 0.8 - 1.2 | 2 |
| Large explosive donor | 3.4 - 3.7 | 5 |
| Command destruct | 0.3 - 0.35 | 0.5 |
| 310-ft drop (CBGS) | ~1.5 | 3 |

* Assumption is based on premise that failure is much less likely to occur when the vehicle has traversed on its own length since by that time it will have cleared all launch support facilities. The maximum vehicle length has been taken as about 300 ft.

PART 1 - CONFINEMENT-BY-THE-MISSILE, CONFINEMENT-BY-THE-GROUND-SURFACE, AND
HIGH-VELOCITY-IMPACT TEST PROCEDURES AND TEST HARDWARE.

CONFINEMENT BY THE MISSILE

These tests were intended to investigate the interaction of the propellants for the case where an internal failure occurs and one propellant falls down onto the other. The duration of interest of this tank confinement case is limited to the time that the propellants remain confined by the walls of the missile. This time is determined by the strength of the tankage, the rate of vaporization of the cryogenic propellants, the initial tank pressures, and the initial ullage space.

Test Tanks

The primary requirement of the tankage for this test condition was to contain the propellants in the proper length-to-diameter ratio until the internal pressure created by the vaporization of the cryogenic propellants exceeded the normal vehicle tankage design burst pressure. From communications with AFRPL and various vehicle manufacturers, it was determined that most vehicle fuel tankage would fail at internal pressures well below 100 psi. Accordingly, this value (100 psi) was chosen as the design burst pressure for the tank.

A complete discussion of the rationale for this test series is presented in Section 3. In this discussion a list of important parameters was presented. These parameters which had a direct influence on the design of the tanks and test hardware are repeated below:

- Propellant type: $LO_2/ RP-1$, LO_2/ LH_2
- L/D ratio: 5:1, 1.8:1 (considered to be the length-to-diameter ratio of the propellant masses)
- Propellant weight: 200, 1,000, 25,000 lb
- D_o/D_t ratio: 1:1 and 0.45:1 (where D_o is the diameter of the opening in the diaphragm and D_t is the vehicle diameter)
- Type of ignition: detonator or squib

The terminal yields for the first five failure modes were obtained from Parts 1 and 2 of this section. The yield for the 300 ft drop was obtained from Fig. 4-23, a plot of terminal yield vs impact velocity. For this estimate only the flat-pad data were used since it is assumed that the area immediately adjacent to the launch pad would most likely be paved or at least hard ground.

A best-fit curve through the flat-pad data indicates that a credible yield for this case would be approximately 1.5%, with an estimated uncertainty of 1.5 to 2.

It will be noted that the highest yields were obtained from the large explosive-donor case. It should be noted, however, that an explosive donor weighing 2 or 3 times the effective weight of the exploding propellant mass was required to achieve this large a yield. Thus, in any case where this situation occurs, there would be more concern about the blast from the donor than that from the propellant explosion. For this reason it seemed reasonable to present two yield values for this case; one with a large explosive donor and one without. The estimated maximum credible yield values for the launch category with a large explosive donor is 5% and without a large explosive donor 3%.

Estimation of a credible yield value for the silo case is slightly more uncertain than for the flat-pad case. A lower credible drop height of say 150 ft. would tend to reduce the yield a factor of 2 below the flat-pad value, however, confinement would tend to increase it. The model-missile tests discussed in Part 2 suggest that for the spill case, silo confinement increases yields by about a factor of 4. Since there is no reason to expect confinement effects to be less than this for low-velocity impact, it appears that confinement effects will more than counteract the effects due to a decrease in impact velocity. Thus the best estimate of a maximum credible terminal yield for the silo case would be 6% (assuming no large explosive donor).

Post Launch

Failures after launch have been divided into two subcategories, in-flight and ground impact. Although no data have been obtained for the in-flight case,

- Internal design operating pressure of the tankage: upper tank 30 psi, lower 0 psi (chosen to simulate the effects of the gravitational and pressure head of the top propellant)
- Tank rupture at approximately 100 psi

Aside from these general parameters, there were many other requirements which were considered to be important and were included in the design criteria for the tanks and test hardware. These were:

- Fuel volumes that were consistent with the oxidizer-to-fuel ratios in normal vehicle usage, i.e., $LO_2/ RP-1 = 2.25:1$, $LO_2/LH_2 = 5:1$
- An ullage space consistent with normal vehicle usage
- Low total mass of tank so as to cause minimal interference with the explosion of the mixed propellants
- Tank materials compatible with the propellant combinations
- Fabrication techniques in general use throughout the industry to keep the lead time short and costs low
- A diaphragm removal system which would bring the two propellants together rapidly, reproducibly, and with minimal influence on the subsequent mixing process

With all these items taken into consideration, the basic design shown in Fig. 5-1 was chosen. This is a thin-walled right circular cylinder with 2:1 ellipsoidal domed ends. This design was the strongest, for the mass of the tank, which could be readily and inexpensively fabricated.

Fabrication

The 200- and 1,000-lb tanks were fabricated by URS. The cylindrical shells and flanges were shop-fabricated from tempered aluminum. The domed ends were spun on hardwood molds and then heat treated before assembly. The shell and dome thicknesses were 1/16 in. for the 200-lb tanks and 1/8 in. for the 1,000-lb tanks.

The 25,000-lb tanks were fabricated by the Marshall Space Flight Center, Huntsville, Alabama. The dome thicknesses were 5/16 in. for the $LO_2/ RP-1$ and 3/16 in. for the LO_2/ LH_2 tanks. The shell thicknesses were 3/16 in. for the $LO_2/ RP-1$ and 1/4 in. for the LO_2/ LH_2 tanks.

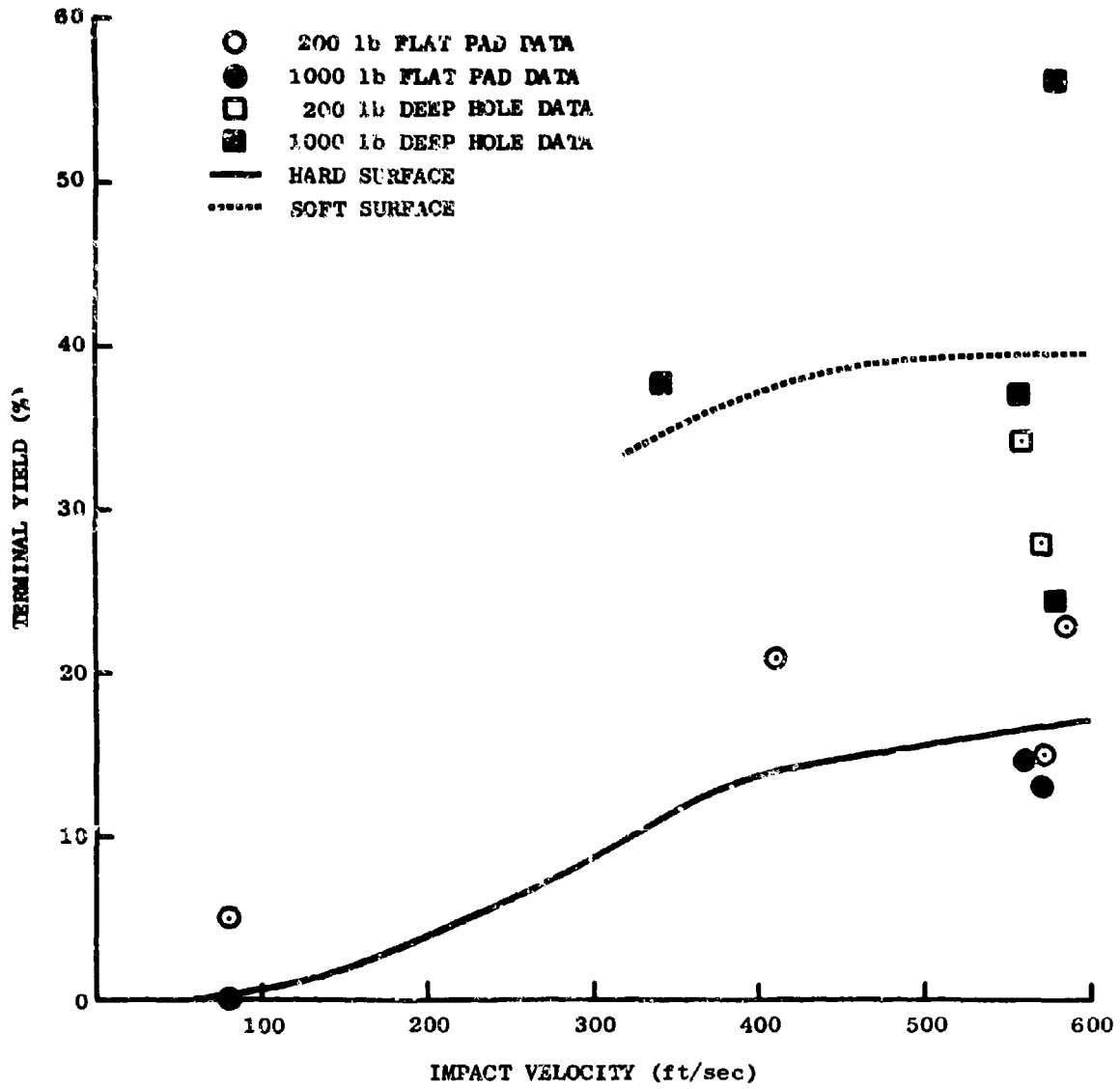


Fig. 4-23. Terminal Yield vs Impact Velocity for Hypergolic High-Velocity Impact

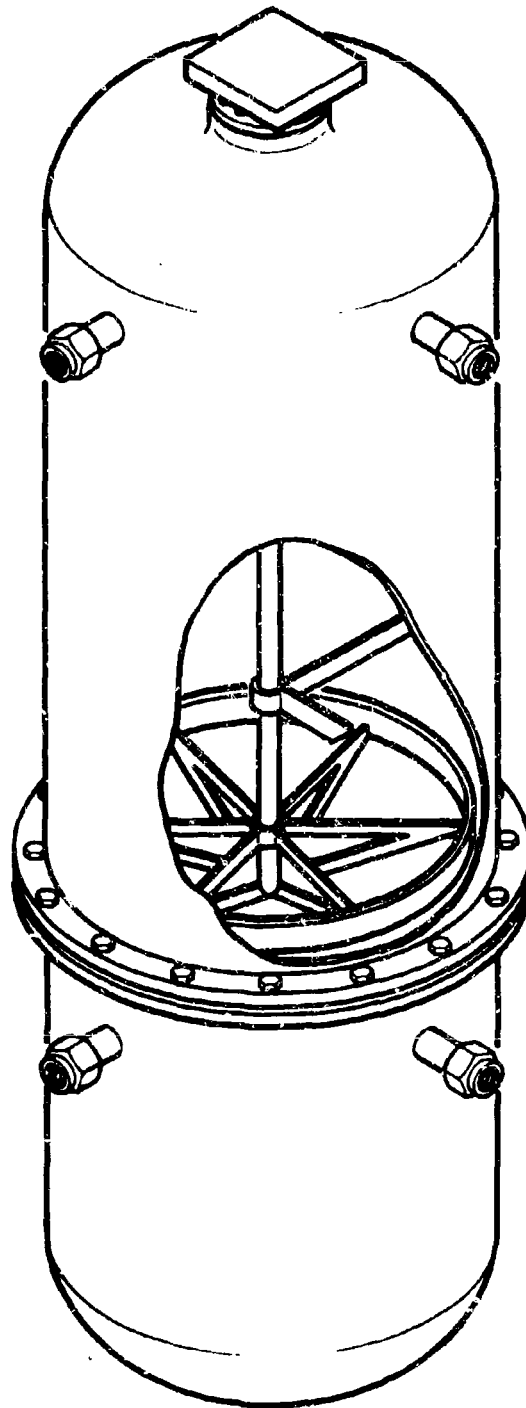


Fig. 5-1. Cryogenic Confinement-by-the-Missile Tank

in the absence of a spill surface there seems no possible way for the failure mode to be more severe than for the launch pad case. Actually, the worst credible failure mode for the in-flight case appears to be the large explosive donor. The data presented earlier suggested that the explosive yield for this case is about 5%.

The worst credible failure mode for the ground-impact case has been assumed to be high-velocity impact. The data from the experimental tests, (see Fig. 4-23) indicates that at the maximum impact velocity investigated, about 570 ft/sec, the mean of the explosive yields varied from about 18 percent for impact on a flat surface up to well over 40 percent, depending on the degree of cratering, and therefore confinement, provided by the target surface. The estimated upper limit for these two failure modes is 25% for the flat surface case and 60% for the cratering case.

Although the ground-impact case gives large yield values, it is not clear how this yield value can be used for design purposes or for establishing safety distances because of the large uncertainty in the point of impact.

Modifications to the Terminal Yield Values

The explosive yield values quoted for each of the failure mode categories is the terminal yield, i.e., the yield obtained at long distances from the explosion where both the pressure and impulse data values tended to reach a constant value.

In order to use these data at locations other than at long distances, it is necessary to examine the comparison between the shapes of propellant pressure and impulse distance curves and those for TNT. These comparisons are shown in Figs. 4-24 and 4-25, which are plots of the pressure or scaled impulse ($I/W_T^{1/3}$) as a function of scaled distance ($D/W_T^{1/3}$). Each point represents the average of the data from all gauges (usually two or three) at a given distance for a given test.

The basic dimensions for the 200-, 1,000-, and 25,000-lb tanks are presented in Fig. 5-2 and a photo of a 25,000-lb $\text{LO}_2/\text{RP-1}$ test tank is presented in Fig. 5-3.

The Diaphragm and Diaphragm Removal Mechanism

The diaphragm system used for the CEM test series consisted of a sheet of tempered glass which was broken by an explosively driven ram (the same system used in the hypergolic program). The tempered glass was held in place by a clamping ring and bolted flanges and was sealed using Teflon-impregnated asbestos gasket material. The breaker used was a six-finned stainless steel probe. To assure complete removal of the tempered glass, a clean-out ring was attached to the tips of the fins. A sketch of a 200-lb test article diaphragm breaker is shown in Fig. 5-4 and a photo of a diaphragm breaker in place in a 25,000-lb test article is shown in Fig. 5-5.

The breaker was mounted on the end of a long rod extending through a Wilson seal in the top of the tank and was capped by a flat, square plate. This plate held a small explosive charge of C-4, which drove the breaker through the tempered glass.

CONFINEMENT-BY-THE-GROUND-SURFACE

This test series was intended to investigate the interaction of propellants subsequent to tank rupture, when the propellants are impacting on a flat, horizontal surface. A complete discussion of the rationale for this test condition is presented in Section 3. In this discussion, a list of important parameters was presented. Those parameters which have had an influence on the design of the experimental hardware are presented below:

- Propellant type: $\text{LO}_2/\text{RP-1}$ - LO_2/LH_2
- Propellant length-to-diameter ratio: 1.8:1 to 5:1
- Propellant velocity: (low drop) 15 to 20 fps
(medium drop) 40 to 50 fps
(high drop) 70 to 80 fps
High-Velocity Impact ~ 600 fps

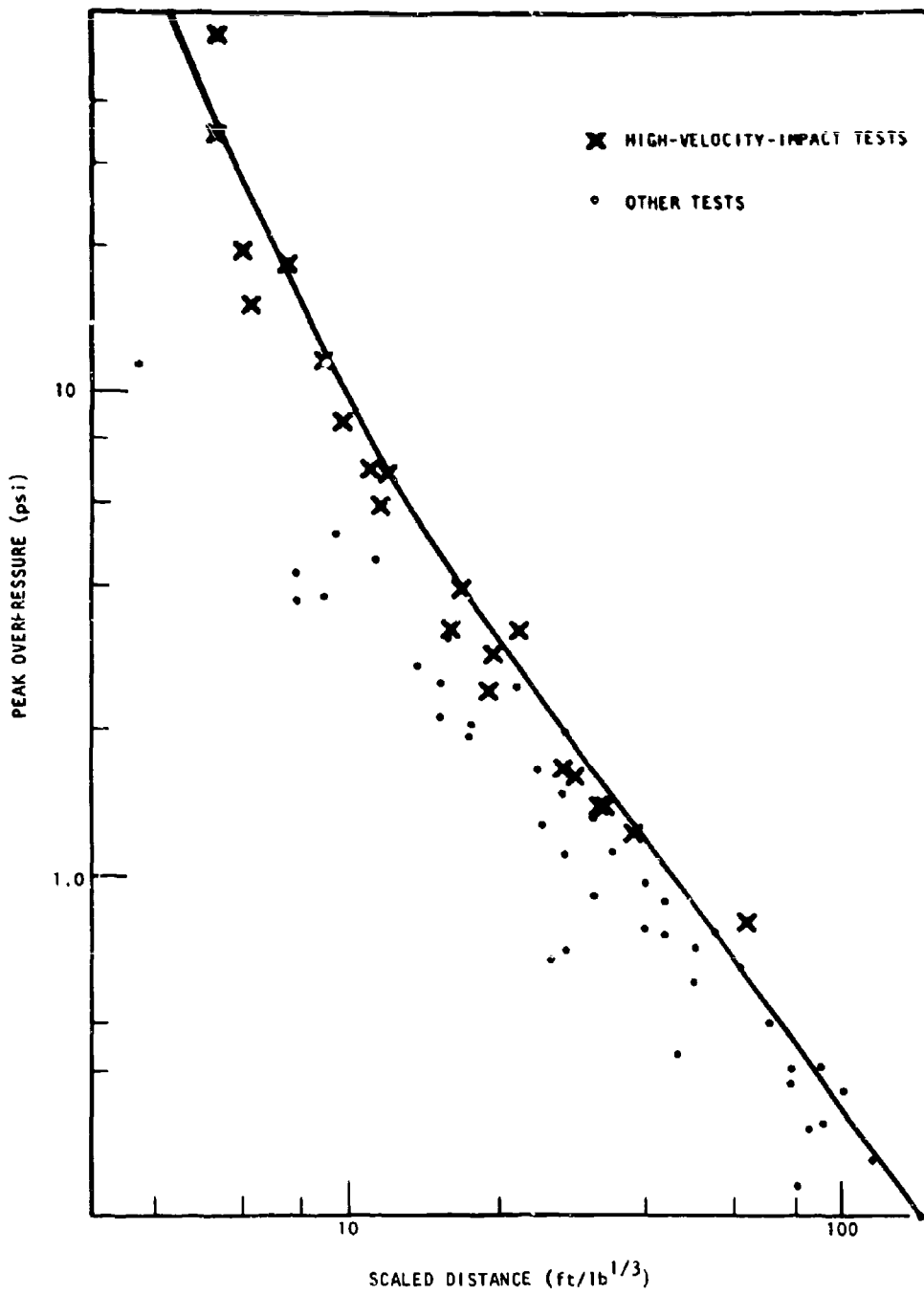
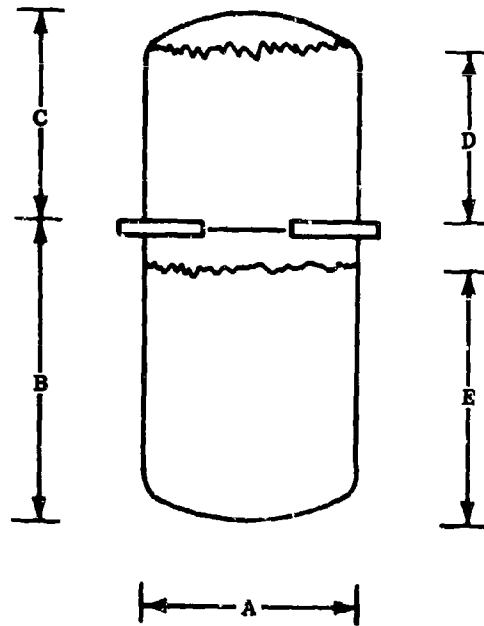


Fig. 4-24. Pressure vs Scaled Distance for Hypergolic Tests



| PROPELLANT WEIGHT (lb) | PROPELLANT TYPE | L/D | DIMENSIONS (in.) | | | | |
|------------------------|----------------------------------|-----|------------------|-----|------|-----|-----|
| | | | A | B | C | D | E |
| 200 | LO ₂ /LH ₂ | 1.8 | 23½ | 13 | 36½ | 31½ | 11½ |
| | | 5 | 16½ | 23½ | 70½ | 62½ | 21½ |
| | LO ₂ /RP ₁ | 1.8 | 15½ | 13½ | 20½ | 17½ | 12½ |
| | | 5 | 11½ | 24½ | 39½ | 33½ | 22½ |
| 1000 | LO ₂ /RP ₁ | 1.8 | 26½ | 19½ | 35½ | 29½ | 21½ |
| | LO ₂ /LH ₂ | 1.8 | 39½ | 20½ | 61½ | 52½ | 18½ |
| 25000 | LO ₂ /RP ₁ | 1.8 | 78 | 65½ | 101½ | 90½ | 61 |
| | LO ₂ /LH ₂ | 1.8 | 114 | 63 | 183 | 164 | 57 |

Fig. 5-2. Cryogenic Confinement-by-the-Missile Tanks

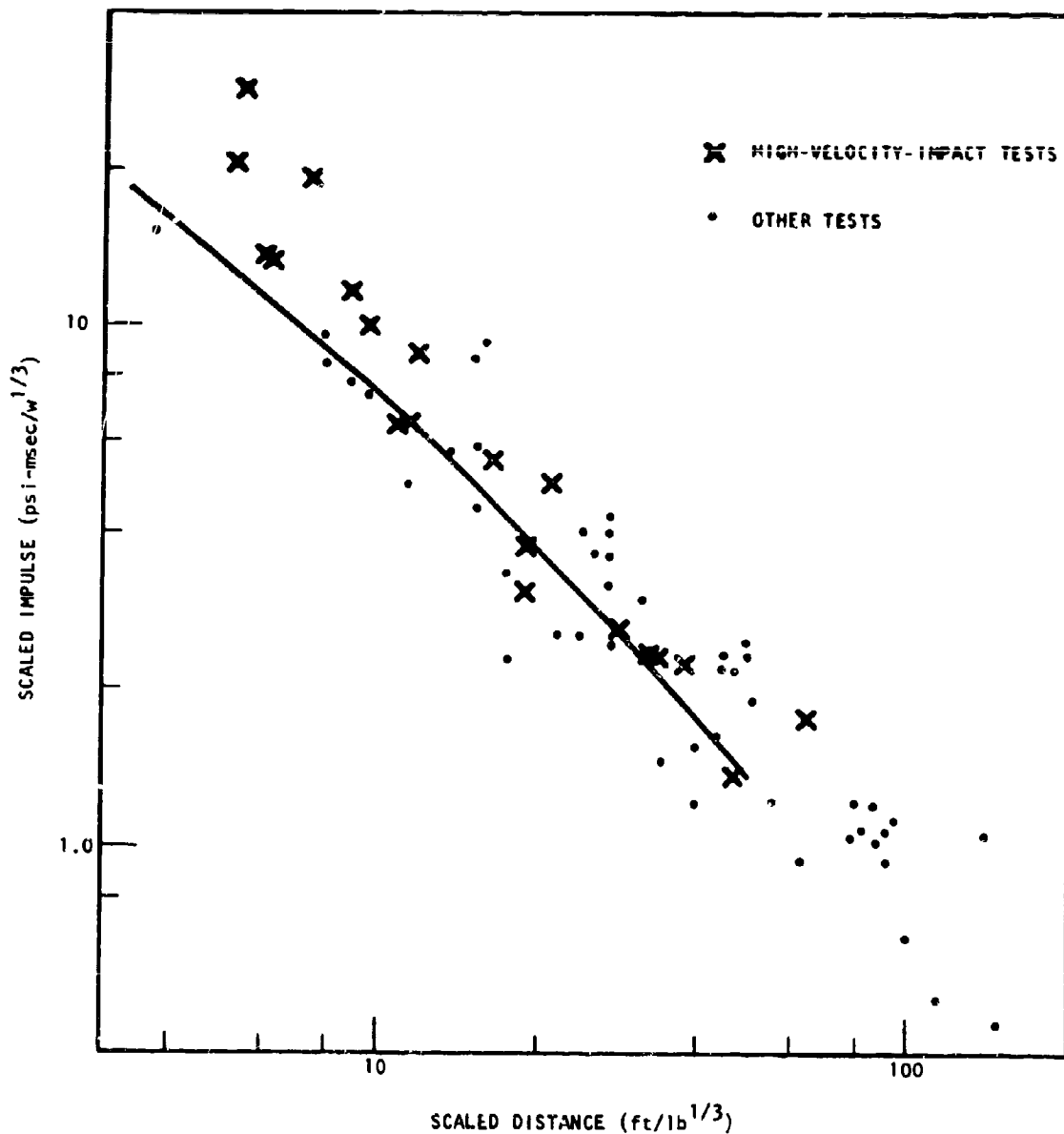


Fig. 4-25. Scaled Positive Impulse vs Scaled Distance for Hypergolic Tests

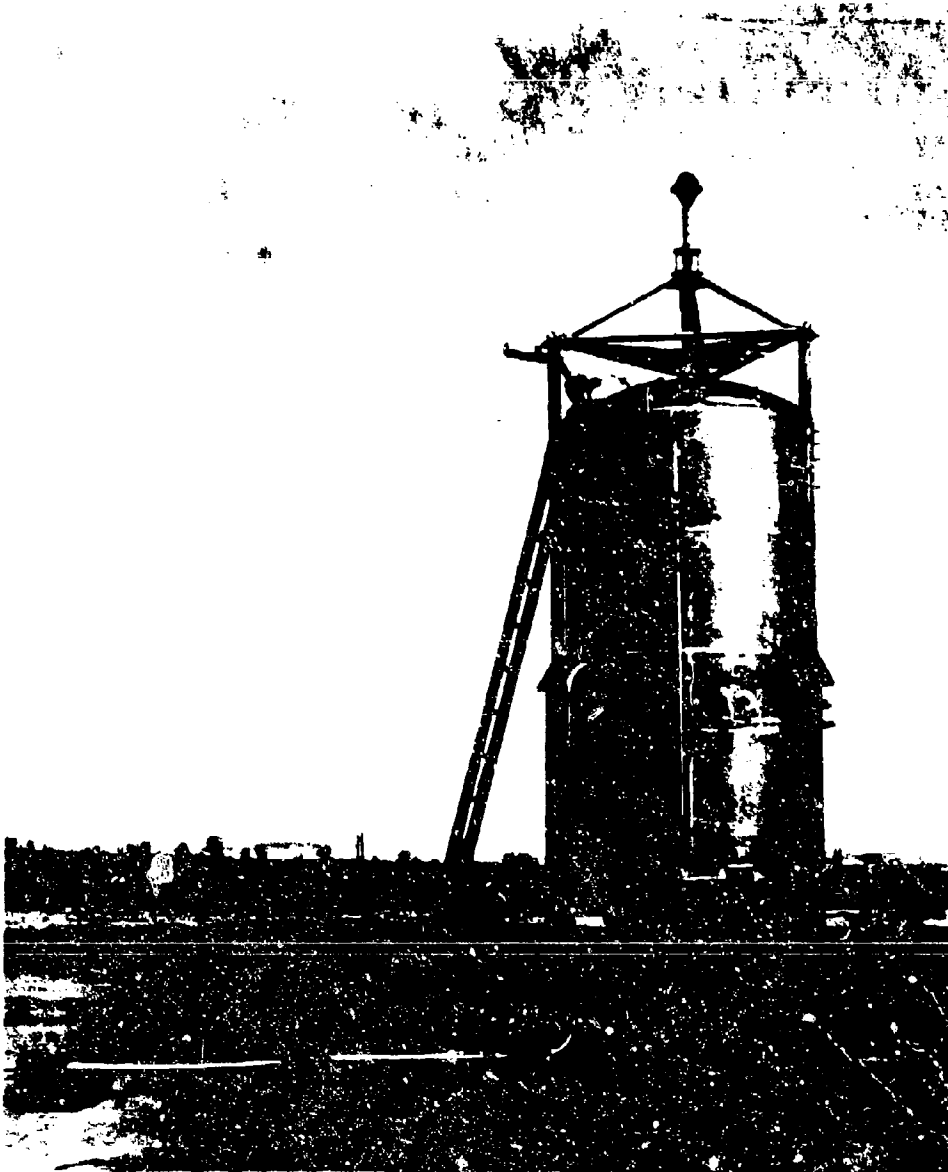


Fig. 5-3. 25,000 lb LO_2 /RP-1 Confinement-by-the-Missile Tank

W_T is the TNT equivalent weight of the test (in pounds) and is equal to the product of the total propellant weight and the terminal yield. In Fig. 4-24, the pressure-distance curve, it can be seen that for all practical purposes the upper bound for the high-velocity-impact data is the TNT curve. For the other data, however, the upper bound tends to lie somewhat below the TNT curve at pressures above 4 psi (scaled distance of about $15 \text{ ft/lb}^{1/3}$).

In Fig. 4-25, the impulse data is compared with the TNT curve. In this case the data tend to lie generally above the TNT curve. In Fig. 4-26a and 4-26b, the peak overpressure and positive-phase impulse upper bounds are plotted as a ratio of the TNT curve. Conservative estimates of the pressure and impulse values to be expected from propellant explosions can be obtained by applying the pressure and impulse values obtained from the standard TNT reference curves by these ratios.

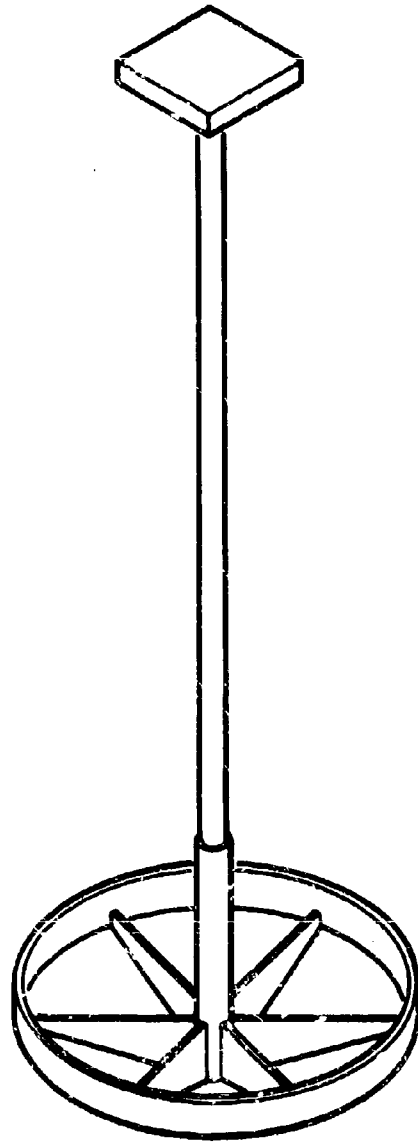


Fig. 5-4. Breaker Ram

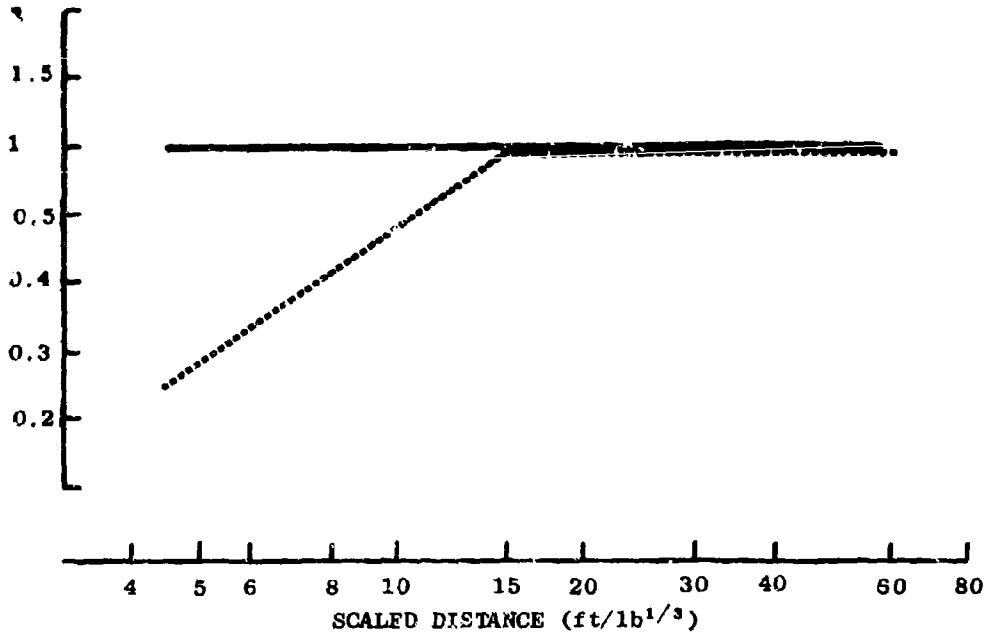


Fig. 4-26a. Ratio of Upper Bound of Propellant Pressure Data to Standard TNT Curve (solid line: high-velocity-impact data; dashed line: other test data)

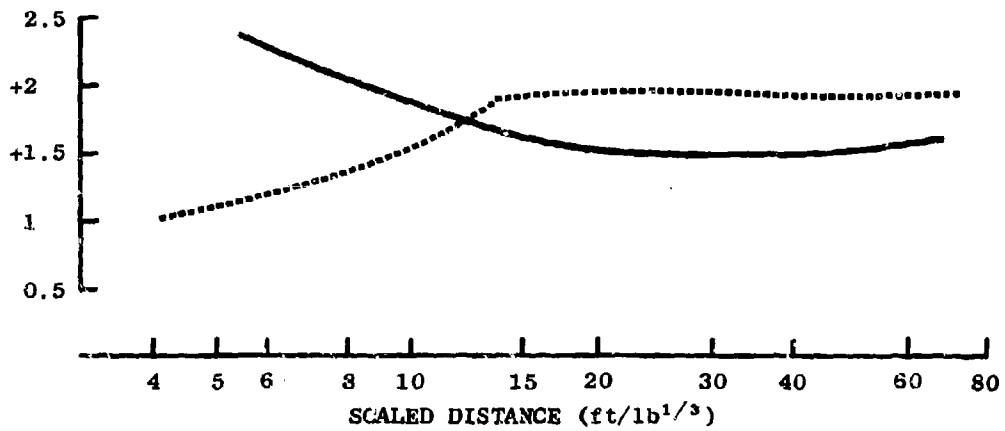


Fig. 4-26b. Ratio of Upper Bound of Propellant Impulse Data to Standard TNT Curve (solid line: high-velocity-impact data; dashed line: other test data)

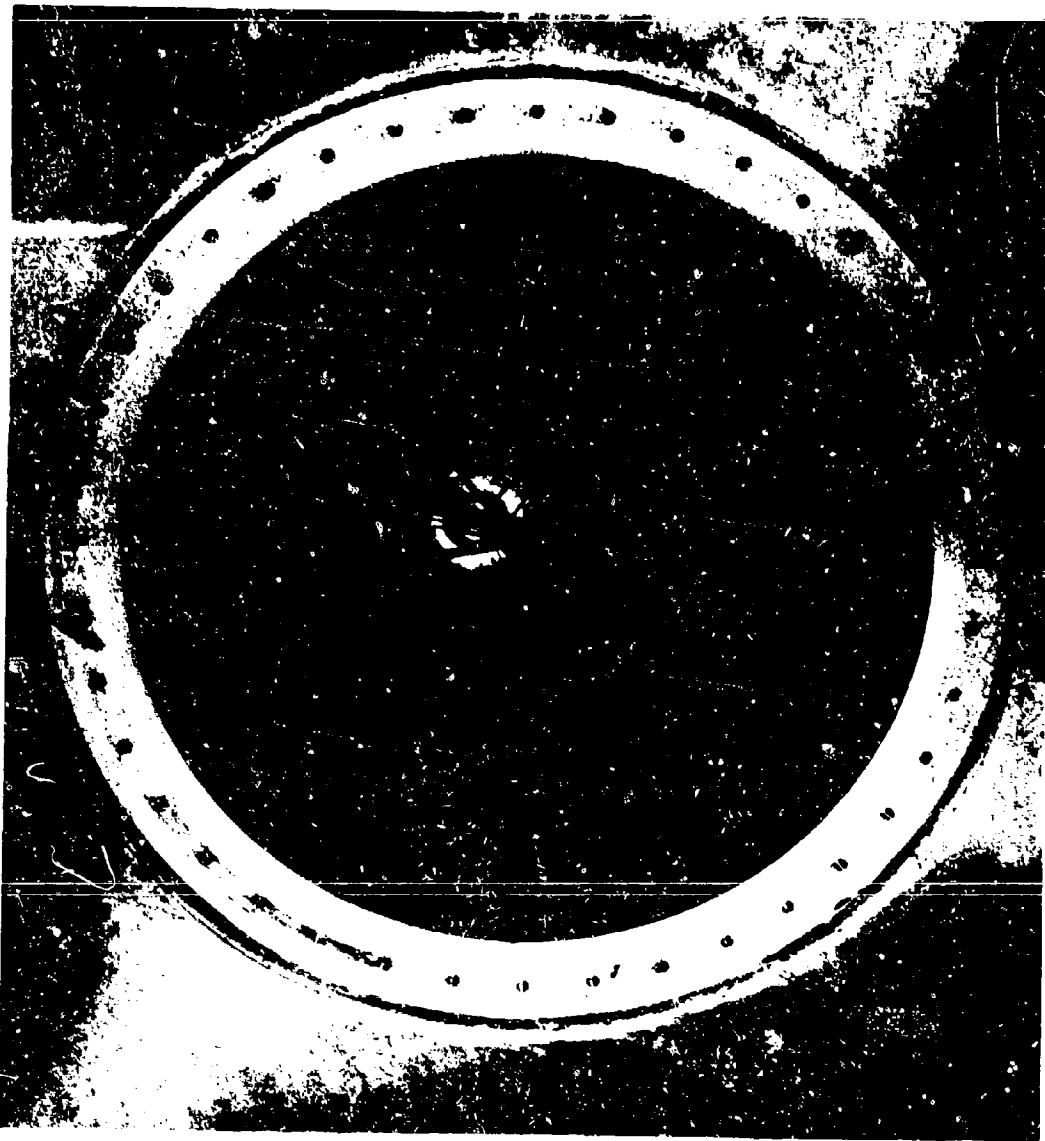


Fig. 5-5. Diaphragm Breaker Ram for 25,000-lb Tank

Section 4

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- 4-8. Henderson, W. Paul, Technical Progress Report on CRDL Project Supported by USAF and NASA, Chemical Research and Development Laboratories, 7 August 1964
- 4-9. Pesante, R. E., M. Nishibayashi, D. G. Frutchey, R. D. Erickson, W. J. Helm, Blast and Fireball Comparison of Cryogenic and Hypergolic Propellants, Report No. 0822-01(01)FP, prepared for NASA by Aerojet General Corporation, 26 June 1964
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* Reference curves from these documents are reproduced in Appendix F.

- Flow direction: vertical and horizontal

Aside from these general parameters, there were many other requirements which were considered to be important and were included in the design criteria for the experimental arrangement. These were as follows:

- Tankage should be light and frangible so as not to interfere with the expansion and subsequent measurement of the blast wave.
- Tankage should release the propellants rapidly and uniformly, and all tankage should be kept clear of the mixing area of the propellants.
- The two propellants should make first contact at or near the ground surface.
- The propellants should flow concentrically from GZ to minimize blast asymmetries.
- Tank material compatible with propellant combinations.
- Fabrication techniques in general use throughout industry.

Low Drop Tests (Impact velocity ~23 ft/sec)

In this test series, the tanks were suspended with the bottom of the tank 2 to 3 ft above the test pad, filled with propellant, and allowed to drop onto a breaker, which ripped open the bottoms of both propellant compartments. As soon as the bottom of the tank was broken, the tank was stopped by a rigid frame and the propellants allowed to spill out onto the test pad.

The basic experimental configuration for these tests (shown in Fig. 5-6) consisted of a lightweight, frangible frame and a thin-walled frangible tank assembly. The frame and tank were constructed of aluminum to minimize the size of the fragments produced.

The tankage assembly consisted of a right cylindrical tank, guide tubes, fueling ports, and for the tanks containing the cryogenic propellants, a remote liquid-level measuring system and lightweight tank insulation. The cylindrical tank was of welded aluminum construction with a wall thickness of 50 mils. The bottom was aluminum foil 5 mils thick. Equally spaced vertically around the tank were three lightweight aluminum pipes, which served as guides during the

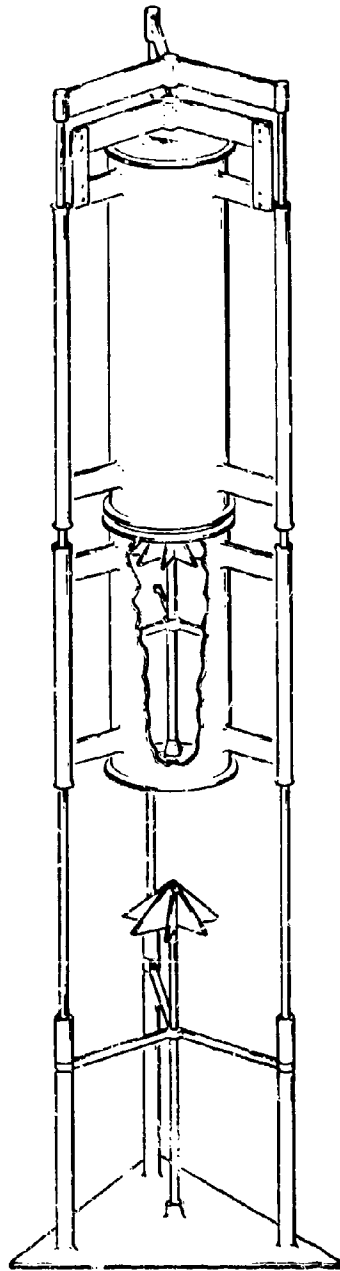


Fig. 5-6. Sketch of Tank Assembly and Drop Frame for the Confinement-by-the-Ground Surface Low Drop Test Series

- 4-11 Friedman, R., W. P. Barnes, Jr., and M. Markels, Jr., A Study of Explosions Induced by Contact of Hydrazine-Type Fuels with Nitrogen Tetroxide, TDR ASD-TDR-62-685, prepared for Aeronautical Systems Division by Atlantic Research Corporation, September 1962
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free-fall of the tank. The tank was suspended on a simple frame, which had three slender (5/8 in.) aluminum-rod legs. These legs passed through the aluminum pipes attached to the tanks. The tank assembly was suspended from the frame by a 1/8-in. aircraft cable, which was passed through an explosive cable-cutting device.

To rapidly remove the diaphragm, the tank was dropped onto a six-bladed diaphragm breaker. This breaker was constructed of tempered aluminum blades which had been filed razor sharp on the top or cutting edges. For the L/D of 1.8 tests, a single breaker was fastened to the bottom of the frame. For the somewhat longer tank in the L/D of 5 tests, a secondary breaker was included in the lower tank. This secondary breaker, which broke the diaphragm on the top tank, was activated by the first breaker as it pushed through the lower tank diaphragm.

Later in the program a 100-ft drop tower was installed for the high drop test series. This tower, which is discussed in detail in Section 4, furnished an excellent support for the remaining 200- and 1,000-lb low-velocity tests, and the configuration shown in Fig. 5-7 was used. In this configuration, the propellant tanks were essentially the same as before, but the tank was guided on two taut aircraft cables attached between the tower and the ground surface. The tanks were released by means of an explosive cable-cutting device and dropped onto a six-bladed diaphragm breaker.

A limited number of tests were conducted using the horizontal flow direction. For this test condition it was required that the lower propellant have as low a velocity as possible and the top propellant have a velocity much higher than the lower propellant. Thus, for this test series, the configuration shown in Fig. 5-8 was used. This configuration used two breakers, one installed on the test pad to open the bottom tank diaphragm and the second installed on the top of the bottom tank to open the top tank diaphragm. A photograph of an $LO_2/ RP-1$ horizontal drop test article in place on the test pad is shown in Fig. 5-9 and a close-up of the breaker on the top of the bottom tank is shown in Fig. 5-10.

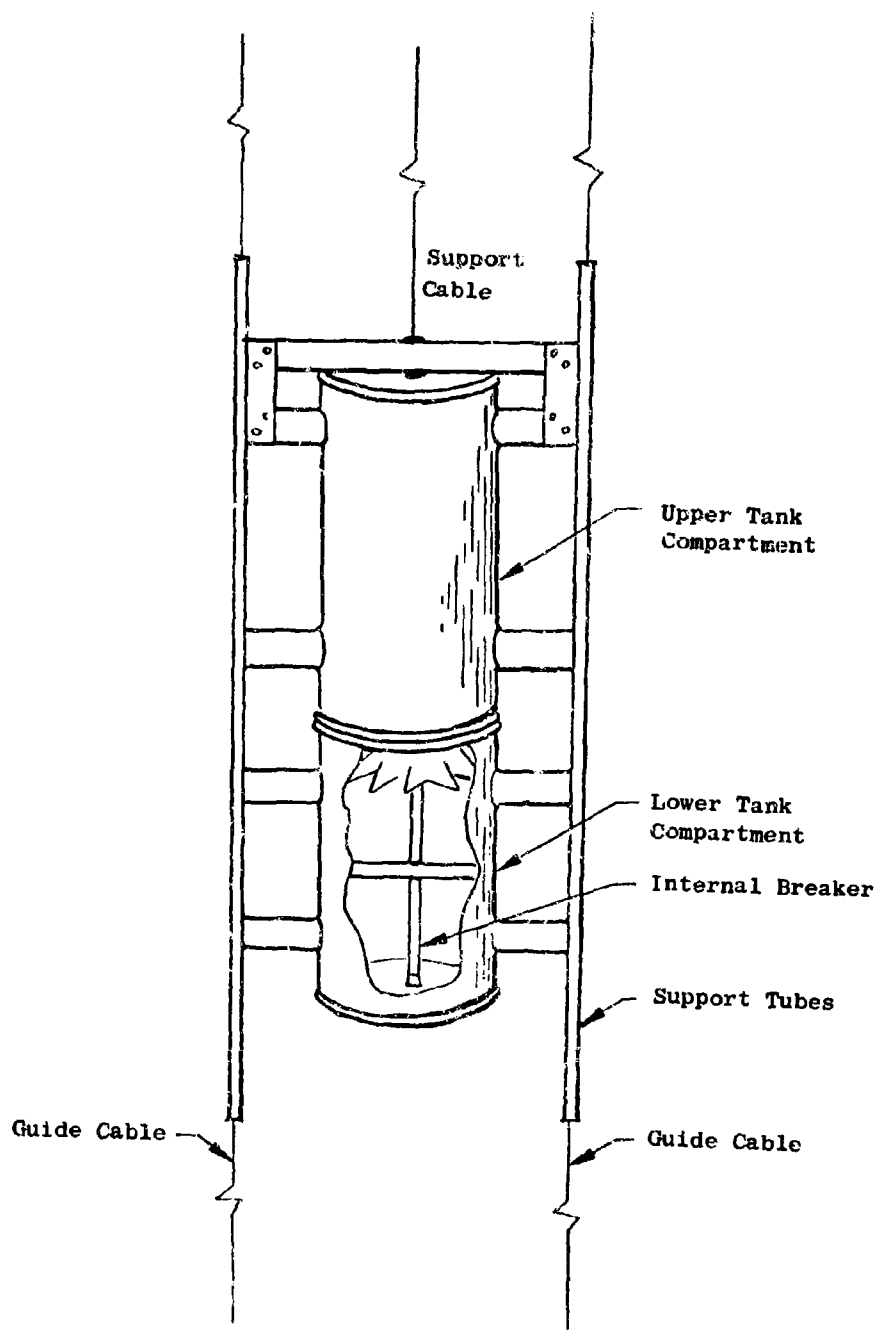


Fig. 5-7. Test Tank for the Confinement-by-the-Ground-Surface Low Drop Test Series

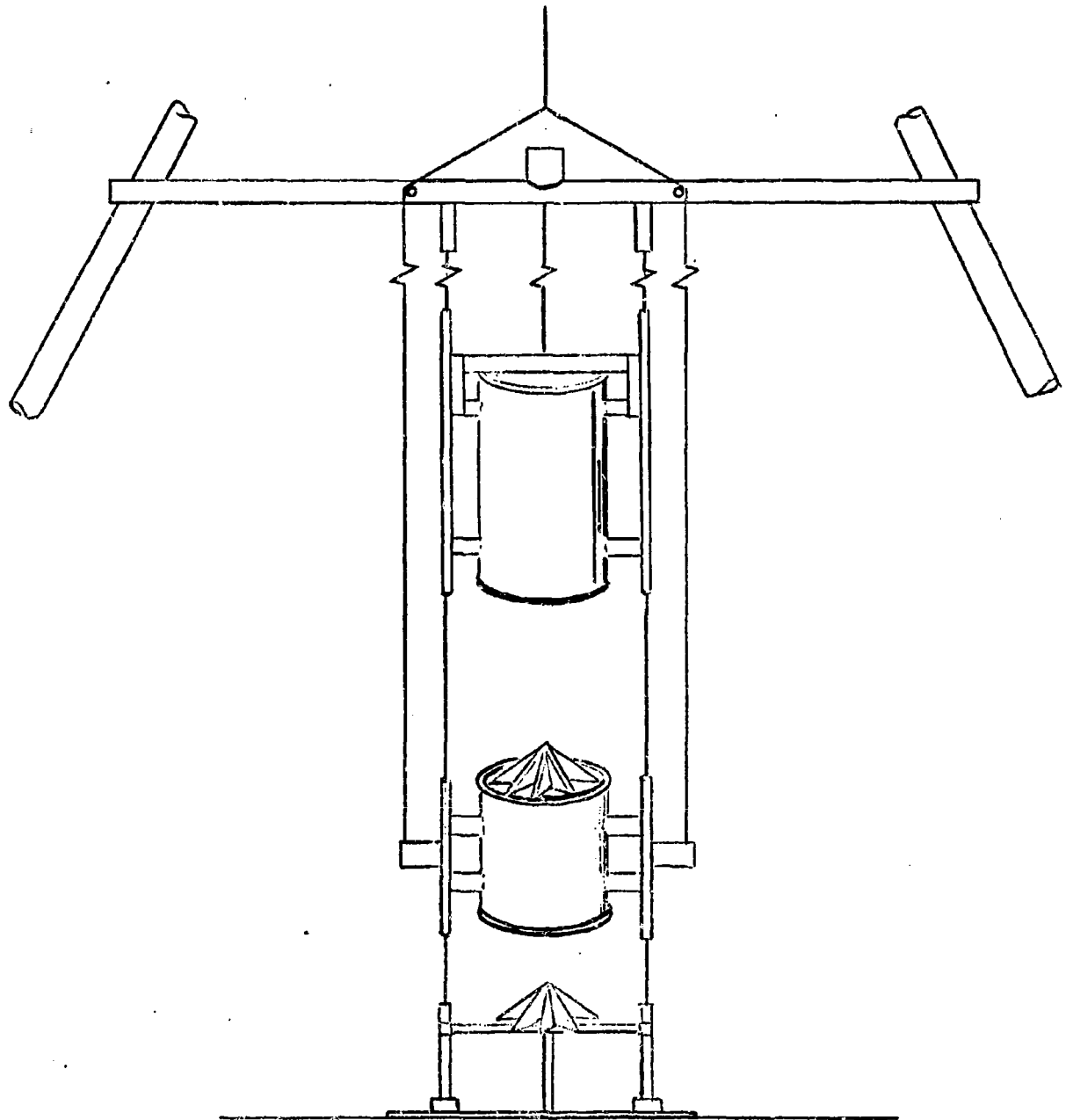


Fig. 5-8. Low-Drop Horizontal CBGS Test Article



Fig. 5-9. 200-lb-LO₂/RP-1 Horizontal CBGS Test Configuration

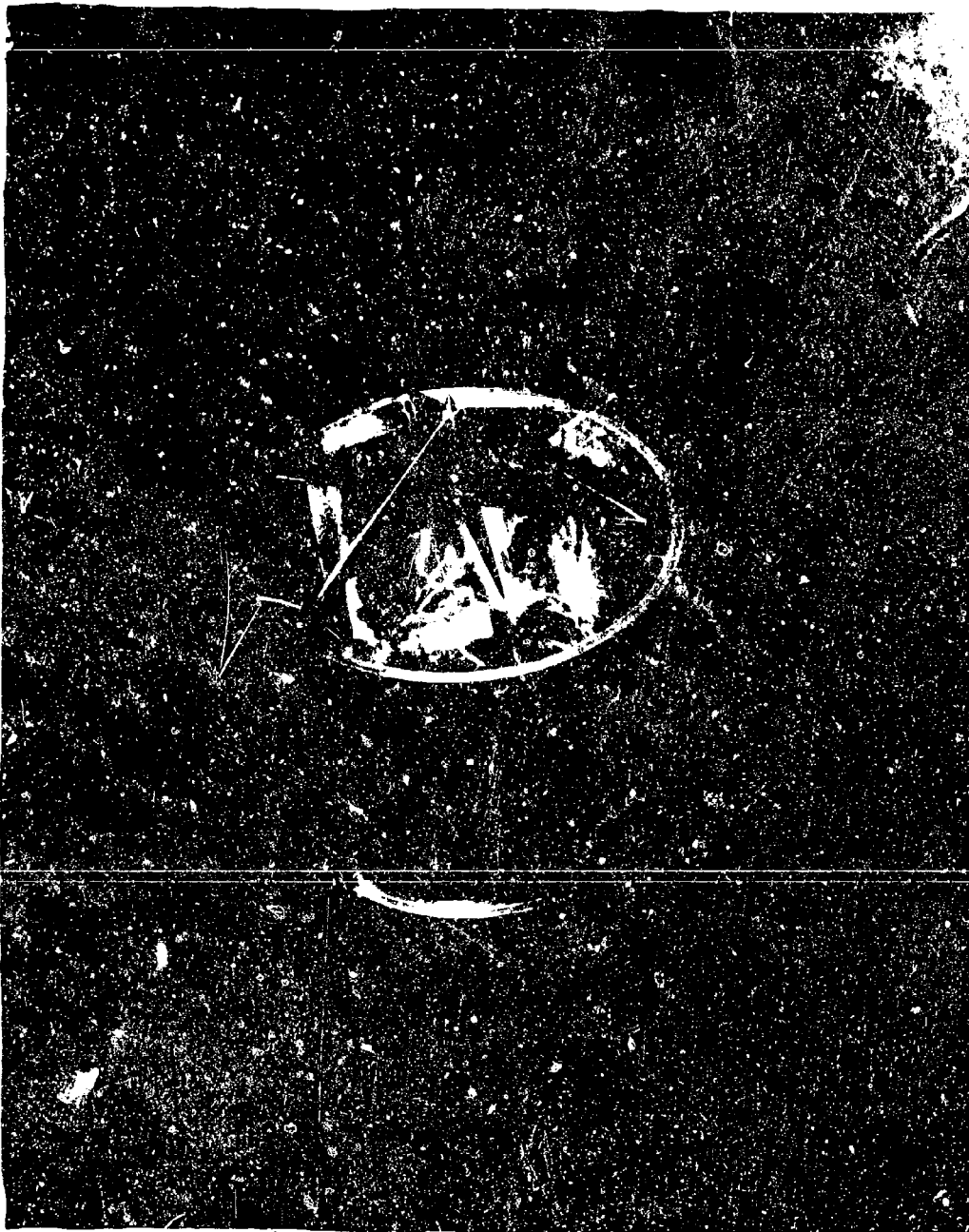


Fig. 5-10. Diaphragm Breaker - Lower Tank of Horizontal
CBGS Tank

Medium Drop Tests (Impact Velocity ~ 44 ft/sec)

In this test series the tanks were suspended so that the bulkhead separating the two propellants was ~ 30 ft above the test pad. The 200- and 1,000-lb tests in this series were conducted using the lower portion of the 100-ft drop tower which is described in the previous section.

The tanks were essentially the same type used in the low-velocity drop test series (i.e., thin walls and an aluminum foil bottom). A typical tank and guide assembly is shown in Fig. 5-11.

For the 25,000-lb medium drop tests, a special 50-ft expendable drop tower was installed. This tower is shown with an $\text{LO}_2/\text{RP-1}$ test article in place in Fig. 5-12. The legs of this tower were fabricated of extra-strong 12-in.-diameter pipe. A trap door type release mechanism triggered by an explosive cable-cutting device released the tank. The tank was dropped onto a breaker ram and the tank stopped by a large hydraulic shock absorber. A photograph of the trap door mechanism is shown in Fig. 5-13.

High Drop Tests (Impact Velocity ~ 78 ft/sec)

In this test series the propellant tanks were suspended so that the bulkhead separating the two propellants was ~ 97 ft above the test pad on the 100-ft drop tower. The remaining test conditions were similar to those used for the medium drop test series.

Summary of Tank Dimensions

As noted throughout the discussion, the test tanks used for the confinement-by-the-ground surface test series were of light and fragile construction. For example the 200- and 1,000-lb $\text{LO}_2/\text{RP-1}$ tanks and the 200-lb LO_2/LH_2 tanks had sidewall thickness of 60-mil aluminum and the diaphragm was 6-mil aluminum foil. The LO_2/LH_2 1,000-lb tank had sidewalls of 60-mil and a diaphragm of 10-mil aluminum foil.

The 25,000-lb drop tank had sidewall thickness of 1/8-in. and diaphragm thickness of 32-mils. The basic dimensions for all tanks are presented in Fig. 5-14.

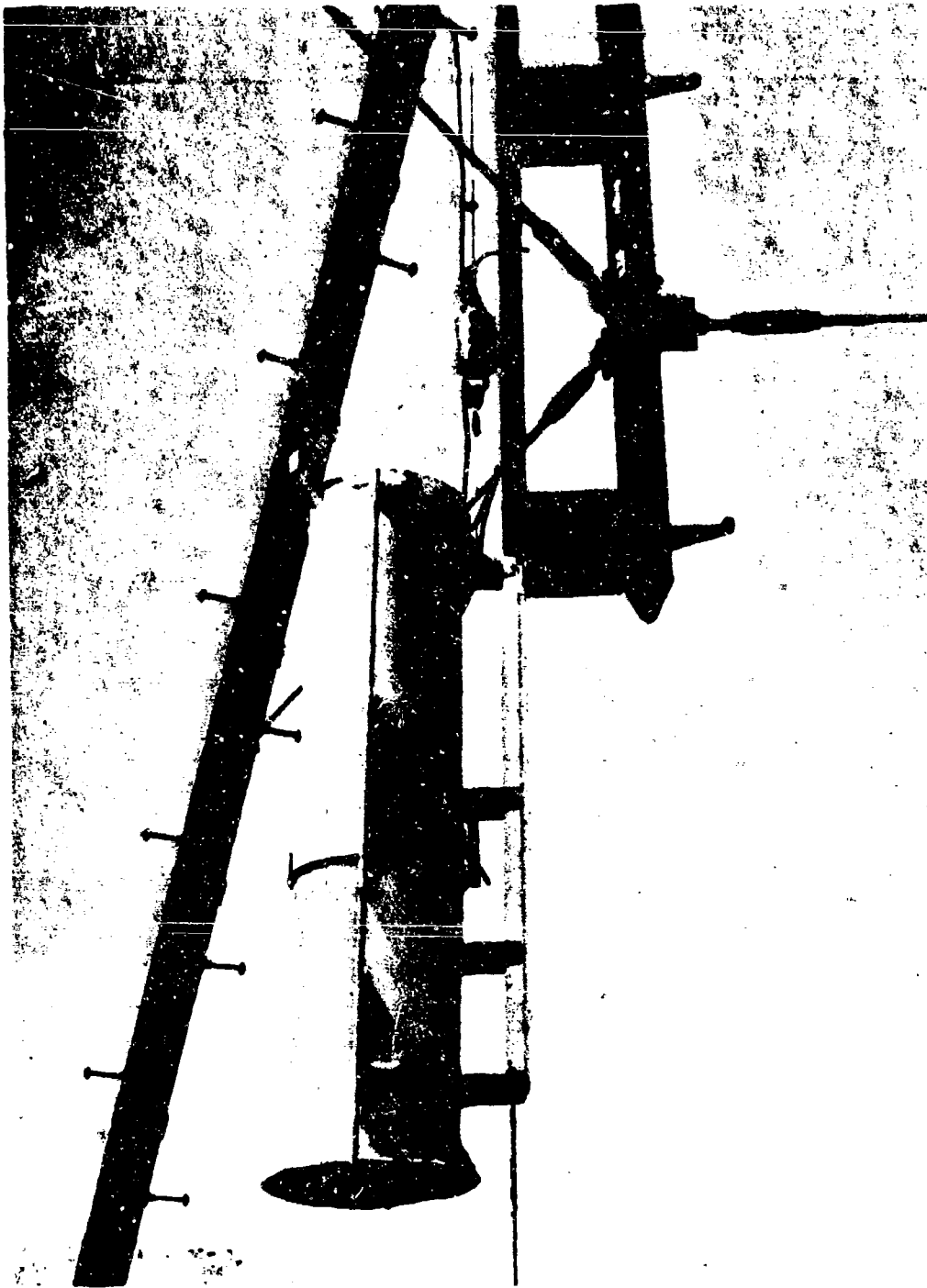


Fig. 5-11. Typical Drop Tank and Guide Assembly

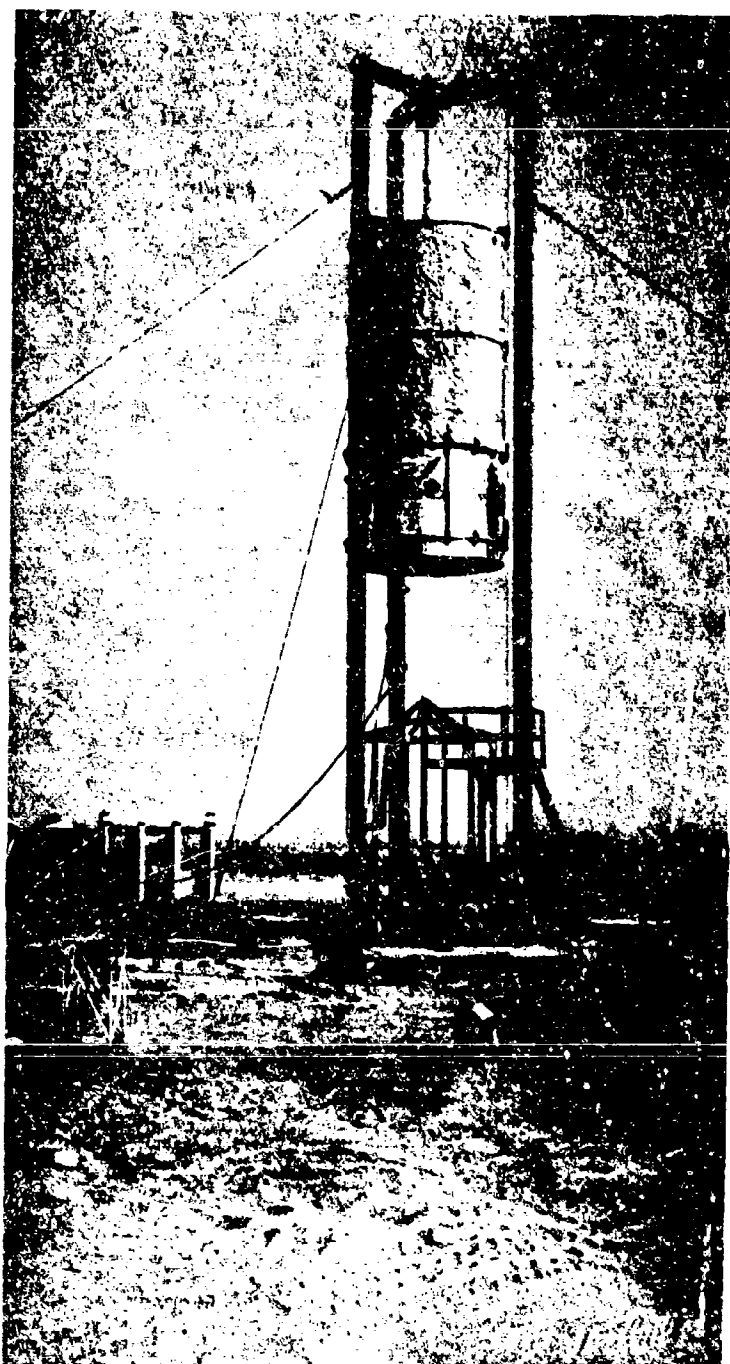
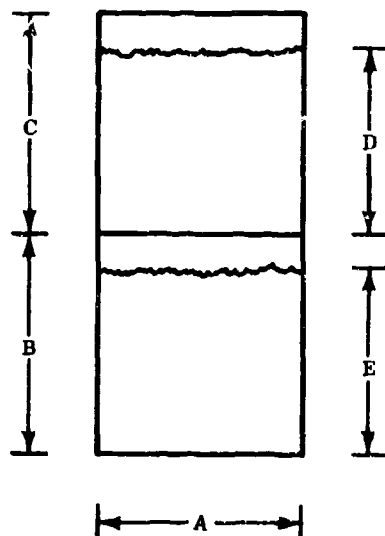


Fig. 5-12. 50-ft Drop Tower and 25,000-lb LO_2 /RP-1 Test Article



Fig. 5-13. Release Mechanism for 25,000-lb Drop Tests



| PROPELLANT WEIGHT (lb) | TEST TYPE | L/D | DIMENSIONS (in.) | | | | |
|------------------------|----------------------------------|-----|------------------|-----|-----|-----|-----|
| | | | A | B | C | D | E |
| 200 | LO ₂ /LH ₂ | 1.8 | 23 | 12½ | 34 | 31½ | 9½ |
| | | 5 | 16½ | 22½ | 65½ | 62½ | 10½ |
| | LO ₂ /RP ₁ | 1.8 | 15½ | 13½ | 20 | 17½ | 11 |
| | | 5 | 11½ | 25 | 37 | 34 | 22 |
| 1000 | LO ₂ /RP ₁ | 1.8 | 26½ | 21½ | 32½ | 29½ | 18½ |
| | LO ₂ /LH ₂ | 1.8 | 39½ | 19½ | 56½ | 53½ | 16½ |
| 25000 | LO ₂ /RP ₁ | 1.8 | 78 | 66½ | 102 | 92 | 58½ |
| | LO ₂ /LH ₂ | 1.8 | 114 | 63 | 176 | 167 | 55½ |

Fig. 5-14 Cryogenic Confinement-by-the-Ground-Surface Tanks

HIGH-VELOCITY IMPACT

The cryogenic high-velocity test series consisted of six tests in which 200-lb quantities of the cryogenic propellant combinations, $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 , were propelled down a sled track at speeds ranging from 500 to 600 ft/sec and allowed to impact into various target configurations.

The target configurations used were: (1) a flat-wall target simulating impact into a rigid flat surface, and (2) a deep-hole target simulating impact into a soft surface in which a crater would be formed.

The test series consisted of two $\text{LO}_2/\text{RP-1}$ and one LO_2/LH_2 propellant tests with the flat-wall target configuration and two $\text{LO}_2/\text{RP-1}$ and one LO_2/LH_2 propellant tests with the deep-hole target configuration.

While it was anticipated that ignition of the cryogenic propellants would occur upon impact and breakup of the tank, a supplemental ignition source in the form of a railroad flare was placed on the ground near the impact target for the first couple of tests. Upon analysis of the films of these tests, it became evident that ignition was indeed occurring upon impact and the flares were not used on the remaining tests.

High-Velocity-Impact Targets

To create the flat-wall targets, a massive concrete block was faced with 1-in.-thick steel plates, 8 ft high and 16 ft wide with a 1-ft splash shield around the edge. To create the deep-hole targets, the massive base was faced with concrete blocks containing preformed steel-lined cylindrical cavities. A sketch of this type of target is presented in Fig. 5-15.

High-Velocity-Impact Tanks

The tanks used for this test series are shown in Figs. 5-16 and 5-17. The general tank geometry and length-to-diameter ratios were similar to those used in the previous hypergolic tests. The tank shells and flanges were aluminum with a shell thickness of 1/16 to 1/8 in. A 1-in. air chamber was left between the tanks for insulation. This air chamber was

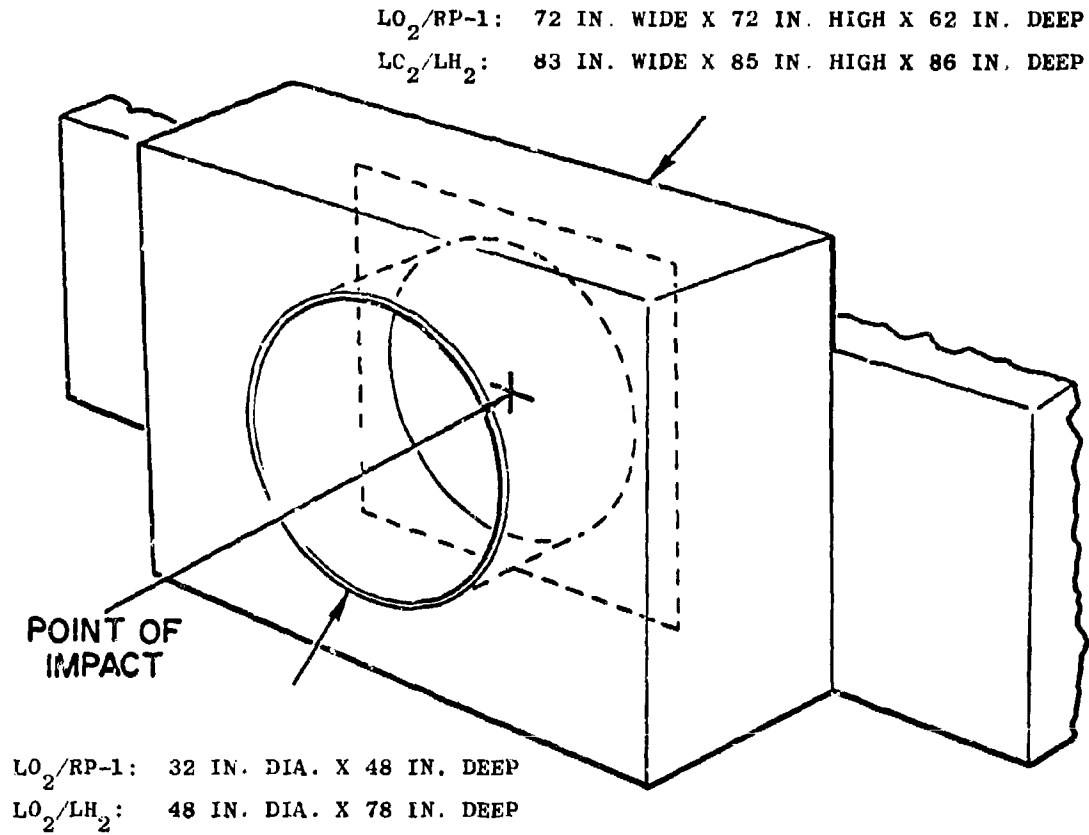


Fig. 5-15 Deep-Hole Target for the Cryogenic Impact Tests

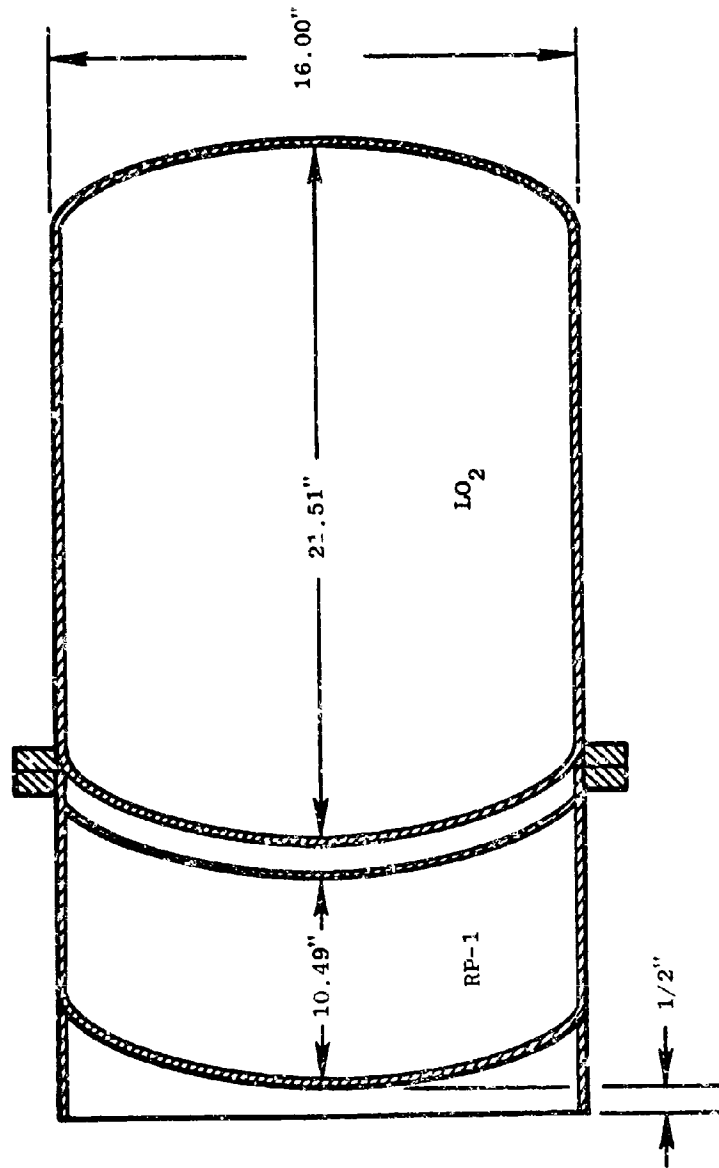


Fig. 5-16 200-lb LO₂/RP-1 Impact Tank

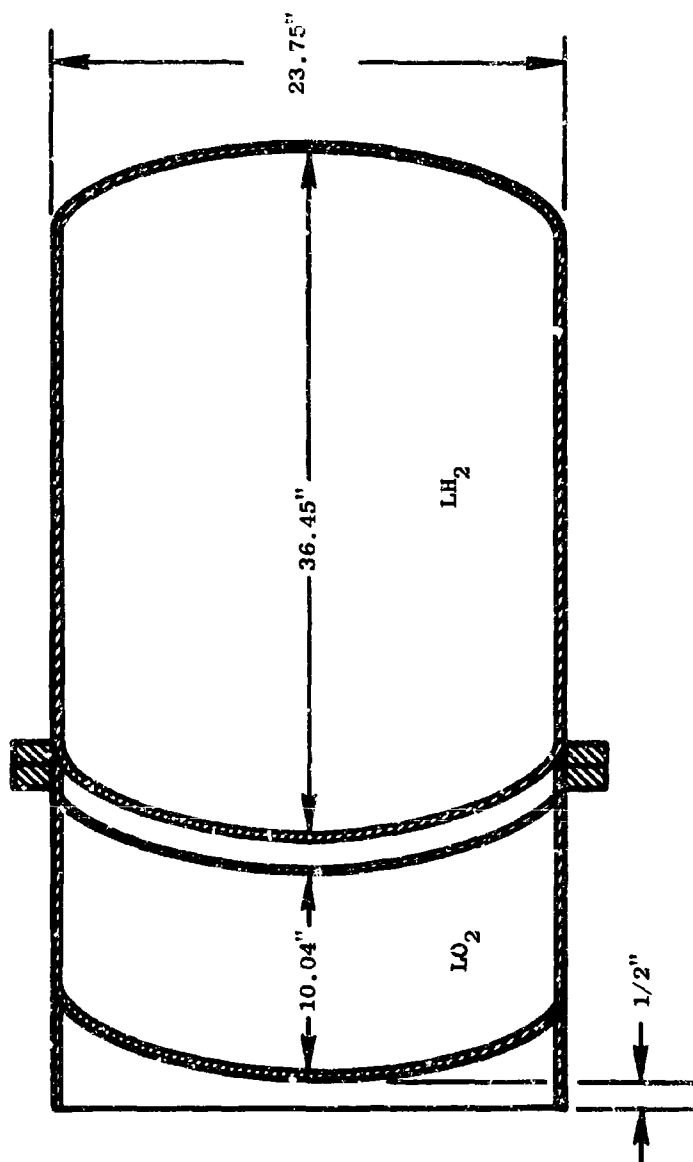


FIG. 5-17 200-lb LO_2/LH_2 Impact Tank

vented to the atmosphere to prevent a possible negative pressure between the bulkheads caused by the cooling of the air in this chamber by the cryogenic propellants. All surfaces of the cryogenic compartments of these tanks were covered with foamed-in-place polyurethane foam.

An enlarged drawing of the tank flange detail is shown in Fig. 5-18. The flange labeled No. 1 is securely welded to the rear tank and has bolt holes of normal size. Flange No. 2 is securely welded to the forward tank, has enlarged bolt holes, and is sandwiched between two Teflon gaskets which are labeled in Fig. 5-18. The third flange is a loose clamping flange, which was slipped on over the forward tank and has bolt holes of normal size. The purpose of the Teflon and enlarged bolt holes in the forward tank flange is to allow for thermal contraction of this tank, which always contained the colder of the two propellants. A photograph of a LO_2 /RP-1 tank in place on the sled track is shown in Fig. 5-19.

The remaining test hardware, the test conditions, and the instrumentation system were the same as that used in the second hypergolic high-velocity test series discussed in Section 4.

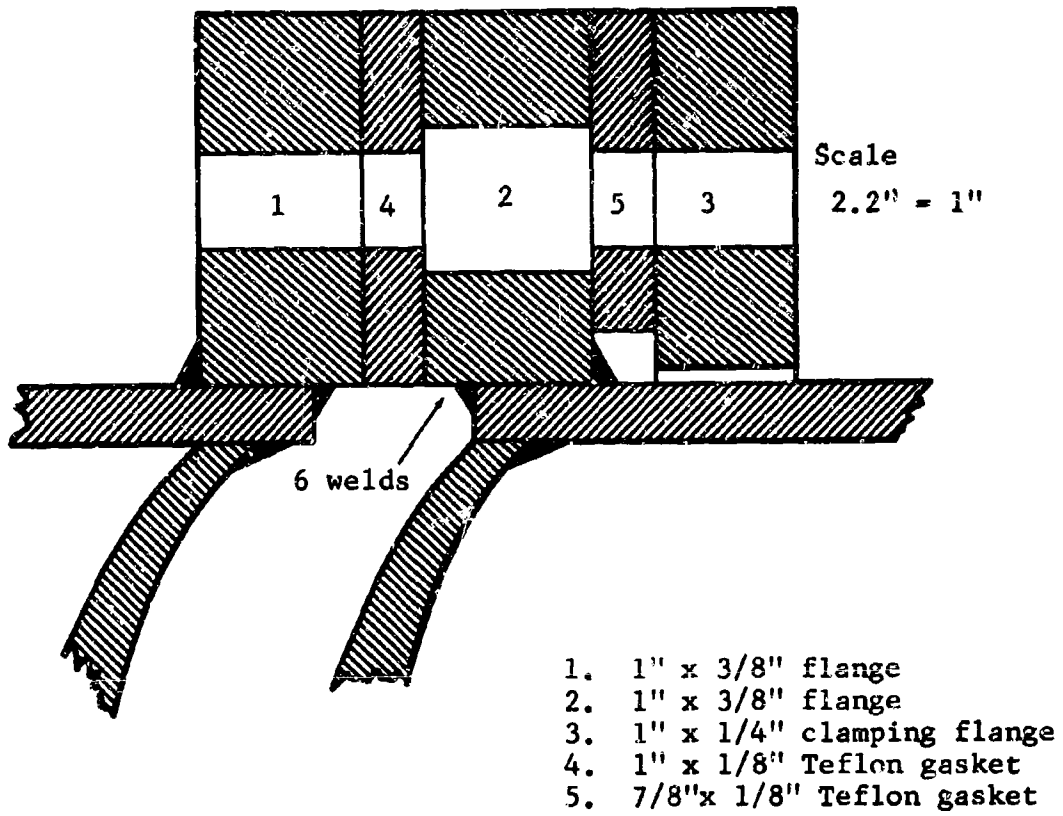


Fig. 5-18. Tank Flange Detail



Fig. 5-19 Photo of Tank on Sled Track

Part 2 - TEST RESULTS FROM THE BASIC PROGRAM

General

As noted in the beginning of this section, a total of 212 tests were conducted using the cryogenic propellant combinations $LO_2/RP-1$ and LO_2/LH_2 . Of the total number, 152 were considered as valid tests, meeting the requirements of the basic PYRO program discussed in Section 3. A summary of these tests is presented in Table 5-1. These tests are discussed in Part 5 of this section. Included in this part of the report are: a brief description of the instrumentation system to indicate the overall scope of the data; a discussion of the method used to obtain representative explosive yield values; a table of the yield values for each valid test; and a discussion of the comparisons between propellant peak overpressure* and positive-phase-impulse* distance curves and those for TNT.

Instrumentation Systems

The cryogenic CEM and CEGS tests were conducted at AFRPL, and the blast environment was measured using the basic PYRO instrumentation system. This system consisted of 15 piezoelectric transducers distributed along three radial lines, 120 deg apart and at five distances from ground zero. A sketch of the gauge layout is presented in Fig. 5-20. The system was designed to cover the pressure range from 1 to 100 psi and to have a minimum of three gauges in the region below about 15 psi, where the shock wave would be expected to be unsupported, i.e., classical in nature for a 10 percent nominal explosive yield.

To maintain these criteria, the gauge location distances were increased with increasing propellant weight. This is illustrated in Table 5-2, which lists typical blast instrumentation distances for tests with 200, 1000,

* The terms peak overpressure and overpressure and positive-phase impulse and impulse are used interchangeably throughout this section.

Table 5-1
SUMMARY OF CRYOGENIC TESTS

| Propellant Type | Test Configuration | Propellant Weight (lb) | Number of Tests |
|---|-----------------------------------|------------------------|-----------------|
| LO ₂ /RP-1 | Confinement-by-the-Missile | 133 | 2 |
| | | 200 | 16 |
| | | 1,000 | 4 |
| | | 25,000 | 3 |
| | | 100,000 | 1 |
| | Confinement-by-the-Ground Surface | 200 | 34 |
| | 1,000 | 7 | |
| | 25,000 | 2 | |
| | High-Velocity Impact | 200 | 4 |
| LO ₂ /LH ₂ | Confinement-by-the-Missile | 133 | 2 |
| | | 200 | 16 |
| | | 1,000 | 4 |
| | | 25,000 | 3 |
| | | 100,000 | 1 |
| | Confinement-by-the-Ground Surface | 200 | 40 |
| | 1,000 | 7 | |
| | 25,000 | 3 | |
| | High-Velocity Impact | 200 | 2 |
| Combined LO ₂ /RP-1 and LO ₂ /LH ₂ | Confinement-by-the-Ground Surface | 1,200 | 1 |

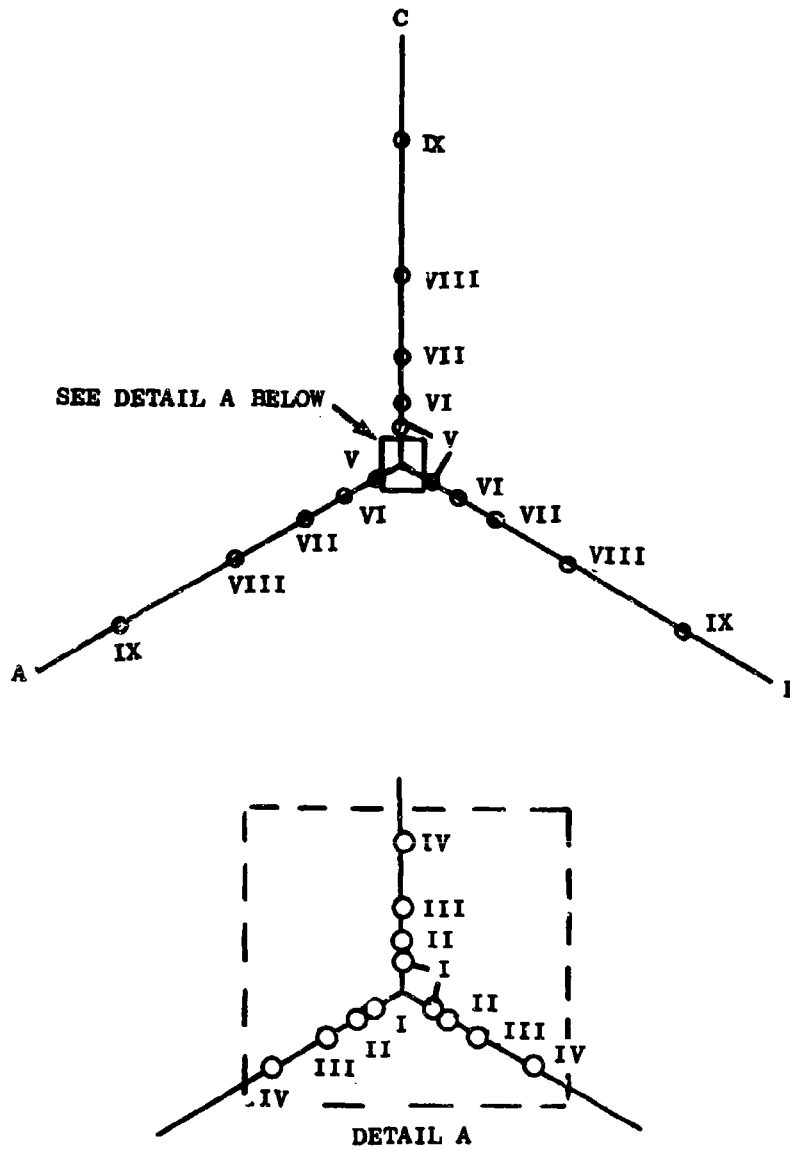


Fig. 5-20 Gauge Layout

Table 5-2
SUMMARY OF BLAST INSTRUMENTATION

| No. | Gauge Line | Nominal Distance (ft) | 200 lb | | 1000 lb | | 25000 lb | | 100000 lb | |
|------|------------|-----------------------|-----------------------------|------------------------------|----------------|----------------|----------------|----------------|----------------|------------------|
| | | | P _s [*] | P _o ^{**} | P _s | P _o | P _s | P _o | P _s | P _o |
| I | A | 2.8 | | X | | | | | | |
| | B | | | | | | | | | |
| | C | | | | | | | | | |
| II | A | 4.5 | X | X | | X | | | | |
| | B | | | | | | | | | |
| | C | | | | | | | | | |
| III | A | 7.5 | X | X | X | X | | | | |
| | B | | X | X | | | | | | |
| | C | | | X | | | | | | |
| IV | A | 13 | X | X | X | X | | X | | X ^{***} |
| | B | | | X | X | | | | | |
| | C | | | X | X | | | | | X ^{***} |
| V | A | 23 | | X | X | X | X | X | | |
| | B | | | X | X | | | | | X |
| | C | | | X | X | | | | | X |
| VI | A | 58 | | X | | X | X | X | | |
| | B | | | X | | X | X | | | X |
| | C | | | X | | X | X | | | X |
| VII | A | 67 | | X | | X | X | X | | X |
| | B | | | X | | X | X | | | X |
| | C | | | X | | X | X | | | X |
| VIII | A | 117 | | | | X | | X | | X |
| | B | | | | | X | | X | | X |
| | C | | | | | X | | X | | X |
| IX | A | 200 | | | | | | X | | X |
| | B | | | | | | | X | | X |
| | C | | | | | | | X | | X |
| X | A | 335 | | | | | | X | | X |
| | B | | | | | | | X | | X |
| | C | | | | | | | X | | X |
| XI | A | 600 | | | | | | X | | X |
| | B | | | | | | | X | | X |
| | C | | | | | | | X | | X |

* head-on-oriented stagnation pressure sensor

** side-on-oriented overpressure sensor

*** a single gauge at the 13-ft distance was alternately located along gauge lines A and C

and 25,000 lb of propellant. To supplement the basic overpressure measurement system, two overpressure gauges were installed as close to the explosion as practical and stagnation pressure gauges spanning the range from near ground zero to the 30-psi level were installed for many of the tests. A complete description of the system and the methods of calibration and estimates of the accuracy of the system are given in Appendix A.

The cryogenic high-velocity-impact tests were conducted at the Naval Weapons Center, China Lake, California. The instrumentation layout used for this series is the same as that used for the second hypergolic high-velocity impact test series. A sketch of this system is shown in Figure 5-21. The propellant tanks in these tests were propelled along a track and allowed to impact into a target. Piezoelectric overpressure gauges were installed at four distances on a line 30 deg from this track, at three distances at 90 deg and at two distances in line with the track and behind the target.

Test Results

The peak overpressure and positive-phase-impulse data for the CBM and CBGS tests are presented in Volume 2. Also presented with these data are the TNT equivalent yield values for each of the peak overpressure and positive-phase-impulse numbers.

A study of these TNT equivalent yield values indicated that the explosive behavior of the cryogenic propellant mixtures in many cases was somewhat different from TNT, in that the explosive yield tends to be a function of distance and in some cases a function of the parameter measured. This behavior appeared to take two general forms. For low yields, it was commonly observed that both pressure and impulse yields increased with increasing distance from the explosion, with the yields tending toward a constant value (the terminal yield) at long distances. In addition, the impulse yield was usually higher than the pressure yield and changed more slowly with distance. For higher yields, the pressure yield behaved in the same fashion, but the

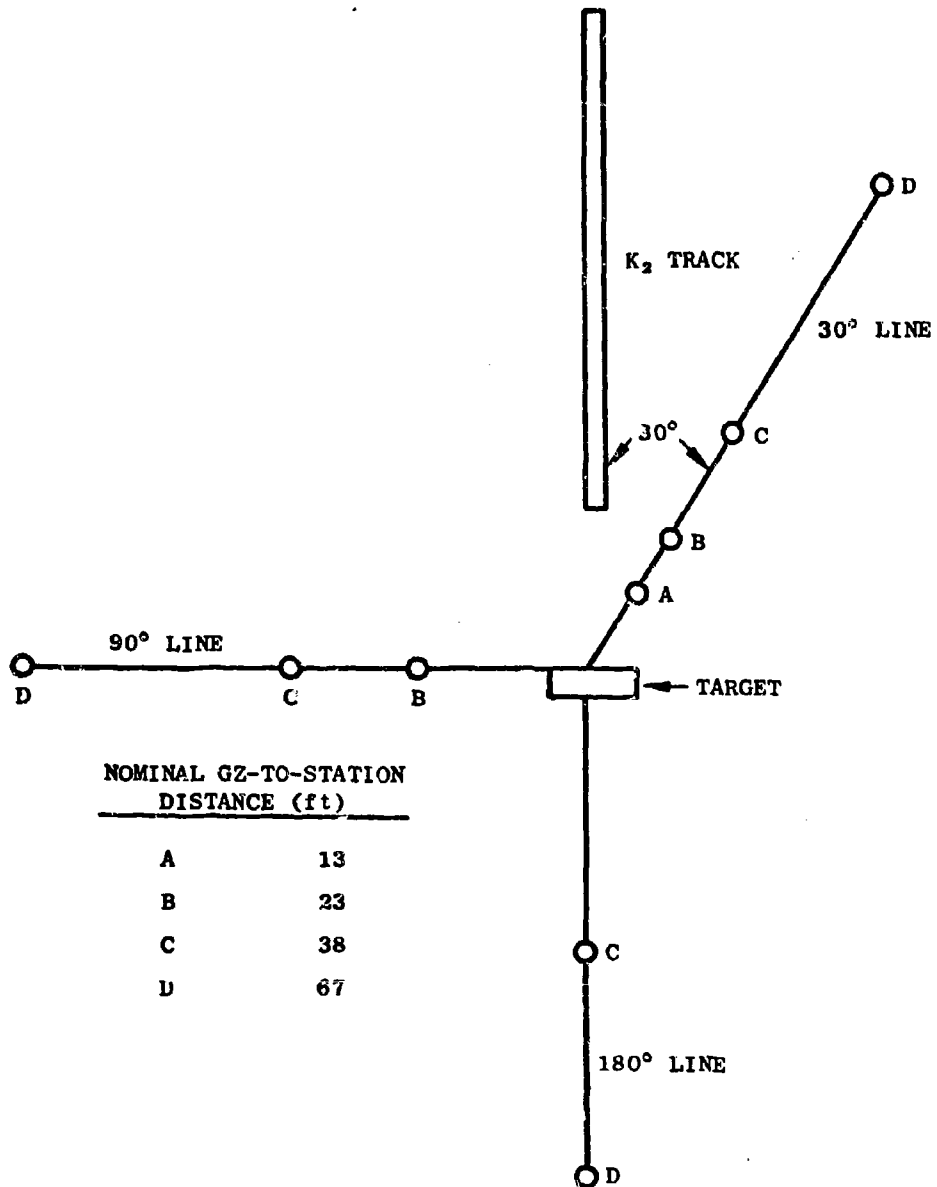


Fig. 5-21. Pressure Sensor Locations for Cryogenic High-Velocity Impact Test Series

impulse yield at close distances was much greater than the pressure yield and tended to decrease with increasing distance and ultimately approached the pressure yield value. Two examples of this are shown in Figs. 5-22 and 5-23 which are plots of peak overpressure and positive-phase impulse vs distance for two LO_2/LH_2 confinement-by-the-missile tests. Included in these figures are curves (dotted lines) representing the peak overpressure and impulse that would be obtained if the various indicated percentages of 200 lb of TNT were exploded on the ground surface (TNT overpressure data were obtained from Ref. 5-1 and impulse data from Ref. 5-2).

Note in Figs. 5-22 and 5-23 that both the peak overpressure and positive-phase-impulse data for the lower yield test, Number 091, tend to show an increase in yield with an increase in distance. The peak overpressure data for the higher yield test, Number 090, also show an increase in yield with distance but the positive-phase-impulse data show a decrease with distance.

Because of this yield variation with distance and in some cases with the shock wave parameter used in the computation, the explosive yield number used in this report is the terminal yield, i.e., the yield obtained at long distances from the explosion where both the pressure and impulse yields tend to reach a constant value.

The terminal yields for the $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 confinement-by-the-missile tests are presented in Tables 5-3 and 5-4 and for the $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 confinement-by-the-ground-surface tests in Tables 5-5a and b and 5-6a and b. The terminal yields for both the $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 high-velocity-impact tests and a combined $\text{LO}_2/\text{RP-1}-\text{LO}_2/\text{LH}_2$ CBGS test are presented in Tables 5-7a and b.

Pressure and Impulse-Distance Curves

A comparison between the shapes of the propellant pressure and impulse - distance curves and those for TNT is shown in Figs. 5-24 through 5-31, which are plots of the pressure or scaled impulse ($I/W_T^{1/3}$) as a function of scaled distance ($D/W_T^{1/3}$). Each point represents the average pressure of all gauges (usually two or three) at a given distance for a given test.

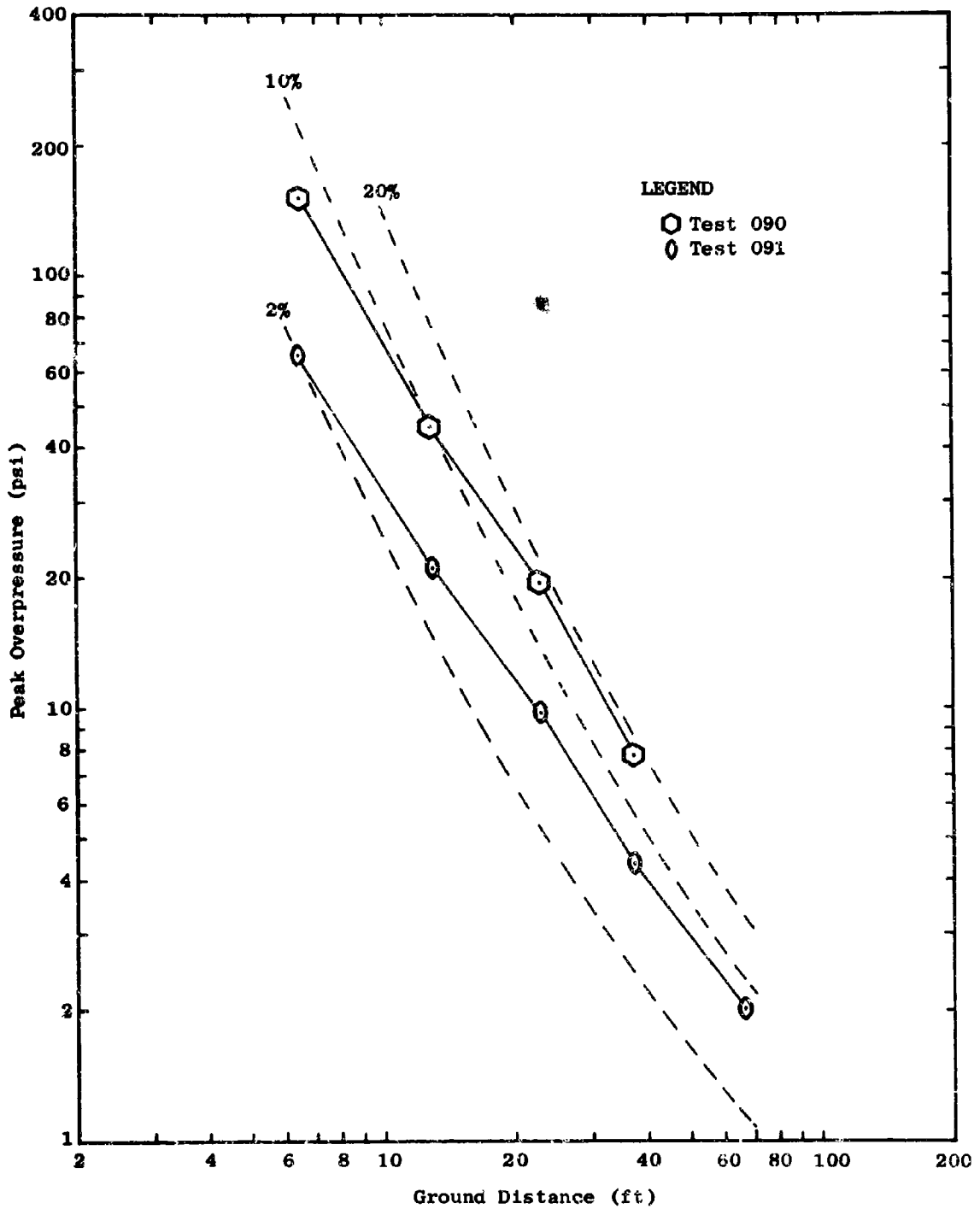


Fig. 5-22 Peak Overpressure vs Ground Distance for 200-lb LO₂/LH₂ Confinement-by-the-Missile Tests, L/D = 1.8, D_o/D_t = 0.45 (Dashed lines are TNT surface burst reference curves)

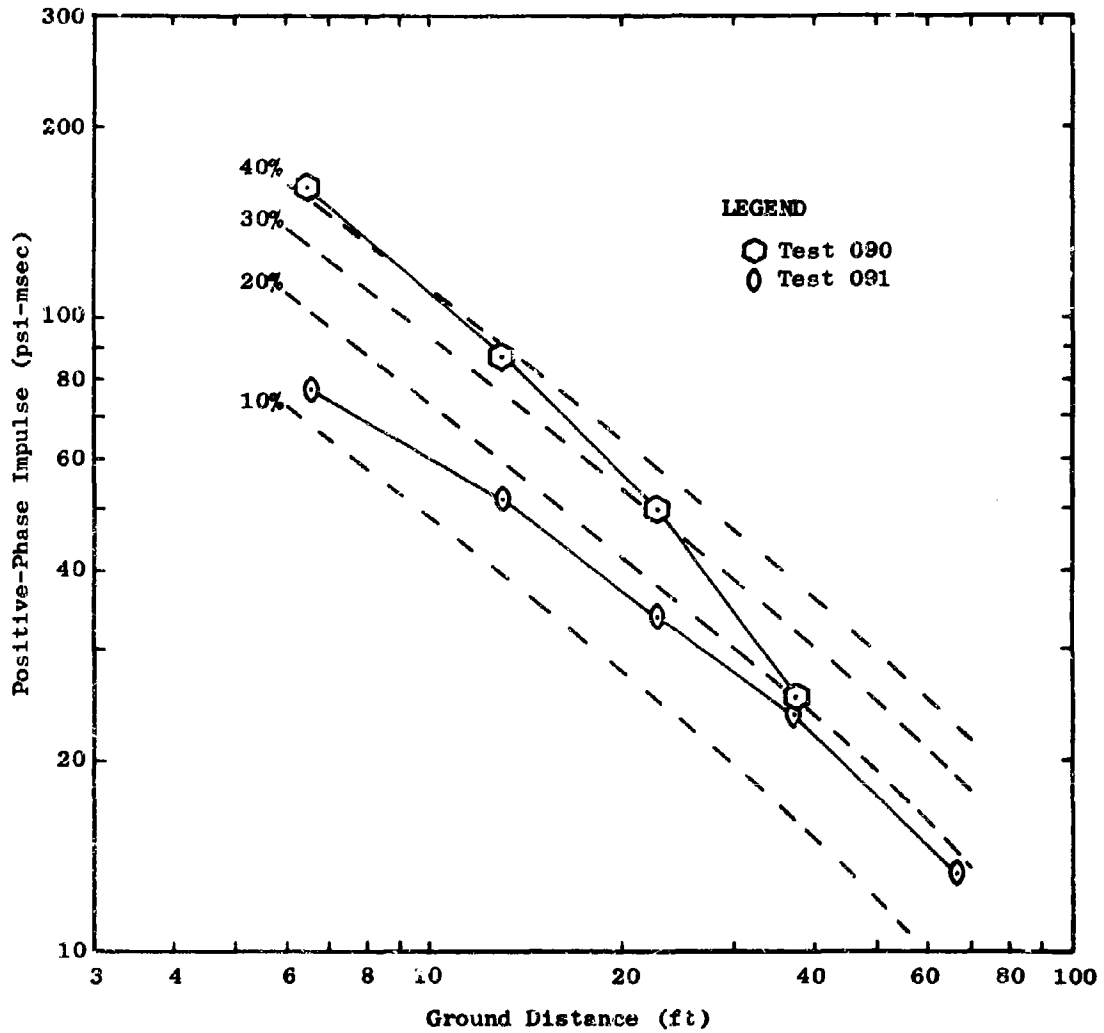


Fig. 5-23. Positive-Phase Impulse vs Ground Distance for 200-lb LO_2/LH_2 Confinement-by-the-Missile Tests, $L/D = 1.8$, $D_o/D_t = 0.45$ (Dashed lines are TNT surface burst reference curves)

Table 5-3
 $LO_2/RP-1$ CONFINEMENT-BY-THE-MISSILE TESTS

| L/D | D_o/D_t | PROPELLANT WEIGHT (lb) | TEST NO. | TERMINAL YIELD (%) | IGNITION TIME (msec) |
|------|---------------|------------------------|----------|--------------------|----------------------|
| 1.8 | 1 | 200 | 042 | 48 | 290 |
| | | | 058 | 27 | 200 |
| | | | 086 | 14 | 100 |
| | 0.45 | 200 | 044 | 18 | 120 |
| | | | 087A | 16 | 70 |
| | | | 095A | 17 | 120 |
| | | | 101 | 35 | 145 |
| | | | 237 | 32 | 127 |
| | | | 238 | 19 | 85 |
| | | | 239 | 32 | 156 |
| | Partial Full* | 174 | 52 | 150 | |
| | | 240 | 60 | 156 | |
| | 0.45 | 1000 | 192 | 14 | 216 |
| | | | 193 | 20 | 222 |
| | | | 209 | 10 | 121 |
| 270A | | | 13 | 225 | |
| 0.45 | 25000 | 275 | 4 | 515 | |
| | | 278 | 13 | 530 | |
| | | 282 | 13 | 540 | |
| 5 | 1 | 200 | 047 | 10 | 120 |
| | | | 049 | 12 | 316 |
| | | | 085 | 12 | 380 |
| 5 | 0.45 | 200 | 046 | 17 | 143 |
| | | | 088 | 4 | 60 |
| | | | 100 | 23 | 220 |
| 4 | 0.1375 | 94000 | 301 | 4 | 840 |

* Tank contained ~133 lb propellant

Table 5-4
 LO_2/LH_2 CONFINEMENT-BY-THE-MISSILE TESTS

| L/D | D_o/D_t | PROPELLANT WEIGHT (lb) | TEST NO. | TERMINAL YIELD (%) | IGNITION TIME (msec) | | | |
|-------|-----------|------------------------|----------|--------------------|----------------------|--------|----------|--|
| 1.8 | 1 | 200 | 050 | 86 | 180 | | | |
| | | | 051 | 22 | 80 | | | |
| | | | 093 | 34 | 147 | | | |
| | 0.45 | 200 | 200 | 053 | 4 | 1 | | |
| | | | | 090 | 29 | 35 sec | | |
| | | | | 091 | 13 | 0 | | |
| | | | | 118 | 20 | 82 | | |
| | | | | 199 | 8 | 816 | | |
| | | | | 200 | 17 | 417 | | |
| | | Partial Full* | 167 | 172 | 167 | 24 | 8.74 sec | |
| | | | | | 172 | 35 | 770 | |
| | | 1000 | 1000 | 1000 | 210 | 7 | 20 | |
| | | | | | 212 | 27 | 1366 | |
| | | | | | 213 | 35 | 708 | |
| | | | | | 265 | 10 | 750 | |
| 25000 | 25000 | 25000 | 277 | 0.2 | 31 | | | |
| | | | 279 | 0.2 | 33 | | | |
| | | | 281 | 0.1 | - | | | |
| 5.1 | 1 | 200 | 052 | 7 | 83 | | | |
| | | | 057 | 1 | 12 | | | |
| | | | 092 | 26 | 3 min | | | |
| | 0.45 | 200 | 200 | 054 | 6 | 17 | | |
| | | | | 055 | 1 | 1 | | |
| | | | | 094 | 25 | 329 | | |
| | | | | 138 | 17 | 100 | | |
| | | | | | | | | |
| | | | | | | | | |

* Tank contained ~133 lb propellant.

Table 5-5a

LO₂/RP-1 CONFINEMENT-BY-THE-GROUND-SURFACE VERTICAL TESTS

| L/D | PROPELLANT WEIGHT (lb) | VELOCITY RANGE | PROPELLANT ORIENTATION | TEST NO. | TERMINAL YIELD (%) | IGNITION TIME (msec) |
|------|------------------------|---------------------|------------------------|----------|--------------------|----------------------|
| 1.8 | 200 | Low 23 ft/sec | Normal | 096 | 14 | 50 |
| | | | | 144 | 24 | 190 |
| | | | | 202 | 42 | 870 |
| | | | | 248 | 25 | 210 |
| | | 097 | 32 | 240 | | |
| | | Medium 44 ft/sec | Normal | 208 | 62 | 460 |
| | 232 | | | 30 | 1220 | |
| | 249 | | | 50 | 710 | |
| | 1000 | Medium 44 ft/sec | Normal | 250 | 52 | 200 |
| | | | | 218 | 4 | 0 |
| | | | | 219 | 14 | 1835 |
| | | | | 220 | 96 | 525 |
| | 25000 | Medium 44 ft/sec | Normal | 257 | 64 | 1770 |
| | | | | 268 | 70 | 340 |
| 200 | High 78 ft/sec | Normal | 284 | 2 | 0 | |
| | | | 285 | 37 | 465 | |
| | | | 110 | 26 | 35 | |
| | | | 141 | 5 | 0 | |
| | | | 205 | 41 | 40 | |
| | | | 206 | 85 | 350 | |
| | | | 207 | 38 | 28 | |
| | | | 236 | 74 | 720 | |
| 200 | High 78 ft/sec | Reversed | 107 | 29 | 42 | |
| | | | 142 | 14 | 33 | |
| 1000 | High 78 ft/sec | Normal | 190 | 96 | 570 | |
| | | | 269A | 44 | 77 | |
| 5 | 200 | Low 23 ft/sec | Normal | 098 | 14 | 300 |
| | | | | 154 | 18 | 470 |
| | | High 78 ft/sec | Reversed | 099 | 23 | 140 |
| | | | | 109 | 10 | 40 |
| 106 | Reversed | 106 | 31 | 60 | | |

Table 5-5b

LO₂/RP-1 CONFINEMENT-BY-THE-GROUND SURFACE HORIZONTAL TESTS

(All Tests 200 lb Propellant Weight)

| PROPELLANT TYPE | VELOCITY RANGE* | PROPELLANT ORIENTATION | TEST NO. | TERMINAL YIELD (%) | 1ST PROP. TO 2ND PROP. (msec) | 2ND PROP. TO IGNITION (msec) | |
|-----------------------|-------------------|------------------------|----------|--------------------|-------------------------------|------------------------------|-----|
| LO ₂ /RP-1 | Low 23 ft/sec | Normal | 121 | 30 | ** | 184 | |
| | | | 122 | 28 | 388 | 139 | |
| | | | 123 | 25 | 362 | 53 | |
| | | | 176 | 65 | 468 | 374 | |
| | | | 177 | 30 | 111 | 203 | |
| | | Reversed | 155 | 55 | 314 | 202 | |
| | | | | 156 | 63 | 182 | 230 |
| | High 78 ft/sec | Normal | 124 | 5 | 238 | 0 | |
| | | | 125 | 52 | 142 | 76 | |
| | | | 127 | 42 | 510 | 55 | |
| | | | 140 | 12 | 420 | 10 | |
| | | | 179 | 12 | 787 | 23 | |

* Velocity shown for top propellant only. Bottom propellant velocity ~12 ft/sec for all tests.

** RP-1 on ground at start of film.

Table 5-6a

LO₂/LH₂ CONFINEMENT-BY-THE-GROUND-SURFACE VERTICAL TESTS

| L/D | PROPELLANT WEIGHT (lb) | VELOCITY RANGE | PROPELLANT ORIENTATION | TEST NO. | TERMINAL YIELD (%) | IGNITION TIME (msec) |
|-----|------------------------|---------------------|------------------------|----------|--------------------|----------------------|
| 1.8 | 200 | Low 23 ft/sec | Normal | 105 | 7 | 0 |
| | | | | 152 | 14 | 480 |
| | | | | 153 | 14* | 121 |
| | | | | 184 | 17 | 810 |
| | | | | 201 | 26 | 1524 |
| | | | | 225 | 34 | 933 |
| | | Medium 44 ft/sec | Normal | 197 | 19 | 500 |
| | | | | 203 | 31 | 800 |
| | | | | 204 | 42 | 317 |
| | | | | 229 | 53 | 1374 |
| | | | | 230 | 21 | 24 |
| | | | | 231 | 24 | 525 |
| | 1000 | Medium 44 ft/sec | Normal | 251 | 64 | 775 |
| | | | | 252 | 38 | 325 |
| | | | | 254 | 32 | 533 |
| | | | | 211 | 12 | 0 |
| | 25000 | Medium 44 ft/sec | Normal | 217 | 33 | 1490 |
| | | | | 262 | 42 | 900 |
| | | | | 264 | 22 | 21 |
| | | | | 266 | 14 | 0 |
| | 200 | High 78 ft/sec | Normal | 288C | 13 | 365 |
| | | | | 289 | 4 | 166 |
| | | | | 290 | 4 | 105 |
| | | | | 314 | 54 | 74 |
| 150 | | | | 35 | 40 | |
| 151 | | | | 46 | 167 | |
| 195 | | | | 104 | 292 | |
| 226 | | | | 51 | 283 | |
| 215 | | | | 20 | 20 | |
| 216 | | | | 9 | 0 | |
| 5 | 200 | Low 23 ft/sec | Normal | 104 | 6 | 258 |
| | | | | 164 | 4 | 125 |
| | | | Reversed | 165 | 8 | 325 |
| | | | | 103A | 39 | 208 |
| | | High 78 ft/sec | Normal | 116 | 10 | 18 |
| | | | | 160 | 32 | 67 |
| | | | Reversed | 161 | 5 | 0 |
| | | | | 113 | 52 | 77 |
| 115 | 15 | 93 | | | | |

* No impulse because of bad timing

Table 5-6b

LO₂/LH₂ CONFINEMENT-BY-THE-GROUND SURFACE HORIZONTAL TESTS

(All Tests 200 lb Propellant Weight)

| PROPELLANT TYPE | VELOCITY RANGE* | PROPELLANT ORIENTATION | TEST NO. | TERMINAL YIELD (%) | 1ST PROP. TO 2ND PROP. (msec) | 2ND PROP. TO IGNITION (msec) |
|----------------------------------|-------------------|------------------------|----------|--------------------|-------------------------------|------------------------------|
| LO ₂ /LH ₂ | Low 23 ft/sec | Normal | 131 | 6 | 441 | 125 |
| | | | 132 | 5 | 0 | 0 |
| | | | 133A | 6 | 289 | 286 |
| | | | 185 | 8 | 469 | 0 |
| | | | 186 | 9 | 597 | 61 |
| | | | 223 | 18 | 708 | 157 |
| | | | 224 | 16 | 899 | 621 |
| | High 78 ft/sec | Normal | 183 | 15 | 448 | 0 |
| | | | 196 | 17 | 639 | 77 |
| | | | 228 | 34 | 560 | 42 |
| | | | 253 | 57 | 561 | 108 |
| | | | | | | |

* Velocity shown for top propellant only. Bottom propellant velocity ~12 ft/sec for all tests.

Table 5-7a
CRYOGENIC HIGH-VELOCITY-IMPACT TESTS

| <u>Propellant Type</u> | <u>Test Number</u> | <u>Target Geometry</u> | <u>Impact (fps)</u> | <u>Terminal Yield (%)</u> |
|----------------------------------|--------------------|------------------------|---------------------|---------------------------|
| LO ₂ /RP-1 | 75 | Flat Wall | 526 | 21 |
| | 77 | Flat Wall | 523 | 20 |
| | 76 | Deep Hole | 523 | 57 |
| | 78 | Deep Hole | 518 | 77 |
| LO ₂ /LH ₂ | 79 | Flat Wall | 597 | 121 |
| | 80 | Deep Hole | 569 | 163 |

Table 5-7b
COMBINED PROPELLANT TEST
(LO₂/RP-1 and LO₂/LH₂)

| <u>L/D</u> | <u>Propellant Weight</u> | <u>Velocity Range</u> | <u>Propellant Orientation</u> | <u>Test No.</u> | <u>Terminal Yield (%)</u> | <u>Ignition Time (msec)</u> |
|------------|---|-----------------------|-------------------------------|-----------------|---------------------------|-----------------------------|
| 1.8 | 1000 LO ₂ /RP-1 and 200 LO ₂ /LH ₂ | Medium 44 ft/sec | Normal | 295 | 70 | 544 |

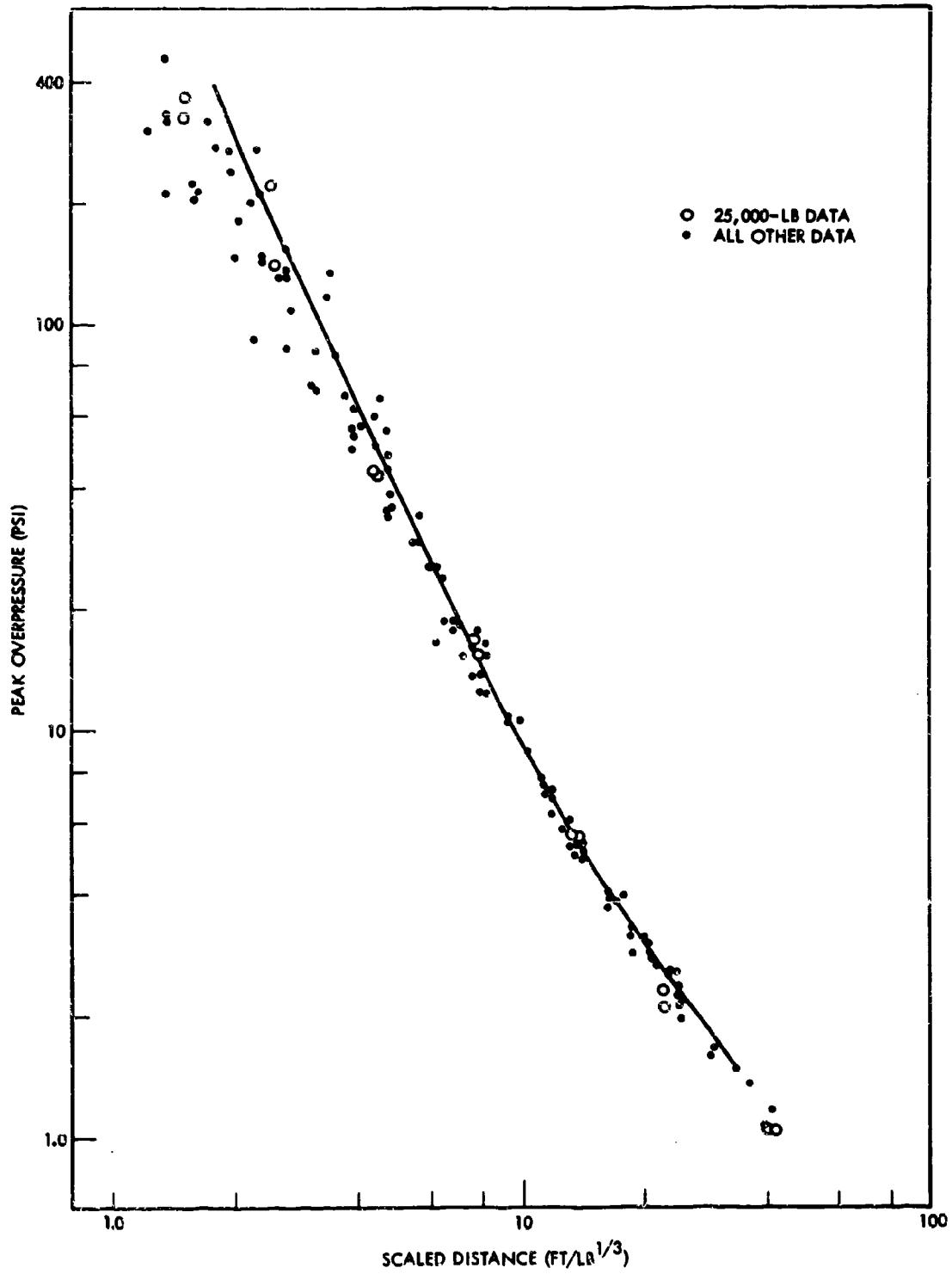


Fig. 5-24. Pressure vs Scaled Distance for LO₂/RP-1 CBM Case

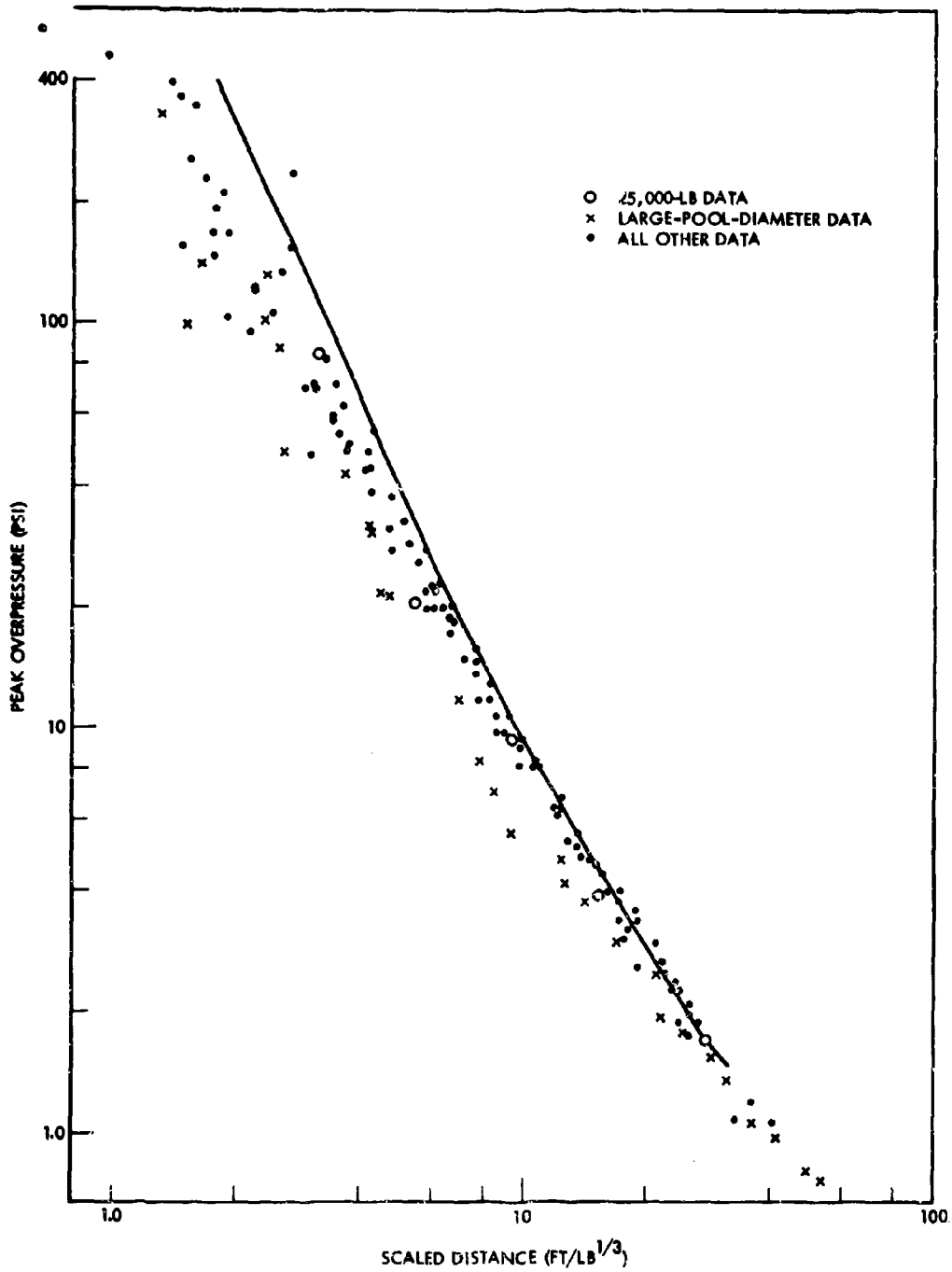


Fig. 5-25. Pressure vs Scaled Distance for LO₂/RP-1 CBGS-V Case

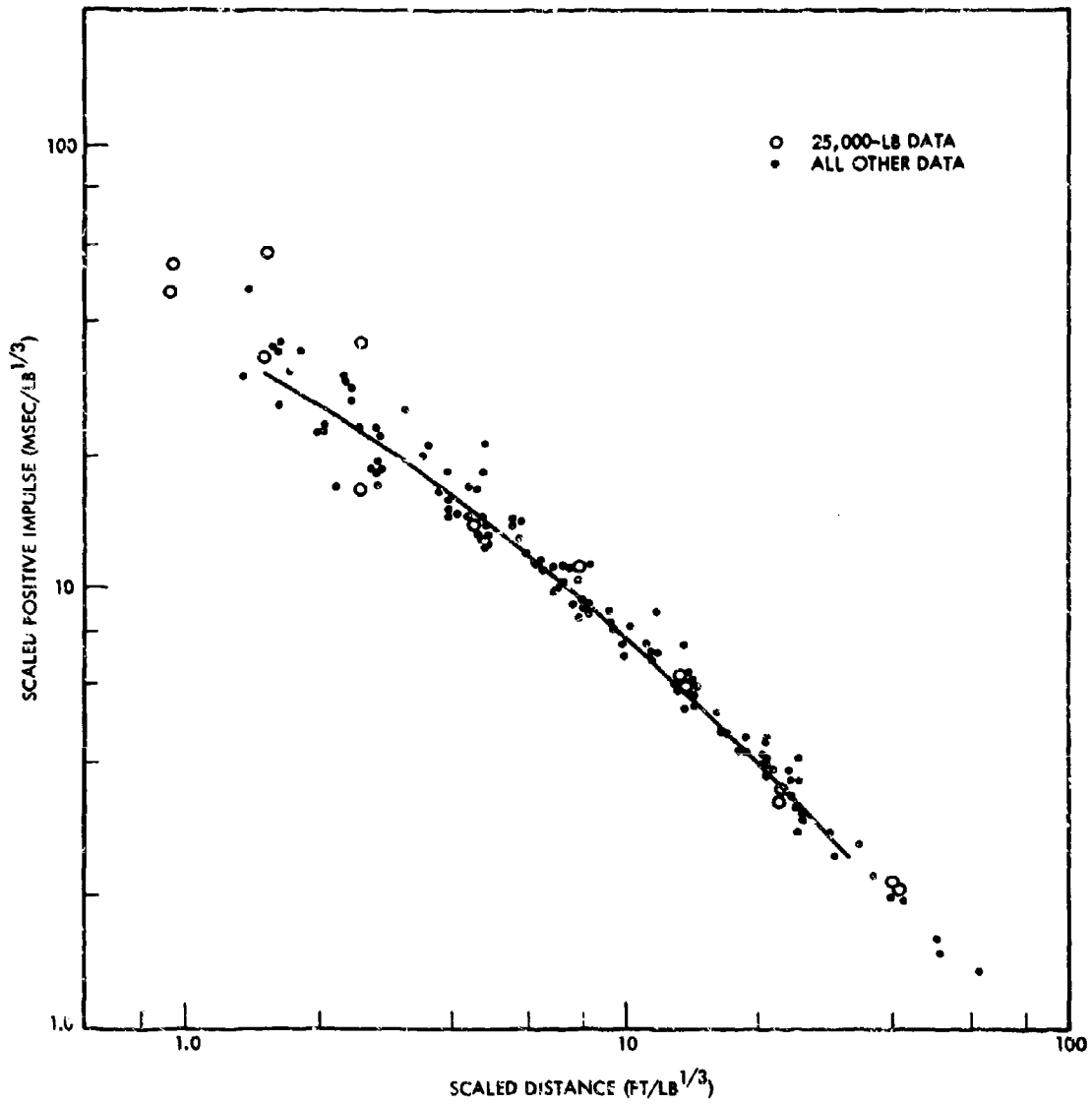


Fig. 5-26. Scaled Positive Impulse vs Scaled Distance for LO₂/RP-1 CBM Case

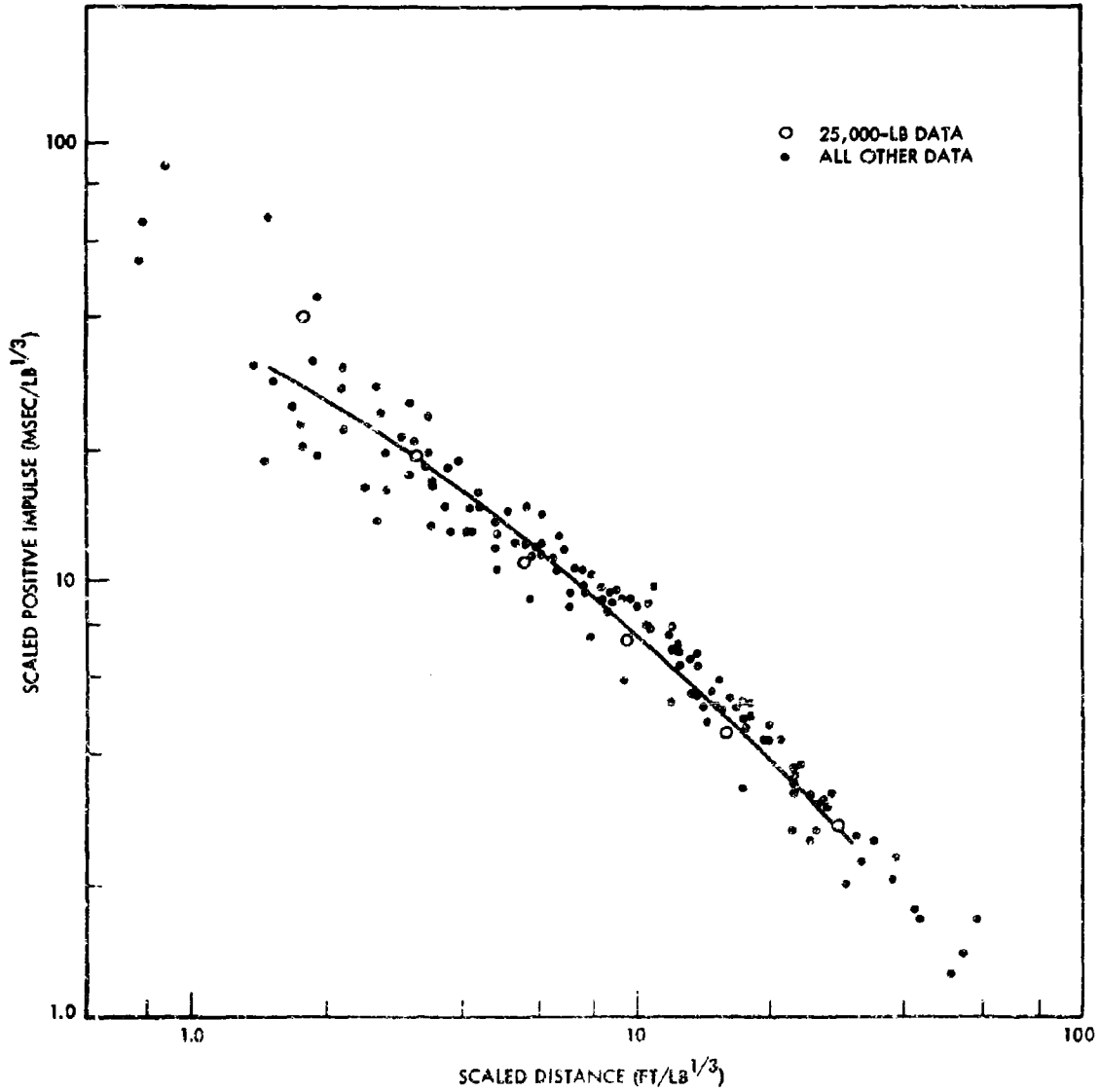


Fig. 5-27. Scaled Positive Impulse vs Scaled Distance for LO₂/RP-1 CBGS-V Case

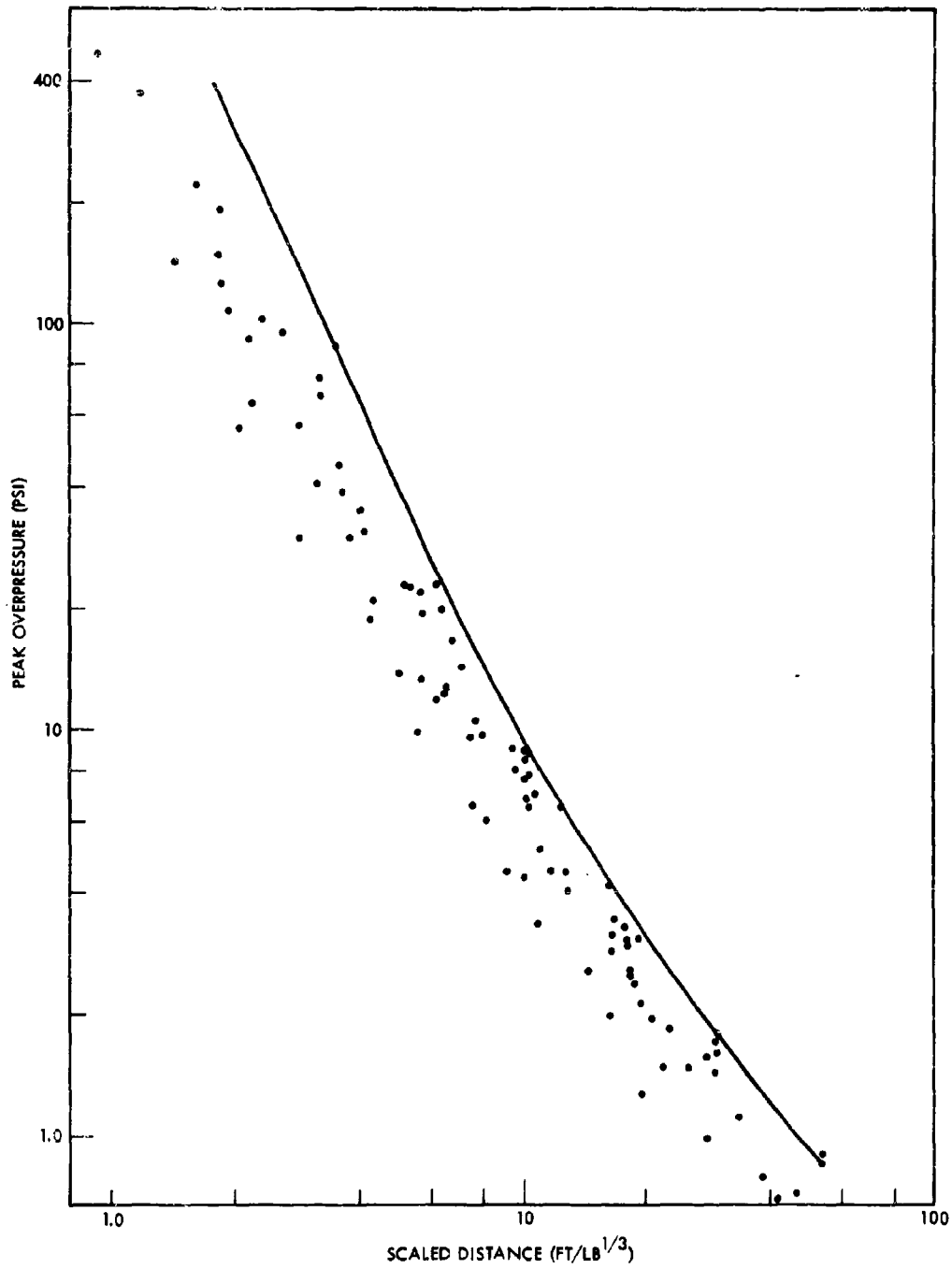


Fig. 5-28. Pressure vs Scaled Distance for LO₂/LH₂ CRM Case

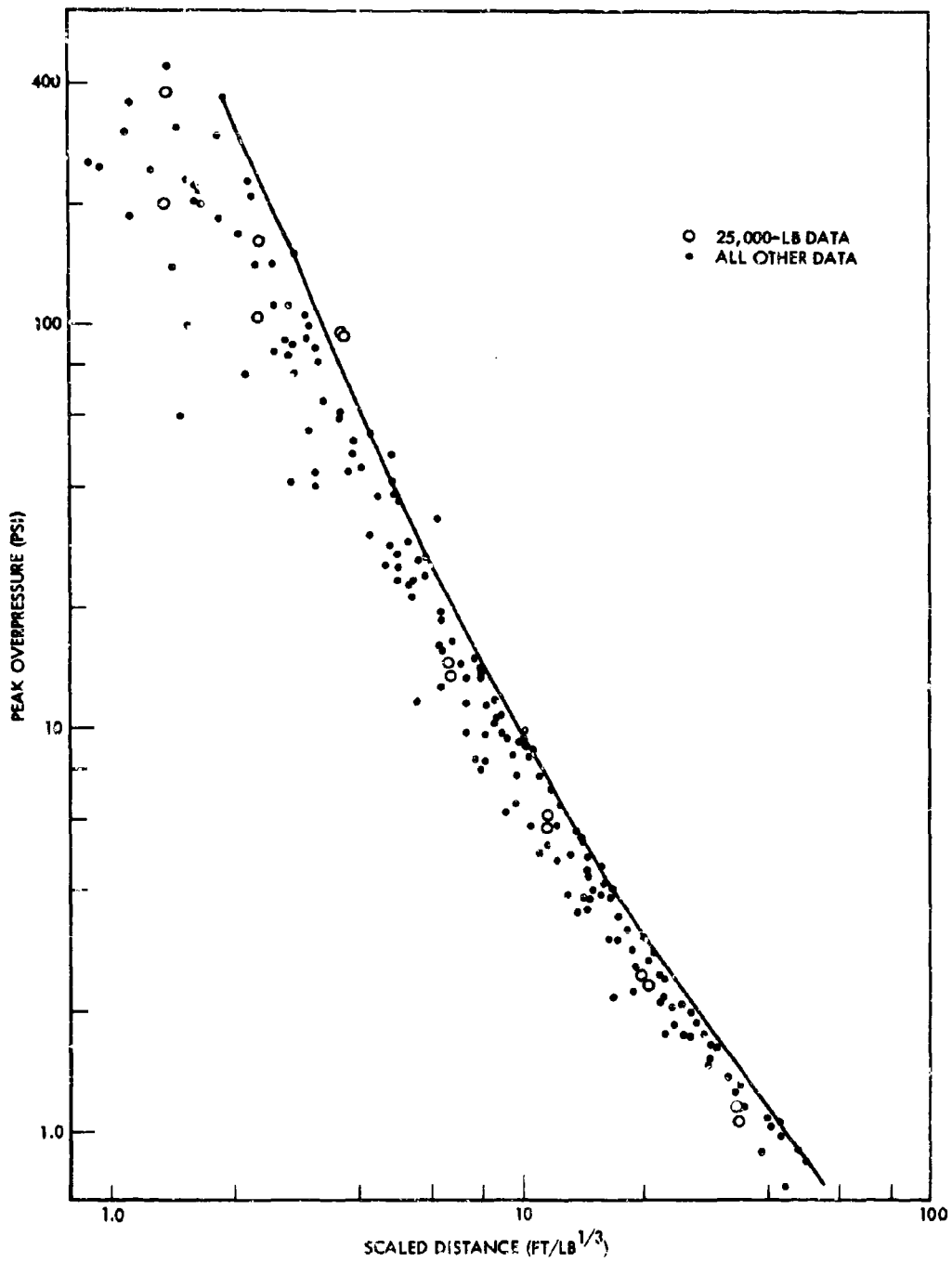


Fig. 5-29. Pressure vs Scaled Distance for LO₂/LH₂ CBGS-V Case

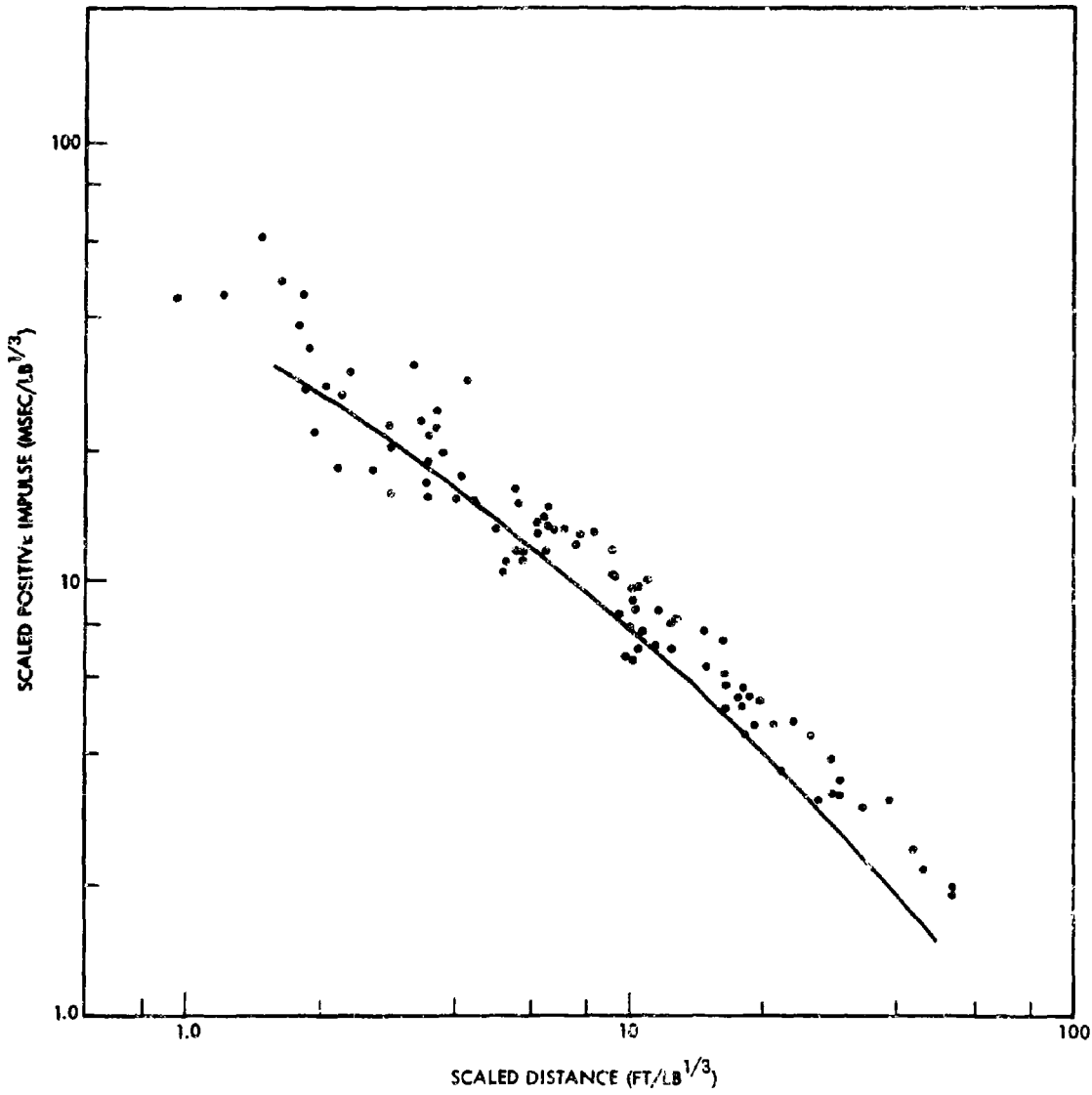


Fig. 5-30. Scaled Positive Impulse vs Scaled Distance for LO₂/LH₂ CEM Case

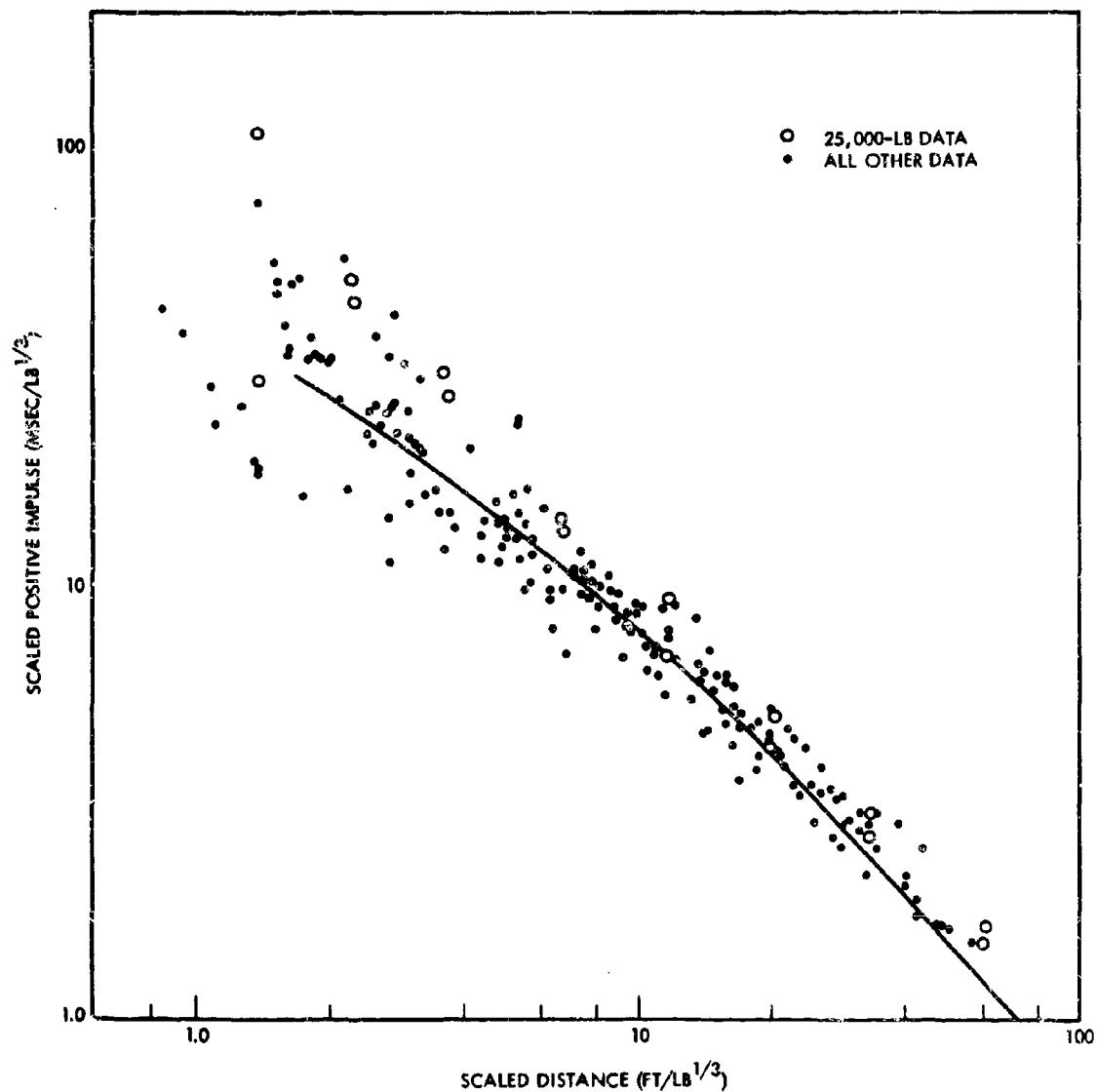


Fig. 5-31. Scaled Positive Impulse vs Scaled Distance for LO₂/LH₂ CBGS-V Case

W_T is the TNT equivalent weight of the test (in pounds) and is equal to the product of the total propellant weight and the terminal yield. These plots include the data from more than 95 percent of all tests satisfying the CEM and CBGS-V cases. The CBGS-H data were much more limited and showed essentially the same trends as the CBGS-V results, so they were not included.

From Figs. 5-24 and 5-25, which are the pressure-distance curves for the LO₂/RP-1 CEM and CBGS cases, respectively, it can be seen that for all practical purposes the upper bound of the data is the TNT curve. It also can be seen that there is some spread below the reference curve. The spread tends to increase as the pressure increases. The bulk of the data, however, are within about 30 percent of the reference curve up to an over-pressure of about 50 psi (scaled distance of about 4.5 ft/lb^{1/3}). With only minor exceptions, the data points which are more than 30 percent below the reference curve in this region are for the CBGS case with long ignition times, where the propellants had spread to make large pools, greater than the critical size for maximum yield (noted as x points on graph).

Above about 100 psi (scaled distance of about 3.5 ft/lb^{1/3}), the spread increases rapidly and some of the data are as much as a factor of 4 or 5 below the reference curve, while others are still fairly close to the curve. Although there is some slight indication that the higher velocity impact points have a bigger deviation in this region, the data are not sufficient to clearly define any trends other than the one noted in the lower pressure region. Thus, although on the average the pressures in the region from 100 to 400 psi are about 60 to 70 percent of the TNT value, it would be difficult to guarantee that any particular test would do this.

Note that the data from the three 25,000-lb tests (included on these plots as O points) show essentially the same results as the smaller scale data. The one low yield value (CEM, 25,000-lb test, Number 275) which is not shown on these plots gave pressure data points which were about 60

percent of the reference curve and impulse data points a corresponding amount above the reference curve.

The $\text{LO}_2/\text{RP-1}$ impulse data are compared with the TNT reference curves in Figs. 5-26 and 5-27. In this case the spread of data is nearly symmetrical about the reference curve and again the spread tends to get larger as the scaled distance gets smaller. With only a few exceptions the data lie within about 30 percent of the curve down to a scaled distance of about 4 (corresponding to an overpressure of about 70 psi). At smaller scaled distances the spread increases to about 50 percent. There is some indication that, on the average, the CBM data are slightly higher than the CBGS data, but these differences are small compared to the spread.

The LO_2/LH_2 pressure data given in Figs. 5-28 and 5-29 again show that the TNT reference curve is a good upper bound and that the spread tends to get larger for increasing pressure. The magnitude of the spread, however, is considerably greater than for $\text{LO}_2/\text{RP-1}$. Even at pressures as low as 1 psi, some of the data, particularly for the CBM case, are as much as a factor of 1.5 to 2 below the reference curve. There is some indication that the higher yield values lie somewhat closer to the line than do the lower yields values, but the trend is not entirely consistent. On the average, the CBM data are about a factor of 2 below the reference curve up to an overpressure of about 100 psi and a little more than that above 100 psi. The CBGS data have about the same spread above 100 psi but a significantly smaller one below, decreasing to about a factor of 1.5 at 10 psi and 1.3 at 1 psi. The trends for the LO_2/LH_2 impulse data, which are given in Figs. 5-30 and 5-31, are generally as expected from the pressure data. At low pressures for the CBM case, where the pressures were well below the curve, the impulses are correspondingly above the reference curve. For both cases, on the average, the impulse data in the low-pressure region lie above the reference curve, although the data scatter includes the curve. In the higher pressure region the spread is more symmetrical about the curve. An upper bound of 40 percent above the reference curve appears suitable down to a scaled distance of about 5 and about 100 percent above the reference curve for smaller scaled distances.

Table 5-8 summarizes the estimated upper bound information for the propellant curves in terms of their ratio to the TNT reference curves. Conservative estimates of the pressure and impulse values to be expected from propellant explosions can be obtained by multiplying the pressure and impulse values from the standard TNT reference curves by these ratios.

Table 5-8
Ratio of Estimated Upper Bound for
Propellant Data to TNT Reference Curves

| PRESSURE | | |
|----------------------------------|--|----------------------------------|
| <u>Propellant</u> | <u>Scaled Distance Range (ft/lb^{1/3})</u> | <u>R₁[*]</u> |
| LO ₂ /RP-1 | all | 1.0 |
| LO ₂ /LH ₂ | all | 1.0 |
| IMPULSE | | |
| <u>Propellant</u> | <u>Scaled Distance Range (ft/lb^{1/3})</u> | <u>R₁[*]</u> |
| LO ₂ /RP-1 | ≥ 3 | 1.3 |
| | ≤ 3 | 2.0 |
| LO ₂ /LH ₂ | ≥ 5 | 1.4 |
| | ≤ 5 | 2.0 |

* R₁ - estimated ratio of upper bound of propellant data to TNT reference curve

Part 3 - DISCUSSION OF CRYOGENIC TEST RESULTS

General Analysis Approach

The general approach used in analyzing the results from each of the basic test conditions in the cryogenic program was to first formulate trial general relationships between explosive yield and the variables investigated for that test condition. Statistical analyses were next performed using these relationships to determine which of the alternate equation forms and parameter combinations best explain the variations in observed yields and to derive values for the constants of the selected equation form. Prediction intervals were then developed which can be used with these functions to predict and bound (with a high degree of certainty) the range of yield values expected to occur for future tests in each condition.

A variety of statistical techniques was used to accomplish these steps, the most important of which were regression analysis and prediction interval estimation. The application of these techniques to the data is described in detail in Appendix B and only the results of the analysis will be given here.

The trial general relationships or models relating explosive yield to the parameters of interest were developed using all available information on the propellant mixing and explosion process. These models have been of an evolutionary nature; preliminary versions were formulated at the time of the original planning of the test program in order to make decisions about the variables to be included in the test program and the relative effort to place on the various phases. These models were based on the propellant explosion information existing at that time and on general hydrodynamic considerations. The models have been modified and updated during the course of the program on the basis of the experimental test data, high-speed photography of the tests, and results from the transparent tank and laboratory test programs.

In the following material, the discussion of the analysis of results is grouped first by the basic test condition (CBM, CBGS-V, CBGS-H, high-velocity impact) and then by propellant type ($LO_2/RP-1$, LO_2/LH_2).

Confinement-by-the-Missile CaseTest Parameters

In the CBM case, the propellants are initially contained in a cylindrical tank divided into two compartments separated by a tempered-glass diaphragm. The ullage space in the upper propellant compartment is pressurized to about 20 psi, while that in the lower compartment is near ambient. On rupture of the diaphragm, the upper propellant is forced downward by the large pressure differential and impacts on the lower propellant.

The basic variables selected for testing in this condition were:

t - the time between diaphragm rupture and ignition

L/D - the length-to-diameter ratio of the propellants

D_o/D_t - the ratio of the diameter of the diaphragm opening to the tank diameter

W - the total propellant weight

In the majority of testing, the propellant compartments were fully loaded, which left approximately 10 percent of the volume of each tank for ullage space. In a few tests, however, a two-thirds-full propellant loading condition was used, which resulted in the ullage volume being about 40 percent of each compartment.

In all cases, the conventional propellant orientation was used, i.e., LO_2 over the RP-1 and LH_2 over the LO_2 .

Descriptive Model for LO_2 /RP-1 CBM Case

On rupture of the diaphragm, the LO_2 is forced downward by the approximately 20-psi pressure head and impacts on the RP-1, with the leading edge somewhat uneven and with some turbulence in the RP-1 created by the falling glass fragments. When the diaphragm opening is equal to the tank diameter ($D_o/D_t = 1$) the initial impact process tends to be relatively stable since the LO_2 mass has essentially the same cross-sectional area as the RP-1. Because the LO_2 density is 40 percent greater than that of the RP-1, however,

an instability should be created fairly rapidly, which may be augmented by the localized differential pressures generated in the boiling heat transfer process. In any case, there is a strong tendency for the LO_2 to penetrate into the RP-1 pool. The details of the penetration process are uncertain, although high-speed movies from a series of transparent tank tests (which were unpressurized) suggest that penetration occurs on a scale much smaller than the tank diameter and that it is not a simple overturning process of the entire interface.

As the mixing process proceeds, increasing quantities of the RP-1 will become partially or completely frozen, and it might be expected that this frozen slush would tend to inhibit mixing and possibly cause it to ultimately stop altogether. The experimental results suggest that some slowdown in mixing may have occurred for the L/D of 5 case but that mixing is not basically limited by such freezing, but rather by the pressure buildup in the tank (produced by the evaporating LO_2) to a value greater than the bursting pressure (approximately 100 psi).

From this model, it would be reasonable to expect that the rate of yield buildup would be less as the L/D of the propellants increases, since this results in an initially smaller interface and since the LO_2 has to penetrate to a much greater absolute depth to involve the same fraction of the propellants. This need to penetrate to a greater absolute depth also suggests the possibility that more of the LO_2 is evaporated in the process. If so, it would not be surprising to find that the maximum yield obtainable (at the time 100 psi was reached) also decreases somewhat with an increase in L/D ratio.

The situation for the partially open diaphragm case ($D_o/D_t = 0.45$) is expected to be somewhat different from that for the full-open case, since the cross-sectional area of the impacting LO_2 stream is only about 20 percent that of the RP-1 tank, thus permitting the LO_2 stream initially to penetrate much more readily into the RP-1 pool. This plunging process is expected to create a very rapid initial rate of mixing, and thus yield should be generated more rapidly than for the fully open diaphragm case. This plunging process

would be expected to slow down greatly once sufficient LO_2 enters the RP-1 tank to fill its ullage space (approximately 10 percent of the total volume). For additional LO_2 to enter, some of the RP-1 must be forced into the upper tank. It is also anticipated that in the zone of mixing in the lower tank, there will likely be a larger surplus of RP-1 than in the fully open case. Thus, it would not be surprising that the amount of O_2 gas generated for a given amount of mixed propellant would be greater than for the fully open case. This would mean that the maximum yield reached before the tank bursts would be less.

As noted above, it is expected that the yield generation process is limited by tank rupture due to O_2 gas buildup rather than by any basic limitation on the mixing process itself. On this basis, it is evident that increasing the ullage volume would increase the amount of oxygen that could be vaporized before the tank ruptures, with a consequent increase in maximum yield. This raises the very likely prospect that partially filled tanks could lead to a larger total yield than completely filled tanks.

Because of the complexity of the LO_2 penetration processes as well as that of the subsequent mixing from the interfaces, there is uncertainty with regard to the specific nature of the expected scaling relations. With regard to time scaling, it can be argued that at least initially the mixing should tend to be proportional to the interface area. This would mean, for example, that an increase in linear scale by a factor of 2 (weight scale of a factor of 8) would give 4 times as much material mixed at a given absolute time, but this weight of mixed propellant would amount to only one-half as much of the original propellant weight.

Similarly, since the ullage volume would also be proportional to the cube of the linear scale factor, the pressure increase in the tank would be less at the same absolute time. Thus, as the scale increases, longer mixing times are expected to be required to give the same yield and pressure increase in the tank. To a first approximation, it seems reasonable to assume that time scaling is on a geometrical basis, i.e., that similar yield values (expressed as a fraction of the total propellant weight) would be obtained

at times divided by the linear scale factor (or cube root of the propellant weight).

Prediction Equation Development for LO₂/RP-1 CBM Case

Based on the above model the following general guidelines were obtained as the basis for formulating the trial equations to use in the statistical analyses.

1. Yield is expected to increase monotonically with time, and the yield value at zero ignition time is expected to be quite small (relative to the maximum value).
2. The most likely manner for time to scale is on the basis of geometrical scaling, which leads to the scaled-time term of $t^* = t/W^{1/3}$.
3. There is no obvious reason to expect strong interactions between the effects of the variables.

The simplest equation which satisfies these guidelines has the following general form:

$$\frac{Y}{f(t^*)} = g(L/D) + h(D_o/D_t) + j(W)$$

where f , g , h , and j denote functions of the variable in the parenthesis.

Trial plots of the data were then made to help determine the best specific form for the individual functions. These plots indicated that:

1. The rate of yield increase is nearly constant in time so that $f(t^*) \approx t^*$.
2. Simple first-power terms of L/D and D_o/D_t appeared to satisfactorily account for the effects of these variables, so that $g(L/D) \approx \alpha' + \gamma(L/D)$ and $h(D_o/D_t) \approx \alpha'' + \delta(D_o/D_t)$ where α' , γ , α'' , δ are constants.
3. There is a tendency for yield to decrease with an increase in scale (W); but the magnitude of the decrease also decreases with scale, so that $j(W) \approx \alpha''' + \beta/W$.

Thus the specific form of the first trial equation becomes

$$\frac{Y}{t^*} = \alpha + \beta/W - \gamma(L/D) - \delta(D_o/D_t)$$

Using this general form of the equation and the set of experimental data given in Table 5-3 (except for the partially full tests, Nos. 174 and 240), a stepwise regression analysis was then run which indicated that all of the parameter terms were significant. The result is given below:

$$\frac{Y}{t^*} = 1.07 + \frac{12\%}{W} - 0.175 L/D - 0.527 D_o/D_t$$

One disadvantage of this equation is that it treats the weight-scale effect as an additive term. Although it satisfactorily accounts for the change in yield with weight for the case studied at various weight scales (L/D of 1.8, D_o/D_t of 0.45), when used for larger scales with larger L/D values it gives unrealistically small yields. For example the equation gives negative yields for weights over about 4,000 lb for an L/D of 5 and a D_o/D_t of 0.45.

From the above it is clear that a more satisfactory form for the equation would involve the weight term as a multiplicative rather than additive factor despite the fact that this complicates the equation, particularly from the viewpoint of the statistical analysis procedures required.

The final form of the equation selected was

$$\frac{Y}{t^*} = (1 + \beta/W) (\beta_o + \beta_1 L/D + \beta_2 D_o/D_t)$$

The regression coefficients β_o , β_1 , and β_2 were first determined using only the 200-lb data and letting the weight terms equal 1. These coefficients were then substituted back in the equation below and the values of α and β were determined using the data for all weights and for an L/D of 1.8 and a D_o/D_t of 0.45:

$$\frac{Y}{t^*} = (\alpha + \beta/W) (\beta_o + \beta_1 L/D + \beta_2 D_o/D_t)$$

By arithmetic manipulation, the coefficient values for the original equation were then determined (see Appendix B for details). The result is:

$$\frac{Y}{t^*} = \left(1 + \frac{217}{W}\right) (0.87 - 0.092 L/D - 0.28 D_o/D_t) \quad (5.1)$$

This equation is compared with the experimental data in Fig. 5-32. To illustrate quantitatively the uncertainty in the prediction equation, the upper bounds to the 90-percent prediction intervals for the D_o/D_t of 0.45 curves are also shown in the figures. The meaning of these lines is that there is a 90-percent probability that the yield value obtained in a single future test will lie below the line.

It should be noted that the equation derived above is directly applicable to the following ranges of conditions

1. L/D ratios of the propellant between 1.8 and 5
2. D_o/D_t ratios from 0.45 to 1.0
3. Fully loaded propellant tankage having an ullage volume equal to 10 percent of the tank volume
4. Propellant tankage having a bursting strength of approximately 100 psi
5. All values of ignition time up to tank rupture
6. Propellant weights greater than 200 lb

The use of this equation for values of the parameters outside the ranges given above is considered below.

L/D Ratio. Values of this ratio less than 1.8 are not anticipated, so that the only concern is with values greater than 5. Although it is anticipated that the yield will continue to decrease with an increase in L/D, there is insufficient information available to know whether the rate of decrease would be the same as in the region where it was determined. Accordingly, it is recommended that a conservative approach be adopted by using an L/D value of 5 for all values greater than 5.

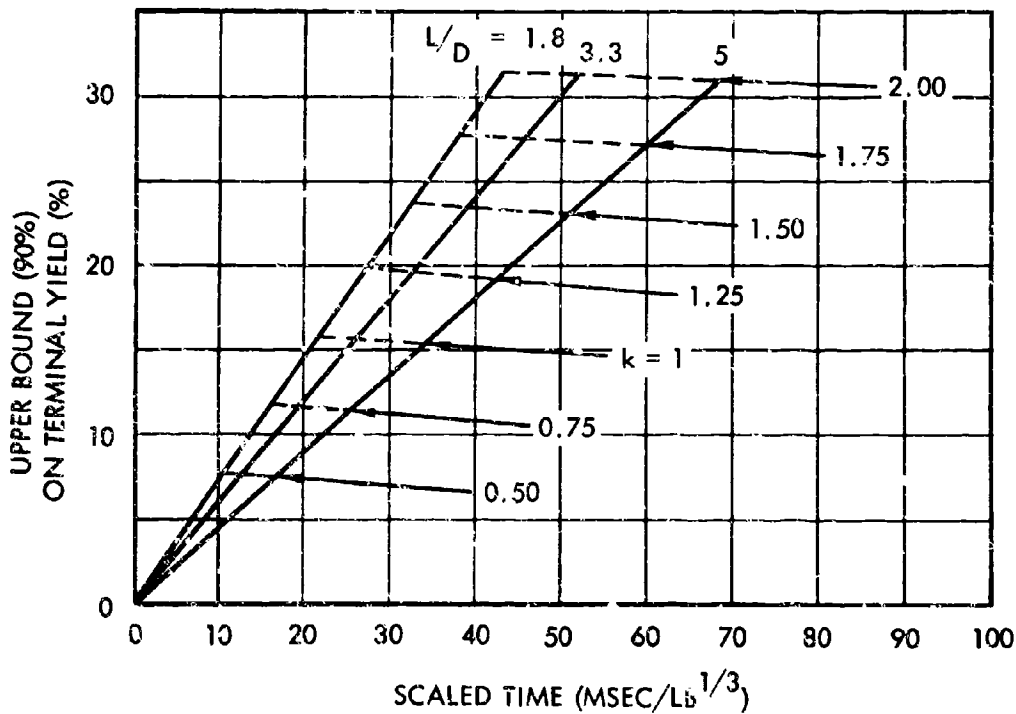
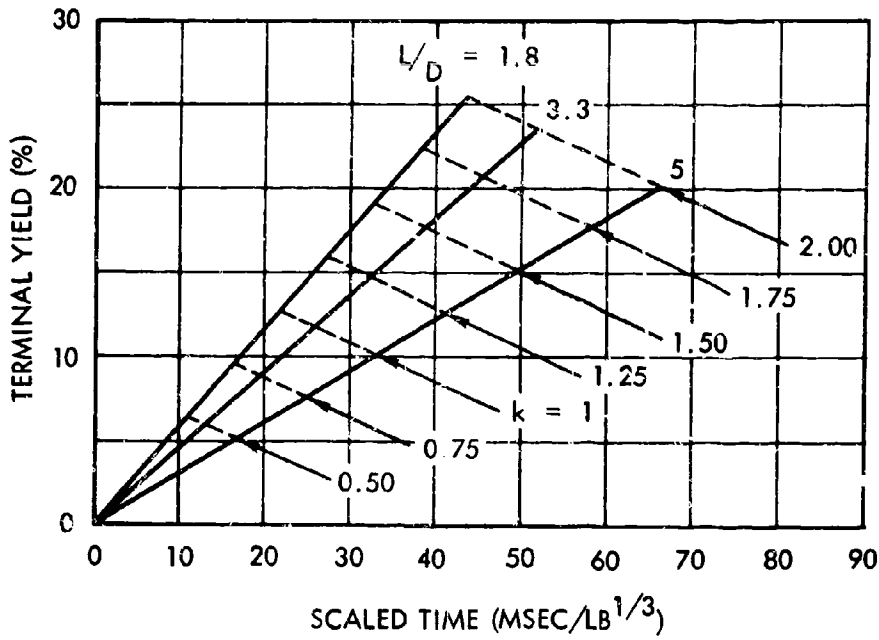


Fig. 5-36. Terminal Yield vs Scaled Time for LO₂/RP-1 CBM Case as a Function of L/D and k Factor

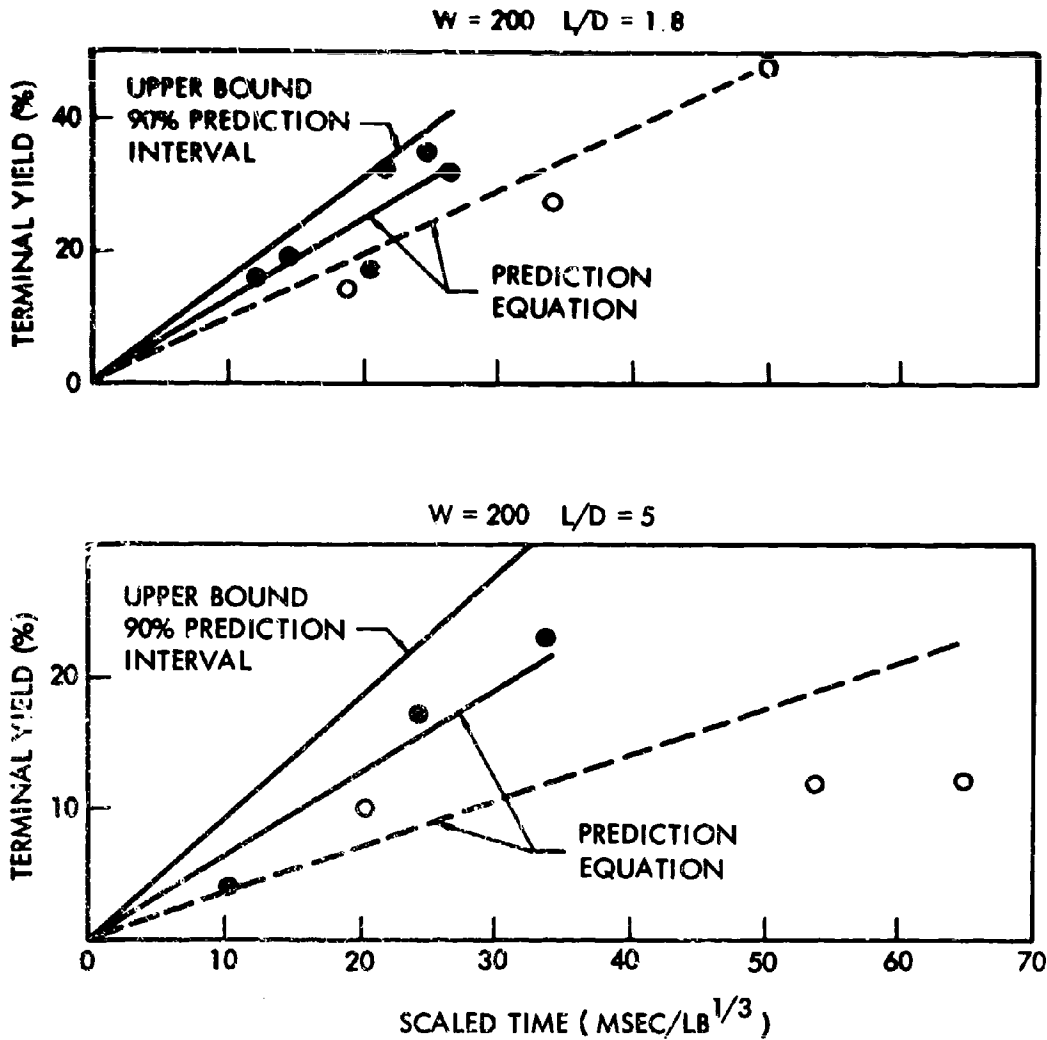


Fig. 5-32. Comparison of Prediction Equation for $LO_2/RP-1$ CFM Case with Experimental Data. (Open circles and dashed lines D_o/D_t of 1, solid circles and solid lines D_o/D_t of 0.45)

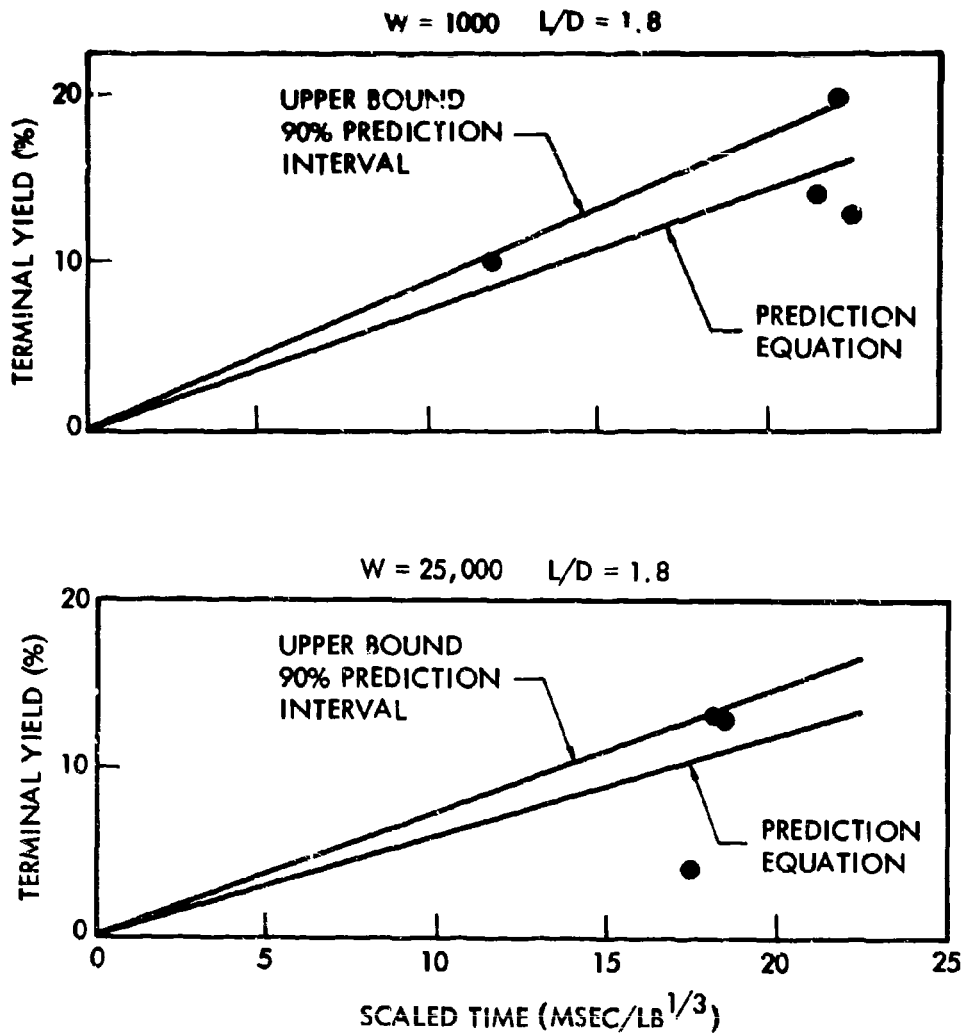


Fig. 5-32, cont. Comparison of Prediction Equation for $LO_2/RP-1$ CBM Case with Experimental Data - D_o/D_t of 0.45

D_o/D_t Ratio. Over the range this parameter was studied, its effect was to cause changes in both the rate of yield buildup and in its maximum value. For both L/D ratios the partially open diaphragm caused a more rapid rate of yield buildup, and for the larger L/D ratio (5) the maximum value was also somewhat greater than for the full-open case. For the smaller L/D ratio (1.8), however, the maximum value was less. Apparently in the case of an L/D of 5 with the fully open diaphragm, mixing tends to be inhibited by the partially frozen RP-1, so that the additional penetration provided by the partially open diaphragm can lead to larger yields. Since the partially open diaphragm case used, D_o/D_t of 0.45, results in an opening that is only 20 percent of the RP-1 tank diameter, it seems reasonable to believe that further decreases in the size of the diaphragm opening will not cause much additional increase in mixing that can be ascribed to increased penetration; rather, it seems more likely that a smaller diaphragm opening will tend to result in a yield decrease because of the great excess of RP-1 in the mixing zone. The LO_2/LH_2 results (discussed later), in which the effects of D_o/D_t were investigated down to a value of 0.083 (approximately 0.7% open), tend to confirm this line of reasoning. It is recommended, therefore, that a value of D_o/D_t of 0.45 (approximately 20% open) be used for all smaller values of D_o/D_t .

Propellant Loading Fraction. The data given earlier in this section showed that higher explosive yields could be obtained for the LO /RP-1 propellant combination when the propellant tanks are partially full than when they are in their normal full-load condition. As discussed previously this result would be expected, provided that propellant mixing is terminated by tank rupture due to O_2 gas buildup - as is indicated by the available evidence. The reasoning is that as the propellant loading fraction is decreased, with a consequent increase in ullage volume, the propellants have a greater time to mix prior to generating sufficient gas to rupture the tank and thus should give a larger specific yield (yield per unit weight of propellant).

As the ullage volume gets very large, it would be expected that the specific yield would tend to approach the theoretical maximum of about 120 percent, a value almost an order of magnitude larger than that obtained for the fully loaded tank (ullage volume of 10 percent). Actually, however, the

yield of most concern is not the specific yield, but the yield relative to the total amount of propellants in the tankage when it is fully loaded. This actual terminal yield value (Y) which is equal to the product of the specific yield (Y_s) and the propellant loading fraction (L), would not increase as much as the specific yield since an increase in Y_s due to an increase in ullage volume (V_u) is normally obtained by reducing L . If an L of 1 corresponds to an ullage volume of 10 percent (such as in the present test program) then

$$L = \frac{1 - V_u}{0.9}$$

or $V_u = 1 - 0.9 L$

and $Y = Y_s L = Y_s \frac{(1 - V_u)}{0.9}$

Although only limited data are available for ullage volumes other than the 10 percent used in the basic test program, a reasonable estimate of how the actual yield value changes with ullage volume for the case of most interest (L/D of 1.8, D_o/D_t of 0.45) can be obtained by using this equation and the available experimental data regarding the change of Y_s with ullage volume as illustrated in Fig. 5-33. Curve A is the experimentally derived relation between Y_s and V_u . The experimental data shown for the ullage volumes of 10 and 40 percent are from the 200-lb test series for an L/D of 1.8 and a D_o/D_t of 0.45. Because only 200-lb data were available for the 40-percent ullage condition (Test Numbers 174 and 240) and because of the weight-scale effect described earlier, only 200-lb test data (Test Numbers 101, 237, and 239) were used for the 10-percent ullage condition. For all of these tests, ignition occurred at approximately the time of tank rupture, which corresponded to a nominal 100 psi overpressure. The yield value used for 100-percent ullage corresponds to the maximum possible yield value for perfectly mixed propellants having an oxidizer-to-fuel ratio of 2.25 and was obtained from small-scale experiments with ideal mixtures of the propellants (Fig. 7-2). Since precooled fuel was used in these ideal mixtures, this yield value is expected to be somewhat on the high side.

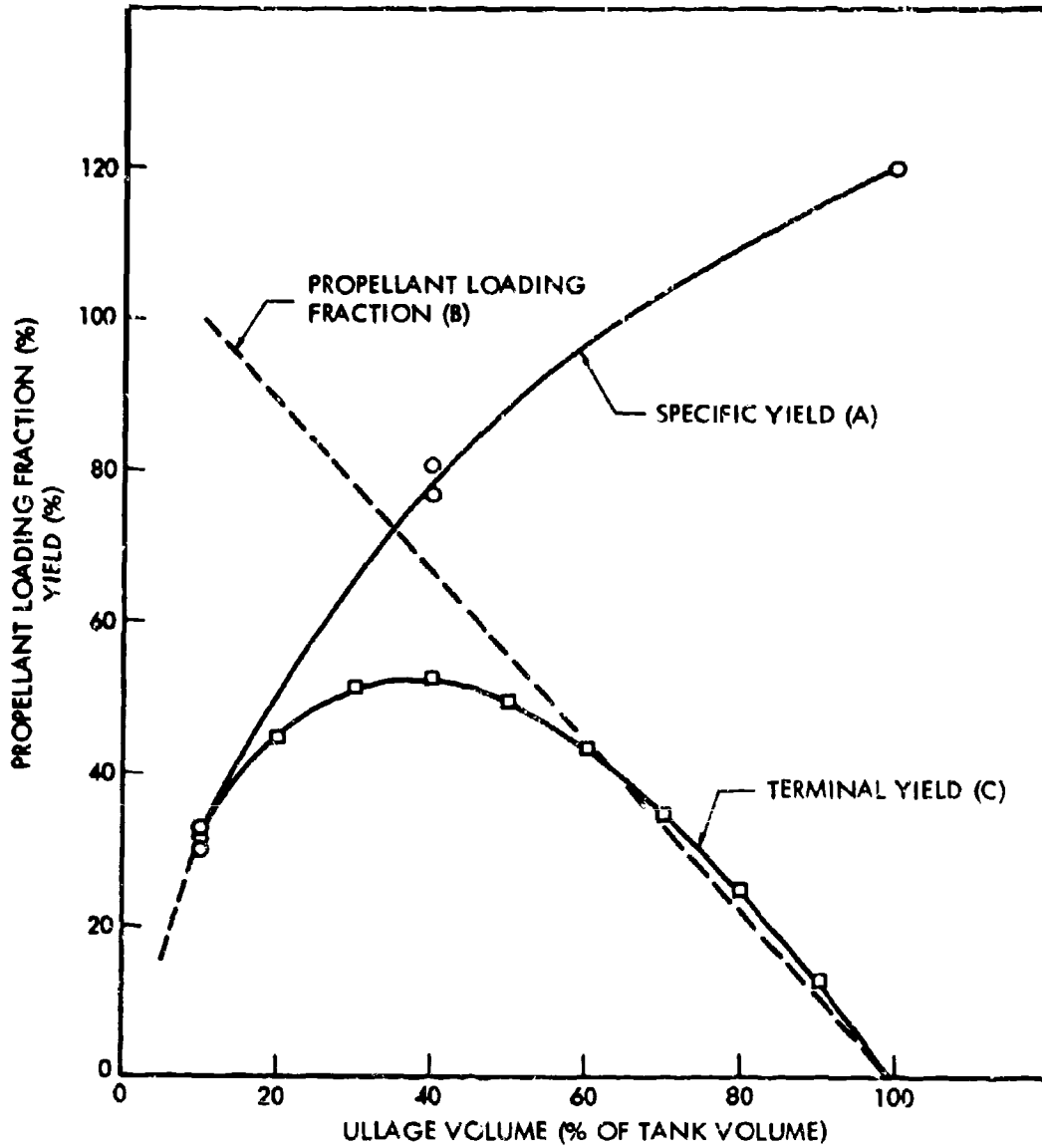


Fig. 5-33. Effect of Ullage Volume on Yield

Curve B is a plot of L versus V_u , and Curve C is the derived curve of Y versus V_u obtained from the product of Curves A and B. One of the interesting features of Curve C is that the maximum value occurs for an ullage volume of about 40 percent, one of the values for which experimental data on Y_g were available. This fact along with the relatively flat nature of Curve C in the vicinity of the maximum is good evidence to indicate that there is not much uncertainty in the magnitude of the maximum value of Y despite the fact that experimental data on Y_g were only available for a few ullage values.

The most important feature of Curve C is that the maximum value is about a factor of 1.7 times greater than the value for the standard fully loaded test condition of 10 percent ullage. Note that the ratio of yields is considered more pertinent here than the absolute values because of the weight-scale effect mentioned earlier.

Curve C also indicates that the yield should decrease as the ullage volume decreases below the standard 10 percent used in the basic test series, and a linear extrapolation of the curve for at least a factor of two in yield, as shown by the dashed line, seems reasonable.

Tank Rupture Pressure. The effects of varying the tank strength are expected to be generally similar to the effect of varying the ullage volume in that an increase in tank strength with all other conditions equal will allow more O_2 gas to be generated prior to tank rupture and thus more propellant mixing and a higher yield. Similarly a decrease in tank strength will result in less propellant mixing and a lower yield. Actually it is not only the burst pressure but also the initial pressure which is of concern, because the amount of O_2 gas which needs to be vaporized to rupture the tank is dependent on the difference between these pressures. In the basic test series, the initial pressure in the LO_2 tank was about 35 to 40 psia and that in the RP-1 tank about 15 psia, for an effective average pressure of about 30 psia, while the burst pressure was about 115 psia. This pressure differential of approximately 85 psi is believed to be greater than any in actual use, so that the yield values derived from these tests should be somewhat conservative.

A quantitative estimate of how yield varies with changes in tank burst pressure can be obtained with the assumption that the specific explosive yield is a function of the number of moles of gas generated. This is a reasonable assumption as long as the same geometry and type of propellant mixing prevails. The reasoning is that to a reasonable approximation the number of moles of gas generated to reach a given final pressure is proportional to the product of the initial ullage volume and the pressure differential between the initial and final pressures, i.e., $\Delta N \propto \Delta P_r V_u$ where ΔP_r = burst pressure - initial pressure. Thus the specific yield can be considered a function of $\Delta P_r V_u$, and Curve A of Fig. 5-33 can be used to generate a family of terminal yield curves (such as Curve C) for a range of ΔP_r values by using an effective value of ullage volume rather than the actual value to determine the specific yield where the effective ullage value is as given below:

$$V_{u\text{-eff}} = \frac{\Delta P_r}{85} V_u$$

Figure 5-34 shows the combined effects of ullage volume and pressure differential on the maximum terminal yield calculated in the manner indicated above. The yield changes are expressed by a factor k, which is the ratio of the yield for a given V_u and ΔP_r to that for the standard condition of $V_u = 10\%$ and $\Delta P_r = 85$ psi.

Prediction Method Development for LO₂/RP-1 CBM Case

In developing a yield prediction method based on the information given in the previous section, it is convenient to consider two basic cases: one where the ignition time is not known and one where it is known. It is also desirable to treat separately certain ranges in values of some parameters which are of little interest or whose occurrence is extremely unlikely.

These excluded parameter ranges, which are listed below, are considered in a special section following the general prediction method development:

- Propellant Weight (W) < 10,000 lb
- Diaphragm-Opening-to-Tank-Diameter Ratio (D_o/D_t) ≥ 0.45

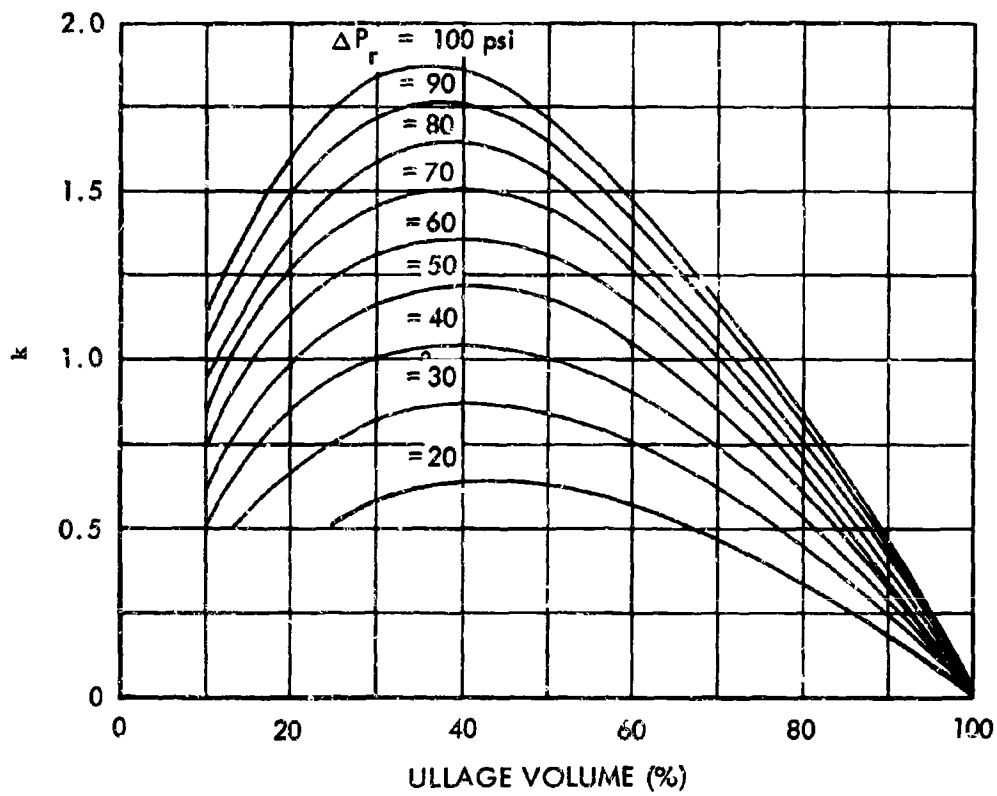


Fig. 5-34. Effects of Ullage Volume and Pressure Differential on Terminal Yield for LO₂/RP-1 CEM Case

Case 1 - Unknown Ignition Time. To obtain a conservative estimate for this case it must be assumed that the ignition time is the maximum possible, i.e., the value at the time of tank rupture. Thus in this case we are interested in predicting yield as a function of L/D, V_u , and ΔP_r , given that $t^* = t_{\max}^*$ and $D_o/D_t = 0.45$.

By excluding W's of 10,000 lb or less and D_o/D_t 's greater than 0.45, Eq. (5.1), the basic prediction equation for the standard conditions of $V_u = 10\%$ and $\Delta P_r = 85$ psi, becomes

$$\frac{Y}{t^*} = 0.76 - 0.092 L/D \quad (5.2)$$

The value of t_{\max}^* for these same standard conditions can be obtained by averaging the experimentally observed ignition times for the tests which are considered to fit in this category. These results are given below

| <u>L/D</u> | <u>Test Number</u> | <u>Ignition Time (msec)</u> | <u>Propellant Weight (lb)</u> | <u>t_{\max}^* (msec/lb^{1/3})</u> |
|------------|--------------------|-----------------------------|-------------------------------|--|
| 1.8 | 237 | 127 | 200 | 21.7 |
| 1.8 | 101 | 145 | 200 | 24.8 |
| 1.8 | 239 | 156 | 200 | 26.6 |
| | | | Average | 24.4 |
| 1.8 | 192 | 216 | 1000 | 21.6 |
| 1.8 | 193 | 222 | 1000 | 22.2 |
| 1.8 | 270A | 225 | 1000 | 22.5 |
| | | | Average | 22.1 |
| 1.8 | 275 | 515 | 25,000 | 17.6 |
| 1.8 | 278 | 530 | 25,000 | 18.1 |
| 1.8 | 282 | 540 | 25,000 | 18.5 |
| | | | Average | 18.1 |
| | | | Grand Average | 21.5 |
| 5.0 | 100 | 220 | 200 | 37.7 |

For an L/D of 1.8 these results indicate a slight decrease in t_{\max}^* with increasing propellant weight. Although conceivably some credit could be taken for this in extrapolating to larger scales, the reductions would not be large, and considering the uncertainties in the burst strengths and tank pressure at rupture, it seems somewhat safer to not extrapolate this trend but to use the average value of all the data as a conservative estimate. For an L/D of 5, only one test at the 200-lb scale was conducted with ignition at tank rupture. The ignition time for this test was a factor of 1.55 times the average value of the ignition time for the 200-lb, L/D of 1.8 data. Thus the best estimate of t_{\max}^* for an L/D of 5 was taken as 1.55 times the average value for an L/D of 1.8 or 1.55 times 21.5 = 33.2 msec/lb^{1/3}.

The yield values obtained from Eq. (5.2) for L/D ratios of 1.8 and 5.0 and the t_{\max}^* values given above are plotted in Fig. 5-35. Also shown in the figure are the upper 90 percent prediction values for these two points. A linear interpolation, as indicated by the straight lines, is recommended for intermediate L/D values. As discussed earlier, the values for an L/D of 5.0 are recommended for larger L/D values.

To account for differences in V_u and ΔP_r from the standard values, the correction factor k has to be multiplied by the yield values given in this figure. The values of k can be obtained from Fig. 5-34.

Case 2 - Known Ignition Time. For this case t^* is retained as a variable and Eq. (5.2) would be used

$$Y = t^* (0.76 - 0.092 L/D)$$

This equation is valid for all t^* values which give yield values less than or equal to the maximum yield values computed for Case 1. The yield as given by this equation is plotted in the upper part of Fig. 5-36 as a function of t^* for various L/D and k values, where k is obtained from Fig. 5-34. The upper bounds for the 90-percent prediction interval for this equation are given in the lower part of Fig. 5-36.

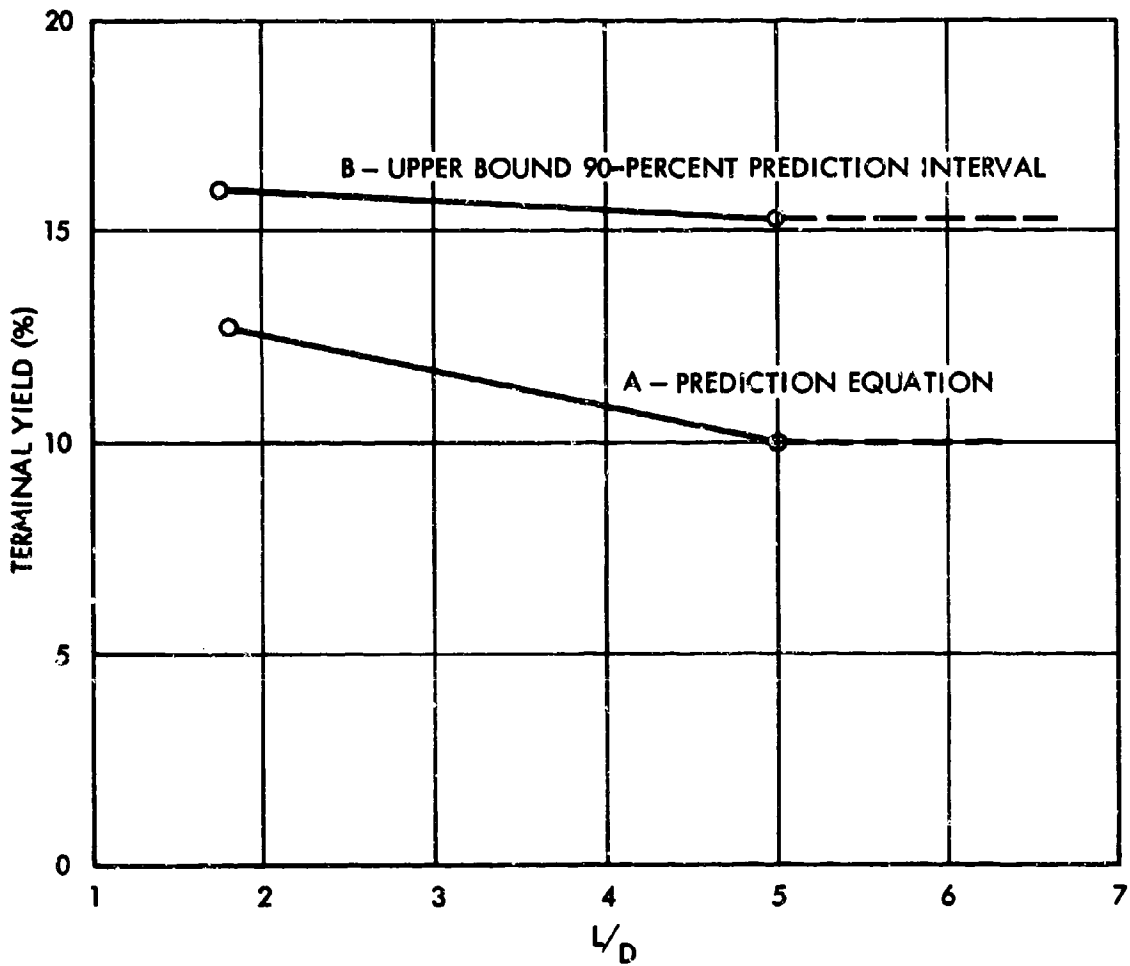


Fig. 5-35. Terminal yield at Tank Rupture for LO₂/RP-1 CBM Case ($V_u = 10\%$
 $\Delta p_r = 85$ psi)

These plots are basically for the purpose of determining either the expected yield or the upper bound for the 90-percent prediction interval about the expected yield given the expected ignition time. The various constant k lines (dashed), however, also give the yield value corresponding to the maximum t^* value possible prior to tank rupture.

Note that a straight-line interpolation has been used between the L/D values of 1.0 and 5 for each of the constant k lines in the upper part of Fig. 5-36. Equation (5.2) actually gives a curved line which lies slightly below the straight line for intermediate L/D values but the differences are very small (less than 5 percent of the yield value), so they have been neglected.

Also note that the form of the selected equation leads to both the yield and the upper bound being zero for an ignition time of zero. Insufficient data were obtained at small scaled times to verify this point, however, so that a minimum time of $5 \text{ msec/lb}^{1/3}$ is recommended.

Special Cases.

- (1) $W \leq 10,000 \text{ lb}$

As shown by Eq. (5.1) the yield values for this condition can be obtained by multiplying the yield values obtained from the general prediction method by the factor

$$\left(1 + \frac{217}{W}\right), \text{ where } W \text{ is in pounds}$$

- (2) $D_o/D_t \geq 0.45$

Use Eq. (5.1) to determine yield values for the standard conditions ($V_u = 10\%$, $\Delta P_r = 85 \text{ psi}$)

Estimated t_{\max}^* values are given below:

| D_o/D_t | L/D | t_{\max}^* |
|-----------|-------|--------------|
| 1.0 | 1.8 | 43 |
| 1.0 | 5.0 | 57 |

To correct for differences in standard conditions, multiply t_{\max}^* values by k factor given in Fig. 5-34.

Descriptive Model for LO₂/LH₂ CBM Case

Although some of the features of the descriptive model for the LO₂/RP-1 case are applicable to the LO₂/LH₂ case, there are some significant differences in behavior due primarily to the very low density of the LH₂. In this case, the LH₂ is on top, and since its density is very much less than that of the LO₂, there is little tendency for it to penetrate the LO₂ once the initial impact and plunging process is over.

Also, because of the very low density of the LH₂, it would be expected that the plunging of the LH₂ stream in the middle of the LO₂ pool for the partially open case should be much less pronounced. This suggests that there would not be as much difference in the rates of yield generation between the partial- and full-open cases as in the LO₂/RP-1 case. The low heat of vaporization of the LH₂ also would be expected to result in a relatively greater amount of gas generation, which would tend to keep the main masses of propellants apart, particularly for the larger L/D ratios.

The likely greater physical stability of this propellant combination suggests that frozen oxygen may have a much greater inhibiting effect on mixing than does frozen RP-1, and the experimental evidence tends to confirm that some such terminating process as this exists, since tank rupture due to LH₂ buildup occurred in only one of the four test configurations studied (L/D of 1.8, D_o/D_t of 1.0).

Because of the inherent characteristics of LH₂, there is some potential for a small explosive reaction between the LO₂ and LH₂ being enhanced by subsequent mixing and reaction of the LH₂ with the ambient air outside of the tankage. Thus the yield for small ignition times may be relatively much greater than for LO₂/RP-1.

Prediction Equation Development for LO₂/LH₂ CBM Case

The important guidelines obtained from the above model with regard to the formulation of trial equations are as follows:

1. Yield is expected to increase with time initially and then to level off and approach a constant value. In most cases, the leveling off point is expected to be reached prior to tank rupture.
2. In contrast to $\text{LO}_2/\text{RP-1}$, the yield value (if ignition occurs at zero time) may be large enough compared to the maximum value that the yield should not be zero at zero time in the mathematical model.
3. As with $\text{LO}_2/\text{RP-1}$ the most likely manner for time to scale is on the basis of geometrical scaling, which leads to the scaled-time term of $t/W^{1/3} = t^*$.
4. There is no obvious reason to expect strong interactions between the effects of the variables.

The simplest equation which satisfies these guidelines has the general form $Y = a + f(t^*) + b/W + C(L/D) + d(D_o/D_t)$, where f denotes a function of t^* and a , b , c , and d are constants.

Using various specific forms for $f(t^*)$ and the data given in Table 5-4, regression analyses were performed with yield as the dependent variable and the other parameters as the factors. The results of the analysis indicated that the L/D and t^* terms were significant, while the W and D_o/D_t terms were not significant. It was also noted that elimination of the case of an L/D of 1.8 and D_o/D_t of 1.0 rendered L/D as no longer significant and that the time effects were only evident in the first 100 msec (based on 200-lb data). Since D_o/D_t values of greater than 0.45 are considered highly unlikely, this case was dropped from the general analysis and considered separately. The final form of the trial equations then became:

$$\begin{aligned} Y &= a + b t^* & t^* &\leq t_{\text{crit}}^* \\ Y &= b' t^* & t^* &\geq t_{\text{crit}}^* \end{aligned}$$

Values of the three constants were then obtained by a stepwise regression analysis and that for t_{crit}^* from the intersection of the two equations. The final form of the prediction equations is given below:

$$\begin{aligned} Y &= 2.8 + 0.82 t^* & t^* &\leq 21.1 \text{ msec/lb}^{1/3} \\ Y &= 20 & t^* &\geq 21.1 \text{ msec/lb}^{1/3} \end{aligned} \quad (5.3)$$

This equation is plotted in Fig. 5-37 along with the experimental data points for comparison. Also shown in the figure is the upper bound for the 90-percent prediction interval.

The use of this equation for values of the parameter outside the range studied is considered below.

L/D Ratio. It is reasonable to expect that this equation is valid for all ranges of L/D of interest (i.e., all values greater than 1.8) and that it will be on the conservative side for L/D values greater than 5.

D_o/D_t Ratio. Several data points are available for D_o/D_t ratios less than 0.45. These are given below and compared with the values predicted from Eq. (5.2).

| Test Number | D_o/D_t | Propellant Weight (lb) | t^* (msec/lb ^{1/3}) | Measured Yield (%) | Predicted Yield (%) |
|-------------|-----------|------------------------|---------------------------------|--------------------|---------------------|
| 169 | 0.083 * | 200 | 54 | 15 | 20 |
| 173 | 0.083 | 200 | 10 | 15 | 11 |
| S-IV | 0.083 | 91,200 | 4 | 5 | 6 |

* Corresponds to a 0.7% area opening

The differences between the measured and predicted yields are well within the uncertainty range of the equation and thus the differences cannot be considered significant. It is concluded therefore that this equation is valid for all D_o/D_t values less than 0.45. This equation is also considered valid for D_o/D_t values between 0.45 and 1.0 providing the L/D value is 5 or greater.

Ullage Volume and Tank Pressure. Since the mixing process for this propellant combination appears to be terminated by some mechanism (most likely by a frozen or partially frozen LO₂ block) other than tank rupture due to propellant vaporization, changes in ullage volume and tank burst pressure are not expected to have the large effect found for LO₂/RP-1 (still excluding the case of $D_o/D_t > 0.45$). Two data points are available which verify this postulate.

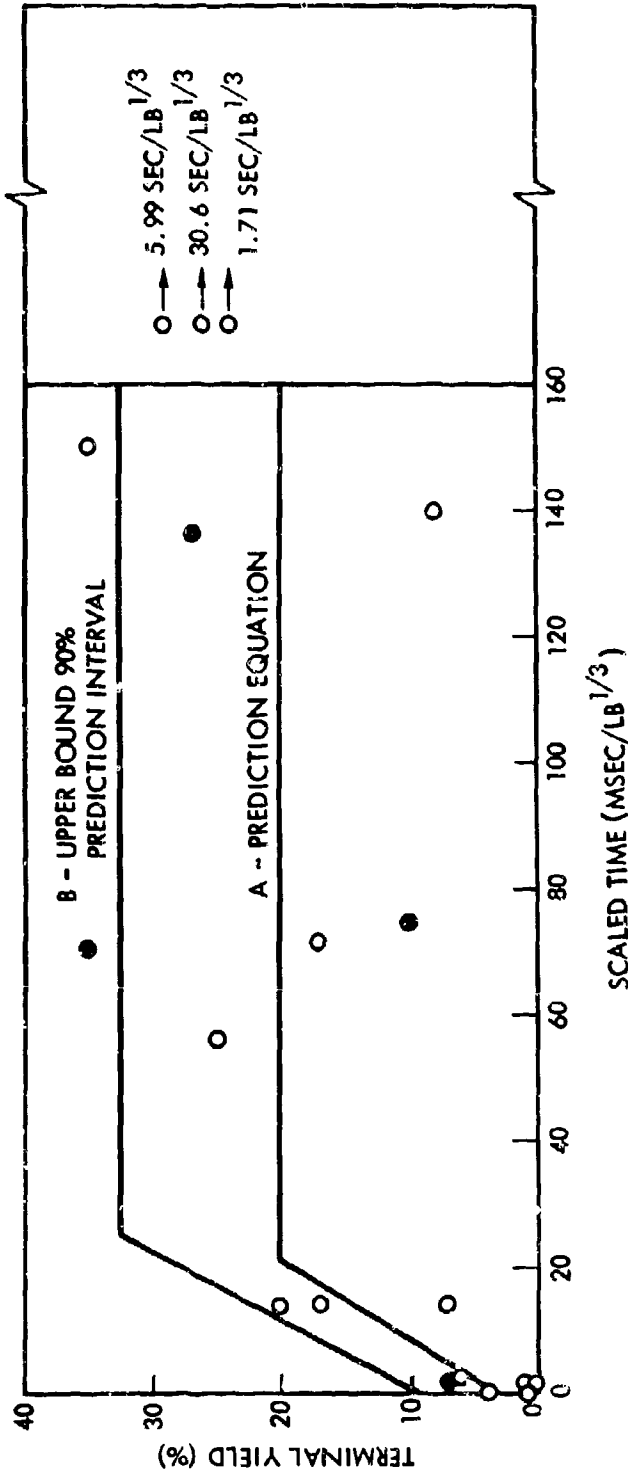


Fig. 5-37. Comparison of Prediction Equation for LO₂/LH₂ CBM Case with Experimental Data for All Cases but L/D of 1.6 and D₀/D_t of 1 (○ - 200 lb, ● - 1,000 lb)

These are shown below and compared with the values predicted from Eq. (5.2).

| Test Number | Ullage Volume (%) | Propellant Weight (lb) | t^* (msec/lb ^{1/3}) | Measured Yield (%) | Predicted Yield (%) |
|-------------|-------------------|------------------------|---------------------------------|--------------------|---------------------|
| 167 | 40 | 133 | 1490 | 24 | 20 |
| 172 | 40 | 133 | 132 | 35 | 20 |

Here the data are above the predicted values but not far enough above compared with the uncertainty, so that the differences can be considered insignificant. Thus it is concluded that the equation is valid for all reasonable ranges of ullage volumes and tank burst pressures.

Special Case: $D_o/D_t > 0.45$ and $L/D < 5.0$. The only data available for this case are for a D_o/D_t of 1 and an L/D of 1.8. The data are plotted in Fig. 5-38, and their use is recommended as a conservative estimate for all D_o/D_t values greater than 0.45 when the L/D ratio is less than 5. These data are insufficient in themselves to compute a 90-percent upper prediction interval curve. Accordingly the ratio between the equation and the upper bound in the flat region of the curve in Fig. 5-37 was used to estimate this bound.

Confinement-by-the-Ground-Surface Case

Test Parameters

In the confinement-by-the-ground-surface case, the propellants are initially contained in separate tanks with thin foil bottoms, one suspended above the other, on a drop tower. The tanks are released from their support cable and allowed to drop under essentially free-fall conditions, guided only by skates fastened to a rail on the side of the drop tower. The bottoms of the tanks are ruptured by a cutter ram when they near the ground surface and the tank proper is stopped about 1.5 tank diameters off the surface. Thus the propellants are permitted to impact and spread on the ground surface free of the tankage.

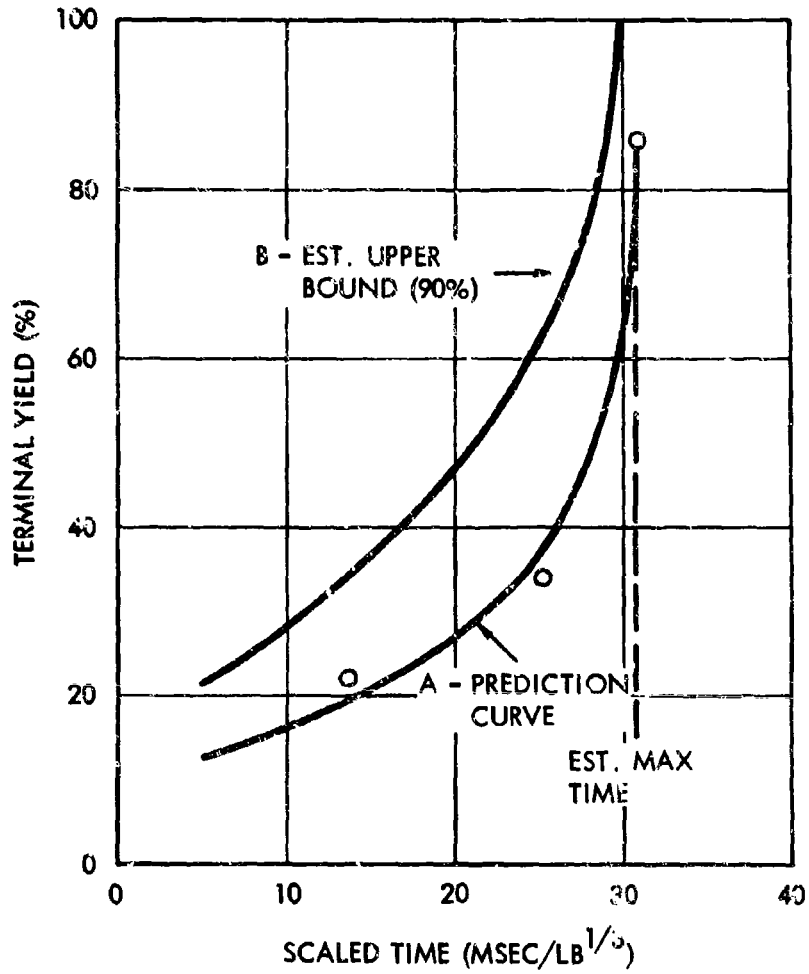


Fig. 5-38. Terminal Yield vs Time for LO₂/LH₂ CBM Case with $D_o/D_t = 1$ and $L/D = 1.8$

The basic parameters selected for testing in this condition were:

- t - Time
- v - Impact velocity
- F - Flow direction
- L/D - Length-to-diameter ratio of propellants
- PO - Propellant orientation
- W - Propellant weight

Descriptive Model for LO₂/RP-1 CBCS-V Case

In the model developed for the vertical condition of the CBGS case, it is convenient to consider the time sequence of events as divided into four consecutive periods.

1. From the time of drop until the second (or top) propellant starts impacting the ground
2. From the time the second propellant starts impacting the ground until it is essentially all down
3. From the time the second propellant is all down until it overlaps the bottom propellant
4. From the time the second propellant overlaps the bottom propellant to the time it has spread to a pool of about three times the size of that of the bottom propellant

If ignition occurs during the first time period, relatively small yields would be expected, since the main masses of propellants have not yet contacted each other. The situation would be expected to be somewhat similar to that for the hypergolic propellant combination under similar impact conditions.

During the second time period, significant mixing can start to occur, because both propellants are in a favorable geometry for mixing, i.e., overlapping thin layers. Since only about 30 percent of the bottom propellant has been overlapped at the end of this time period, however, the yield at this time is still expected to be much below the maximum value. If it is assumed

that the yield is proportional to the fraction of the bottom pool overlapped, it would be about 30 percent of the maximum.

During the third time period, as the propellants continue to spread out on the ground surface, an increasingly larger percentage of the bottom propellant becomes overlapped, and if both propellants continued to spread indefinitely at the same velocity, the yield would tend to asymptotically approach some maximum value (assuming a negligible amount of boil-off). For the normal orientation, however, the RP-1, which is on the bottom, does not spread indefinitely at a constant velocity, but rather tends to slow down and stop rather abruptly at some maximum pool diameter. Thus, the LO₂, which has been lagging behind the RP-1, rapidly catches up and completely overlaps the RP-1. It is anticipated that the maximum yield value should occur at approximately this catch-up-time, since that portion of the liquid oxygen which is not trapped in the RP-1 pool can overrun it.

As long as the propellants are moving at constant velocity and geometrical scaling is assumed, it can be shown that the fraction of pool overlapped during the third time period, at least up until the time the RP-1 pool stops, remains invariant with a change in the scale of testing, provided that time and distance are scaled by the linear scale factor, i.e., the cube root of the weight. It also was noted in the films that the maximum diameter of the RP-1 pool increased as the velocity of impact increased. Thus, it would be expected that the pool diameter at which the maximum yield would be obtained would scale in the following fashion:

$$D/W^{1/3} f(v)$$

During the fourth time period, the LO₂ pool is overrunning the RP-1, and the yield will decrease until all the LO₂ which is not trapped with the RP-1 has left the vicinity. During this period, boiling of the oxygen will also contribute to the yield decrease; however, it is believed that this is a somewhat slower phenomenon than the overrunning and thus not a major factor in the time period of concern.

For the reversed propellant orientation, the LO_2 - which is on the bottom - would not stop, so that the RP-1 is unlikely to catch up completely with it. This indicates that the maximum yield for the reversed propellant orientation should be less than for the normal orientation.

This general model also suggests that the effects of increasing the L/D ratio of the impacting propellants would cause the rate of buildup of yield to be slower (because the LO_2 would initially lag farther behind the RP-1) but that it would likely reach the same maximum value at a slightly longer time (because the RP-1 would stop at approximately the same radius).

In addition to the velocity effect on the critical pool diameter, it also would not be surprising to find that throughout the entire time range the yield would be somewhat greater for the higher impact velocities because of the greater turbulence in the propellant flows.

Prediction Equation Development for LO_2 /RP-1 CBGS-V Case

On the basis of this descriptive model of the process, the following guidance can be obtained with regard to the trial mathematical models:

1. Once the propellants are all down, the yield is expected to increase rather rapidly as the pools spread and to reach a maximum value when the LO_2 pool just overlaps the RP-1 pool. This overlap is expected to be reached at a pool diameter proportional to the product of $W^{1/3}$ and some function which increases with an increase in v . In other words, the overlap pool diameter is expected to be reached for a constant value of $D/W^{1/3}f(v)$.
2. As the LO_2 pool spreads beyond the overlap pool diameter, the yield is expected to decrease at a fairly rapid rate (but less rapidly than on the buildup side) and to asymptotically approach some relatively constant value (ignoring boil-off).
3. The yield values throughout the entire range of pool diameters are likely to increase with increasing impact velocity.

These points suggest that correlation of the results can be obtained by the following general type of equation

$$\frac{Y}{h(v)} = h \left(\frac{D^*}{f(v)} \right)$$

where $D^* = D/W^{1/3}$ and f , g , and h denote functions of the variables enclosed in the parentheses. After some preliminary trials of various specific forms of this general equation, it appeared that $f(v)$ and $h(v)$ could be adequately expressed as simple positive powers of v . Thus the equation could be reduced to

$$\frac{Y}{v^n} = g\left(\frac{D^*}{v^m}\right)$$

where n and m are constants.

Because of the relatively complicated form anticipated for the function $g(D^*/v^m)$, i.e., a rapid increase to a maximum value followed by a somewhat slower decrease approaching some constant value asymptotically, it seemed desirable in the actual regression analysis to divide the equation into two parts, one function to express the increase in yield to the maximum value, the other to account for the decrease.

The final trial forms of the equation selected were as follows:

$$\frac{Y}{v^n} = a + \frac{bD^*}{v^m} \quad \text{for } 0 \leq \frac{D^*}{v^m} \leq \left(\frac{D^*}{v^m}\right)_{\text{crit}}$$

$$\frac{Y}{v^n} = a' + b' \left(\frac{D^*}{v^m}\right)^{-1} \quad \text{for } \frac{D^*}{v^m} \geq \left(\frac{D^*}{v^m}\right)_{\text{crit}}$$

It should be noted that the effects of L/D and propellant orientation are not included in the trial equation. Only a small number of tests were run using conditions other than the standard ones of the normal propellant orientation and an L/D of 1.8, and it seemed better to consider variations of those parameters after the equation development for the standard conditions.

The values of the constants (a, b, a', b', n and m) in these equations were determined by a stepwise regression analysis using the set of data given in Table 5-5. (See Appendix B for treatment of n and m .) The final form of the prediction equation is given below:

$$\frac{Y}{v^{0.9}} = 0.22 + \frac{2.7D^*}{v^{0.2}} \quad \text{for } 0 \leq \frac{D^*}{v^{0.2}} \leq 1.1$$

(5.4)

$$\frac{Y}{v^{0.9}} = 0.33 + \frac{3.2v^{0.2}}{D^*} \quad \text{for } \frac{D^*}{v^{0.2}} \geq 1.1$$

This equation is plotted in Fig. 5-39 along with the experimental data for comparison. Also shown in this figure is the upper bound on the 90-percent prediction interval about this equation. Note that the 25,000-lb data point, which was not used in deriving the equation, fits the curve very well. Also note that the equation is based only on the data where all the propellants are out of the tankage and on the surface. (Corresponding to $\frac{D^*}{v^{0.2}}$ values greater than 0.1, and is expected to be conservative for earlier times when some of the propellants are still confined in the tankage.)

Another rather interesting check on the prediction equation in the vicinity of its maximum is Test Number 295, which simulated a two-stage missile in the CBGS-V test condition. The bottom stage consisted of the standard 1000-lb $\text{LO}_2/\text{RP-1}$ tankage with an L/D of 1.8, while the upper stage consisted of the standard 200-lb LO_2/LH_2 tankage with an L/D of 1.8. The impact velocity was 44 ft/sec, the time of ignition was 540 msec, and the terminal yield 70 percent.

The $\text{LO}_2/\text{RP-1}$ prediction equation gives a yield of 93 percent for 1000 lb or an equivalent TNT weight of 930 lb. The LO_2/LH_2 prediction equation [Eq. (5.5)], discussed later, gives a yield of 37 percent for the 200 lb of LO_2/LH_2 or an equivalent weight of 74 lb. The total equivalent weight for both stages is 1004 lb for an overall yield of about 84 percent. This compares very well with the measured value of 70 percent.

The test data for the reversed propellant orientation and for an L/D of 5 are compared with the prediction equation in Fig. 5-40. As expected, the data for the normal propellant orientation with an L/D of 5 lie below the prediction equation, reflecting the greater separation between the two spreading pools and thus the smaller fraction of the RP-1 pool overlapped by the LO_2 at a given time for this case compared with that for the L/D of 1.8 case. The arrows on each of the data points show the pool diameter for the L/D of 1.8 case which has the same percent overlap of the RP-1 by the LO_2

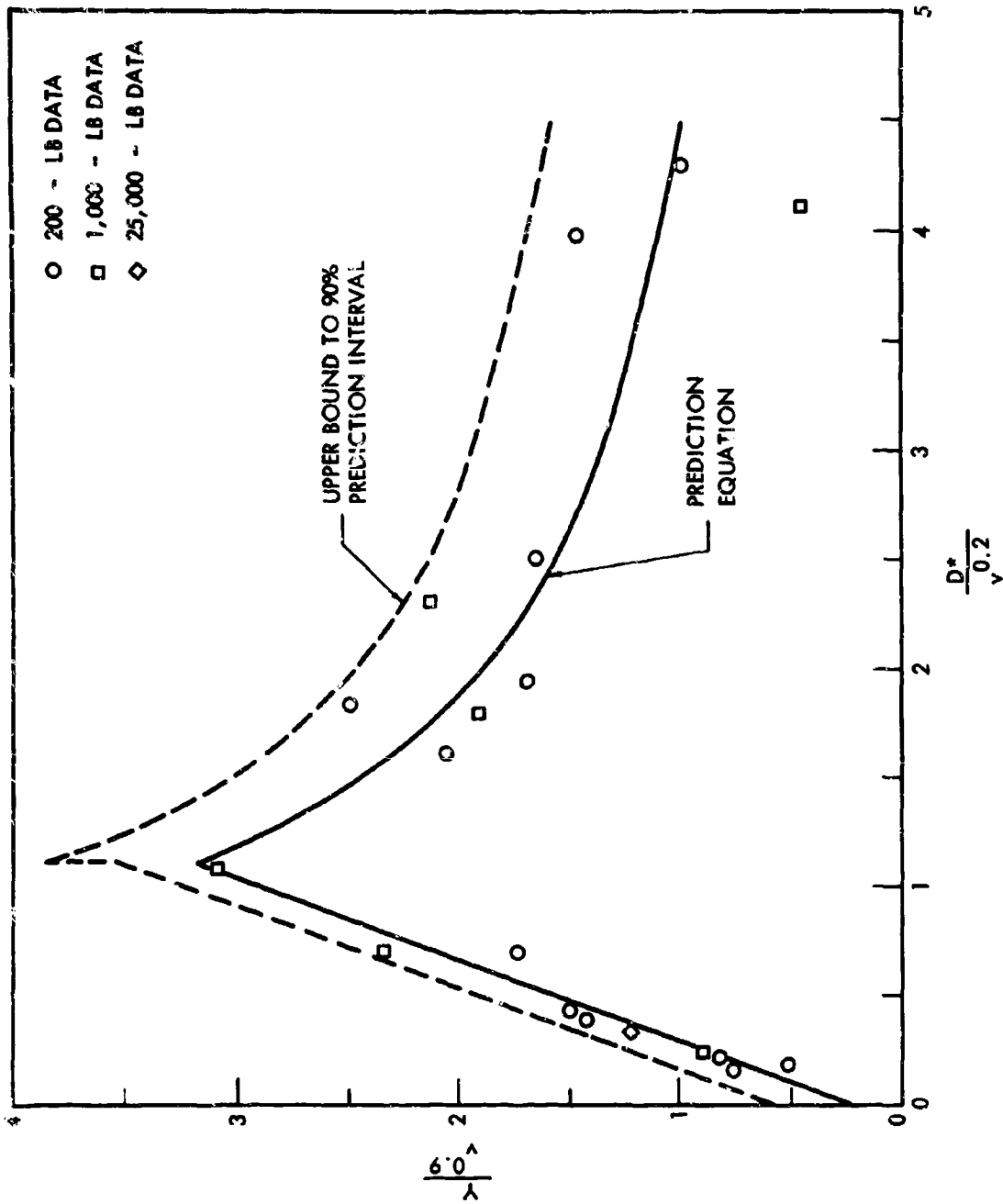


Fig. 5-39. Scaled Terminal Yield vs Scaled Pool Diameter $LO_2/RP-1$ CBGS Case

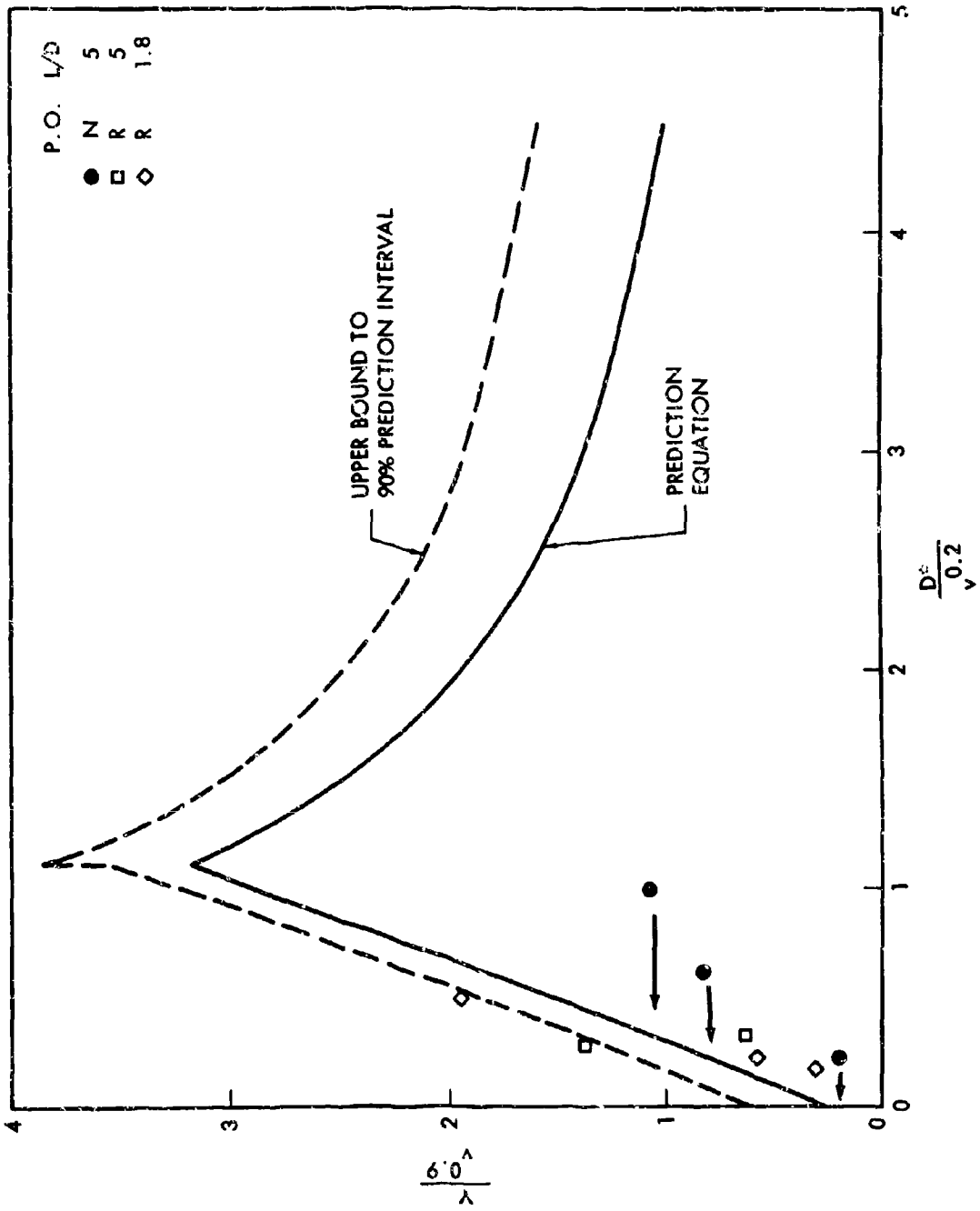


Fig. 5-40. Comparison of Data for Reversed Propellant Orientation and for an L/D of 5 with LO₂/RP-1 CBGS Prediction Equation

as for the L/D of 5 case (calculated on the assumption of a uniform thickness pool). This comparison generally supports the postulated effect of L/D on yield, although the data suggest the possibility that yield values for an L/D of 5 might be somewhat lower than those for an L/D of 1.8. The data for an L/D of 5, however, are too limited both in number and range of D^* values to use in trying to derive a correction factor. Accordingly it is recommended that the results for an L/D of 1.8 be used as a conservative estimate for all larger L/D values; the estimate is conservative in the sense that on the increasing portion of the curve, including the maximum value, the yield values for larger L/D values will be less. Because of the expected shift in peak position, the larger L/D values could give higher yields in the falling part of the curve.

The data points for the reversed propellant orientation scatter around the prediction equation and seem to fit it within the uncertainty in results indicated by the difference between the equation and the 90-percent upper prediction interval. Accordingly Eq. (5.4) is also considered valid for the reversed propellant orientation.

Prediction Method Development $LO_2/RP-1$, CBGS-V Case

As with the CBM case, it is convenient to consider two basic cases, the ignition time known or unknown. If the ignition time is unknown, it is possible that ignition could occur at the time (or pool diameter) which gives the maximum yield ($D^*/v^{0.2} = 1.1$ from Fig. 5-39). Curve A of Fig. 5-41 is a plot of this maximum yield as a function of impact velocity. This curve indicates that yields over 100 percent are expected for velocities greater than 50 ft/sec (corresponds to free fall from a height of about 38 ft).

Since the peak of the curve shown in Fig. 5-39 is rather narrow, it could be argued that the probability of ignition at or near the peak is rather low and that it is too conservative to use the peak value. Instead, one might choose the yield value which is exceeded during only 10 percent of some selected time interval. For example, if this were done for a time interval up to a $D^*/v^{0.2}$ of 5, the maximum yield value as a function of impact velocity would be Curve B shown in Fig. 5-41. It can be seen that this does not change

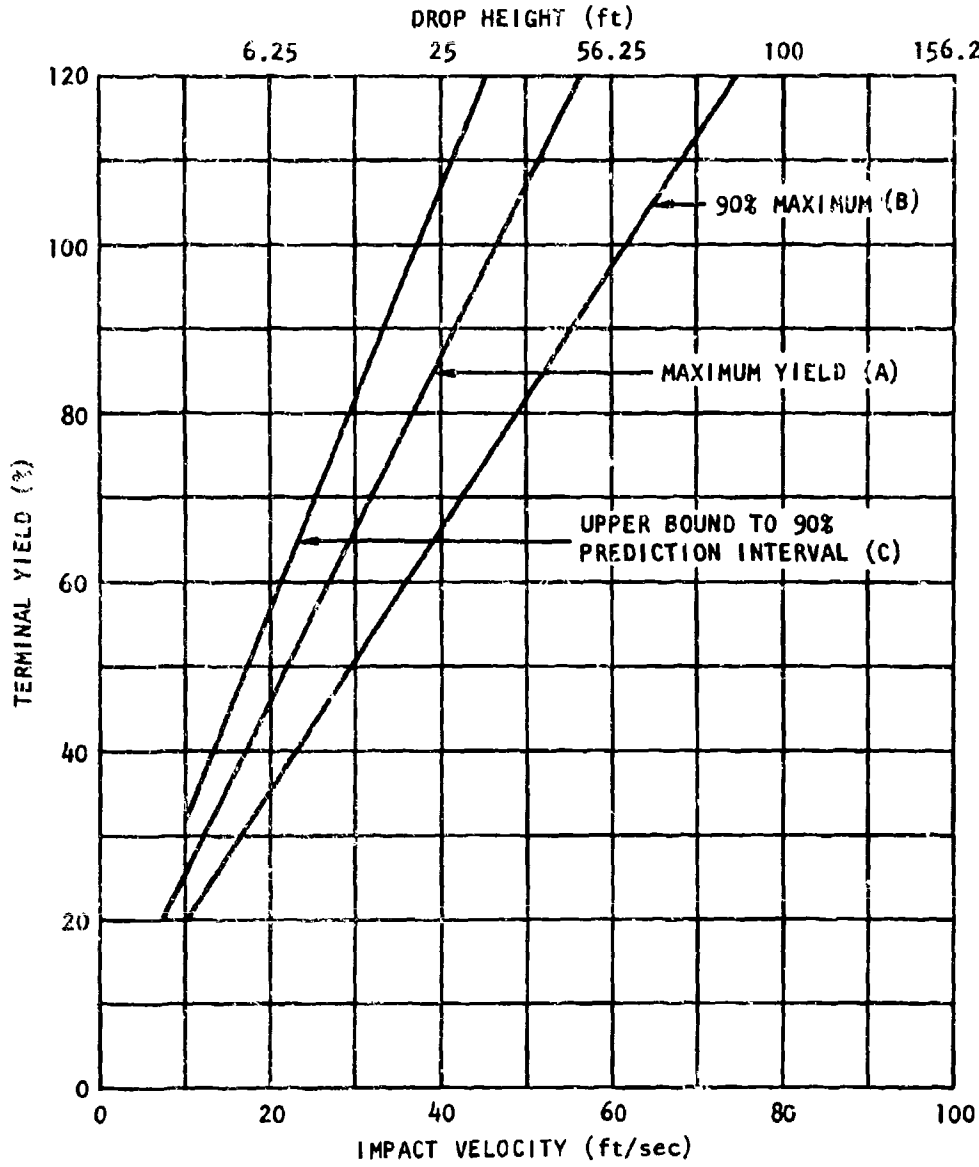


Fig. 5-41. Maximum Terminal Yield vs Impact Velocity for LO₂/RP-1 CBGS Case

the overall picture significantly, since Curve B also indicates large yields at relative low velocities. The upper bound to the 90-percent prediction interval about Curve A is given in Fig. 5-41 as Curve C.

If the ignition time is known, the yield values can be obtained directly from Eq. (5.4). Figure 5-42 is a plot of yield versus t^* for selected values of velocity. Also shown are the drop heights which would lead to the velocity values. Figure 5-43 is a similar plot for the upper bound to the 90-percent prediction interval.

Descriptive Model for LO₂/LH₂ CBGS-V Case

With one important exception, the descriptive model for the LO₂/RP-1 case is applicable to the LO₂/LH₂ case. This exception is that the LO₂, the bottom propellant for this case, does not stop spreading, as does the RP-1. Thus the yield is expected to asymptotically approach some maximum value, with the only factor tending to eventually reduce the yield being the boil-off of the propellants.

Prediction Equation Development for the LO₂/LH₂ CBGS-V Case

On the basis of the descriptive model of the process, the following guidance can be obtained with regard to the mathematical models:

1. Once the propellants are all down, the yield is expected to increase rather rapidly as the pools spread and to asymptotically approach a maximum value.
2. The yield values are generally expected to increase with an increase in impact velocity.

These points suggest that correlation of the results can be obtained by the general type of equation $Y = f(v)g(D^*)$, where f and g denote a function of the variable in parenthesis. After some preliminary plots and trials of various specific forms of this general equation, it appeared that each of the two functions could be adequately represented by a simple power series. Thus the equation reduced to

$$Y = (a + bv + cv^2 + \dots) (a' + b'D^* + c'D^{*2} + \dots)$$

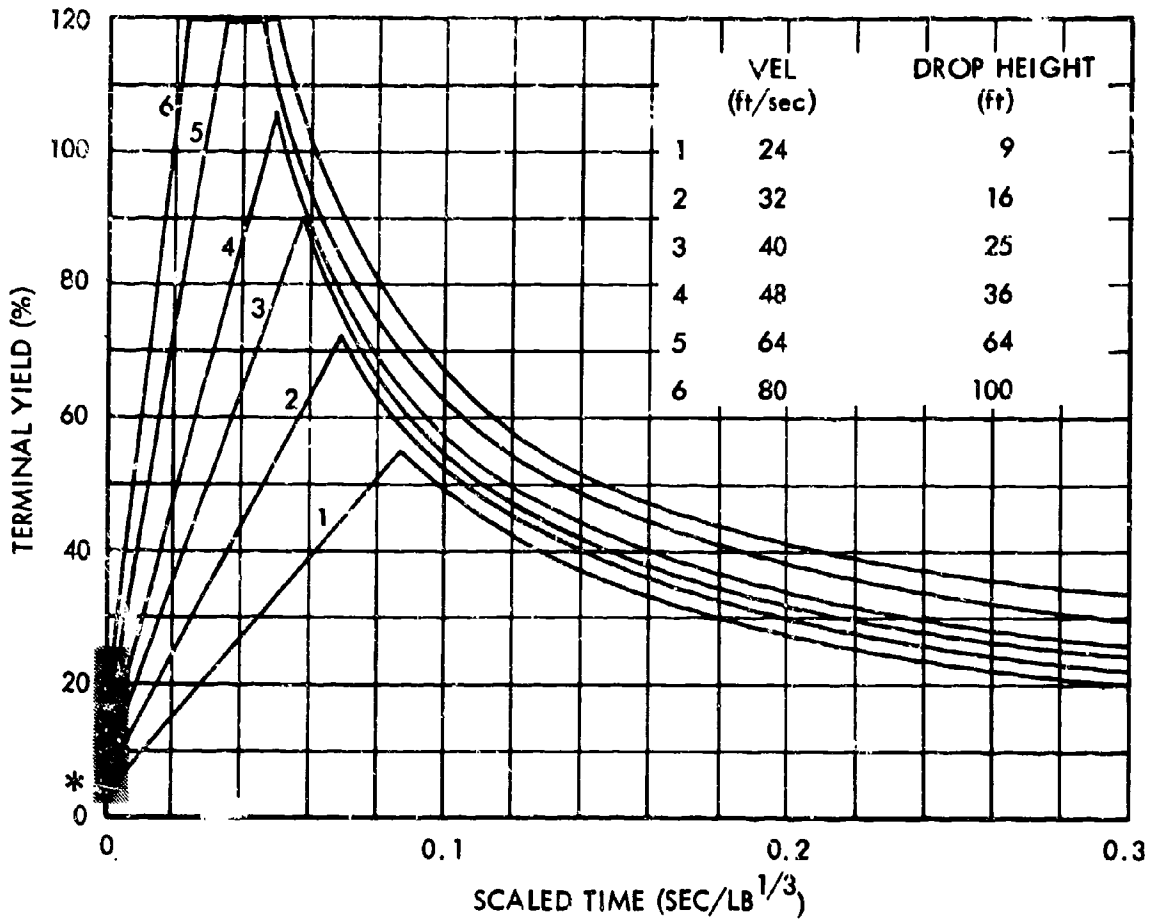
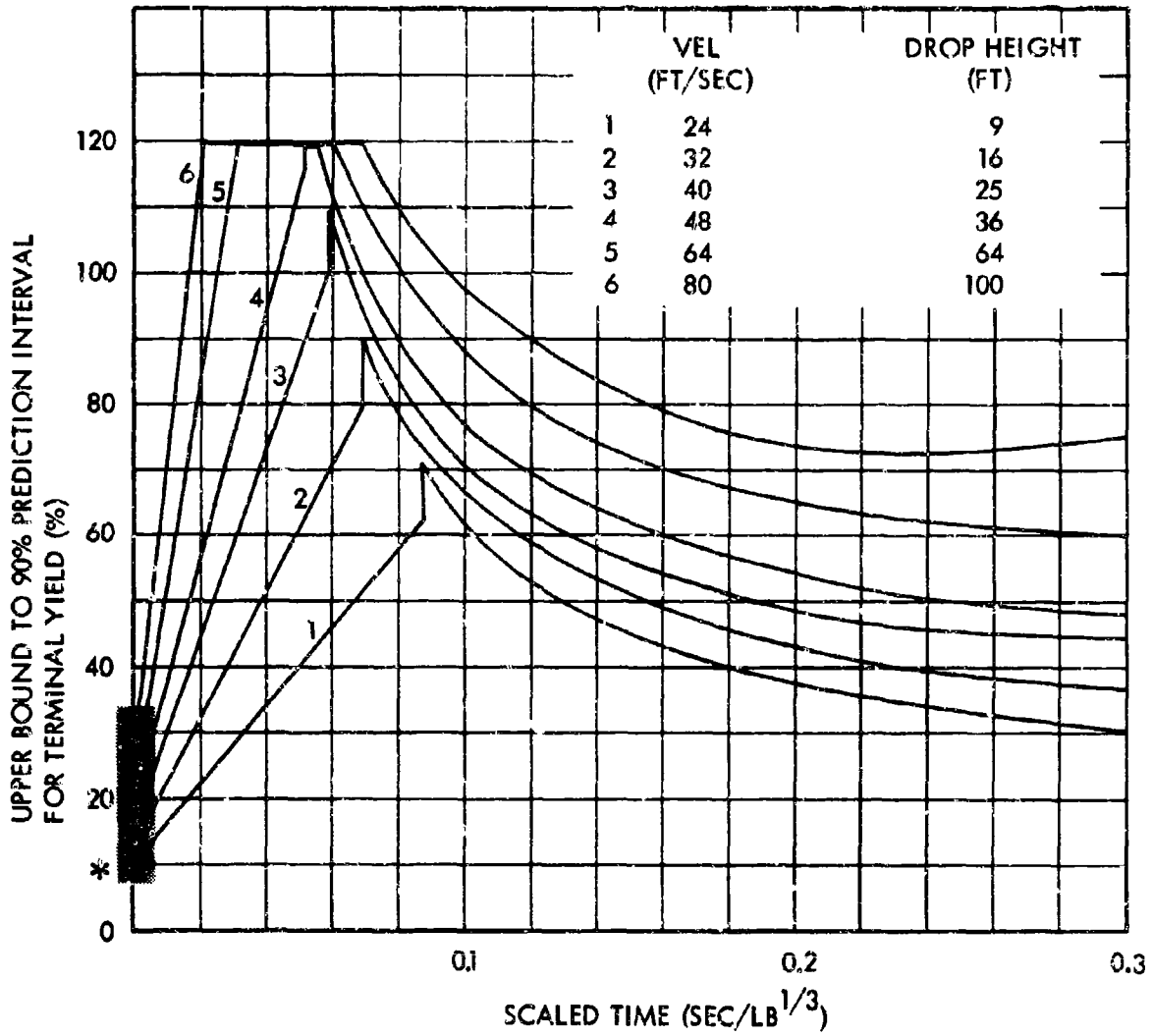


Fig. 5-42. Terminal Yield vs Scaled Time for LO₂/RP-1 CBGS Case as a Function of Impact Velocity

* Yield for Scaled Time = 0

| | |
|---------------|---------|
| V = 24 ft/sec | Y = 4 % |
| 32 | 5 |
| 40 | 6 |
| 48 | 7 |
| 64 | 9 |
| 80 | 12 |



* Upper Bound Value for Scaled Time = 0

| V = 24 ft/sec | Y = 10 % |
|---------------|----------|
| 32 | 13 |
| 40 | 15 |
| 48 | 18 |
| 64 | 24 |
| 80 | 29 |

Fig. 5-43. Upper Bound to 90% Prediction Interval for Terminal Yield vs Scaled Time for LO₂/RP-1 CBGS Case

A stepwise regression analysis was then made using this form for the equation. The results indicate that the only terms of significance were aa' , v^2D^* , and v^2D^{*2} . Thus the final form of the equation becomes

$$Y = 18.4 + 0.0031v^2D^* - 0.00015v^2D^{*2} \quad (5.5)$$

This equation is plotted in Fig. 5-44 along with the experimental data for comparison. Also shown is the upper bound to the 90-percent prediction interval.

Prediction Method Development For LO₂/LH₂ CBGS-V

As with the LO₂/RP-1 case, it is convenient to consider two basic cases, the ignition time known or unknown. If the ignition time is unknown, it is possible that ignition could occur at the time (or pool diameter) which gives the maximum yield [$D^* = 10.3$ from Eq. (5.5)]. Curve A of Fig. 5-45 is a plot of this maximum yield as a function of impact velocity. This curve indicates that yields over 100 percent are expected for velocities greater than about 70 ft/sec. Also shown as Curve B in Fig. 5-45 is the upper bound to the 90-percent prediction interval for this case.

If the ignition time is known, the yield values can be obtained directly from Eq. (5.5). Figure 5-46 is a plot of yield vs t^* for selected values of velocity. Also shown are the drop heights which would lead to these velocity values. Figure 5-47 is a similar plot for the upper bound to the 90-percent prediction interval.

Descriptive Model and Prediction Method Development for LO₂/RP-1 CBGS-H Case

In this case for the normal propellant orientation, the RP-1 was always dropped from the lowest practical height, which produced a velocity of about 12 ft/sec, while the LO₂ was dropped from various heights and resulted in impact velocities ranging from about 25 to 78 ft/sec. Various time delays between dropping the two tanks were used to obtain various sized pools of RP-1 at the instant the LO₂ impacted the ground.

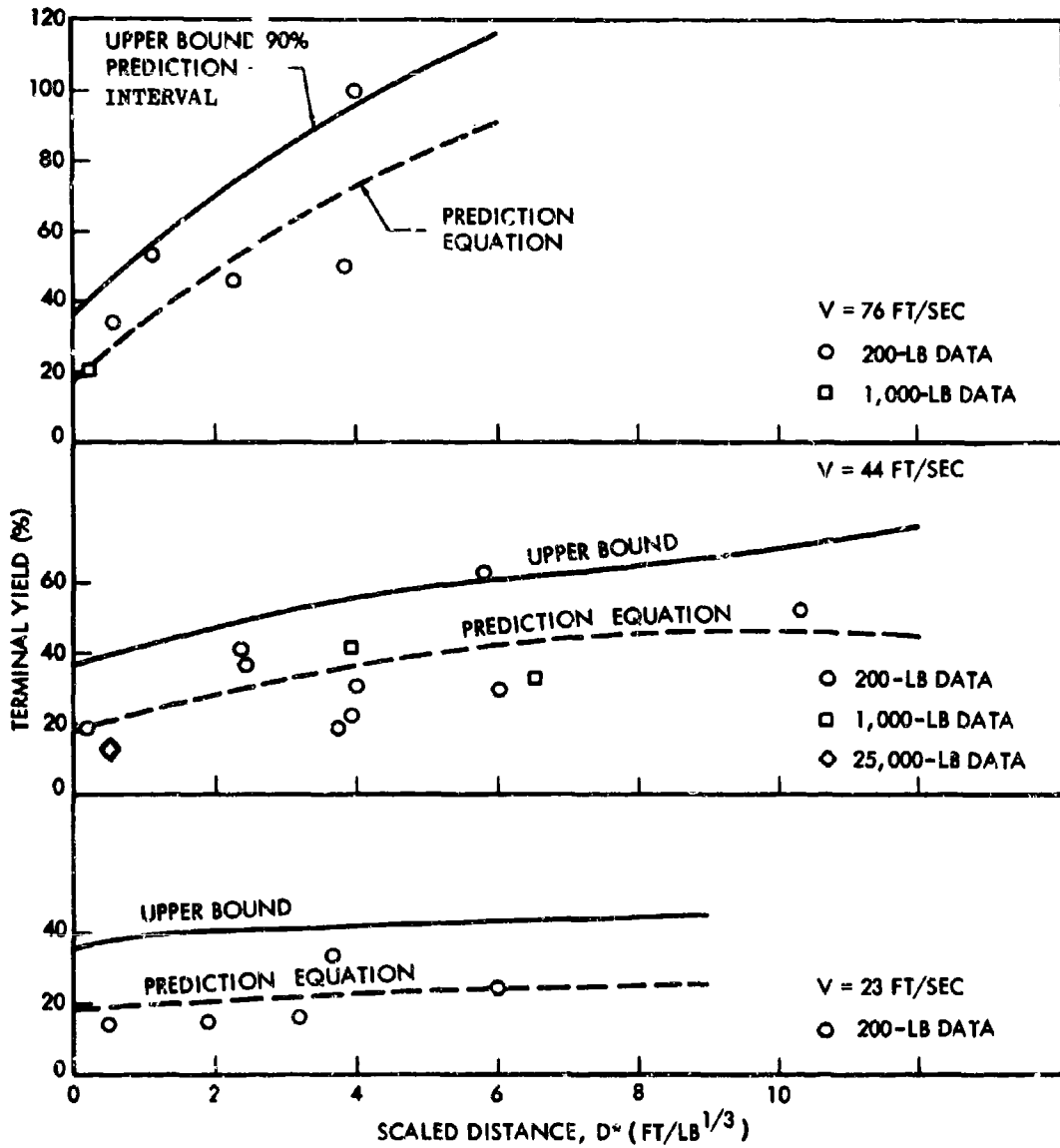


Fig. 5-44. Comparison of Experimental Data with Prediction Equation for LO₂/LH₂ CBGS-V case (dashed line - prediction equation; solid line upper bound to 90% Prediction Interval)

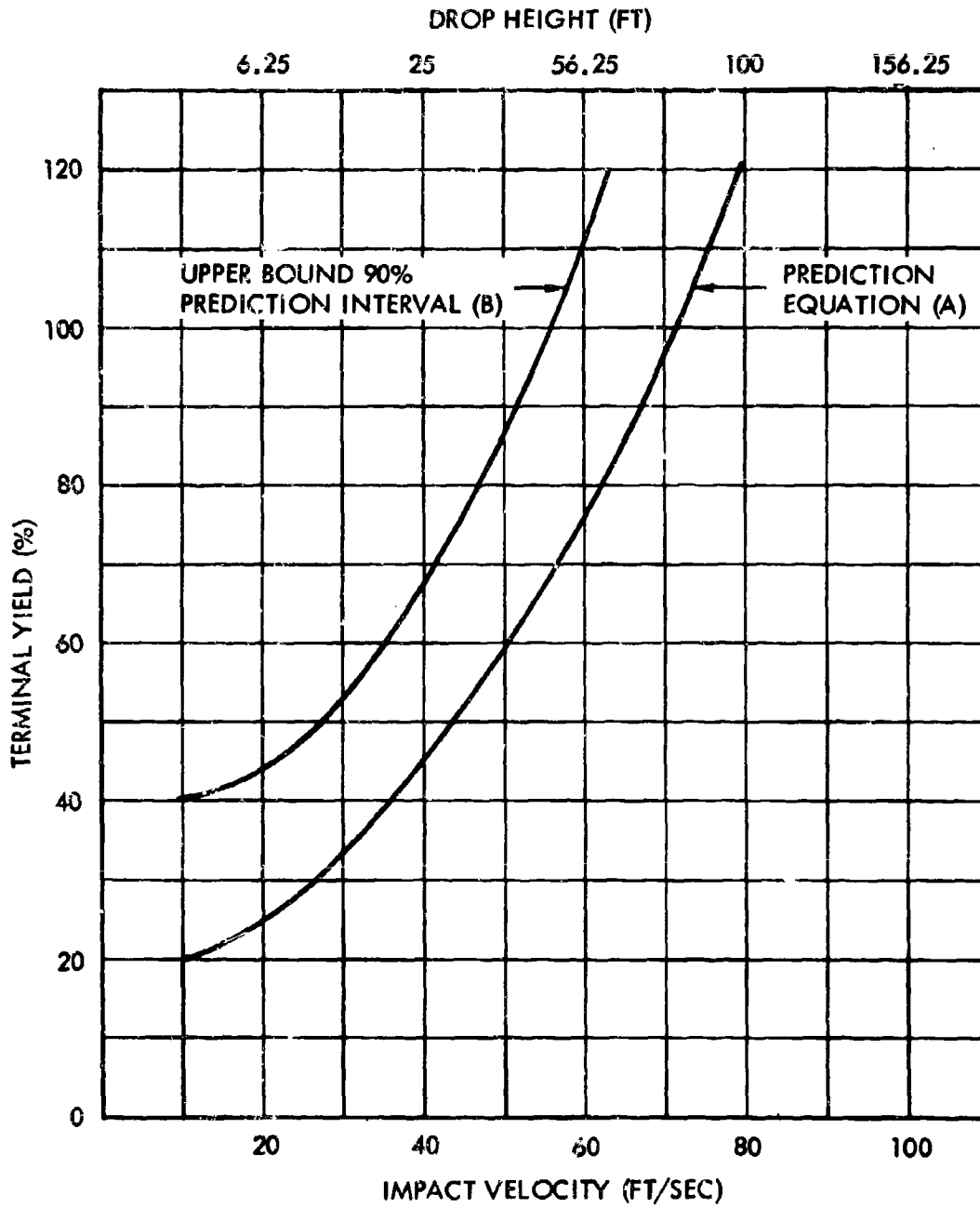


Fig. 5-45. Maximum Terminal Yield vs Impact Velocity for LO_2/LH_2 CBGS Case

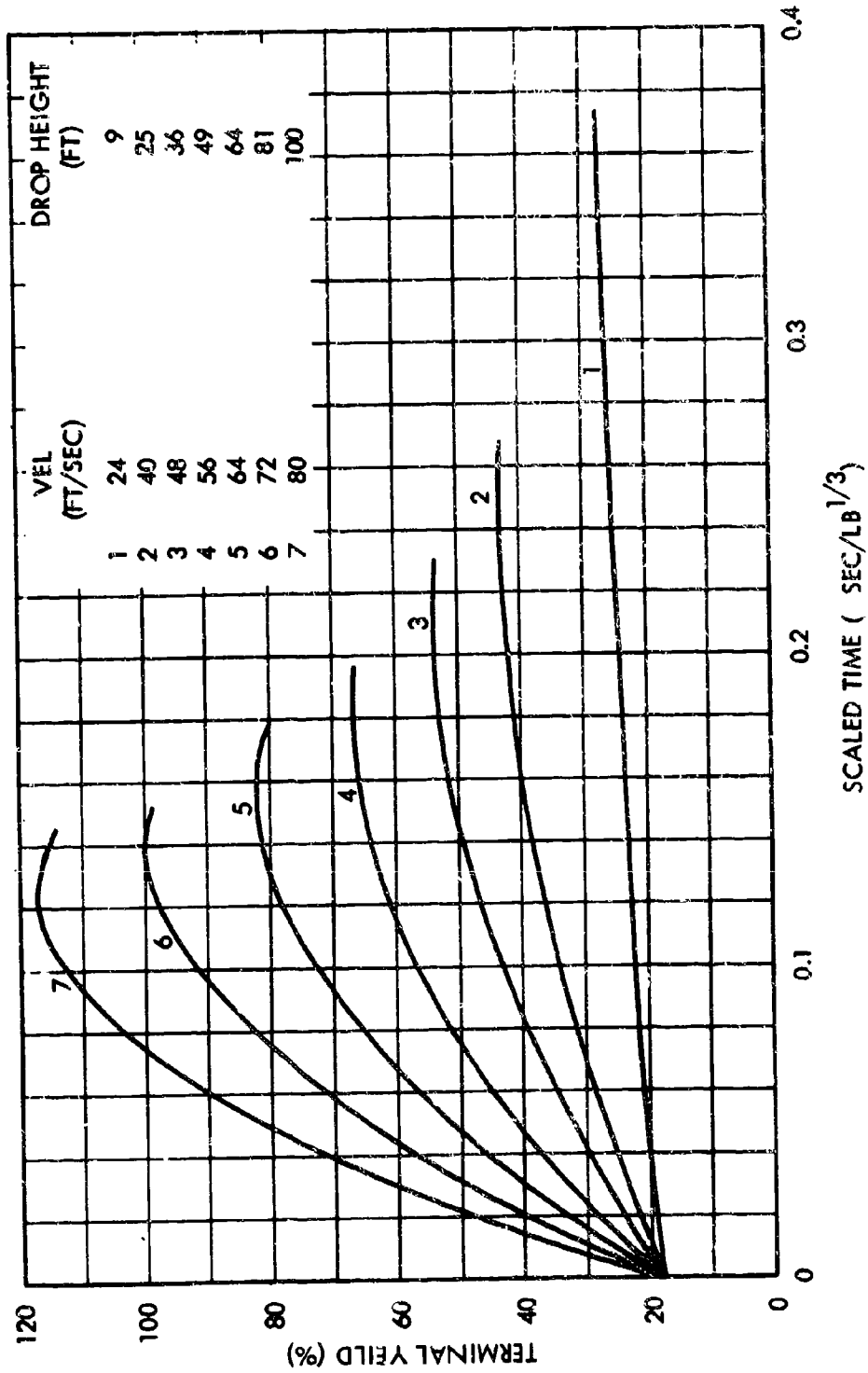


Fig. 5-46. Terminal Yield vs Scaled Time for LO₂/LH₂ CRGS Case as a Function of Impact Velocity

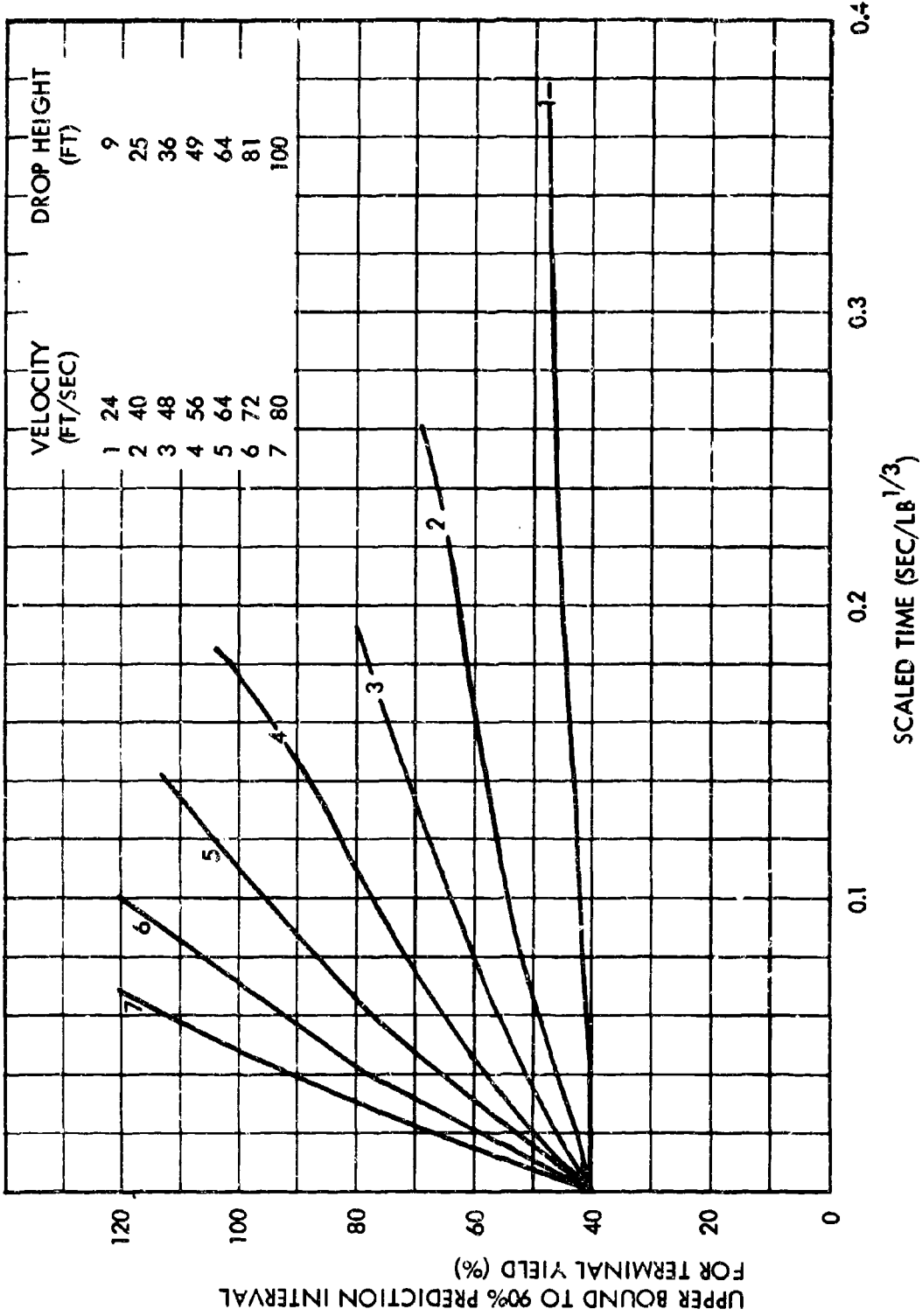


Fig. 5-47. Upper Bound to 90% Prediction Interval for Terminal Yield vs Scaled Time for LO₂/LH₂ CRGS Case

The propellant mixing behavior for this case is expected to be generally similar to that for the CBGS-V case, i.e., yield should increase rapidly as the LO₂ overlaps the RP-1 pool and reach a maximum at about the time the pools are completely overlapped and then decrease again as the LO₂ overruns the RP-1.

This similarity is illustrated in Fig. 5-48, which compares the basic prediction curve for the vertical case with the experimental data for the horizontal case. In plotting the experimental data for the normal propellant orientation, the velocity of the LO₂ was used in determining $Y/v^{0.9}$ and $D^*/v^{0.2}$, and the time used in computing D^* was the time after the LO₂ impacted the ground plus the time to travel the length of the RP-1 tank. In other words, the horizontal data were plotted such that the size of the LO₂ pool was the same as it would have been for the vertical case. For the reversed propellant orientation, the RP-1 velocity and time were used.

It would be expected that the horizontal data would generally be on or below the vertical prediction curve since the vertical case with both propellants being dropped together and having the same velocity tends to create the maximum propellant overlap conditions. The horizontal case with the top propellant having a much higher velocity tends to minimize the overlap time. The experimental data confirm this point, since only one data point is above the upper prediction bound and the great majority lie well below the prediction curve. Note that the reversed propellant orientation data also follow the same pattern. The one high data point is for a very low impact velocity, about 19 ft/sec, and the actual yield was only 25 percent.

Since the vertical prediction curve provides a reasonable upper limit to the horizontal case, its use is recommended to obtain a conservative estimate for yields in the horizontal case. Its use may be somewhat overly conservative when the bottom propellant pool is very large at the time of impact of the second propellant and when ignition occurs soon after the second propellant impacts; but it is not expected to be so for longer times of ignition, since complete propellant overlap for the horizontal case can also occur.

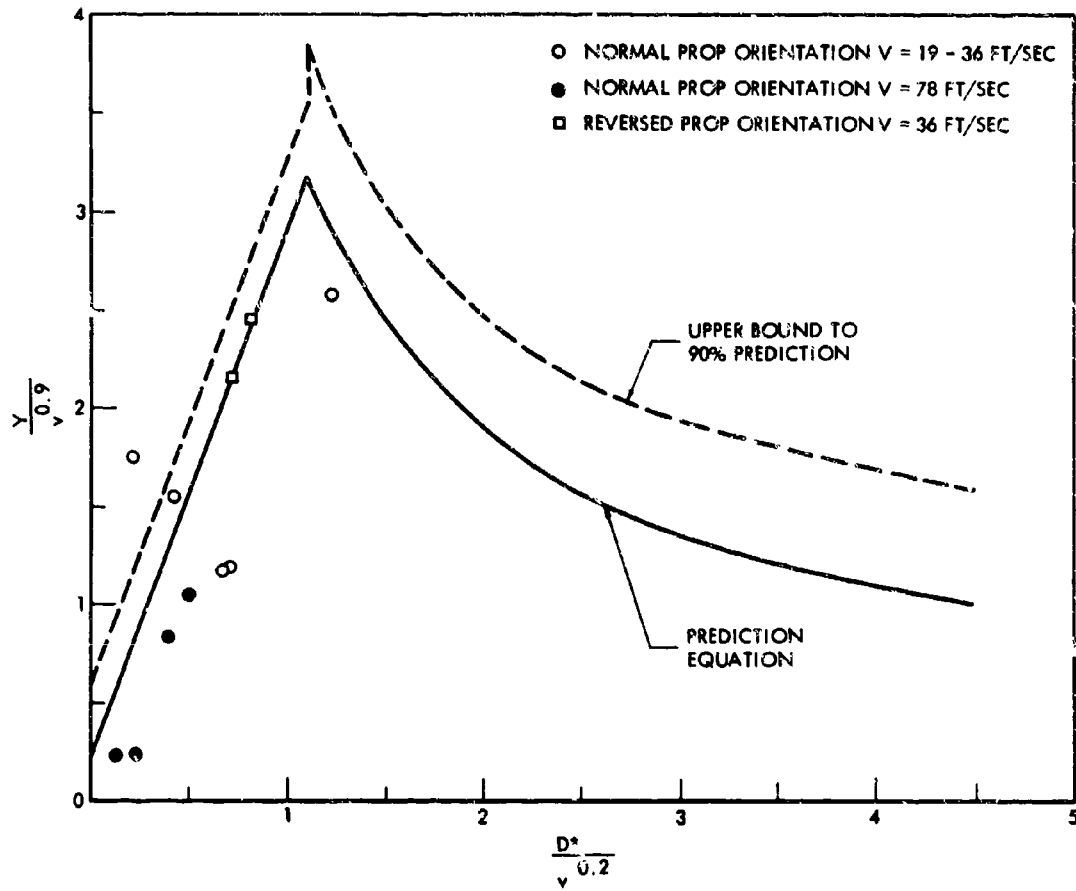


Fig. 5-48. Comparison of Experimental Data for $LO_2/RP-1$ CBGS-H Case with Prediction Equation [Eq. (5.4)]

To minimize the degree of conservatism at early times of ignition for the large pool case, another upper limit in this region can be obtained by: first, determining the quantity of bottom propellant overlapped at the time of ignition (W_b); second, computing a new effective total propellant weight ($W_b + W_t$) by adding W_b to the original top propellant weight (W_t); third, computing an effective oxidizer-to-fuel ratio (W_t/W_b); fourth, determining the maximum specific explosive yield (Y_s) in percent for this ratio (Fig. 7-2); and fifth, determining the total yield (in percent) by multiplying the specific yield by the ratio of the effective weight to the original total weight of propellant (W), i.e.,

$$Y = Y_s \left(\frac{W_t + W_b}{W} \right)$$

Because of uncertainties in propellant locations and the possibility of some reaction after ignition, a minimum yield value of 20 percent is recommended.

Descriptive Model and Prediction Method Development for LO₂/LH₂ CBGS-H Case

As with LO₂/RP-1, the horizontal case for LO₂/LH₂ is expected to be generally similar to the vertical case. This similarity is illustrated in Fig. 5-49, which compares the horizontal data with the vertical prediction equation. Because of this good agreement, it is again recommended that the vertical case be used as a conservative approximation for the horizontal case.

High-Velocity-Impact Case

Test Parameters

In the high-velocity-impact tests, the tanks were accelerated down a sled track to speeds ranging from 500 to 600 ft/sec and allowed to impact into a massive target having two basic configurations. This was a very limited test series and the target geometry was the only variable. One target configuration was a flat wall, which simulated a rigid ground surface, the other was a deep hole, which simulated impact into a soft surface in which a crater would be formed by the impact process.

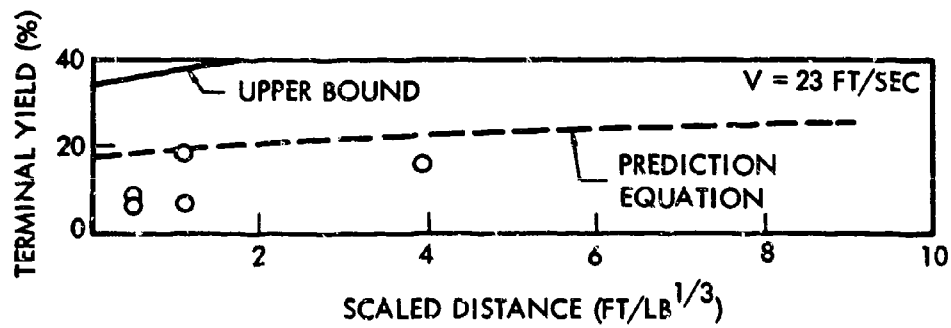
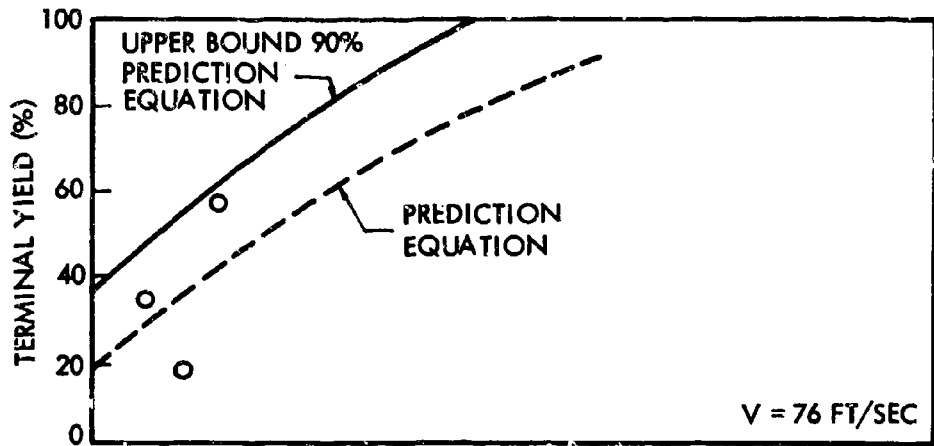


Fig. 5-49. Comparison of Experimental Data for LO₂/LH₂ CBGS-H Case With Prediction Equation [Eq. (5.5)]

Descriptive Model and Prediction Method Development for High-Velocity-Impact Case for Both Propellant Combinations

Since ignition at or very soon after the time of impact is obtained for this case, there is little time for the type of mixing experienced in the CBM case or the much lower velocity CBGS case. The propellants, however, have a great deal of kinetic energy, and it is anticipated that significant mixing can occur after ignition. In general, the amount of mixing, and thus yield, would be expected to increase with increasing impact velocity. It also would be expected that the confinement offered by the deep-hole target would tend to contain the reacting propellants sufficiently to result in increased post-ignition mixing.

The experimental results for $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 are plotted in Figs. 5-50 and 5-51, respectively. Also shown on the figures are the CBGS results for an ignition time $t^* = 0$. These results confirm the expected trends in the data, i.e., increase in yields with increase in impact velocity and higher yields for the deep-hole target.

For the $\text{LO}_2/\text{RP-1}$ flat-wall case, it would seem reasonable to use as the best estimate of the terminal yield vs impact velocity curve, the line given by the prediction equation [Eq. (5-4)] up to 150 ft/sec, and then the constant value of 21 percent. The data for velocities above 100 ft/sec are insufficient to rigorously establish an upper bound to the 90-percent prediction interval in this region. If the upper bound for Eq. (5-4) is extrapolated to 150 ft/sec, it would indicate an upper bound of about 40 percent. This seems somewhat on the conservative side, however, considering the good reproducibility of the 2 data points at approximately 520 ft/sec. Furthermore, the general reproducibility of the hypergolic high-velocity-impact tests also suggests less variability. Accordingly it is recommended that 30 percent be used as the estimated upper 90-percent bound. The two selected curves are shown in Fig. 5-52 (Curves A and B).

For the $\text{LO}_2/\text{RP-1}$ deep-hole case, the extension of the basic prediction equation [Eq. (5-4)] which fits the two-high-velocity points was selected as the best estimate. The upper bound was estimated by extending the upper bound

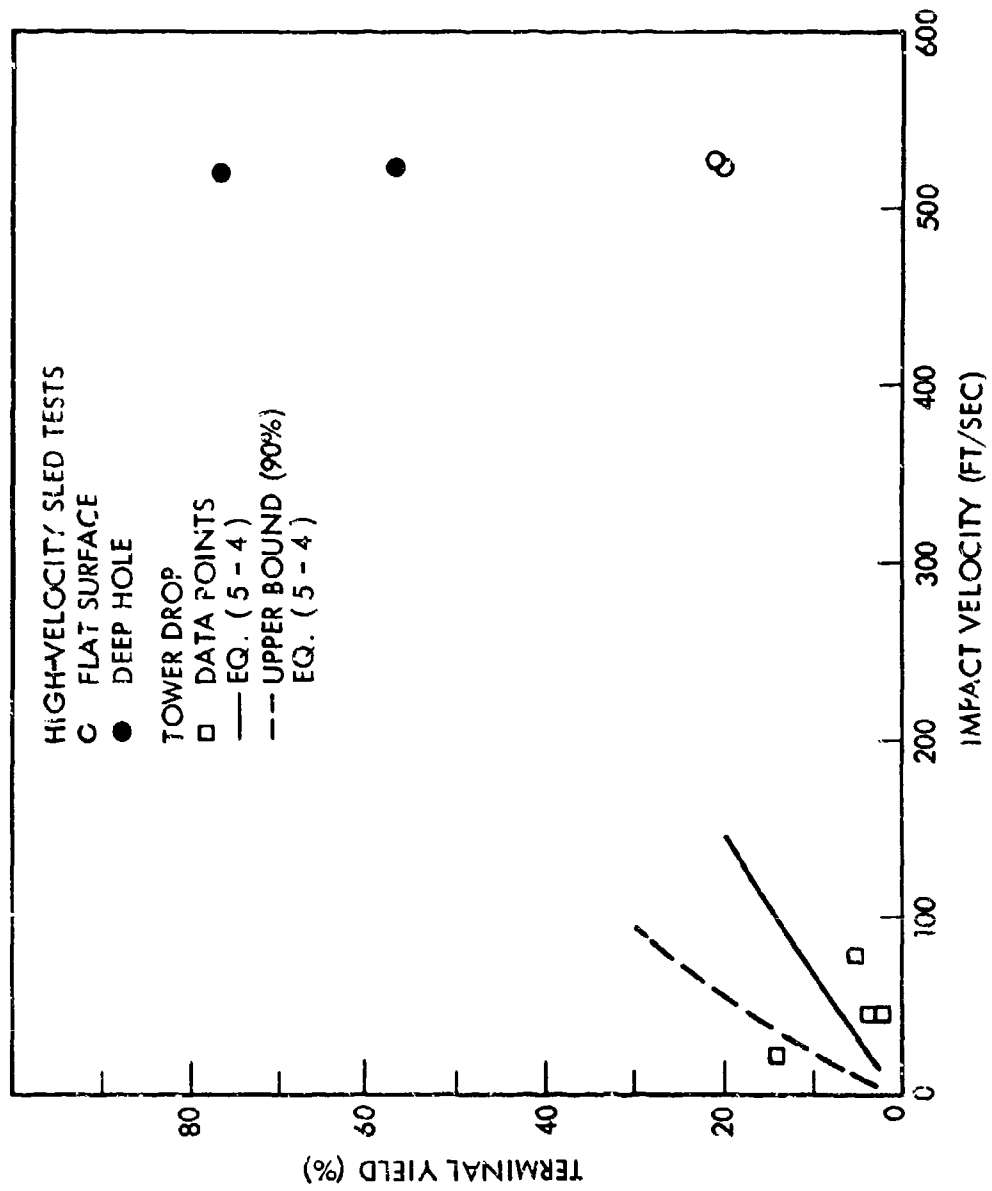


Fig. 5-30. Experimental Yield Data vs Impact Velocity for LO₂/RP-1 With Ignition at or about the time of Impact

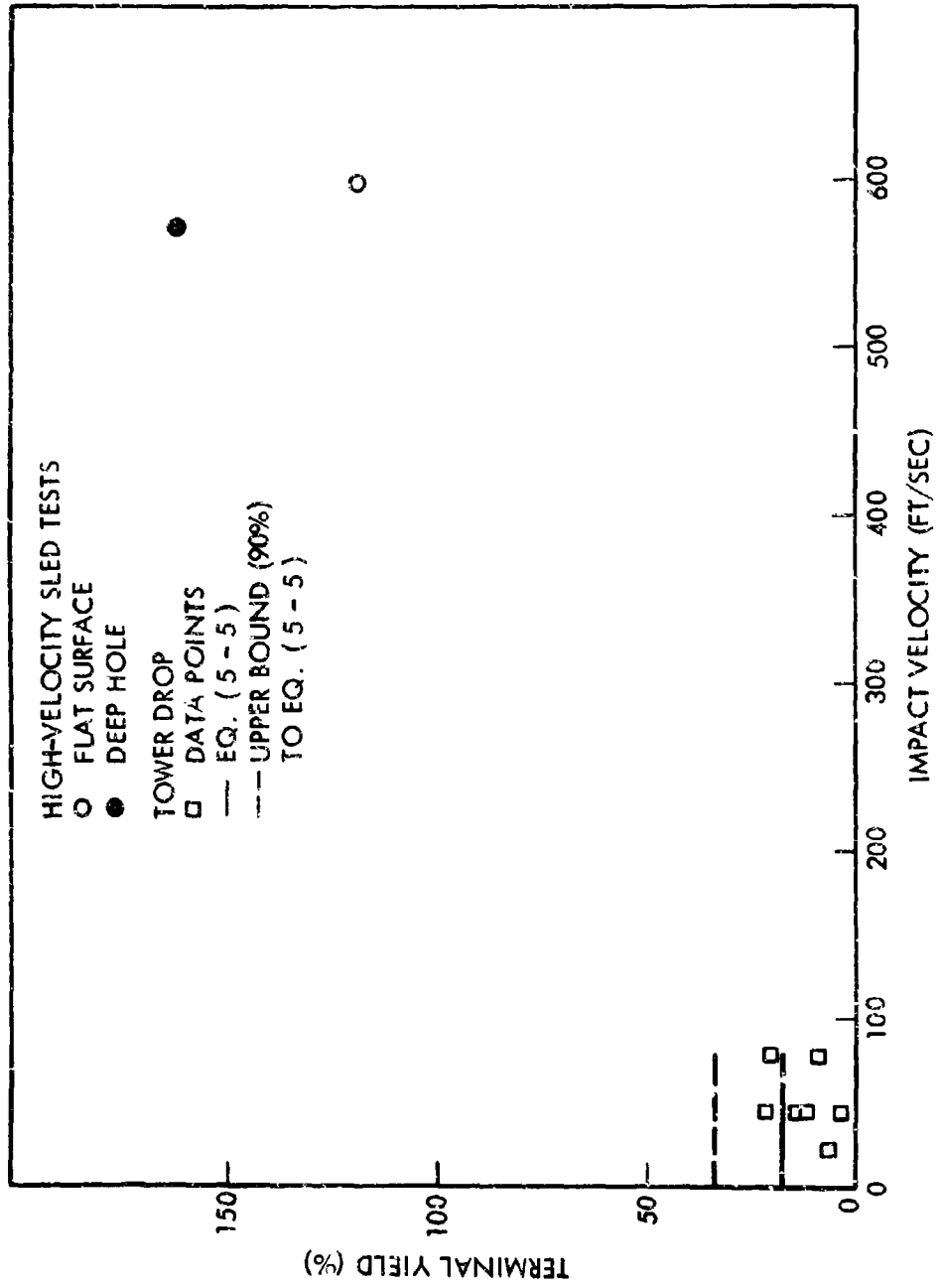


Fig. 5-51. Experimental Yield Data vs Impact Velocity for LO₂/LH₂ With Ignition at or about the time of impact

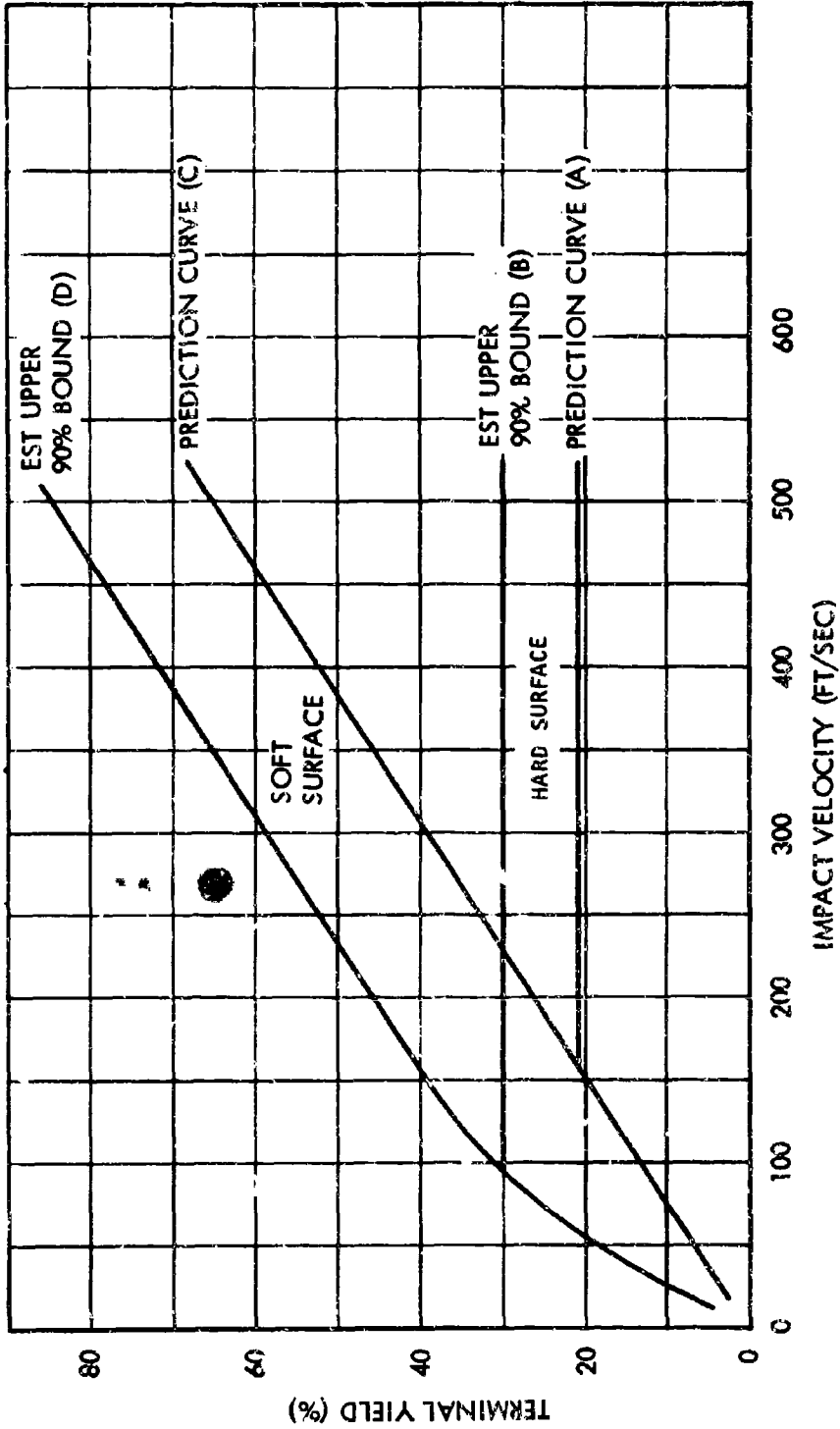


Fig. 5-52. Terminal Yield vs Impact Velocity for LO₂/RP-1

of Eq. (5.4) to 150 ft/sec and then extending it in a line parallel to the basic prediction equation. Again this is not a derived value, but an estimated one based on engineering judgment. The two selected curves are again shown in Fig. 5-52 (Curves C and D).

Note that the similarity between the flat-wall and deep-hole curves in the low-velocity region is reasonable because these impact velocities are not sufficient to cause significant impact craters even in a soft surface.

For the LO_2/LE_2 flat-wall case, the best estimate of the yield vs. impact-velocity curve was obtained by a linear extrapolation between the CBGS prediction equation [Eq. (5.5)] and the high-velocity data point. The deep-hole case was estimated in a similar manner. The upper bounds to the 90-percent prediction intervals were estimated by assuming a 30-percent uncertainty at a yield value of 100 percent and assuming a linear extrapolation to the value determined in the low-velocity region. The selected curves are shown in Fig. 5-53.

It should be emphasized that the two surface conditions used were selected to bound the range of interest. The rigid or hard surface condition corresponds to essentially no penetration of the surface by the impacting tankage. Rock or concrete surfaces would satisfy this condition. The soft surface condition means essentially complete penetration of the entire tankage into the surface. A water surface would satisfy this condition.

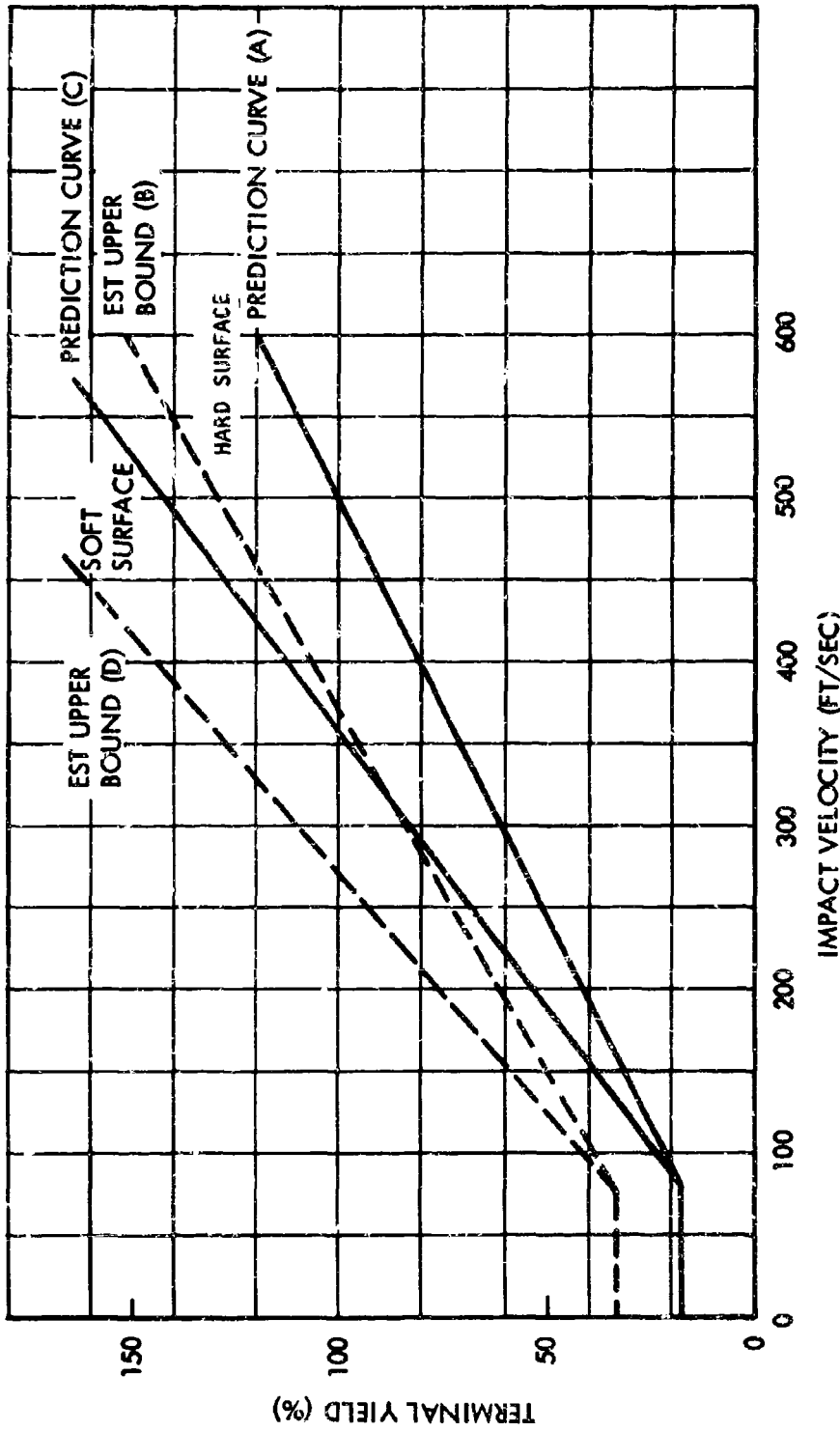


Fig. 5-53. Terminal Yield vs Impact Velocity for LO₂/LH₂

Part 4 - FULL-SCALE TESTS

During the program two valid data tests were performed using full-scale missile hardware. These were a Saturn S-IV stage containing approximately 91,000 lb of LO_2/LH_2 and a Titan I first stage containing approximately 100,000 lb of $\text{LO}_2/\text{RP-1}$. The objectives of these tests were to obtain full-scale data points regarding the blast, thermal, and fragmentation hazards of $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 propellant combinations.

Saturn S-IV

The planned failure conditions for this test were to rupture the common bulkhead, forcibly bring the two propellants together by having a pressure differential between the two propellant compartments, and to ignite the resulting propellant mixture at a known delay time. A photo of the S-IV test article is shown in Fig. 5-54.

The common bulkhead of the S-IV vehicle is an 18-ft-diameter dome constructed of two thin sheets of aluminum separated by an insulating layer of hexcell material. The overall thickness of this bulkhead is approximately 1 in.

This bulkhead was ruptured from the underside with an explosively driven segmented cutter, 18 in. in diameter. This cutter was designed to slice through the bulkhead, creating six small segments which could then be easily removed by fluid flow through the hole. A photo of this cutter ram assembly is shown in Fig. 5-55.

This cutter was attached to a drive rod and was installed in the LO_2 tank in the guide tube assembly as shown in Fig. 5-56. The lower end of the drive rod extends through the stopping tube and part way into the drive barrel.

To rupture the common bulkhead, the drive rod cutter assembly was pushed by detonating an explosive charge in the driver barrel. Once the

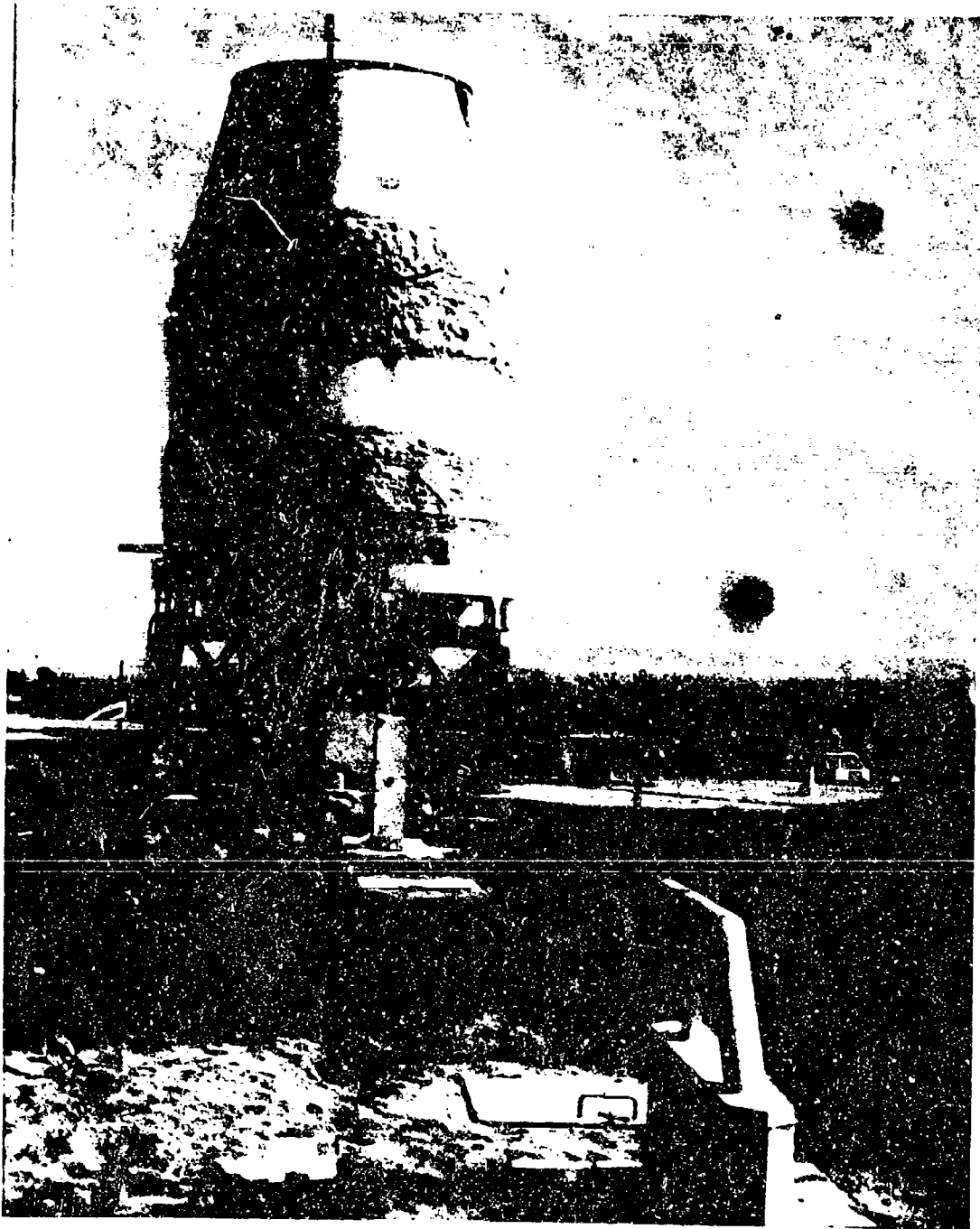


Fig. 5-54 Saturn S-IV



Fig. 5-55 Cutter for the Saturn S-IV Cutter Ram Assembly

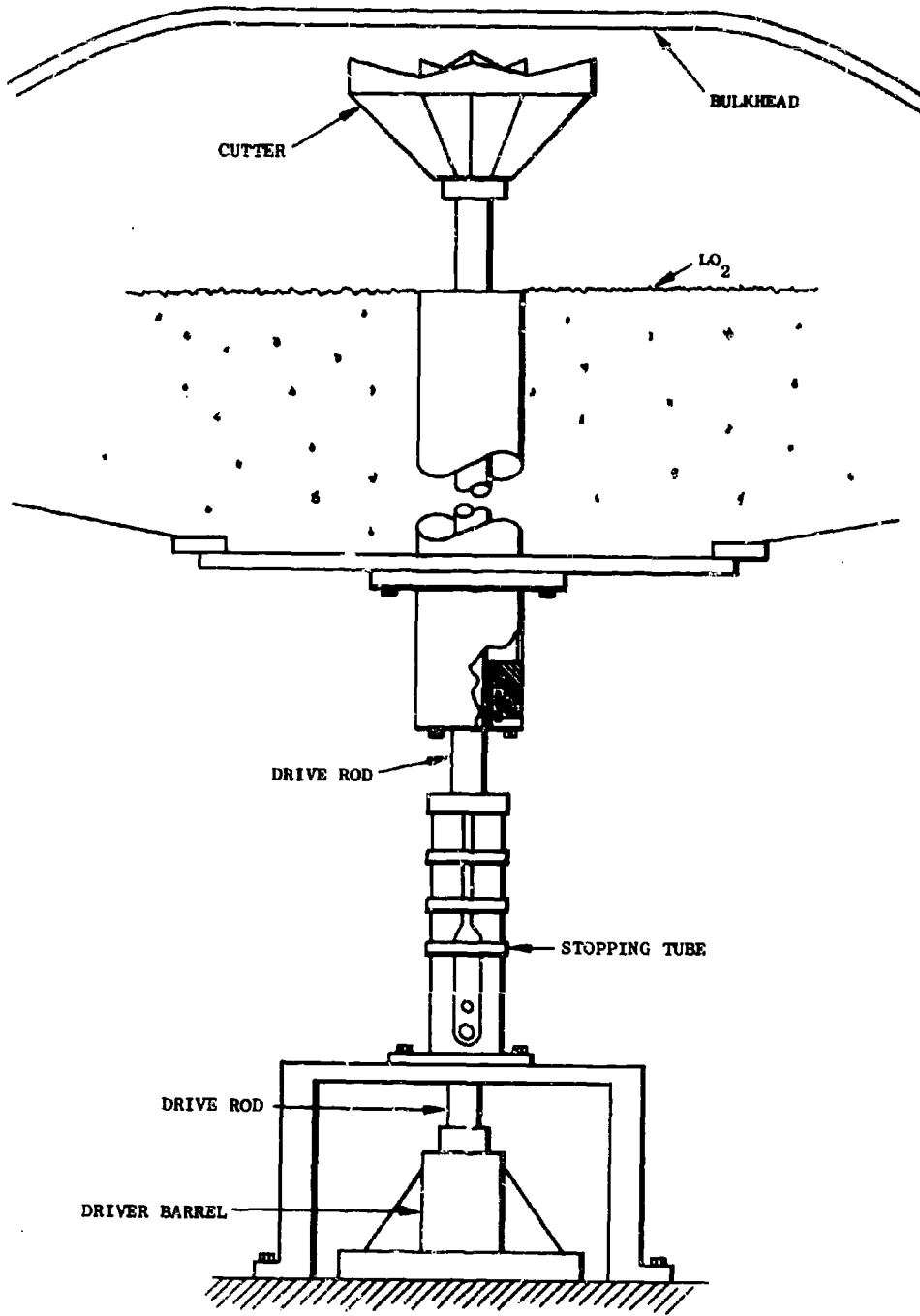


Fig. 5-56. Saturn S-IV Cutter Ram Assembly

cutter ram penetrated the common bulkhead, a pin installed through the drive rod impacted a slot in the stopping tube. This brought the drive rod and cutter to a stop and prevented it from continuing on into the LH₂ tank and possibly damaging the top of that tank.

To aid in removing the small segments cut by the cutter assembly and to forcibly bring the two propellants together, the upper tank was pressurized to approximately 15 psi, while the lower tank was kept at ambient pressure. The ullage volume in the tanks was approximately 3 percent in the LH₂ tank and 10 percent in the LO₂ tank. The planned ignition time for this test was ~10 sec, although it was expected that the tank would reach burst pressure (~100 psi) much sooner than this and self-ignite.

Test Results

According to all available data, including direct observations, high-speed motion picture films, pressure records, etc., the failure mechanism hardware functioned as planned. Ignition, however, occurred 183 msec after rupture of the common bulkhead, which was much shorter than the planned ignition time and well before the internal pressure reached 100 psi.

The overpressure and positive-phase-impulse data obtained from this test are given in Table 5-9.

Average yield values at each distance have been calculated from the data in Table 5-9 and are presented in Table 5-10. Note that the pressure yield seems to level off at a value of about 3 to 3.5 percent and the impulse yield at a value of 6.5 to 7.0 percent.

Table 5-9
PEAK PRESSURE AND POSITIVE-PHASE IMPULSE FROM S-IV TEST

| GROUND DISTANCE (ft) | GAUGE LINE | PEAK OVERPRESSURE (psi) | POSITIVE-PHASE IMPULSE (psi-msec) |
|----------------------|------------|-------------------------|-----------------------------------|
| 38 | A | 96.0 | 440 |
| 67 | C | 26.42 | 231 |
| 117 | A | 12.1 | * 192 |
| | B | 12.4 | ** |
| 200 | A | 4.57 | *** |
| | B | 4.40 | ** |
| 335 | A | 2.55 | 87.2 |
| | B | 2.55 | 73.3 |
| 600 | A | 1.02 | ** |
| | B | 1.03 | 41.56 |
| | C | 1.15 | 44.5 |

* Uncertain because of large correction.

** Noise on traces.

*** Suspected base shift.

"A" refers to 11 o'clock leg, "B" refers to 7 o'clock leg, "G" refers to 3 o'clock leg.

Table 5-10

S-IV YIELDS

| Distance (ft) | Yield | |
|------------------|-----------------|----------------|
| | Pressure (%) | Impulse (%) |
| 38 | 1.5 | 6.0 |
| 67 | 1.5 | 4.2 |
| 117 | 2.5 | 5.8 |
| 200 | 2.4 | - |
| 335 | 3.5 | 6.8 |
| 600 | 2.9 | 6.6 |

S-IV Fragmentation Survey

Following the S-IV test a complete fragmentation survey was conducted of the test area. This survey included locating, measuring, weighing, and describing each fragment from the S-IV vehicle weighing over 1 lb. The area covered was from 100 ft from ground zero to the outer limit of the fragment dispersion, which was approximately 900 ft.

The procedure used for this survey was to lay out on the ground surface a system of 16 radial lines marked off in 100-ft increments, thus dividing the area into 128 easily defined areas. Teams of URS and AFRPL personnel then searched each of these areas, and as each fragment was collected, its approximate location within the area was noted on a map and an identification number written on the fragment. The fragments were then loaded on a truck for transportation to a central point for weighing and measuring. The data obtained from this survey are presented in Volume 2. These data indicated that the majority of the fragments were within a radius of 800 ft from ground zero, with a few out to 1,000 ft.

Titan I Tests

The planned failure condition for this test was similar to that used for the Saturn S-IV in that a common diaphragm between the propellants was to be ruptured, differential pressure in the two tanks would force the propellants together, and ignition would be initiated after a known delay. A photo of the Titan I on the test pad is presented in Fig. 5-57. This missile normally contains approximately 170,000 lb LO₂/RP-1. The test conditions planned for these tests were to fill the propellant tanks two-thirds full, which would be 78,600 lb LO₂ and 35,000 lb RP-1, or a total propellant weight of 113,600 lb.

In a Titan I first stage, the two propellant tanks are separated by some 10 in. To allow the propellants to come together, a tubular conduit with an internal diameter of 16-1/2 in. was installed between the two propellant compartments and a tempered-glass diaphragm installed at the top of the conduit. This diaphragm was removed during the test by an explosively driven segmented cutter ram similar to those used in the small-scale CBM test series. This 16-1/2-in.-diameter hole is equal to approximately 2 percent of the tanks' cross-sectional area.

Test Results

Two tests of the Titan I were attempted. The first Test Number 300, produced no data because a structural failure in the missile resulted in ignition before instrumentation was scheduled to be turned on.

In the second test, Number 301, propellant filling difficulties resulted in the LO₂ tank being only one-half full rather than the two-thirds full originally planned. The failure mechanism hardware appears to have functioned properly, and pressure in the RP-1 tank rose smoothly from ambient to about 54 psi immediately before ignition, which occurred about 842 msec after rupture of the diaphragm.

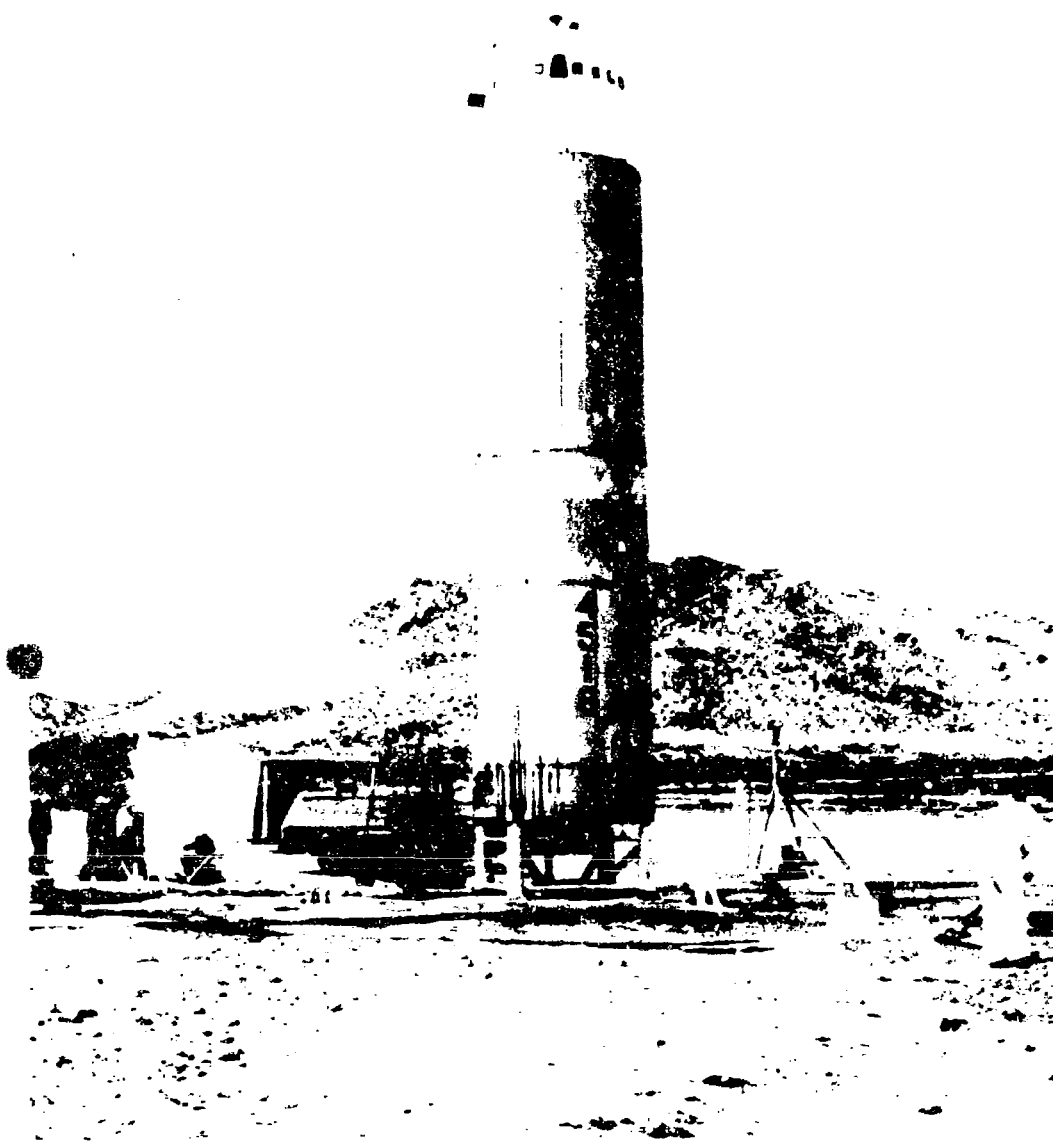


Fig. 5-57. Titan I on Test Pad

The peak overpressure and positive-phase-impulse data for this test appear in Table 5-11. The terminal yield for this test, based on the estimated 94,000 lb of propellants on board, is 4 percent. A posttest photo of this test appears as Fig. 5-58.

Table 5-11
Peak Overpressure and Positive-Phase-Impulse Data
for Titan 1 LO₂/RP-1 Test

| Distance (ft) | Peak Overpressure | | Positive-Phase-Impulse | |
|------------------|----------------------|-----------|------------------------|-----------|
| | psi/in. ² | yield (%) | psi/msec | yield (%) |
| 37 | 45 | 0.5 | 310 | 3.0 |
| 67 | 39 | 2.5 | 218 | 3.6 |
| 117 | 12 | 2.5 | 158 | 4.3 |
| 200 | 5.6 | 3.2 | 78 | 3.0 |
| 333 | 2.3 | 2.7 | * | * |
| 600 | 1.1 | 3.0 | 34.2 | 4.6 |

* Impulse not readable because of noise

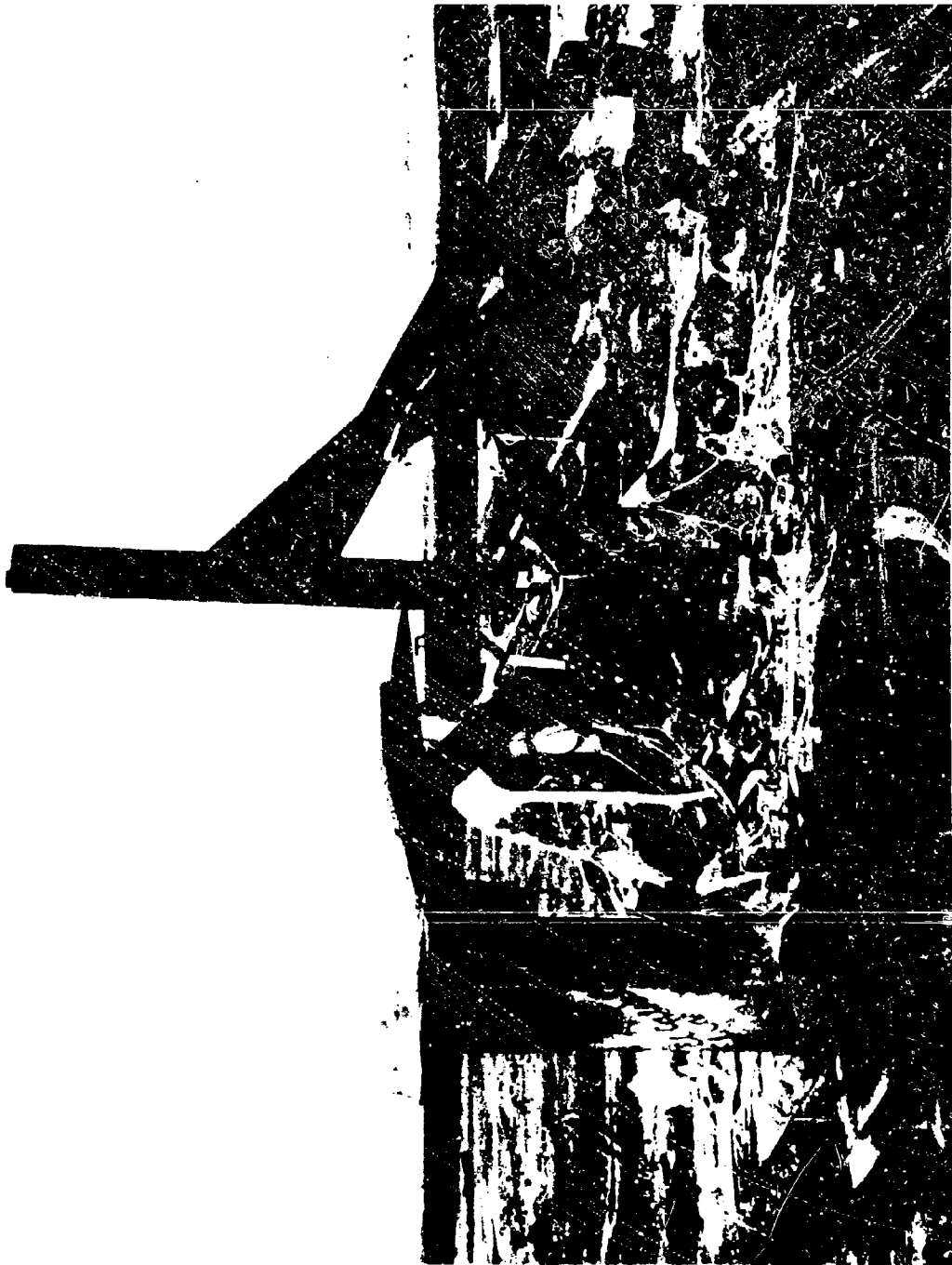


Fig. 5-58. Posttest Photo Titan I Test

Part 5 - DISCUSSION OF YIELD AND IGNITION TIME SCALING

Yield Scaling

The basic blast prediction equations presented in the previous sections were initially developed using data obtained from 200-lb weight scale and from postulated scaling relationships based on available information regarding the propellant mixing and explosion phenomena. Specifically, the initially postulated relationship was that the explosive yield (in percent of the total propellant weight) would be independent of the propellant quantity and that the time of ignition to obtain equal explosive yields would scale as the cube root of the propellant weight. In other words, $Y = f(t/W^{1/3})$. This relation is illustrated below for the LO₂/RP-1 CBM case and with L/D = 5 and a k = 1.

| <u>PROPELLANT WEIGHT (lb)</u> | <u>IGNITION TIME (msec)</u> | <u>SCALED IGNITION TIME (msec/lb^{1/3})</u> | <u>YIELD (%)</u> | <u>YIELD (lb of TNT)</u> |
|---------------------------------------|-------------------------------------|---|----------------------|------------------------------|
| 200 | 193 | 33 | 10 | 20 |
| 1,000 | 330 | 33 | 10 | 100 |
| 5,000 | 564 | 33 | 10 | 500 |
| 25,000 | 965 | 33 | 10 | 2,500 |
| 125,000 | 1,650 | 33 | 10 | 12,500 |
| 625,000 | 2,820 | 33 | 10 | 62,500 |
| 3,125,000 | 4,725 | 33 | 10 | 312,500 |

1,000-lb Scaling Tests

The validity of the postulated scaling was tested first using results from the 1,000-lb scale with the following results:

LO₂/RP-1 CBM. Comparison of the 200- and 1,000-lb data showed significant differences, which indicated a deviation from the postulated scaling. The magnitude of the effect could not be adequately evaluated without use of the 25,000-lb data.

LO₂/LH₂ CBM. Comparison of the 200- and 1,000-lb data showed no significant differences and all data were used in deriving the prediction equation (see Fig. 5-37).

LO₂/RP-1 CBGS. Comparison of the 200- and 1,000-lb data showed no significant differences, and all data were used in deriving the prediction equation (see Fig. 5-39).

LO₂/LH₂ CBGS. Comparison of the 200- and 1,000-lb data showed no significant differences and all data were used in deriving the prediction equation (see Fig. 5-44).

25,000- to 100,000-lb Scaling Tests

Further tests of the validity of the postulated scaling were made with tests using propellant quantities ranging from 25,000- to 100,000-lb scale with the following results:

LO₂/RP-1 CBM. Since the results from the 200- and 1,000-lb tests showed a scale effect and were not in themselves sufficient to establish its magnitude, the basic prediction equation was developed using all the 200-, 1,000-, and 25,000-lb data. The results showed that the yield tended to decrease with increasing weight but that the rate of decrease also tended to decrease with increasing weight so that the yield was essentially independent of weight for weights greater than 10,000 lb.

One 94,000-lb test was run which was considered consistent with this postulated scaling even though the measured yield value was below the predicted range as shown below: *

* A second similar test was run but the diaphragm failed early and no data were obtained.

Measured Yield 4% for 94,000 lb, 2% for 170,000 lb

Predicted Yield

Best Estimate 6%

Range* 4-8%

The prediction method was based on results with intertank openings equal to 20% of the tank cross-sectional area and thus would be expected to be conservative (i.e., somewhat high) for this case because the inter-tank bulkhead opening area was only 2%.

LO₂/LH₂ CBM. Three 25,000-lb tests were run. In all three tests ignition occurred very early and prior to the time the LH₂ could have contacted the LO₂, the minimum time of interest for this case.** Since these tests did not satisfy the basic criteria for this test case, they are not suitable for a quantitative check of scaling. Their results, however, which ranged from 0.1 to 0.2% are not inconsistent with the predicted zero time yield value of 2.8% and range of 0-10% since they would be expected to be smaller than the predicted value. One 91,200-lb test was run (S-IV) which was consistent with the postulated scaling as shown below:

S-IV Yield

Measured Yield 5%

Predicted Yield

Best Estimate 6%

Range 0-13%

* Bounds of 80% two-sided prediction interval. (There is an 80% probability that a single test will fall within these bounds.)

** It is believed that in two of the cases, the initial reaction was between the LH₂ and frozen oxygen trapped in the LH₂ during the filling process. In the other case, the diaphragm did not break.

LO₂/RP-1 CBGS. Two 25,000-lb tests were run, one of which ignited prior to the second propellant reaching the ground surface (the minimum time of interest for this case) and thus was not suitable for a quantitative scaling check. The results from the other test were consistent with the postulated scaling as shown below:

| | |
|------------------------|--------|
| <u>Measured Yield</u> | 37% |
| <u>Predicted Yield</u> | |
| Best Estimate | 36% |
| Range | 26-46% |

The early time test was also in qualitative agreement with the predicted value. For a zero time of ignition, the equation gives a yield of about 6.5% with a range from 0-16.5%. The measured yield was 2%, which is in the lower part of the range, as would be expected since it ignited prior to the defined zero time.

LO₂/LH₂ CBGS. Three 25,000-lb tests were run, two of which ignited prior to the second propellant reaching the ground surface (the minimum time of interest for this case) and thus are not suitable for a quantitative scaling check. The results from the third test were consistent with the postulated scaling as shown below:

| | |
|------------------------|-------|
| Measured Yield | 13% |
| <u>Predicted Yield</u> | |
| Best Estimate | 22% |
| Range | 2-42% |

The two early ignition time tests are also qualitatively consistent with the predicted value. Both tests gave 4% yields, which compare with a predicted value of 18.1% and a range from 0-40%.

Ignition Time Scaling

Because the scope of Project PYRO did not include investigation of ignition probabilities, the resulting data are not considered suitable for quantitative estimates of credible ignition time or the manner in which ignition times scale. This is primarily because the results of any such analysis are more dependent on the assumptions involved in the analyses than on the data themselves. The inherent limitations and assumptions in using the PYRO results for this purpose are discussed below.

First of all it should be emphasized that the objective of Project PYRO was to determine the yield as a function of time and other controlling parameters, so that in the experimental design, time of ignition was selected to be a controllable parameter rather than a random variable. In essentially all cases (except for some of the high-velocity-impact tests) a controlled time ignition source was provided and efforts were made, insofar as practical, to eliminate other possible ignition sources. In the majority of the tests conducted, ignition did occur from the planned ignition source, and clearly these data are not very helpful in determining the probability of ignition.

The next major limitation in using the PYRO results to infer ignition probabilities, particularly as a function of scale (i.e., propellant weight), is that a decreasing number of tests were conducted as the propellant quantities increased. The reason this complicates the analysis is that when spurious ignitions were obtained on the initial tests for a given test mode and scale, efforts were made to identify their source, and changes were made in the experimental arrangement to try to minimize the spurious ignitions. In some cases these efforts were reasonably successful, so that the probability of obtaining a spurious ignition tended to decrease as the number of tests for a particular testing mode and scale increased. This combination of an experimental learning curve with regard to controlling ignition and a decreasing number of tests with increasing propellant weight rather automatically leads to an increasing percent of spurious ignitions with increasing

weight due solely to the experimental procedure used and not necessarily because of any inherent scale effect on ignition probability. Since the spurious ignitions generally occurred well before the planned ignition time, this in turn leads to lower average yields as the weight scale increases.

Another limitation in using the results from the spurious ignition tests for inferring ignition probability scaling (or yield scaling) is that many of the ignitions occurred so early in time that the resulting failure mode did not satisfy the basic criteria for the desired test case. In the CBM case, for example, the earliest time of ignition considered of interest was at the moment when the upper propellant first contacts the lower propellant. As noted earlier in all three 25,000-lb LO_2/LH_2 tests, ignition occurred well before this time, so that these tests could not be included in the standard CBM case. For the CBGS case, the major time regime of interest is after all the propellants are on the ground surface, and the earliest time of interest is when the top propellant reaches the ground surface. In a number of the spurious ignition tests, including two of the three 25,000-lb LO_2/LH_2 tests, ignition occurred prior to this time, so that these tests cannot be considered as part of the standard CBGS case.

The last major limitation in using the PYRO data for inferring ignition probabilities is that the test tankage and failure modes used in the experiments at the 200-, 1,000- and 25,000-lb scales were designed to create certain generalized types of propellant interactions (i.e., ways in which the propellants come together and mix) that could occur in failure of full-scale vehicles, but no attempt was made to directly model actual full-scale tankage with regard to strength, detailed failure mechanisms, or inherent ignition characteristics. Even between the various scales of testing in PYRO, no considerations were given to duplicating these characteristics. Thus any similarity between the basic characteristics which control ignition probability in full-scale accidents and those of PYRO would be fortuitous.

Section 5

REFERENCES

- 5-1. Kingery, C. N. and B. F. Pannill, Peak Overpressure vs Scaled Distance for TNT Surface Bursts (Hemispherical Charges), ERL Memorandum Report No. 1518, Ballistic Research Laboratories, April 1964 (AD 443 102) *
- 5-2. Defense Atomic Support Agency, Operation SNOW BALL Project Descriptions, Volume 1 (U), DASA Data Center Special Report 24-1, DASA 1516-1 (AD 441 974) *

* Reference curves from these documents are reproduced in Appendix F.

Section 6
THERMAL ENVIRONMENT

OBJECTIVES

The objective of the thermal portion of the study was to make measurements which enable "reasonable" bounds to be placed on, or which substantiate theoretical predictions of, the heat transfer to objects in and about the "fireball" created by the explosion of liquid propellant mixtures. Residual fires are not included.*

GENERAL ORGANIZATION

The thermal hazard portion of the study is located in four separate sections of the report. The contents of these sections are as follows:

- Section 6 of Volume 1 commences with a general discussion which describes the fireball characteristics, the nature of the heat transfer within the fireball, and the general experimental approach that has been taken. This is followed by a brief summary of the thermal instrumentation, and finally the results from the 25,000-lb and Titan tests are presented.
- Appendix C of Volume 1 consists of a detailed description of the thermal instruments, their mounting and location, and the errors of the associated measurements.
- Section 2 of Volume 2 contains the basic thermal data records. Data are given regarding the,
 - (1) Heat flux density into slabs immersed in the fireball for the 25,000-lb and Titan tests
 - (2) Radiant flux density within the fireball for the 25,000-lb tests
 - (3) Radiant flux density outside the fireball from the 25,000-lb and Titan tests
 - (4) Temperature of thermocouple probes selected from the 25,000-lb test data

* For thermal hazards of residual fires see, for example, Ref. 6-1.

Heat flux vs time at "large" propellant weight scales is then considered. A recommended scaling relationship for heat flux vs time is given, along with a listing of the experimental evidence upon which the relationship is based. The Titan I heat flux data are then compared with the LO₂/RP-1 bounding heat flux-time curve that has been scaled, using the recommended scaling, to the 100,000-lb level.

Finally, the radiant flux density measurements from outside the fireball are considered. The results of the 25,000-lb tests are presented only in terms of summary data plots indicating the data mean and range. A single measurement from the Titan I test is given.

Results of the 25,000-lb Tests

Gas Temperature from the Photo-Recording Pyrometer

A summary of the pyrometric gas temperature measurements for the 25,000-lb tests is presented in Fig. 6-1 for both the LO₂/RP-1 and the LO₂/LH₂ tests. (No equivalent Titan data are currently available.) The plots include a curve of the mean temperature and an indication of the highest and lowest measured temperature, where the plotted values are time averages over, in most cases, 100- and 200-msec time intervals for the LO₂/RP-1 and LO₂/LH₂ tests, respectively. The number of data curves from which the summary plots are constructed changes with time because the fireballs have a range of durations, and this number is indicated just beneath the plot. It should be remarked also that the temperature range of the pyrometer for these tests was such that temperatures below about 1900°K could not be measured. For two tests (Tests 279 and 282), the temperature during the fireball duration fell temporarily to levels at or somewhat below 1900°K, and for this time and these two tests, a value of 1900°K was used in obtaining the temperature average over the tests.

A most evident feature of the gas temperature values is their consistency from test to test. The variation appears to be somewhat larger for LO₂/LH₂ than for LO₂/RP-1 with - for instance - the greatest deviation of a single measurement from the mean of the measurements being about 300°K. There is a

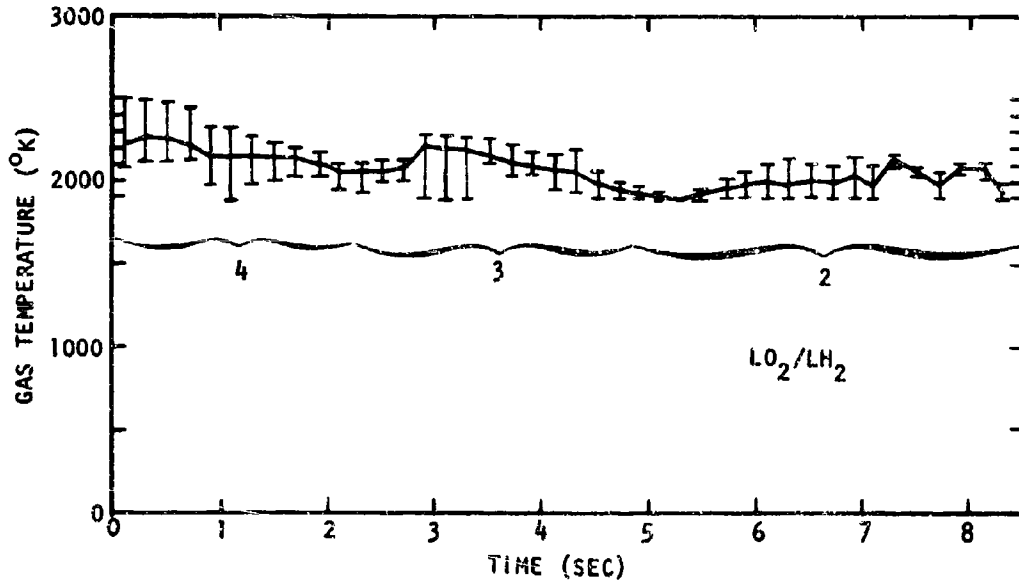
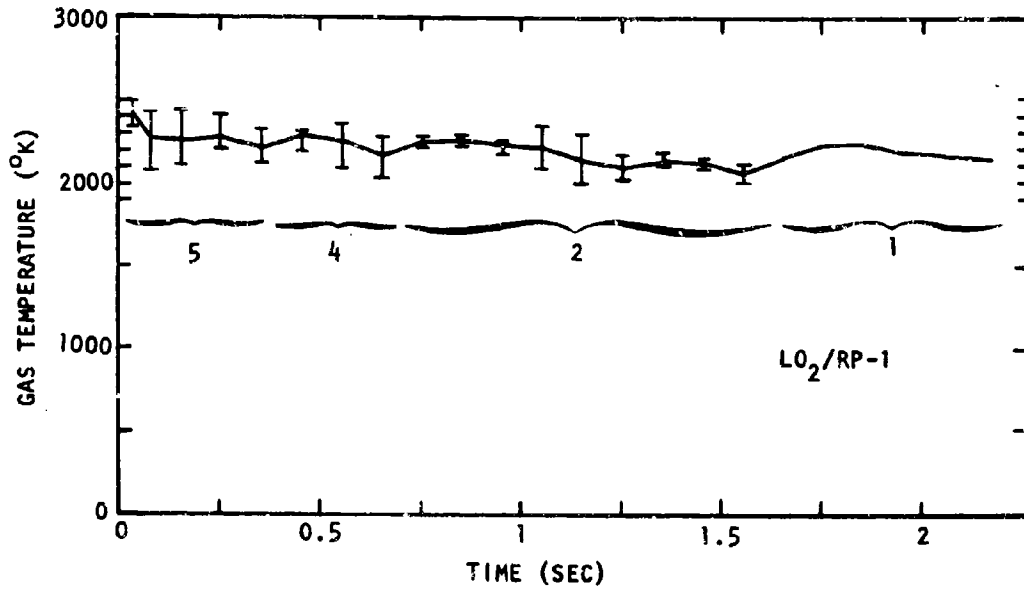


Fig. 6-1. Pyrometric Gas Temperature Measurement Summary for the 25,000-lb Tests

- Section 6 of Volume 3 consists of a summary of the heat flux density-time vs propellant weight prediction.

It should be acknowledged that certain of the thermal measurements were planned and conducted, and the data reduced, by the Sandia Corporation, and the Sandia instruments are designated as such in the description of instruments in Appendix C of Volume 1.

PREFATORY REMARKS

The expressions "heat flux density," "total heat flux density," "heat flux," and "flux density" are used interchangeably and represent the energy per unit area per unit time transported through a material surface due to both the radiant and convective modes of heat transfer. Similarly, the expressions "radiant flux density" and "radiant flux" represent the energy in radiant form per unit area per unit time.

GENERAL DISCUSSION

Characteristics of the Fireball

The size, duration and gross motion of the propellant fireball depends on the total quantity of propellants. During its growth and the early stages thereafter, the fireball from a propellant mixture on or near the ground surface is more or less hemispherical, reaching a maximum diameter that is approximately proportional to the cube root of the propellant weight. More specifically, an empirically derived expression relating the maximum diameter D in feet to the total propellant weight W in pounds is given by*

$$D = 9.56^{0.325} W \quad (6.1)$$

* Equation (6.1) along with Eq. (6.2) below, have been extracted from Ref. 6-2. The equations were obtained through the statistical analysis of data from either the literature or by reduction of photographic records from a total of 71 tests or incidents with the propellant combinations of $LO_2/ RP-1$ or LO_2/ LH_2 and which ranged in propellant weight from 10 to 250,000-lb.

notably short duration for the $\text{LO}_2/\text{RP-1}$ compared with both the LO_2/LH_2 gas temperature duration and, more importantly, with the measured $\text{LO}_2/\text{RP-1}$ heat flux durations that will be considered below. Substantial temperatures appear to have persisted within the fireball for significantly longer periods than could be measured by the remote temperature instruments, evidently due to obscuration by "cooler" gases and/or particulate matter along the surface of the fireball.

A feature of the temperature data that does not appear in Fig. 6-1 is the frequent occurrence of a comparatively high but abruptly decreasing temperature in the initial stage of fireball expansion; these temperature "pulses" are not resolved in Fig. 6-1 since their duration is generally a small fraction of the 100- and 200-msec time intervals of Fig. 6-1. A listing of the highest magnitude and the duration of the pulses is given in Table 6-2, where the duration is defined as the time from ignition for the temperature to decay to (and remain below) 2450°K ; a radiant flux density of 200 watt/cm^2 corresponds to a temperature of 2450°K for an emissivity of one. Also included in the table are maximum values of the radiant flux density during this time for an emissivity of 1.

While comparatively large temperatures or corresponding magnitudes of heat flux frequently occur, the damaging response of most structures or objects to these pulses is small compared with the response from the remaining or subsequent heat transfer.

An indication of the spatial variation of gas temperature over the fireball is given in Fig. 6-2, where the temperature over three separate regions of the fireball from Test 283 is given. A crude indication of the size of each region of measurement relative to the fireball dimensions and the location of each region is given in the upper right hand corner of the figure.

It can be seen that spatial differences of the temperature at given times are commonly of the order of 200°K , and it should be remarked that comparative examination of film coverage of the various 25,000-lb tests suggests that the fireball from Test 288 was relatively uniform. Temperature variations of this

where the estimated standard error of the diameter is 30 percent. Thus, diameters, for instance, for 1,000 and 1,000,000 lb are about 93 and 850 ft, respectively. It is emphasized, however, that Eq. (6.1) does not always indicate maximum fireball dimensions, in part because it was derived on the basis that "in those instances where the fireballs were markedly asymmetrical, attempts were made to estimate equivalent spherical diameters."* Moreover, in the tests from which Eq. (6.1) was obtained, there was a tendency for the spatial dispersal of propellants prior to their ignition to be less extensive than can practically occur, and comparatively extensive dispersal can lead to correspondingly large maximum dimensions; larger fireballs would tend to result from propellant spills, for instance, if the propellants spread sufficiently along the ground surface prior to their ignition. The Titan test (involving about 100,000 lb of LO₂/RP-1) provides an illustration, first, of the departure of the maximum dimensions that can occur from the diameter given by Eq. (6.1), and second, of still another mechanism of propellant dispersal. In this test, a fraction of the propellants was intentionally allowed to mix and subsequently ignite while within the confines of the missile tankage. However, the forces of the explosion evidently caused part of the remaining unmixed propellants to be displaced in such a manner that they did not mix and react until they had been substantially displaced laterally from the center of explosion. The maximum dimension of the resultant fireball was estimated to be from 800 to 1000 ft, while the diameter as given by Eq. (6.1) is approximately 400 ft.

Fireball durations, i.e., the period over which visible radiation exists, also increase approximately with the cube root of propellant weight as given by

$$\tau = 0.196 W^{0.348} \quad (6.2)$$

where the duration, τ , is in seconds for a weight in pounds, with a standard error of 84 percent; 1,000- and 1,000,000-lb durations are 2.2 and 24 sec,

* Ref. 6-2.

Table 6-2
 FIREBALL TEMPERATURE DURING INITIAL STAGE
 OF FIREBALL EXPANSION FROM PHOTO-RECORD PYROMETER

| PROPELLANT TYPE | TEST NO. | TIME FOR TEMPERATURE TO DECREASE TO 2450°K (msec) | MAXIMUM TEMPERATURE (°K) | MAXIMUM RADIANT FLUX FOR AN EMISSIVITY OF 1 (watts/cm ²) |
|----------------------------------|----------|---|--------------------------|--|
| LO ₂ /RP-1 | 275 | 30 | >2650 | >270 |
| | 278 | 25 | >2500 | >210 |
| | 282 | 15 | >2600 | >250 |
| | 284 | 10 | 2560 | 235 |
| | 285 | 10 | 2860 | 360 |
| LO ₂ /LH ₂ | 277 | 0 | 2380 | 175 |
| | 279 | 0 | 2320 | 160 |
| | 281 | 0 | 2250 | 140 |
| | 285 | 25 | 2900 | 285 |

respectively. For large enough propellant quantities, the duration is sufficient for the fireball to rise to heights of the order of a few fireball diameters, the rise occurring with accompanying modifications of the shape of the fireball, first to that resembling a sphere, and finally approaching, should it continue to persist, the characteristic toroidal geometry. Vertical motion is, for practical purposes, generally nonexistent at the 1,000-lb (or less) level, while significant heights are generally attained with quantities of 25,000 lb or more. A significant rise is accompanied also by the formation of a vertical and temporarily rising column, generally referred to as the stem, which extends from the lower region of the fireball to the ground surface. While this column may become heated due to its proximity to the fireball, it essentially does not consist of the products of explosion, but rather of gases and particulate matter that have been swept from regions lateral to the initial fireball.

Velocities that accompany fireball growth are at first supersonic but rapidly decay to subsonic levels. From film coverage of 25,000-lb tests, radial expansions of 60 ft (about one-half maximum dimension) typically occur in about 50 msec, giving an average velocity during that time of 1200 ft/sec. While the growth velocities thereafter rapidly subside, randomly directed local motion persists throughout the duration of the fireball.

Temperatures that prevail in the fireball are typically of the order of 2,500°K (~ 4,000°F), with spatial variations tending to occur particularly after the period of fireball growth. Unlike explosions of high explosives or "well-mixed" propellants, there is evidence suggesting that the process of fuel-oxidizer mixing and subsequent chemical reaction continues during and perhaps somewhat after fireball growth. Since the rate of chemical reaction in, and the consequent energy emitted from, an elemental region depends on the quantities of fuels and oxidants in the region that are in sufficient proximity to interact, and since the spatial distribution of these potential reactants can easily be nonuniform, there is no guarantee of thermodynamic equilibrium; and radiation from any such region is therefore not necessarily governed by the Planck radiation laws. With time, of course, uniformity throughout any given elemental

magnitude result in comparatively large spatial variations in heat flux, particularly since the radiant flux varies with the fourth power of the temperature. For an emissivity of 1, the radiant flux computed from the temperatures of about 2320 and 2520°K at 0.5 sec in Fig. 6-2, for example, are 160 and 220 watt/cm², respectively. Similarly, temperatures of 2090° and 2320°K at 1.3 sec correspond to flux densities of 103 and 160 watt/cm². In addition to spatial variations in heat flux of this magnitude, temporal variations (at a stationary point) of the same magnitude can be expected to occur in small fractions of a second, as evidenced, first, by the abrupt temperature changes in Fig. 6-2, and more vividly through high-speed film coverage of the apparent motion of high- or low-temperature regions about the fireball.

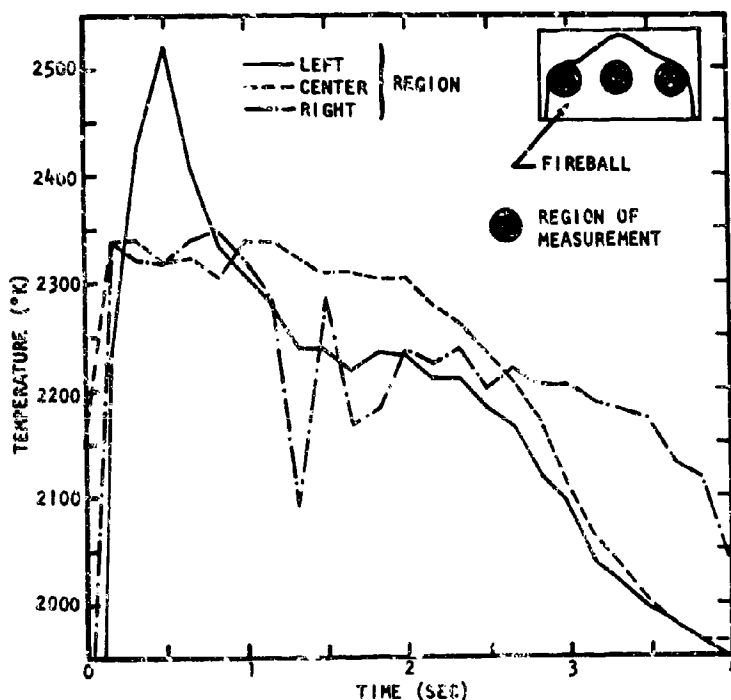


Fig. 6-2. Spatial Variation in Gas Temperature for Test 288

region will be approached. Therefore, it is possible to consider Planckian radiation and its associated laws. When laws such as Stefan's radiation law are applied, however, they require the uncertainty of the emissivity, an uncertainty stemming primarily from ignorance of the fireball constituents at a given instant.

Heat Transfer within the Fireball

The transfer of energy to material surfaces immersed in and moving relative to a "high-temperature" gas is effected through some combination of the mechanisms of radiation and forced convection. An object whose surface is everywhere convex or planar will be uniformly irradiated, provided the mean free photon distance, the mean distance that a photon travels through the fireball before being absorbed, is significantly less than the distance to either the edge of the fireball or some other object that can obstruct its irradiance. With the quantities of carbon that are present in the $LO_2/RP-1$ fireballs, this mean distance is evidently small compared with the dimensions of the fireball, so that for most circumstances there would be uniform irradiance. This is less clear for fireballs from the LO_2/LH_2 propellant combination.

For the $LO_2/RP-1$ propellant combination, particulate carbon is present in sufficient quantities to suggest that the emissivity of a layer of the fireball would approach that of carbon for a thickness that is small compared with the fireball diameter. If this is assumed, along with a gas temperature of $2,500^\circ K$, a radiant flux density of about 215 watts/cm^2 ($190 \text{ Btu/ft}^2\text{-sec}$) is obtained.

Convective transfer from ordinary gases at these temperatures tends to be small compared to the radiant transfer mentioned above except as flow velocities approach supersonic levels. As noted previously, the velocities that accompany the fireball growth commence at supersonic levels, but the growth approaches completion within tens or hundreds of milliseconds. Consequently, while convective transfer rates can be comparable to expected radiative rates during this time, the short duration renders their contribution negligible compared with the total transfer. However, high-speed motion does not cease with the completion of growth. Local and randomly directed "swirling" motion continues

Heat Flux Density

All heat flux density data from the 25,000-lb tests are presented in Figs. 2-1 through 2-28 of Section 2, Vol. 2. Included in the present section are discussions of the results and comparisons of the data with other data. More importantly, however, it is desirable to use the heat flux density data to obtain a "conservative yet realistic" bound to the heat flux that a structure or object immersed in the fireball can receive, and this section commences with the presentation and discussion of this upper bound.

Two complications occur in acquiring the upper bound. First, the flux instruments are not continuously in the most severe region of the fireball, and second, because the test design was based on blast rather than thermal considerations, there are some difficulties with regard to identifying thermal test conditions.

Regarding the first complication, the fireball parameters of potential importance to heat transfer, such as the gas temperature, are not generally uniform throughout the fireball. This is particularly evident from film coverage of the tests, and was illustrated for gas temperature specifically in Fig. 6-2. It would be expected, therefore, that a direct flux density measurement from a stationary instrument would approach or attain the maximum possible value only occasionally, that is, for some times for some tests. It is an observed characteristic of the measured flux density-time curves to exhibit periodic "surges" or maxima, evidently reflecting the variations in the gas parameters as the gas flows past the instrument, and statistically, some of these maxima will be, or will approach, the highest value possible at that time for the fireball in question. Provided there is a sufficiently large number of measurements per test and a sufficiently large group of tests having identical test conditions, the upper bound to the heat flux density for this set of test conditions will be revealed by superimposing the flux density pulses having the higher maxima on a single flux density-time graph; or more specifically, a smooth curve fitted to the highest maxima of this graph will represent an upper bound. This approach of estimating an upper bound, or a

throughout the duration of the fireball, with velocities, as photographically indicated, typically ranging from 100 to 400 ft/sec. An immersed object, therefore, will tend to receive "surges" of convectively transferred energy at levels that are not negligible compared with radiation transfer, and the object may experience several such surges from more or less random directions. It is again emphasized that this discussion pertains to ordinary gases.

There is a difference of potential importance between a gas and the so-called fireball "gas" in that the fireball contains particulate matter, a fact which can greatly affect energy transfer. To illustrate the possible extent of the effect, consider a flow of high-temperature gas within which are particles that have the same temperature and velocity as the gas. On approaching an object, the comparatively large momentum of the particles will tend to permit their impingement on the object, and during the time of contact, energy will be conducted from the particles to the object. As an example, a 500- μ carbon particle at 2,500°K has about 3 joules of internal energy that can be transferred to a room-temperature heat sink, and if the particle is assumed to temporarily deform on impact in such a way that a circular area of diameter equal to the particle is in contact for 0.01 sec, approximately 1 joule of energy will be transferred from the particle. The continual bombardment from the flow of a concentration of particles will, of course, result in a heat flux to the object, as illustrated in Table 6-1, where the heat flux density associated with ranges of particle size, particle concentration, and duration of contact are considered for a velocity of 200 ft/sec.

Table 6-1
ESTIMATES OF HEAT FLUX DENSITY (in watts/cm²) BY PARTICLE IMPINGEMENT

| | | DIAMETER (μ) | | | |
|--------------------------|-----|--|-----|--|-----|
| | | 50 | | 250 | |
| | | CONCENTRATION (particles/cm ³) | | CONCENTRATION (particles/cm ³) | |
| | | 10 ⁻² | 1 | 10 ⁻² | 1 |
| Duration of Contact (ms) | 0.1 | 0.01 | 1.2 | 0.5 | 50 |
| | 10 | 0.01 | 1.5 | 1.8 | 180 |

bounding curve, has been applied, and the results obtained from it, along with indications of the limitations associated with the approach, are presented below.

With this approach, it is essential that the data that are combined to obtain an upper bound are from tests which have identical test conditions. The discussion in the following paragraphs serves primarily to indicate how closely we are able to comply with this requirement. It will be seen that a practical choice of separating the tests into thermal test condition categories is made without complete technical justification for the choice. The discussion commences with a brief description of test conditions.

There were variations of the propellant test tankage, propellant ignition time, etc., which were introduced to determine their effect on blast wave hazards, and these will be referred to as blast wave test conditions. These conditions may or may not influence the thermal hazard, and thus, may or may not constitute a thermal test condition. There were three blast wave test conditions for the 25,000-lb tests. The first is the propellant type, and is specified by indicating either the $LO_2/RP-1$ or the LO_2/LH_2 combination. A second condition, which we will refer to as the propellant configuration, separates the tests on the basis of whether they were of the so-called confinement-by-the-missile condition (CBM) or the confinement-by-the-ground-surface condition (CBGS). For the CBM condition, the propellants were permitted to mix within the propellant tankage by the abrupt creation of an opening in the diaphragm that originally separated the two propellants. For the CBGS condition, the propellants were permitted to spill from the tankage and to spread (and mix) along the ground surface. For each of the several tests conducted for each of these conditions the propellants were permitted to mix for varying lengths of time prior to their ignition, and "ignition time" constitutes a third condition. In order to describe a test, then, it is necessary to specify the propellant type, propellant configuration, and the ignition time. The blast wave test conditions for each 25,000-lb test (for which there are heat flux data) are indicated in Table 6-3 for reference in the discussion to follow.

While it is difficult to obtain clearly realistic assumptions, the results of Table 6-1 suggest that energy transfer by particle bombardment can not easily be ignored. At the outset, the assumption that the contact of all of the impinging particles is temporary may be unrealistic, since the explosions are accompanied by a permanent deposition of particles, a case in which a greater fraction of the internal energy of a particle will be conducted to the object. It thus becomes important whether some permanent particle adherence occurs during the heating pulse or whether the deposition is confined to a later settling of cooled particles.

Experimental Approach

A completely general experimental approach would require measurement of all properties and variables of the fireball which are necessary to evaluate heat transfer to any object, and while ideal, it is prohibitive, aside from the difficulty of evaluation, in terms of the required instrumentation. Diminishing the scope of this approach by measuring only the most essential or dominant variables, such as the gas temperature, introduces an uncertainty in evaluating the heat flux density, of course, since assumptions about the fireball are required; a case in point is that of the effect of heat transfer through particle impingement mentioned above, a case which is difficult to evaluate realistically. However, an approximate gas temperature, a most critical variable, can be measured remotely, which permits selecting the fireball region to be measured, and thus enables a measurement of the highest apparent temperature region of the fireball. This is important because an upper bound of the heat flux density is sought. Indeed, remote gas temperature measurements of this kind have been made throughout the program by the Sandia Corporation, using a photographic recording pyrometer.

A less general though more direct approach is that of simply measuring the heat flux into various immersed objects, the distinct advantage being that no assumptions regarding the fireball are required. The most serious disadvantage of measurements of this sort within the fireball is that the instrument is not always in the most severe environment, the environment of primary interest.

Table 6-3
BLAST WAVE TEST CONDITIONS FOR THE 25,000-LB TESTS

| TEST NUMBER | PROPELLANT TYPE | PROPELLANT CONFIGURATION | IGNITION TIME (sec) | |
|-------------------|----------------------------------|--------------------------|---------------------|------|
| 275 278 | LO ₂ /RP-1 | CBM | 0.51 | |
| | | | 0.53 | |
| 284 285 | LO ₂ /RP-1 | CBGS | 0.00 | |
| | | | 0.46 | |
| 279 | LO ₂ /LH ₂ | CBM | 0.03 | |
| 288 289 290 | | | CBGS | 0.36 |
| | | | | 0.17 |
| | 0.11 | | | |

Since the approach used to construct the bounding curves requires a number of individual flux-time curves for each test condition, it is desirable to combine all tests whose blast wave test conditions do not differ in ways that are of importance to heat transfer. To the extent that it is practical then, we will consider the effect of blast wave test conditions on the heat flux density and on the heating duration. Commencing with the effect on heat flux, the photo-recording pyrometer measurements were first considered, and it was found that the gas temperature in the fireball region where the temperature was maximum does not, for practical purposes, depend on blast wave test conditions other than propellant type. (The consistency of gas temperature for all conditions for a given propellant was indicated in Fig. 6-1.) Gas temperature, moreover, is expected to be the most critical parameter; that is, it will have the greatest influence on the magnitude of the heat flux.

The test conditions of propellant configuration and ignition time can affect the flow velocity (and pattern) and so, in turn, can affect the heat flux. For "ordinary" gases, however, estimates of the flux contribution from convective heat transfer at the most extreme flow velocities (except during the

early fireball growth stages) are small compared to the radiant transfer. The possible presence of particulate matter in the explosive products of the $LO_2/RP-1$ tests, however, makes it mandatory that test conditions influencing flow velocity be considered as a possible thermal test condition. Since particulate matter is present, any condition which affects either the flow velocity or particle concentration may constitute a thermal test condition. The only practical means of attempting to establish the dependence or independence of the heat flux on particulate concentration is through analysis of the heat flux density data themselves. The assortment of test conditions that existed for the 25,000-lb tests, as indicated in Table 6-3, is not readily amenable to analysis, and we resort to the following method. We first compared the mean value of the time average of each flux trace from Test 285 with the corresponding mean of the combined test data from Tests 275 and 278, where the test conditions of these two test groups differ only in propellant configuration. It was concluded that there is no reason to believe that the two mean values are different. That is, there is no reason to believe the flux depends on propellant configuration, and the probability that this analysis has failed to reveal a difference that really exists is about 0.01. Next, with the assumption that this conclusion also applies to the LO_2/LH_2 propellant combination, the analysis of the flux data from several tests may be used to determine the effect of propellant type on the time average of the flux. Specifically, the mean of the time average of each flux trace from Tests 275, 278 and 285 was compared with the corresponding mean from Tests 286 and 289, and it is concluded that the flux does not depend on propellant type, and the probability that this part of the analysis has failed to reveal an existing difference is about 0.30. Since the ignition time for Test 289 is substantially less than that of the other four tests, as can be seen in Table 6-3, the analysis was also performed with the data from Test 289 excluded, and the conclusion was the same. If there is no significant effect of propellant type on heat flux, as the analysis suggests, the presence of particulate matter evidently has no pronounced effect; the flow velocity, then, should not have a pronounced effect; and finally, there should be no pronounced effect of either propellant configuration (which was already concluded from the crude data analysis above) or ignition time. Regarding

Thus, a statistical sampling of measurements must be obtained, and several direct measurements of heat flux density were made per test at locations distributed throughout the fireball.

Two additional types of measurements were made that pertain to heat transfer within the fireball, in particular, the radiant flux density and the thermocouple probe measurement, although there is a tendency to regard them as secondary measurements with respect to the two measurements mentioned above, in part because of certain experimental uncertainties associated with these measurements. The associated inaccuracies do not necessarily exist; rather, it is difficult without further, and perhaps unwarranted, effort to firmly establish that they are negligible or to determine a suitable correction. The problems are discussed in the instrument and error analysis sections, although, in summary, it can be said that for most circumstances, the radiant flux density errors are not expected to be "excessive" nor are those of the thermocouple probe, except as they approach their melting temperature.

These two measurements are also considered secondary in that they cannot satisfactorily accomplish by themselves the primary objectives of the thermal program; rather, they tend to support (or depreciate) the heat flux density and pyrometric gas temperature measurements discussed above. It should be recognized, however, that supporting measurements are relatively important in light of the ease with which a substantial error can occur with any one type of measurement.

In summary, the experimental approach intended to accomplish the more important objective of determining a bound to the heat flux density within the fireball consists of a combination of the general and the direct approach, supplemented by supporting measurements.

SUMMARY OF THERMAL INSTRUMENTATION

This summary of instrumentation permits comprehensive reading of the presentation of results without requiring reference to the detailed description given in Appendix C of this volume.

The heat flux density is measured near the surface of various slabs located within the fireball. The dimensions of the slabs are of the order of 1.5 inches in thickness (normal to their exposed surface) and range from 1 to 3 inches laterally; over the heating duration, however, the slabs are thermally representative of semi-infinite slabs, that is, they effectively occupy half of a space. The heat flux density is evaluated by measuring the temperature with time just beneath the exposed surface of the slab and applying this temperature data to the computation of the heat flux density through the following relation (or its equivalent):

$$q(t) = K' \int_0^t \frac{\frac{\partial T_s(\tau)}{\partial \tau}}{\sqrt{t - \tau}} d\tau \quad (6.3)$$

where q is the heat flux density; $K' = (K\rho c/\pi)^{1/2}$; K , ρ , and c are the conductivity, density, and specific heat, respectively, of the slabs; T_s is the slab surface temperature; and t is time. The slab temperature is measured by means of a thermocouple junction embedded just beneath the slab surface.

Radiant flux density is measured with a "Gardon" type radiometer. The energy-sensitive element of the radiometer consists of a circular metallic foil whose circumferential edge is joined to a heat sink. Irradiance of the foil induces a temperature differential between the center and edge of the foil that is proportional to the incident radiant flux density. The temperature differential is measured with a differential thermocouple whose output, then, is a measure of radiant flux density.

Heat flux density measurements were obtained at instrument stations H, S, and P, two each at stations H and S and five at station P, and up to four radiant flux density measurements were obtained at station H. Station H is approximately 1 ft from the test pad center along gauge line A,* and about 52 in. above the

* Radial gauge lines designated gauge lines A, B and C are 120-deg from each other.

ignition time, a crude analysis was performed with the data from Tests 288 and 289, which differ only in ignition time, and it indicated that the average heat flux does increase with increasing ignition time, in contradiction to the conclusion above. In searching for a way to account for the contradiction, it was noticed that the pyrometric gas temperature for Test 288 was somewhat higher during the time of interest than for the other LO_2/LH_2 tests, although there are no pyrometric data available from Test 289 with which to make a direct comparison. If the abnormally high temperature for Test 288 was the cause of the influence of ignition time on average heat flux, this tends to detract from the first conclusion above, namely, that the gas temperature is, for practical purposes, independent of these test conditions. This conflicting result illustrates the limitations of separating the tests into distinct test condition categories. In the case above, for purposes of estimating upper bounds of the heat flux, we have chosen to disregard propellant configuration and ignition time as test conditions, although we were unable to establish a firm basis for the choice.

In order to consider the effect of test conditions on heating duration, the time from propellant ignition for the fireball to lift from the ground surface was obtained from film coverage of the tests, this time being a measure of the potential heating duration at the location of the instruments. These durations are listed in Table 6-4.* In this case, there appears to be a decidedly longer duration for $LO/ RP-1$ tests than for LO_2/LH_2 tests, and analysis (using all data given) supports this. The suggestion from a comparison of Tests 284 and 285 that duration decreases with increasing ignition time is offset by the opposite trend suggested by a corresponding comparison of Tests 288 and 290. (It should be noted that the duration of 3.4 sec for Test 285 is particularly uncertain.) There appears to be no reasonable means to ascertain the effect of propellant configuration. As a practical choice, propellant type is treated as the only test condition affecting heating duration (or heat flux magnitude), and the flux data will be combined on this basis.

* The details of these durations are described in a later discussion.

Table 6-4
HEATING DURATIONS FROM THE 25,000-LB TESTS

| PROPELLANT TYPE | TEST NUMBER | HEATING DURATION (sec) |
|----------------------------------|---|------------------------|
| LO ₂ /RP-1 | 275 | 4.7 |
| | 278 | 4.0 |
| | 282 | 5.2 |
| | 284 | 6.8 |
| | 285 | 3.4 |
| | LO ₂ /RP-1 Average | 4.8 |
| LO ₂ /LH ₂ | 281 | 3.7 |
| | 288 | 3.8 |
| | 290 | 3.5 |
| | LO ₂ /LH ₂ Average | 3.7 |

Heat flux density maxima plots are presented for LO₂/RP-1 and LO₂/LH₂ tests in Figs. 6-3 and 6-4, respectively. Each flux density "pulse" is labeled with the letter H, S, or P to indicate the instrument station (as designated in Appendix C) from which it was obtained, and the test number is identified by the type of curve as indicated in the legend. The numbers "1" through "5" adjacent to the station letter "P" on Fig. 6-4 designate the instrument position at that station. Bounding curves, or curves which are estimates of the upper bound, are also indicated in the figures.

Regarding these graphs, a high concentration of maxima near the bounding curve tends to support the firmness of the curve, and, of course, suggests that it would not be unusual to obtain values that approach the curve in a subsequent test. A reasonably large concentration near the curve is in fact required for the construction of a bounding curve to be a sensible endeavor. This requirement is clearly satisfied for the LO₂/RP-1 test condition. Examination of Fig. 6-3 indicates, for instance, that the absence of any measurement would not require marked revision of the bounding curve, perhaps the worst case being a minor modification in the 3- to 5-sec region associated with the

ground surface. The two slabs of station M are adjacent and arranged so that the flow of gas along each is similar. Station S, consisting of two slabs, is located 13 ft above the ground surface and as near to the center of the explosion as practical. In Tests 275 through 285, it was located at a ground distance of 23 ft along gauge line A, and thereafter was 32 ft from the test pad center at a point approximately intersecting gauge lines A and B. Station P, installed after Test 285, consists of a series of five instrumented slabs (designated as being at positions "1" through "5") distributed at 60-deg intervals along the circumference of a 12-in.-diameter, vertically oriented pipe. The station is 11 ft above the ground surface and is part of the support structure of station S.

The primary gas temperature measurement was obtained from a photographic recording pyrometer. This measurement technique consists of matching, over a portion of the visible spectrum, the intensity of an image of a region of the fireball with the intensity of a similar image of one of several tungsten filaments, each of which is at a known and preset temperature. Since the intensity from the tungsten in the optical spectrum approximates that of a blackbody, the measured fireball temperature is near the blackbody equivalent temperature. It was the policy to measure the fireball region having the highest intensity or temperature. The pyrometer was located about 450 ft from the test pad along gauge line A.

The secondary gas temperature measurement was obtained with a thermocouple probe, which consists of a metallicly sheathed thermocouple junction, with the thermocouple leads insulated from the sheath except at the junction. One or more thermocouple probes per test were mounted on the Sandia instrument station, located 13 ft above the ground surface about 30 ft from the test pad center at a point approximately intersecting gauge lines A and C.

Radiant flux density was also measured at locations external to the fireball by means of the Gardon-type radiometer described above. Generally, three radiometers were used per test, two at the same distance (335 ft) from ground zero along radial gauge lines (A and B) 120 deg from each other, with the third

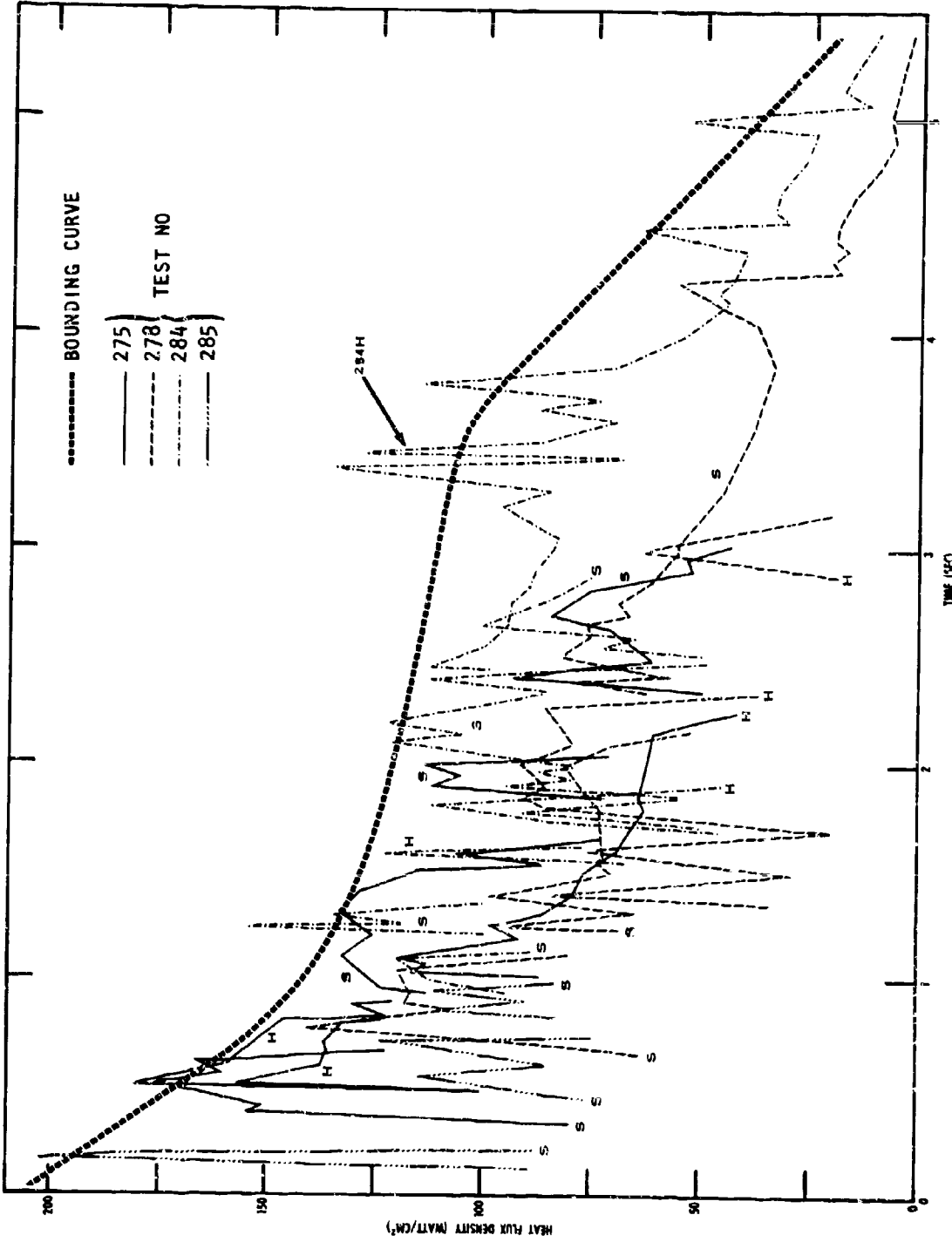


Fig. 6-3. Heat Flux Density Pulses from the 25,000-lb LO₂/RP-1 Test

more remotely located (600 ft) along gauge line A. These distances are such that with the existing field-of-view of the instruments, radiant energy originating in any region of the fireball prior to its rise is not restricted from falling incident on the sensitive foil of the instrument.

PRESENTATION OF RESULTS

General Organization

Results of the primary measurements of slab heat flux density and pyrometric gas temperature are presented first. This commences with a graphical summary of the pyrometric temperature that shows the range and mean value of the measurements for each propellant type ($LO_2/RP-1$ and LO_2/LH_2) for the 25,000-lb tests. (No Titan I data are currently available.) In addition, a graph is presented which indicates the spatial variation of temperature over the fireball from a single test.

The results of the heat flux density measurements are presented next, including curves which represent the upper bound of this parameter (bounding curves) and curves which represent values of this parameter that are suitable to anticipated applications (recommended curves). Indications of the basis and limitations of both the bounding and recommended curves are also given. The correspondence between the heat flux density measurements and the pyrometric gas temperature measurements is given next by comparing the radiant flux density computed from the gas temperature measurements with the heat flux density measurements.

The secondary measurements from the 25,000-lb tests are then considered, commencing with illustrations of the degree that the radiant flux-density measurements within the fireball support the primary measurements (the corresponding measurements for the Titan I test were unsuccessful). This is followed by similar illustrations of support from the temperature measurements obtained from the thermocouple probes (no equivalent Titan I data are currently available).

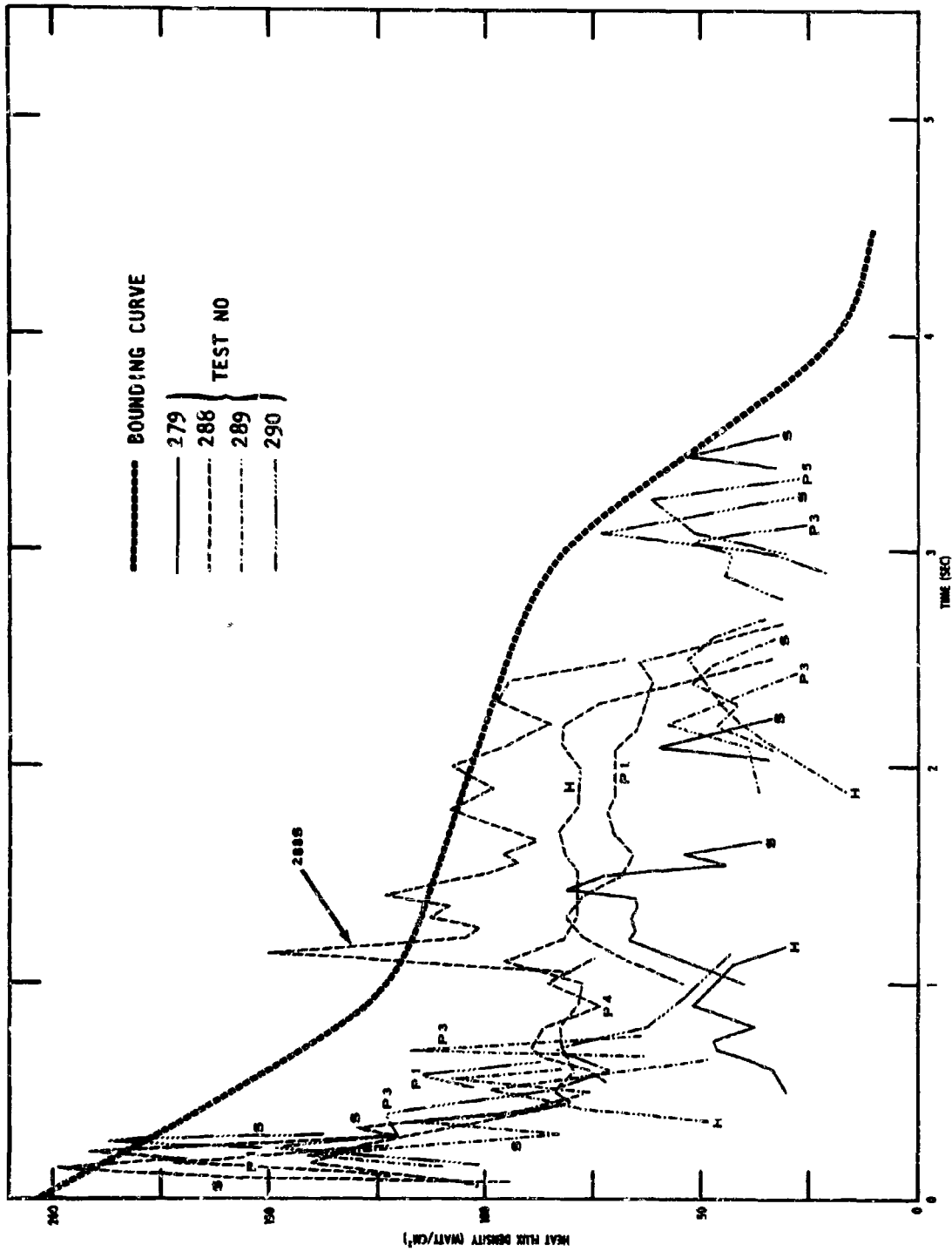


Fig. 6-4. Heat Flux Density Pulses from the 25,000-lb LO₂/LH₂ Tests

elimination of the pulse labeled "284H." For the LO_2/LH_2 plots, the distribution of maxima does not as firmly support the position of the curve. It can be seen on Fig. 6-4 that the elimination of the particular pulse labeled "288S," for example, would result in a substantial modification of the curve and would reduce the associated accumulative flux by approximately 10 percent. With the statistical sampling at this limited level, it is not clear if this particular measurement is unusual or reasonably typical, and the position of the bounding curve is evidently somewhat less certain than the previous $LO_2/RP-1$ case. It would be expected that the addition of further measurements would more substantially alter the LO_2/LH_2 curve than that of the $LO_2/RP-1$. The LO_2/LH_2 curve is included, however, since it represents the best (and evidently a reasonable) approximation to the bound for this test condition that can be made with the available data.

As noted above, the flux density associated with the bounding curves is indicative of the energy that an immersed object can receive and is, for most applications, a reasonably conservative condition to impose, both in terms of the magnitudes and, particularly, in terms of the length of time that such magnitudes would be sustained. No object tested, for instance, received a cumulative flux density in excess of 60 percent of that associated with the bounding curve. Since the bounding curve is evidently somewhat conservative, it is desirable both to investigate the degree of conservatism, and to provide a curve which is more suitable to anticipated applications. Both of these require statistical investigations and several associated points should be emphasized. The first is, simply, that the quantity of data is sufficiently limited that the analysis will provide only a reasonable approximation to the numerical results. Secondly, as noted above, an assumption has been made with regard to what constitutes a test condition, so that some of the data scatter is probably attributable to unrecognized test conditions. Finally, it is not clear how extensive would be the variation of heat transfer with test condition outside the range that has been tested; for instance, failures leading to more thoroughly mixed propellants at ignition may, in turn, lead to somewhat higher temperatures and heat transfer rates. Thus, the restricted range of test conditions over which these data were obtained should be considered in any application.

The conservatism of the bounding curve is illustrated in Table 6-5, which lists the ratio of the energy accumulated in 5.4 sec at each instrument station to that of the bounding curve for the same time, the latter being 590 and 420 watt-sec/cm² for the LO₂/RP-1 and LO₂/LH₂ curves, respectively. Also included in the table are the magnitudes of the cumulative flux density for 5.4 sec. The measured cumulative flux density can be seen to range from 9 to 60 percent of that associated with the bounding curve. If one assumes that the measured values conform to a Gaussian distribution, the probability of obtaining, for instance, 75 percent of the cumulative bounding flux is 0.02 and 0.002 for LO₂/RP-1 and LO₂/LH₂, respectively, and the corresponding figures for 65 percent are 0.07 and 0.02.

In obtaining a curve which is more suitable to application (than the bounding curve), a difficulty arises in that a recommended curve depends on the particular application (e.g., on the degree of conservatism the application calls for, on the materials and geometry that the structure whose response to the environment is desired, and so on). It is not possible then to provide a curve that is applicable to all situations, and one is given which is suitable to many anticipated applications. The criteria upon which this curve, hereafter referred to as the recommended curve, is based are discussed below.

The first criterion is that the cumulative flux density associated with the recommended curve, that is, the area under the recommended flux density-time plot, is such that the probability of exceeding this cumulative flux density is 0.01. With this criterion, analysis of the cumulative flux distributions in Table 6-5 (again assuming a Gaussian distribution) indicates that the cumulative flux densities of the recommended curve are approximately 450 and 300 watt-sec/cm² for LO₂/RP-1 and LO₂/LH₂, respectively.

Several difficulties arise when consideration is subsequently given to time distributions of flux density which are appropriate and consistent with these cumulative flux values. It is typical for the measured flux density surges to approach and temporarily remain near the bounding curve and for the heating activity during the time periods separating these surges to be comparatively moderate or negligible. (This is perhaps most easily observed by

Table 6-5
 CUMULATIVE FLUX DENSITY FOR 25,000-LB TESTS

| PROPELLANT TYPE | TEST NO. | INSTRUMENT STATION | | | | | |
|----------------------------------|----------|---|----------------------------|---|----------------------------|---|----------------------------|
| | | H | | S | | P | |
| | | CUMULATIVE FLUX (watt-sec/cm ²) | FRACTION OF BOUNDING CURVE | CUMULATIVE FLUX (watt-sec/cm ²) | FRACTION OF BOUNDING CURVE | CUMULATIVE FLUX (watt-sec/cm ²) | FRACTION OF BOUNDING CURVE |
| LO ₂ /RP-1 | 275 | 249 | 0.44 | 334 | 0.60 | - | - |
| | 278 | 165 | 0.29 | 280 | 0.50 | - | - |
| | 284 | 304 | 0.54 | 265* | 0.47* | - | - |
| | 205 | 49 | 0.09 | 140 | 0.25 | - | - |
| LO ₂ /LH ₂ | 279 | 89 | 0.20 | 163 | 0.37 | - | - |
| | 288 | 234 | 0.53 | 259 | 0.58 | 242 | 0.54 |
| | 289 | 135 | 0.30 | 145 | 0.33 | 116 | 0.26 |
| | 290 | 98* | 0.22* | 139 | 0.31 | 177 | 0.40 |

* The cumulative flux values for Station S, Test 284 and for Station H, Test 290 are to a time of 2.8 and 2.6 sec, respectively, and these two values were not used in the analyses.

scanning the individual flux-data curves in Figs. 2-1 through 2-28 given in Section 2, Vol. 2.) This suggests that the recommended curves should be superimposed on the corresponding bounding curves and that the reduced cumulative flux values of 450 and 300 watt-sec/cm² be obtained by imposing flux voids (or more moderate flux values) over one or more time periods, and the remaining question, in that case, would be in regard to a suitable selection of voids and their distribution. No evident pattern in this distribution is suggested by examination of the individual flux-time measurements. However, it is not uncommon for the flux to remain uninterruptedly near the bounding curve for large fractions of the heating duration. This is illustrated by the example data presented in Figs. 6-5 and 6-6. The flux shown in Fig. 6-5 from Test 288, for instance, remains near the bounding curve from about 1 to 2.5 sec, and similarly for Test 284 on Fig. 6-6 from about 2.5 to 5.5 sec. It is important to recognize that most of the energy transfer for these examples is associated with a single pulse, that the flux during these pulses is near the bounding curve, and that this is evidently not a rare event. This coupled with the factors previously mentioned suggests that the recommended curves should be as follows: That they should uninterruptedly superimpose the bounding curves and that their cumulative flux density should be reduced from that of the bounding curves to approximately 450 and 300 watt-sec/cm² for LO /RP-1 and LO /LH , respectively. Recommended curves as such are presented in Figs. 6-7 and 6-8, where the deletion of the cumulative flux density from that of the bounding curve is conservatively located at the late end of the heating period.

For some applications, the recommended curves may be slightly conservative, and the stringency may be reduced, for instance, by modifying from 0.01 the probability of exceeding the cumulative flux-density. Other possible means may be justified and suitable under given circumstances, as described in the following example. For purposes of illustration, the measured flux density for Test 275 is given in Fig. 6-9, and it can be seen that the flux is near the bounding curve from about 0.5 to 3 sec, although there is periodic but short-lived relief in the flux level during this time (at about 0.8, 1.75, and 2.1 sec). Intermittent relief such as this can significantly affect the response of some structures, and such flux density-time patterns are not uncommon.

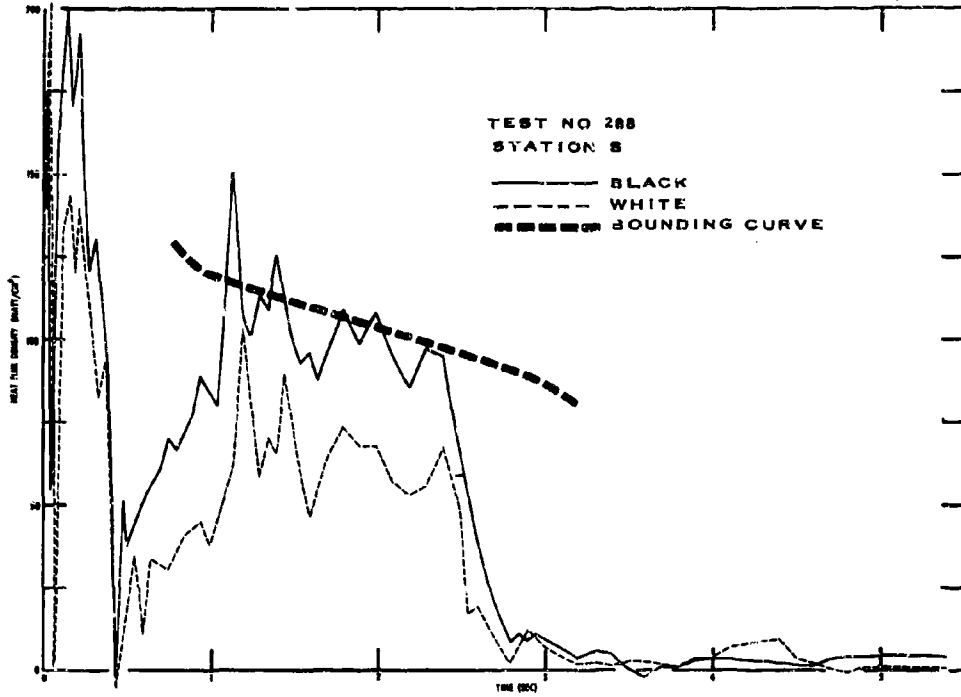


Fig. 6-5. Heat Flux Density at Station S from Test 288

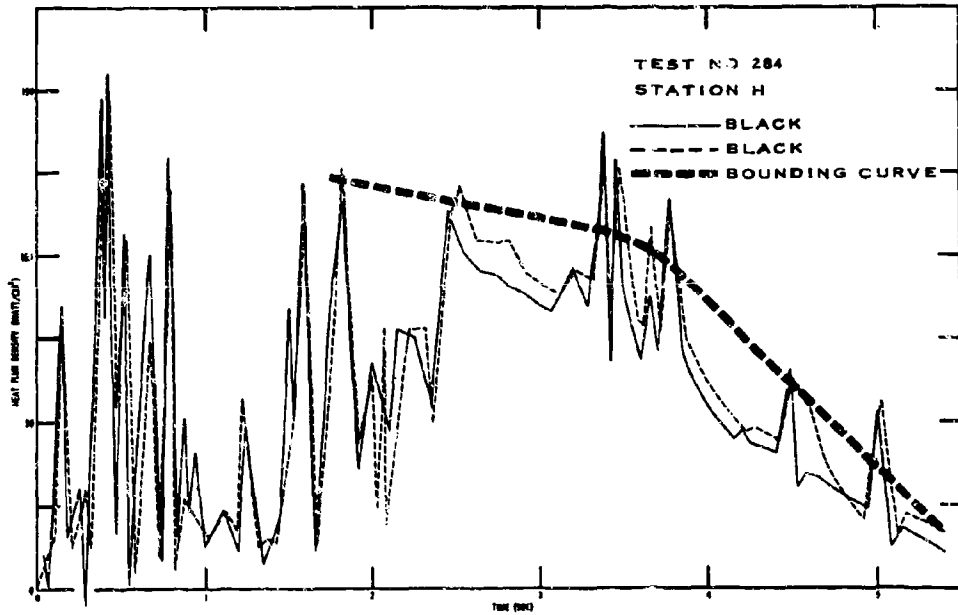


Fig. 6-6. Heat Flux Density at Station H from Test 284

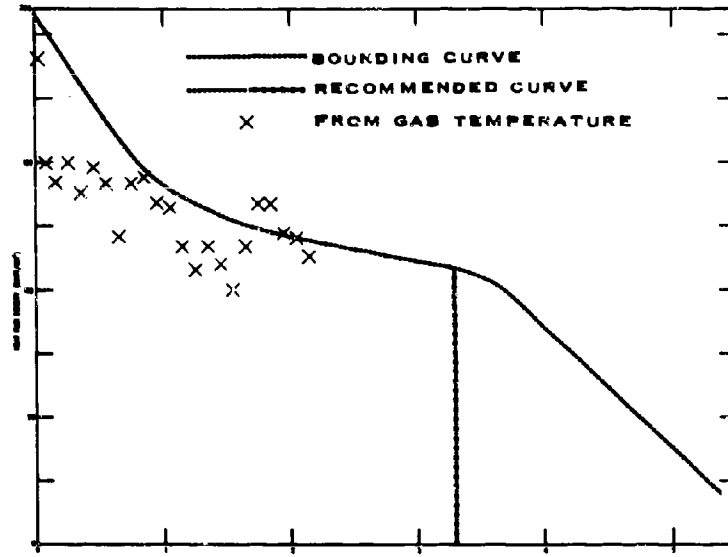


Fig. 6-7. Bounding and Recommended Heat Flux Density Curves for 25,000-lb LO₂/RP-1 Tests

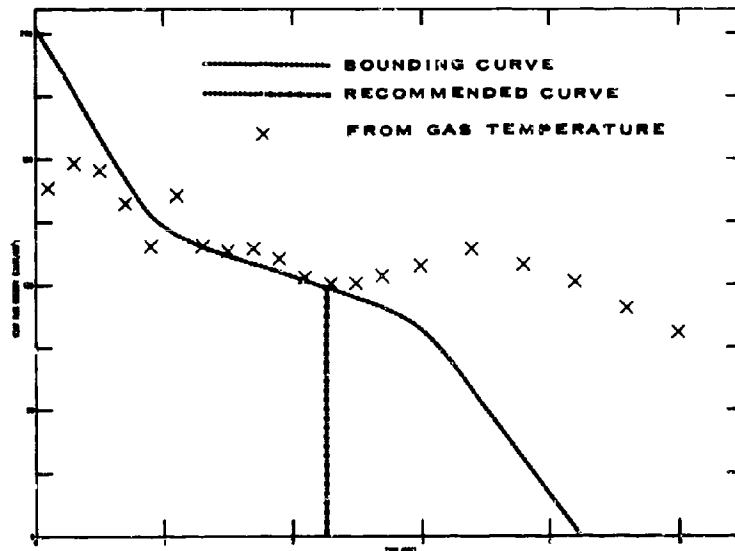


Fig. 6-8. Bounding and Recommended Heat Flux Density Curves for 25,000-lb LO₂/LH₂ Tests

This relief, for instance, can afford an opportunity for energy to be conducted through the structure from high- to low-temperature regions, thus reducing the maximum temperature that the high-temperature regions of concern will sustain compared with that for an uninterrupted heat pulse having the same energy. If, in applying the recommended curve, the structural response is found to reach, but not greatly exceed, damaging proportions, one may be justified in introducing an appropriate region of reduced flux. Justification of such a modification would, of course, require statistical analysis of the durations, magnitudes and time distributions of the energy pulses, and such an analysis is not given here.

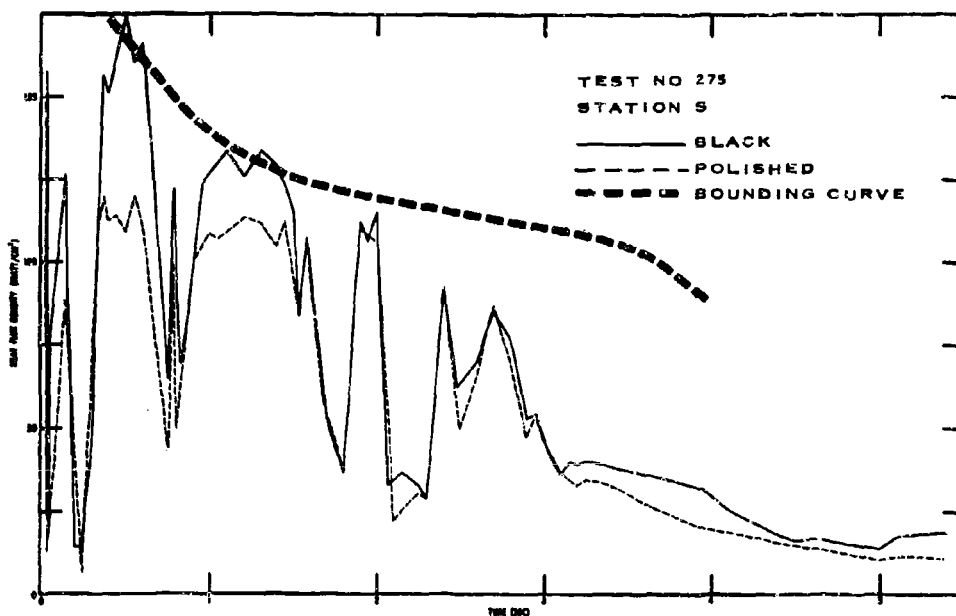


Fig. 6-9. Heat Flux Density at Station S from Test 275

Information computed from the pyrometric gas temperature measurements are included on Figs. 6-7 and 6-8 in order that the rather important comparison

can be made between these measurements and the bounding curve. It has been previously mentioned that the gas temperature measurements are of the fireball region having the highest apparent temperature. It has also been implied above that the bounding flux curve approaches the flux that would be sustained in the region(s) of highest temperature. Thus, there should be a correspondence between (not necessarily an equality of) the bounding flux curve and the flux implied by the gas temperature measurement. To this end, the radiant flux density values were computed from the gas temperature (on the assumption of a conservative emissivity of unity) and the results plotted (X's) on Figs. 6-7 and 6-8. The correspondence is remarkable. Consider first the LO_2/LH_2 case in Fig. 6-8. During the early fireball stages, when the gas flow velocities can be sufficient for significant convective transfer to occur, it is reasonable to expect that the total heat flux measurements will exceed the remote measurement implications, since the latter account for radiative transfer alone, and Fig. 6-8 is compatible with this notion. As the more extreme velocities subside, it is reasonable to expect that the two curves will converge, with the radiative mode of energy transfer predominating; and the curves are also consistent with this supposition, at least to approximately 2.5 sec, where the two values commence to diverge, the "pyrometric" flux thereafter exceeding the directly measured values. This divergence is accounted for through film coverage of these tests. In particular, it is at this time that the fireball lifts from the ground surface or from the direct heat flux instruments. For the $LO_2/RP-1$ data given in Fig. 6-7, the pyrometric gas temperature measurements are restricted to about 2 sec so that a comparison is only possible during this time.* It can be seen that the results are consistent with expectations in the same manner as described for the LO_2/LH_2 case.

The results of this comparison are rather significant in that two totally independent means of evaluating heat transfer indicate similar magnitudes in the region where a similarity should exist. Moreover, in other regions, the comparative magnitudes of the two measurements are qualitatively correct.

* This apparent difference in duration was discussed above in the section "Gas Temperature from the Photo-Recording Pyrometer."

A common feature of the heat flux data that is not indicated either in the data figures presented in this section or in those presented in Section 2 of Vol. 2 is the presence of a comparatively high-magnitude but very short-lived pulse at the beginning of the heating period. When this pulse occurs, it commences with a magnitude in the vicinity of 400 watts/cm² and decays to below 200 watts/cm² within, for the 25,000-lb tests, about 20 msec. It can be recalled that pulses of the same magnitude and duration were inferred from the pyrometric gas temperature measurements. The pulse was present in the heat flux measurements for Tests 278, 285, and 288; for the pyrometric measurements, it was present for Tests 275, 278, 282, 284, 285, and 288. A more detailed comparison will not be made for the following reason. In computing the heat flux from the slab temperature data, two comparatively large errors occur in the first few computational time intervals (that is, over the first 10 or 20 msec), and these errors become increasingly large as the time approaches the first (or earliest) computational interval. One of the errors is systematic and correctable, but a substantial effort is required. Since the pulse is of such small duration, the effort appeared to be clearly unjustified. It should be noted that the pulse was also present in the heat flux data from the Titan I Test, although its duration was approximately 50 msec.

One further characteristic of the heat flux density data should be mentioned. The flux density data from instrument stations S and H given in Section 2 of Volume 2 are presented in pairs, one pair for each station, corresponding to the adjacent slab pairs of those stations. Usually the exposed surface of one of each pair was coated with a black deposit, and the companion slab was either coated with a "white" deposit or the surface was polished. The comparative data trends for these companion slabs with dissimilar radiation absorption properties (as given approximately in Appendix C) are as follows. For the LO₂/LH₂ data, the ratios of the energy of the black slab to that of the white range from just over 1 to about 1.5, with the ratio for a given slab pair remaining approximately the same throughout the heating duration. For the LO₂/RP-1 tests, the data trends are somewhat different, with two patterns appearing. For some cases, the energy into each slab is essentially the same, while for others, the energy into the black slab is initially substantially

larger than that into the white, but they become equal by about 1.5 sec. (See, for example, the data at Station S for Test 275 on Fig. 6-9 above and Test 284 on Fig. 2-6 of Section 2, Vol. 2.) This combination of data trends for both propellant types suggests that modification of the radiation-absorbing properties of the slabs for LO₂/RP-1 tests through deposition of particulate carbon - always found in posttest examination and at thicknesses ranging to about 0.03 or 0.04 in. - occurs during the heating duration, and the deposition is, in some instances, immediate. It appears advisable, therefore, to use radiation absorptance values near unity for structural surfaces immersed in LO₂/RP-1 fireballs regardless of their initial value.

One of the primary reasons for the installation of instrument Station P (heat flux density measurements at locations distributed around the circumference of a vertically oriented cylinder) was to reveal any consistent, significant differences in the heat transfer at different locations on the surface of an immersed object, with particular reference to the orientation and location of the instrument with respect to the gas flow direction of the initially expanding fireball. Data were obtained at this station from three 25,000-lb tests, and no marked trends with respect to instrument location were obtained. (Individual heat flux density records from these tests for this station are given in Figs. 2-15 through 2-17, 2-20 through 2-22, and 2-26 through 2-28 of Section 2, Vol. 2, for Tests 288, 289, and 290, respectively.)

Radiant Flux Density within the Fireball

Radiometer data within the fireball are presented in Figs. 6-10 through 6-12 for the 25,000-lb tests. (No equivalent Titan data were obtained.) These data are presented primarily to indicate the degree that they support the total heat flux density and gas temperature measurements above; and since the radiometers were always mounted near a slab heat-flux meter, as described in Appendix C, the radiant flux data are plotted with the heat flux curve that is evaluated from the adjacent black-coated slab.

The variation among the radiometer data is somewhat larger than is desirable, although some of the scatter can be attributed to the difference in time

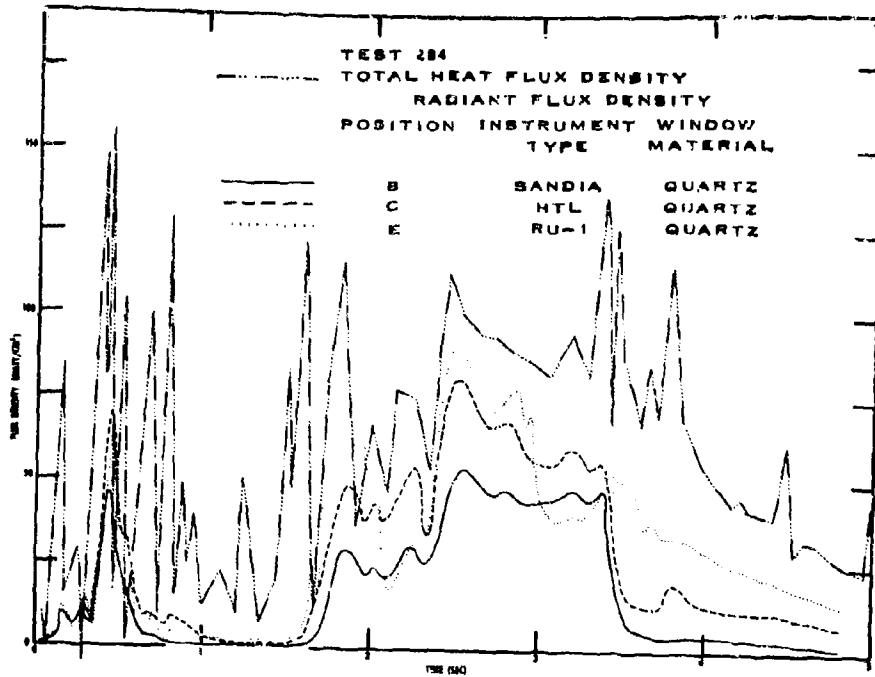


Fig. 6-10. Total and Radiant Flux Density for Test 284

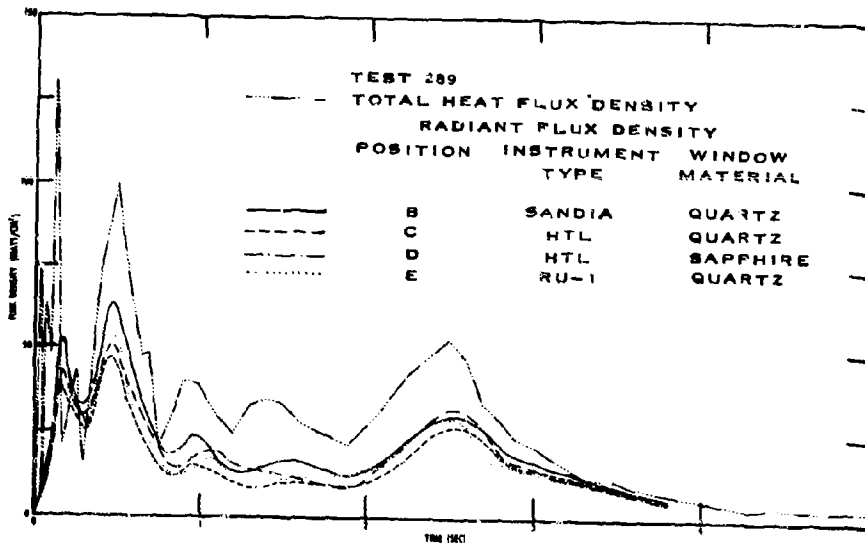


Fig. 6-11. Total and Radiant Flux Density for Test 289

response of the various instruments. For instance, during the sharp pulses that occur within the first second for Tests 284 and 289, the more rapidly responding radiometers at Positions B and E more nearly follow the total flux. Large differences among the radiometer data were also obtained for Test 284, for instance, from about 1.5 to 4.5 sec, but this difference is not attributable to instrument response. Test 284 was a LO₂/RP-1 test (the only 25,000-lb LO₂/RP-1 test for which radiometer data within the fireball were obtained), which requires purging of the radiometer windows to prevent deposition of the products of the explosion. Posttest examination indicated a clean window at Position E and partially coated windows at Positions B and C, and this appears to account for the differences. The flux for Position B is significantly low throughout this time and, along with the flux from C, does not respond between 3.5 and 4 sec to the energy transfer that is indicated both by the radiometer at Position E and by the total flux measurement.

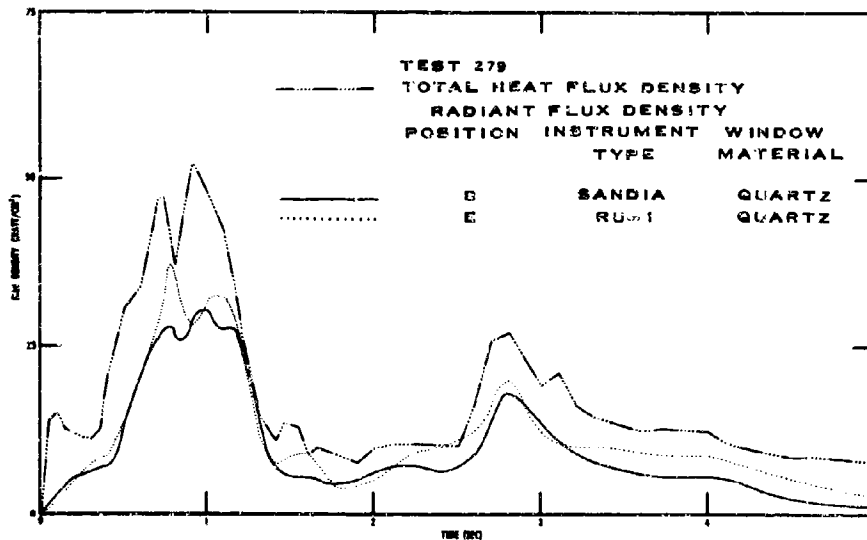


Fig. 6-12. Total and Radiant Flux Density for Test 279

In comparing the radiant with the total flux density, several factors should be considered. First, a correction factor has been applied to the radiometer data to account for energy losses by absorption within and reflection at the surfaces of the windows, and, as indicated in Appendix C, the correction is based on a fireball at about 2200°K. Thus, only during times when there are indications of comparatively high-level energy transfer corresponding to such gas temperatures is the estimated correction applicable. For lower temperatures, the radiant flux density measurement will tend to be lower than the actual flux density. In addition, due to the comparatively slow response of the radiometers, the radiometer data are not expected to closely follow the total flux for "sharp" energy pulses, such as those during the early times of the three tests given in Figs. 6-10 through 6-12. Circumstances in which a reasonable comparison can be made are found between 2.5 and 4 sec for Test 284. In light of a relatively small convective component that should exist, the radiometer data, to the extent that their accuracy permits, clearly support or lend confidence to the total flux measurements.

Temperature of Thermocouple Probes

Measurements of gas temperature as indicated by thermocouple probes at one or two locations within the fireball were obtained by the Sandia Corporation throughout the program. The two measurements from Test 288 are given in Fig. 6-13, and example data from a few other tests are given in Section 2 of Volume 2.

The data of Fig. 6-13 may be compared with the gas temperatures for Test 288 from the remote photo-record pyrometer given in Fig. 6-8. The "peak" values of the thermocouple temperatures are somewhat higher than the pyrometric temperatures. However, an error is not implied by this difference in temperatures; since the pyrometer measures a blackbody equivalent temperature and the thermocouple probe, with some reservation regarding its accuracy, measures the true gas temperature, the pyrometric temperature will depending on the fireball emissivity, tend to be lower than, but at most equal to, the thermocouple temperature. Due to the uncertainty in the fireball emissivity, exacting comparison between the measurements is not possible.

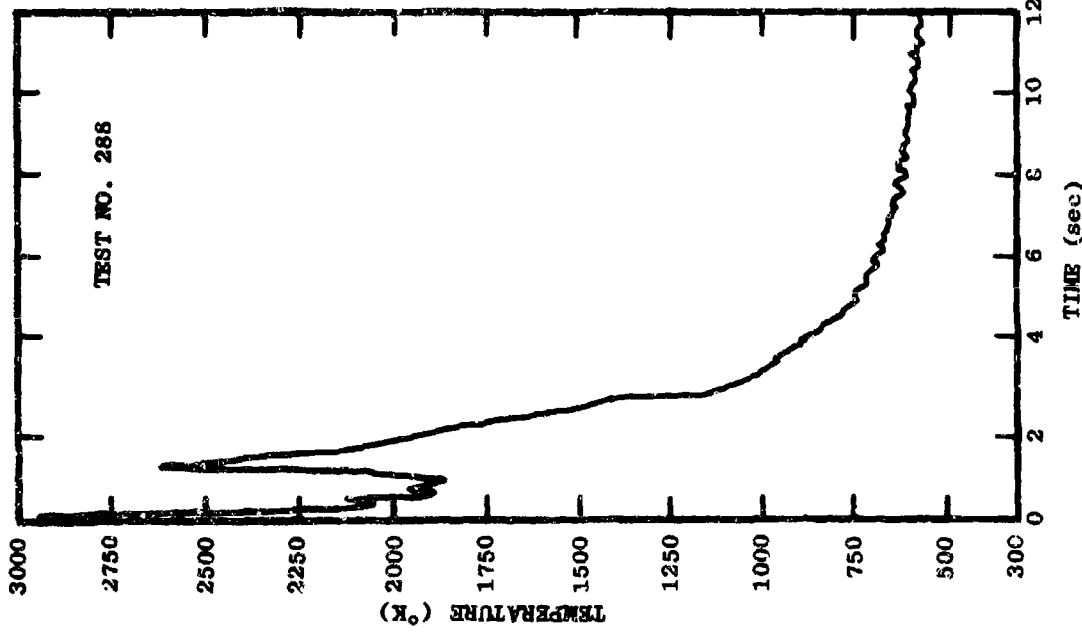
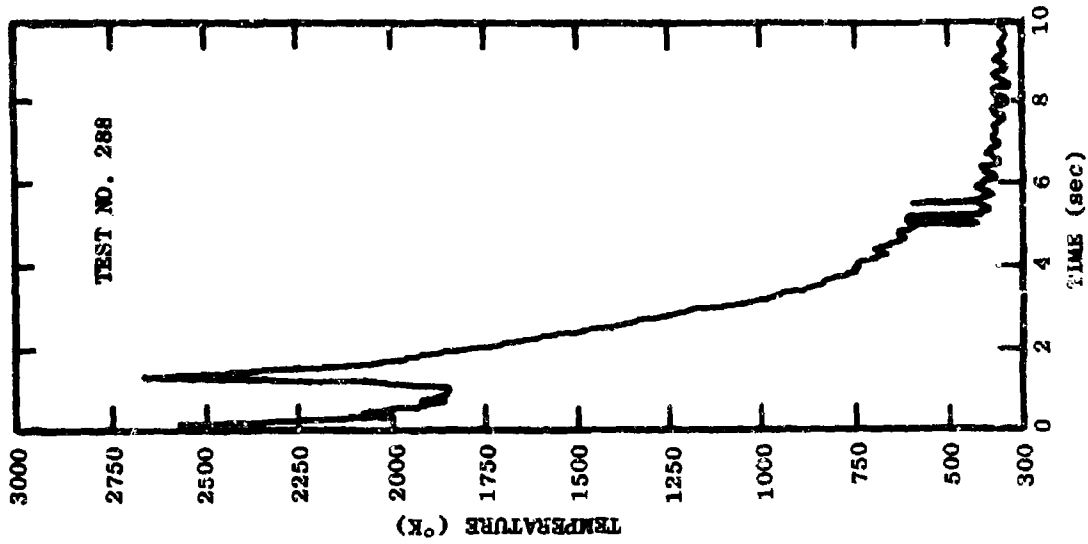


Fig. 6-13. Thermocouple Probe Temperatures from Test 288

A crude comparison can also be made between the thermocouple data and the heat flux density data. For example, the thermocouple temperature in Fig. 6-13 can be compared to the heat flux density for the same test (Test 288) in Fig. 6-5 (pg. 6-29). A similarity can be seen first with regard to the presence of first and second "peaks" at similar times. However, the radiant flux densities computed from the thermocouple temperature values (for an emissivity of 1) for both data traces are 380 and 250 watts/cm² for the first peaks and 250 and 280 watts/cm² for the second peaks, and this is substantially higher than the measured heat flux density at corresponding times in Fig. 6-5, which are in the vicinity of 200 and 150 watts/cm² for the first and second peak, respectively. In this case, the direct heat flux density measurements should be equal to or higher than the flux computed from the pyrometric temperatures for any possible emissivity; thus the thermocouple probe measurements do not support the direct heat flux measurements.

Heat Flux at "Large" Propellant Weight Levels

Heat flux data from explosions of propellant quantities in excess of 25,000-lb are available from only a single test - the Titan I Test involving approximately 100,000-lb of the LO₂/RP-1 propellant combination - and reliable scaling relationships from which the bounding and recommended curves may be extended from the 25,000-lb level cannot therefore be obtained on the basis of heat flux data. Scaling relationships are described in the following paragraphs for curves that are instead based on fireball temperature data obtained from the photo-recording pyrometer measurements and on heating duration data obtained from film coverage. This is followed by a comparison between the bounding curve scaled to the 100,000-lb level and the heat flux data from the Titan I Test.

Scaling of Bounding and Recommended Curves

Fireball temperature measurements from the photo-recording pyrometer are available from more than 200 propellant tests, which range in propellant weight from 200 to 25,000 lb. (Only a summary of the 25,000-lb data is presented in this report.) It is found that the fireball temperature, for practical

purposes, is invariant with propellant weight over this weight range. The weights of interest extend well above this range, however, and in the absence of experimental data on weights of interest or of suitable analytical relationships between temperature and weight, it is assumed that temperature remains invariant with weight. The uncertainty of this assumption in regard to weights of the order of a million pounds is clearly greater than is desirable. The assumption is, however, not entirely without support; the rough indications of temperature derived from the fireball "color" on the film coverage of comparatively large explosions suggests that the assumption is not grossly unrealistic.

In considering the effect of a temperature invariance on heat flux, it was seen in Figs. 6-7 and 6-8 that the bounding curve and radiant flux computed from the pyrometric gas temperature measurements were similar except at early times. An emissivity of 1 was used in this computation, which should be a close approximation at least for the fireballs from $LO_2/RP-1$ explosions. The transfer of energy was therefore almost entirely through radiant transfer; and since the radiant transfer depends on the fireball temperature, the magnitude of the heat flux density should also be invariant with propellant weight.

The scaling relationships for the heating duration are based primarily on film coverage of tests that range to 100,000 lb of propellant. The heating duration as observed in the films is defined as the time from ignition for the fireball to rise from the ground surface, and this duration is nearly identical with the potential heating duration of the heat flux instruments. The heating duration of a structure, of course, may easily be smaller because of its ejection from the fireball. While it may also be larger, it is difficult to hypothesize a set of circumstances which would cause a structure to follow the motion of the fireball. A heating duration given as the time to fireball "lift-off" is chosen because it is the longest that a structure is likely to experience.

It is found experimentally that for explosions of 200-lb quantities of propellant, the fireball temperature subsides before appreciable rising motion

occurs, and the heating duration is thus synonymous with the fireball duration. For quantities of 25,000 lb and more, the fireball will rise substantially, and it appears that the ratio of the heating duration to the fireball duration may be some decreasing function of propellant weight. Since the bounding and recommended curves are based on 25,000-lb heat flux measurements and since most applications are for propellant quantities in excess of 25,000 lb, it is of particular interest to determine the dependence of heating duration on propellant weight for these larger quantities. Heating durations obtained from all the large-scale-test film available are given in Table 6-6, along with the fireball durations and the ratios of these two durations. In addition, estimates of heating durations from the heat flux measurements are included for data traces for the cases in which the duration can be readily identified. These latter durations, which are included to indicate the extent to which they support the film data, should be somewhat less than or equal to the heating durations derived from the film.

There are two uncertainties associated with the magnitudes of the heating duration that have been obtained from the film. First, events such as the fireball lift-off are not always distinct and are subject to interpretation. For tests where it is appropriate, therefore, qualifying remarks will be made. For instance, for several tests, and particularly for Tests 277, 281, and 284, a substantial region of burning remained near the ground after the original fireball lifted from ground surface. Lift-off time for these cases, however, was taken to be the time that the original fireball lifted from the ground surface because the remaining region of burning was much less extensive than the original fireball, e.g., this region did not include the instruments. The fireball for Test 285 was particularly lacking in definition, and the lift-off time is especially uncertain. Regarding the second uncertainty, while accurate timing marks were superimposed on the test film, for very few cases were useful indications provided on films that were otherwise useful for evaluating the lift-off time. It was consequently necessary to evaluate most of the times on the basis of frame rate settings of the cameras, and this results in a larger error than is desirable. It was observed that the scatter tended to increase as the frame rate increased. Consequently the data were obtained from the

films with the slowest rates (usually 64 frames per second). Nevertheless, the standard deviation associated with the lift-off times (and total fireball durations), which are listed in Table 6-6, are probably of the order of 20 percent.

Consideration of the heating durations in Table 6-6 suggests that the heating duration does not increase as rapidly with weight as does the fireball duration, i.e., does not scale to the one-third power. With the one-third power law, the average heating duration of 4.9 seconds for $\text{LO}_2/\text{RP-1}$ at 25,000-lb would result in a heating duration of 7.8 seconds at 100,000-lb, while a value of 6.7 seconds was obtained for the Titan I Test. Similarly for LO_2/LH_2 , a heating duration of 4.5 seconds was obtained for the Saturn IV Test, while a value of 5.7 seconds would be expected at 92,000-lb for an average of 3.7 seconds at 25,000-lb.

Consider next the ratios of the heating durations to the fireball durations given in Table 6-6. The errors that are contained in the durations which are due to the uncertainties in film speed mentioned above are not contained in these ratios. Once again, the evidence indicates that the heating duration has a somewhat weaker dependence on propellant weight than the fireball duration. For instance, the average ratio from the 25,000-lb $\text{LO}_2/\text{RP-1}$ tests is 0.85, while the ratio lies between 0.82 and 0.56 for the Titan I Test. Similarly for LO_2/LH_2 , the average ratio for the 25,000-lb tests is 0.39, while for the Saturn IV Test, the ratio is 0.28. Since it was established in Ref. 6-2 that the fireball duration increases with the cube root of propellant weight, ratios which decrease with weight suggest a weaker dependence of heating duration on weight.

The conservative assumption that the heating duration increases with the cube root of the propellant weight for weights in excess of 25,000-lb is recommended even though the film data suggest a somewhat weaker dependence. The data are clearly too few and uncertain to recommend the reduction of durations from this rule at comparatively large propellant weights.

Table 6-6
MEASURED HEATING DURATIONS

| PROPELLANT TYPE | TEST NO. | FROM FILM COVERAGE | | | FLUX DENSITY HEATING DURATIONS (sec) FOR STATION | | |
|----------------------------------|-------------------|------------------------|-------------------------|--|--|-----|-----|
| | | HEATING DURATION (sec) | FIREBALL DURATION (sec) | RATIO OF HEATING DURATION TO FIREBALL DURATION | H | S | P |
| | | | | | | | |
| LO ₂ /RP-1 | 275 | 4.7 | 5.7 | 0.82 | 4 | 4.5 | - |
| | 278 | 4.0 | 4.5 | 0.89 | - | 4.5 | - |
| | 282 | 5.2 | 6.1 | 0.85 | - | - | - |
| | 284 | 6.8 | 7.6 | 0.90 | 5.5 | - | - |
| | 285 | 3.4 | 4.8 | 0.71 | - | - | - |
| | 25,000-lb Average | 4.8 | 5.7 | 0.85 | - | - | - |
| | Titan I | 6.7 | 8.2(12)* | 0.82(0.56)* | - | - | - |
| LO ₂ /LH ₂ | 281 | 3.7 | 9.5 | 0.39 | - | - | - |
| | 288 | 3.8 | 11.7 | 0.33 | 3 | 3 | 3 |
| | 290 | 3.5 | 6.7 | 0.52 | - | 3.5 | 3.5 |
| | 25,000-lb Average | 3.7 | 9.3 | 0.41 | - | - | - |
| | Saturn IV** | 4.5 | 16 | 0.28 | - | - | - |

* There is some difficulty obtaining a distinct fireball duration for the Titan I Test. It may be as short as 8.2 sec, but probably extends to about 12 sec.

** About 92,000 lb of LO₂/LH₂ were involved in the Saturn IV Test.

In summary, it has been assumed that the heat flux magnitudes will remain invariant with propellant weight, while the heating duration will increase with the cube root of propellant weight. In order to obtain a bounding or recommended curve at a scale in excess of 25,000 lb, that the curves given in Figs. 6-7 or 6-8 be used, with the times in these figures multiplied by $(W/25,000)^{1/3}$, where W is the propellant weight of interest in pounds.

In the section that follows, the heat flux data from the Titan I test are compared to the bounding curve scaled to the 100,000-lb level, applying the above scaling rule.

Titan I Data

The instruments and their locations for the Titan I test, with one exception, were the same as for the 25,000-lb test series. The only modification was the replacement of copper slabs by iron slabs at Positions 1 and 5 of instrument Station P. In anticipation of the relatively long durations of this test, some of the copper slabs were replaced by iron slabs to avoid an error that would be encountered toward the end of the heating pulse if 1.4-in. thick copper slabs were used. The flux curves from the copper slabs will gradually become larger than the correct value. It should also be noted that the thermocouple junction depth for the iron slabs is comparatively small (0.0002 in. compared with 0.005 in. for copper), with the consequence that slab temperature data and heat flux data will tend to be more erratic since the thermocouple junction can more readily sense minor and comparatively short-lived heating activity.

The heat flux density data are given in Figs. 6-14 through 6-16. Two of the data curves from Station P (Fig. 6-16) have somewhat larger-than-ordinary errors over certain time regions due to noise in the corresponding temperature records, and the details of these errors are noted in the data presentation section (Section 2, Vol. 2).

Included on Figs. 6-14 through 6-16 are scaled versions of the "bounding curve" that was given for the 25,000-lb $LO_2/RP-1$ test series in Fig. 6-3, applying the scaling rule described in the previous section.

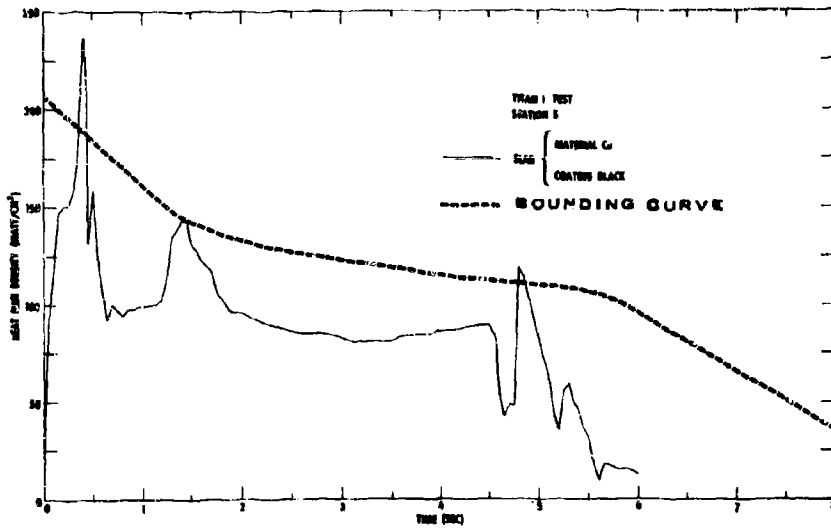


Fig. 6-14. Heat Flux Density at Station S for the Titan Test

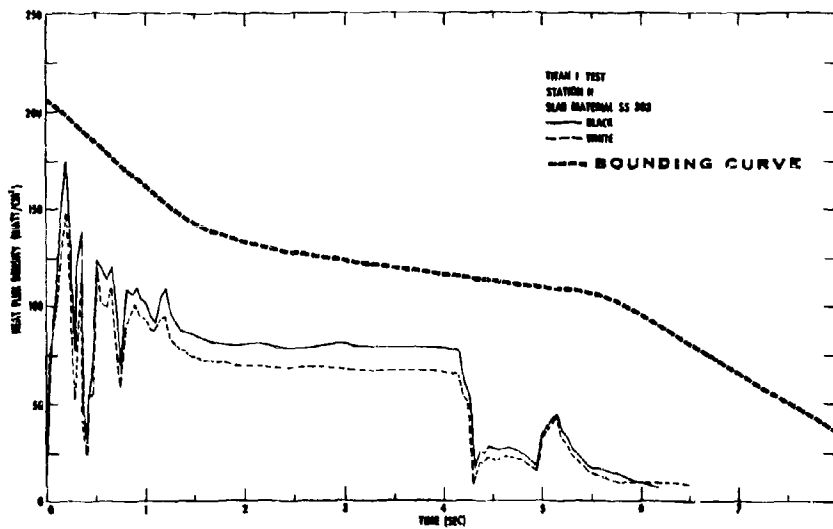


Fig. 6-15. Heat Flux Density at Station H for the Titan Test

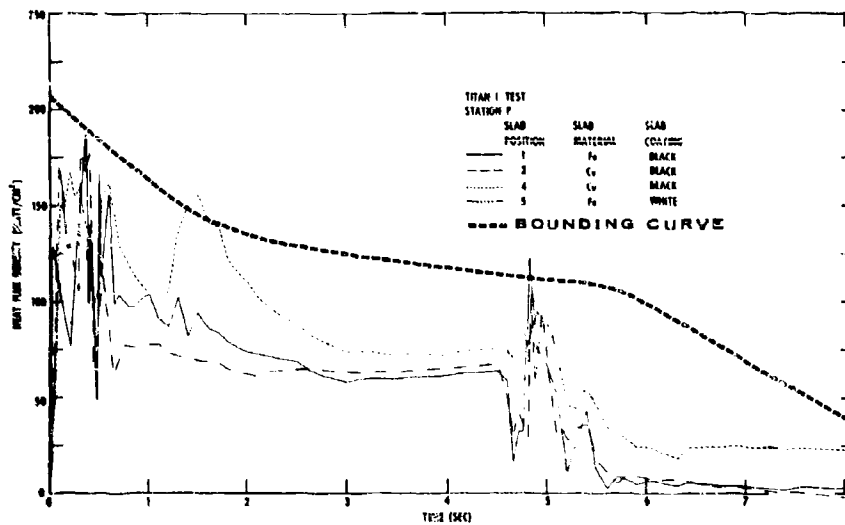


Fig. 6-16. Heat Flux Density at Station P for the Titan Test

Although the data from a single test can not validate the scaling that has been applied, it can be seen that the flux data are clearly not inconsistent with the scaled curve.

The cumulative or time-integrated flux to 8 sec for each flux curve is listed in Table 6-7, along with the ratio of each to the time-integrated flux of the scaled bounding curve, the latter being approximately 1075 watt-sec/cm².

The ratios of the measured accumulative flux density to that of the bounding curve are similar to those obtained throughout the 25,000-lb LO₂/RP-1 tests (Table 6-5). There is a similarity also between the ratios of cumulative flux density from black-coated slabs to those of white-coated slabs, suggesting again that there is a rapid deposition of explosive products. The actual deposit was not as thick as had been obtained for the 25,000-lb test, although it was sufficiently thick to be opaque both on upper horizontal surfaces, and on side (vertical) and face-down surfaces.

Table 6-7
 CUMULATIVE FLUX DENSITY FROM THE TITAN I TEST

| INSTRUMENT STATION | SLAB MATERIAL ¹ | SURFACE COATING | CUMULATIVE FLUX DENSITY (watt-sec/cm ²) | FRACTION OF THE SCALED BOUNDING CURVE |
|--------------------|----------------------------|-----------------|---|---------------------------------------|
| E | Fe | Black | 416 | 0.35 |
| | Cu | Black | 588 ² | 0.50 ² |
| | Fe | White | 390 | 0.33 |
| | Cu | Black | 535 ² | 0.45 ² |
| S | Cu | Black | 555 ² | 0.47 ² |
| H | SS | Black | 418 | 0.36 |
| | SS | White | 371 | 0.32 |

1. SS refers to Stainless Steel 309.
2. Cumulative Flux Value slightly larger than correct value.

Radiant Flux Density Outside the Fireball

25,000-lb and Titan Tests

Summary plots of radiant flux measurements at locations outside of the fireball are presented in Figs. 6-17 and 6-18 for the 25,000-lb LO₂/RP-1 and LO₂/LH₂ tests, respectively. Information presented in these plots is limited to a curve representing the data mean for all tests (the measurements from each test rather than each measurement being weighted equally) along with an indication of the data range (shaded area). Other than adding a few qualifying remarks, the Presentation of Results subsection does not consider these data in any greater detail than is found in the summary plots. However, all of the available data (from both the 25,000-lb and Titan tests) are presented in Section 2 of Vol. 2 in order that the data may be examined in greater detail.

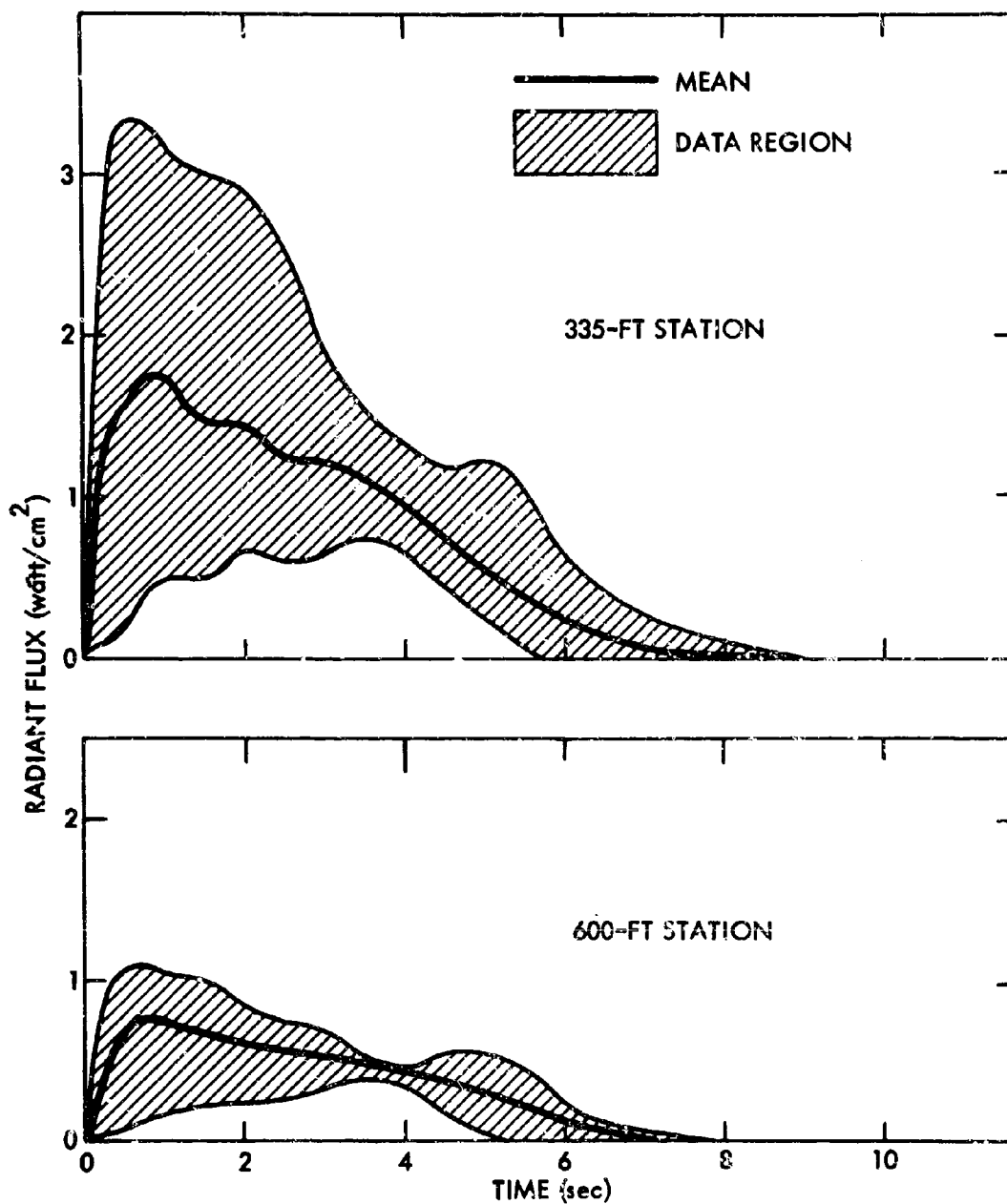


Fig. 6-17. Summary Plots of Radiant Flux Density Outside the Fireball for the 25,000-lb LO₂/RP-1 Tests

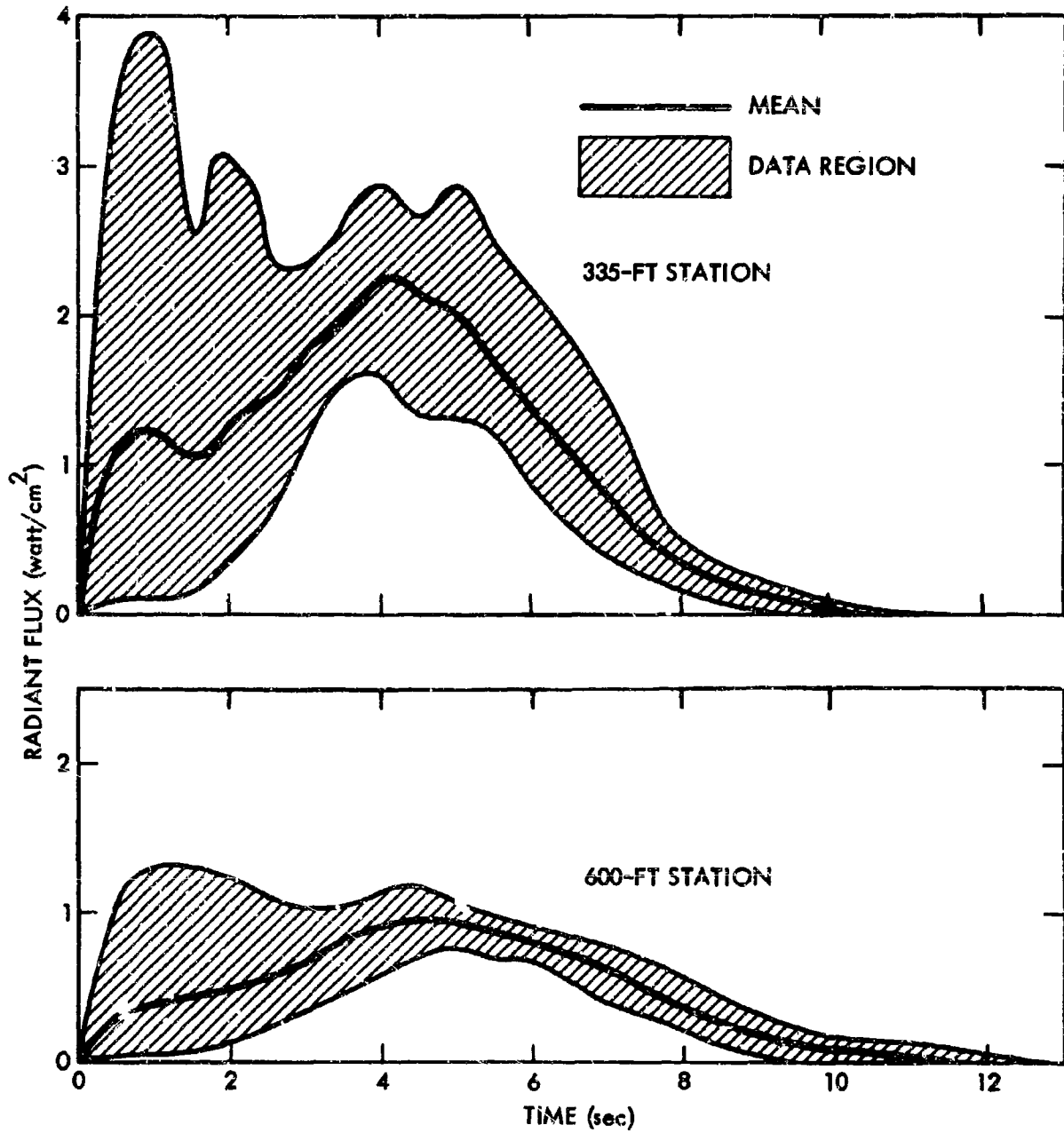


Fig. 6-18. Summary Plots of Radiant Flux Density Outside the Fireball for 25,000-lb LO₂/LH₂ Tests

Generally, three radiometers were used per test, two at 335 ft from ground zero along radial gauge lines 120 deg from each other (gauge lines A and B), with the third at 600 ft from ground zero along one of these gauge lines (along gauge line A). For only two tests, however, were satisfactory data acquired from both radiometers at 335 ft. For one of these two tests, Test 278, the flux at 335 ft along gauge line B rose during the later stages of the heating pulse to a value significantly larger than that from the corresponding 335-ft gauge line A station, as seen in Fig. 6-19. Photographic coverage indicates that the fireball drifted toward the gauge line B station, thereby accounting for the inflated value. Gauge line B data from this test were consequently not used in the construction of the summary plot of Fig. 6-17. The data from gauge line A, even though somewhat deflated for the same reason, were used, however, since their deflation will tend to be less pronounced. This comparatively extreme example has been mentioned primarily to illustrate the "coarse nature" of these data, because such physical occurrences, but of lesser degree, were present in the other tests. Moreover, these measurements are comparatively inaccurate, ranging, it is estimated, between 10 and 20 percent, depending on the quality of a given raw data trace; and as fireball temperatures subside, these errors tend to increase due to energy absorption within the radiometer window.

One further qualification of the data should be noted. The "field of view" of the external radiometers is inherently restricted to 90 deg (45 deg in any direction from the instrument "line of sight"), and the instruments are directed approximately 10 deg above the horizontal. It is thus possible for the fireball to rise sufficiently to escape view of the radiometers, first from those at 335 ft and later at 600 ft, before its temperature or radiant output has entirely subsided. This occurred noticeably for Test 288, as can be seen by comparing the 335 and 600 ft gauge line A flux traces in Fig. 6-20. During the early stages, the flux at 600 ft should be, and is to the first approximation, one-third of that at 335 ft, while at later stages this ratio changes, and eventually the flux is greater from the 600-ft instrument. While this has not occurred while any significant flux levels persisted it cannot be concluded that the emitted energy from the fireball has subsided, since much

of the decrease at the instrument is due to increase in distance between the fireball and the instrument produced by the elevation of the fireball. Thus fireball durations are not accurately reflected in the flux-time curves. It is of interest, however, to compare the duration for 25,000 lb as given by Eq. (6.2), which is approximately 6 sec, with the somewhat larger durations suggested by Figs. 6-17 and 6-18, the latter averaging approximately 7 and 11 sec for $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 , respectively. While the difference in duration for LO_2/LH_2 is nearly a factor of two, it will be recalled that the estimated standard error of Eq. (6.2) is 34 percent.

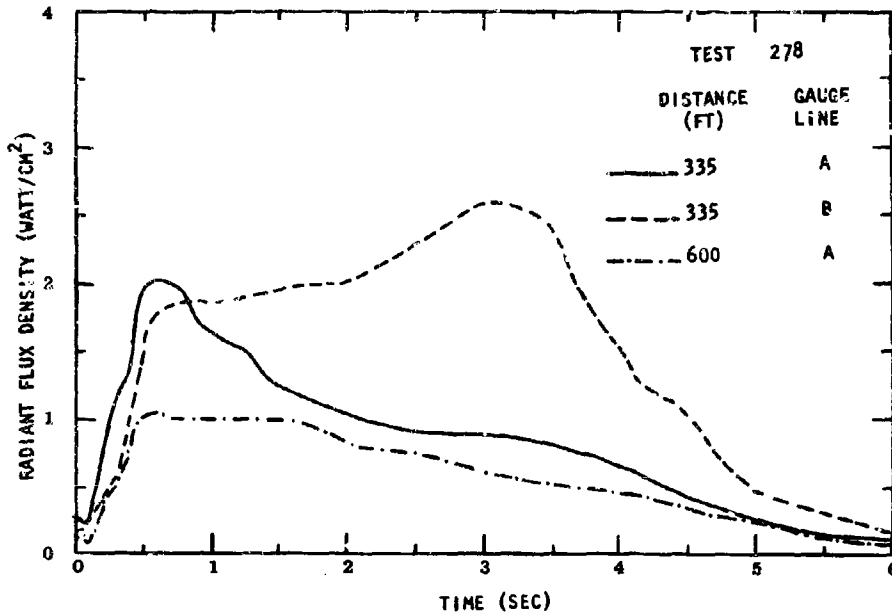


Fig. 6-19. Radiant Flux Density Outside the Fireball from Test 278

Radiant flux density at the 600-ft station for the Titan test is given in Fig. 6-21. The fireball dimensions and their uncertainty prevented interpretation of the radiometer data at the 335-ft stations; the fireball expanded

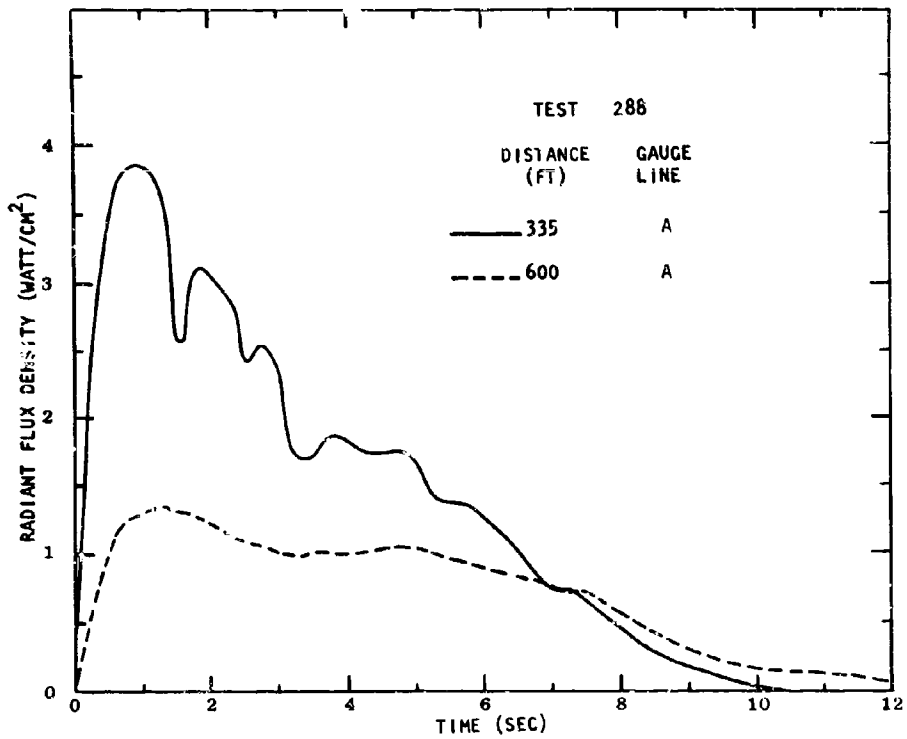


Fig. 6-20. Radiant Flux Density Outside the Fireball from Test 288

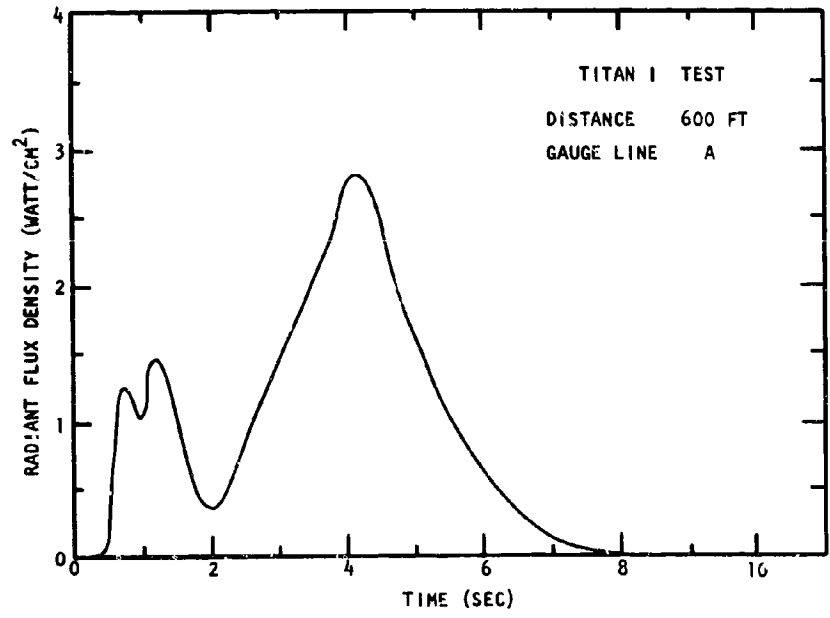


Fig. 6-21. Radiant Flux Density Outside the Fireball from the Titan I Test

beyond the field of view of all photographic coverage, but diameters along the horizontal near the ground surface were estimated to be from about 800 to 1000 ft. The uncertainty in diameter also prevents exact interpretation of the data at 600 ft.

Section 6

REFERENCES

- 6-1. Bader, B. E., Heat Transfer in Liquid Hydrocarbon Fuel Fires, paper presented at the symposium on Shipment of Radioactive Materials, Fifty-seventh Annual Meeting, Boston, Massachusetts, Dec. 6-10 1964
- 6-2. Gayle, J. B. and J. W. Bransford, Size and Duration of Fireballs from Propellant Explosions, NASA TM X-53314, George C. Marshall Space Flight Center, Huntsville, Alabama, 4 Aug 1965

Section 7
SUPPORT STUDIES

Several auxiliary studies involving analytical or small-scale experimental work were initiated in support of the PYRO hot-test program. These include:

- Particle-size studies: an investigation of the effect of particle size on explosive yield
- Heat transfer studies: inquiry into the rate of cooling of small fuel drops in LO_2
- Mixing studies: an investigation of possible methods for assessing the relative importance of mechanical and thermodynamic forces in the mixing process
- Small-scale H.E. tests: a test series to obtain data on the effects of detonation conditions on blast characteristics

These studies were conducted as the initial phase of an overall program to investigate the basic phenomena involved in propellant mixing and explosion processes. This program was initiated because a better understanding of the basic phenomena would assist in the overall design of the hot-test program, in the interpretation of the results from the program, and in the extrapolation of the results to propellant types and conditions other than those tested. It would also provide theoretical justification for the largely empirical scaling relationship derived from the main hot-test program.

The major goals of the overall program were to establish what properties of a propellant mixture determine or control the explosive characteristics of that mixture and to find the predominant mechanisms by which these definitive properties develop during the propellant mixing process.

The particle-size, heat transfer, and small-scale H.E. tests were all directed towards the first of the above goals, the identification of the definitive properties of a propellant mixture.

The specific objective of the particle-size study was to find the variation of the explosive yield of LO_2 /RP-1 charges with fuel particle size, total propellant weight, and oxidizer-to-fuel ratio. Previous investigations (Ref. 7-1) had indicated that the particle size of the dispersed fuel was one of the most important properties of a propellant mixture with regard to its explosive potential. These previous studies, however, were limited to only one oxidizer-to-fuel ratio and one charge size, and to further understand the particle-size effect required further investigation of oxidizer-to-fuel ratio and charge-size effects.

The heat transfer study was the first step towards finding the effect, if any, that oxygen gas films, which are prevalent during the film boiling of LO_2 over fuel surface, may have on explosive yield of the LO_2 /RP-1 propellant mixture. The importance of this situation, if, in fact, it does occur, is that charges composed of particles with gaseous envelopes (due to boiling at the particle surface) might have explosive properties different from those of charges in which the fuel-particle surfaces are in contact with oxygen in the liquid phase. A charge composed of particles of fuel enveloped in gaseous oxygen would necessarily be reduced in charge density because of the presence of boiling films on the fuel-particle surfaces. It might also develop lower explosive yield, owing to the lower concentration of oxygen in the gaseous film at the fuel surface and the consequently less favorable conditions for rapid reaction, which would support the detonation wave.

The specific objective of the heat transfer study was to determine the time required to cool RP-1 fuel particles below the temperature at which film boiling prevails. If the cooling times were an order of magnitude (or more) less than the time required for breakup of major propellant masses, it could be concluded that only an insignificant proportion of fuel drops capable of contributing to explosive yield could be in the cooling process at any given point in time; and hence, the problem of the possible effect of the film layer on the yield could be ignored.

The mixing study was directed towards the second long-range goal, finding the predominant mechanisms by which spilled propellants form explosive mixtures. The specific objective was to formulate an experimental approach for determining the relative importance of the two major mechanisms postulated for mixing of cryogenic propellants.

- the kinetic energy of the flowing fluids
- turbulence created by the effects of boiling heat transfer

The method considered depends on using a simulant for RP-1 which can be precooled to the approximate temperature of LO_2 and have essentially the same properties at this temperature that RP-1 has at room temperature. Mixing of the precooled simulant fuel and LO_2 would thus be solely due to flow processes while LO_2 /RP-1 mixing under the same conditions would be due to both heat transfer and flow processes. The relative explosive yields measured for the two kinds of fuel in the same mixing geometry would thus reveal the relative importance of each mixing process.

Because of the relatively limited effort available in the PYRO program for these types of basic studies, it was not possible to pursue any of them very far. The progress which was made on each study, however, is covered in the following subsections along with a brief description of the recommended follow-on work. Also included is an outline of additional areas which warrant study as part of the overall program to investigate propellant mixing and explosion processes.

PARTICLE-SIZE STUDY

It has been hypothesized that the following properties of a mixture of LO_2 /RP-1 might uniquely determine its explosive yield:

- Interface area of the two propellants per unit volume of mixture
- Uniformity of distribution of interface area within the mixture
- Total weight of propellants in the mixture and its geometrical configuration

In the case of LO_2 and LH_2 mixtures, it would likely be necessary to include factors influencing the contribution of gas-phase detonation to the over-all explosive yield.

The particle-size studies were started to investigate the effects of each of the hypothesized definitive properties listed above on the explosive properties of mixtures of LO_2 /hydrocarbon fuel, using charges composed of paraffin particles of known uniform size in LO_2 . It has been shown in previous work (Ref. 7-1) that perfectly mixed or adsorbed-fuel* charges composed of paraffin in LO_2 produce the same yield as perfectly mixed LO_2 /RP-1 charges of the same weight ratio of oxidant and fuel. Paraffin can, therefore, be used as an excellent simulant for RP-1 in test charges, with the advantage that properties such as interface area per unit volume, etc., can be controlled by the use of preformed and sized particles in the test charges.

Particle size, at a given ratio of oxidizer to fuel, determines the interface area of the propellants per unit volume. It has been demonstrated in previous work (Refs. 7-1, 2) that the explosive yield of small test charges (27 gm) decreases as particle size of the fuel is increased; however, these experiments involved only one size of charge and one ratio of oxidizer to fuel (3:1). In exploring the effect of particle size on explosive yield, it is necessary to include the effects of varying charge size and the ratio of propellant weights, because it has been determined that this ratio affects yield, and it may be expected that particles of the same size would produce different yields in different charge diameters, just as in the case of conventional explosives.

* These are charges that were constructed by adsorbing hydrocarbon fuels, such as kerosene, RP-1, JP-4, and paraffin, while in a liquid state, onto an inert structural material like diatomaceous earth, thus providing a uniformly dispersed distribution of fuel with, in effect, a particle size that is a minimum.

With the relatively little effort available for this study, it has not been possible to complete the investigation of all three of these parameters, i.e., particle size, charge size, and oxidizer-to-fuel ratio; the effect of charge size, for example, has not yet been explored at all. Nevertheless, from tests completed so far, a number of significant observations were made and some insights gained. The general approach, experimental effort, and results are described below.

Approach

The experimental result mentioned previously, in which it was observed that for a given charge size the explosive yield decreased with increasing particle size, suggested that the reaction may have been limited by transport phenomena, e.g., the reaction rate may not have been sufficient to completely burn up the larger particles during the time in which the reaction could contribute its energy to the formation of the blast wave. These results gave no information on whether or not all of the oxidizer enters the reaction. As the first trial hypothesis, however, it was assumed that all of the oxidizer did enter the reaction.

The previous experimental results also indicated that for fuel particle sizes small enough to completely react, the explosive yield was dependent on the oxidizer-to-fuel ratio. For particle sizes large enough that all the fuel does not enter the reaction, it would also be expected that the oxidizer-to-fuel ratio would be important. In this case, however, there is not a unique oxidizer-to-fuel ratio, since the initial ratio differs from the end-point ratio (the ratio of oxidizer to fuel actually reacted). Since, with the hypothesis discussed above, the unreacted kernel of each particle could be replaced with inert material, it was assumed for the initial hypothesis that it was the end-point ratio that was most important.* In

* In actuality, the use of a simple parameter to characterize the reaction process for incomplete reactions, such as either the initial or end-point oxidizer-to-fuel ratio for the case of large particles, may not provide a complete explanation, since the effects of this parameter no doubt enter in complex ways. At this time, however, consideration was limited to defining this parameter only in terms of these two ratios, since they are the only values that can be determined experimentally without making additional assumptions.

applying the above concept, end-point ratios would be determined from knowledge of the initial ratio and from a ratio of reacted to unreacted fuel. Any selection of the latter ratio uniquely fixes both the total mass of reactants relative to the initial mass and the end-point ratio.

The fuel-limited-reaction concept is seen to lead to two sources of change in explosive yield associated with increase in fuel particle size, one acting always to reduce the charge yield, because of reduced charge weight due to unreacted fuel, and the other acting to either decrease or increase the charge yield in accordance with the end-point, or effective, reaction ratio of oxidizer to fuel. The net of both of these effects determines the explosive yield in a given charge. This simplified fuel-limited version of the transport-limited concept obviously lacks the sophistication of a method for excluding any LO_2 in the transport process, but the nature of the observations should provide further insights into the processes. The experimental information required is discussed below.

By conducting additional tests with adsorbed-fuel charges in the current program, a more complete spectrum of explosive yields, as a function of the end-point ratio of oxidizer to fuel, has been obtained. With this more complete set of data, an iterative process may be used to determine, from subsequent experimentally measured yields on particulate-fuel charges, an effective end-point LO_2 -to-fuel ratio and hence a corrected charge weight. Repetition of the process finally results in fixing the weight of fuel unreacted. The spherical fuel-particle geometry used makes it possible to calculate, from the inferred weight of fuel unreacted, a value for the depth of the layer of fuel burned off the surface. Thus, it is possible to plot layer thickness consumed as a function of particle size.

Several other concepts aid in drawing inferences from the available information. It would seem reasonable to expect, for a given charge weight, that at each initial ratio of oxidizer to fuel there will be a different upper limit to the fuel particle size effectively completely reacted, i.e., the size which will give the same yield as the ideal adsorbed-fuel charges. It may be further expected that this size will increase with increases in the initial ratio of oxidizer to fuel because of a higher probability of unreacted material coming together during the time available for such to still contribute to the

main reaction. There may also exist some particle size and, as previously mentioned, some initial LO_2 -to-fuel ratio above which reaction rates are LO_2 -transport limited. This would of course affect the yield and, further, could act to alter the nature of peak pressure fall-off with distance.

Finally, the qualitative effect of an increase in charge size is anticipated to operate, as in a conventional explosive, to provide an increase in the time for the reaction to occur. Thus, as charge size increases, it is anticipated that the size of the fuel particle that is effectively completely reacted at each LO_2 -to-fuel ratio will increase also. With sufficient data of this sort, the postulated concept of an effective burning or reaction rate might be examined experimentally.

Experimental Method

The paraffin particles used in the experiments were produced by atomization of liquid paraffin. The particles solidify while settling a distance of about 8 ft through the air. The solidified particles pass through an opening in the upper wall of a horizontal duct of rectangular cross section. Air flow velocity in the duct was regulated so that particles of different sizes in the range of experimental interest (10 to 400 μ diameter) were separated owing to their differing fall rates within the duct. The sized particles were collected from appropriate areas of the floor of the duct. The particles were further graded accurately to narrow size ranges by sieving them while they were bathed in LN_2 .

The uniformly sized particles were dispersed in kieselguhr, from which test charges were formed. The proportion of kieselguhr and its compaction in the charge was adjusted so that it would absorb an amount of LO_2 corresponding to the desired LO_2 -to-fuel weight ratio when immersed in LO_2 . It has been shown in previous experimental work that the amount of kieselguhr required does not significantly affect the explosive yield (Ref. 7-3).

After a charge was constructed, it was frozen and then introduced into a lightweight wire-mesh basket constructed of household screening material. The basket prevents erosion of the charge when it is finally introduced into

the LO_2 bath prior to detonation and holds the Primacord securely in place. A typical charge can be seen in Fig. 7-1. (For purposes of the photograph, the wire tabs that secure the Primacord have been bent aside.)

After the charge had soaked in the bath long enough to assure complete penetration of the LO_2 it was rapidly withdrawn by means of a manually operated pulley arrangement and detonated by an explosive cap attached to the Primacord. Side-on pressure-time measurements of the resulting blast wave were made with a series of four Kistler 605 piezoelectric pressure gauges mounted at various distances from the charge. The pressure gauges were calibrated before and after each test series in a shock tube, and standard high-explosive charges were used for field calibration and for establishing the reference peak-overpressure-versus-distance curves used in computing explosive yield values.

Experimental Results

The primary objectives of the experimental program have been to test the validity of portions of the concepts discussed earlier and to gain insights into controlling processes.

The first experimental effort consisted of extending the quantitative relationship of maximum explosive yield versus the ratio of oxidizer to fuel to higher ratios by means of adsorbed-fuel (kerosene) charges of 27 gm total weight. A plot of these data appears in Fig. 7-2. This particular quantitative relationship may be considered to be invariant with charge size, since all the material reacts in time to contribute to explosive yield. This is supported by the fact that the theoretical calculations (Ref. 7-4), based on all material reacting, agree with the experimentally measured values and that the maximum experimentally measured yield is seen to occur at a ratio of 3:1, where the theoretical maximum is expected. Ratios greater or less than 3:1 result in reduced yields.

In addition to measurements of explosive yields at higher ratios of oxidizer to fuel, tests were conducted with the initiator only and with standard, oxygen-soaked adsorbed charges, in which water was substituted for the

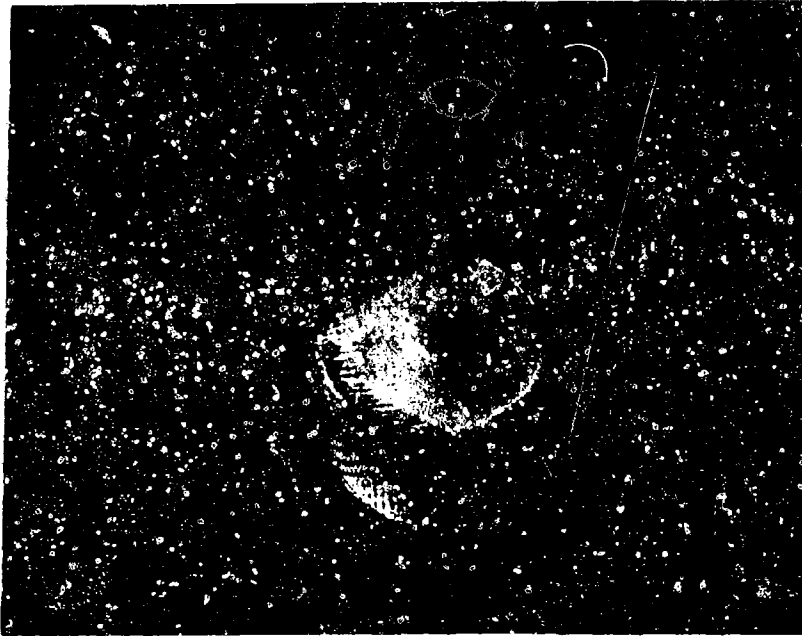


Fig. 7-1. Photograph of Test Charge

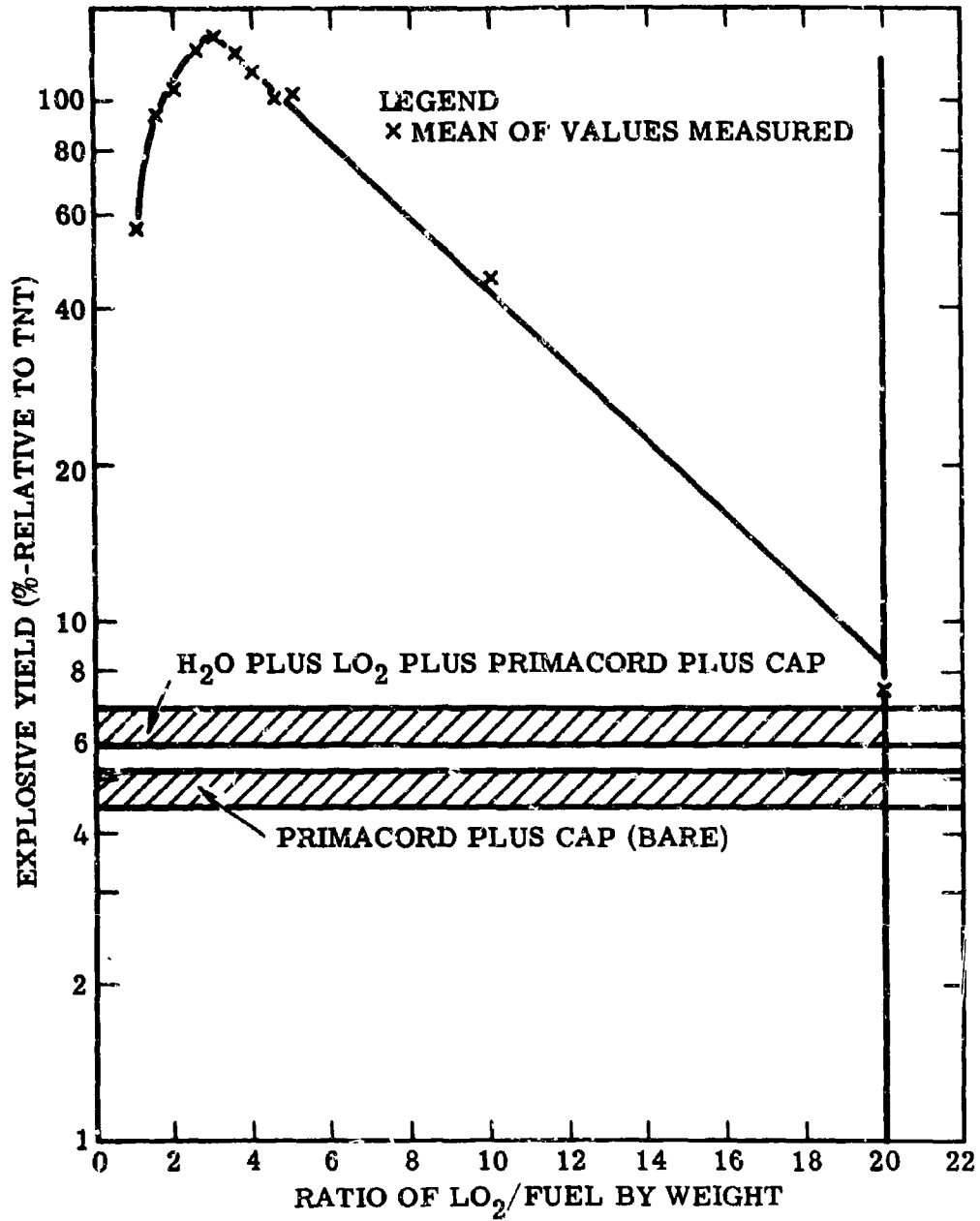


Fig. 7-2. Explosive Yield vs Ratio of LO₂-to-Fuel for Maximum Surface Area of Fuel in Contact with LO₂

hydrocarbon fuel.* The objective of these tests was to set a lower limit to yields and an upper limit to ratios of oxidizer to fuel that would be meaningful. It is seen in Fig. 7-2 that no specific conclusions can be drawn where oxidizer-to-fuel ratios are above, say, about 20:1 or where yields are below 7 percent TNT equivalent.

The second experimental effort was aimed at determining the effect of particle size on explosive yield for an initial LO_2 -to-fuel ratio other than the previously studied ratio of 3:1, where maximum yield occurs. Figure 7-3 shows a plot of yield versus particle size at the ratio of 3:1, obtained from data in Refs. 7-1 and 7-2. Figure 7-4 shows the results obtained in the present study for an oxidizer-to-fuel ratio of 5:1, plotted in the same manner. In both Figs. 7-3 and 7-4 the vertical lines indicate the yield for the case where the initial and end-point ratios are the same, i.e., for adsorbed fuel charges. Thus, it may be inferred that the point of intersection of these lines with the yield-versus-particle-size line indicates the maximum particle diameter effectively completely reacted. This would be 2μ at 3:1 and 28μ at 5:1. In addition, the two plots indicate an exponential decrease in yield with increasing particle size, approaching the yield of the initiator asymptotically. The rate of decrease has an apparent dependence on the initial ratio of oxidizer to fuel; but as noted earlier, it was assumed that the correct view is to consider it on the basis of the final effective LO_2 -to-fuel ratio, which ultimately depends on the weight of fuel actually burned off the particle surfaces.

Reconsider, in the light of the previous paragraph, the information contained in Fig. 7-2. Since all the fuel is apparently consumed** these data can also be interpreted as indicating the yield to be expected for any reaction

* It was anticipated that some of the organic composition of the Primacord would react with the liquid oxygen to give a slightly increased yield.

** These data are for adsorbed-fuel charges, where no transport problems are considered to exist, i.e., the subdivision and distribution of fuel and oxidant is considered to be such that every molecule of fuel has in its immediate vicinity all the oxidant necessary for a complete reaction at the ratio indicated.

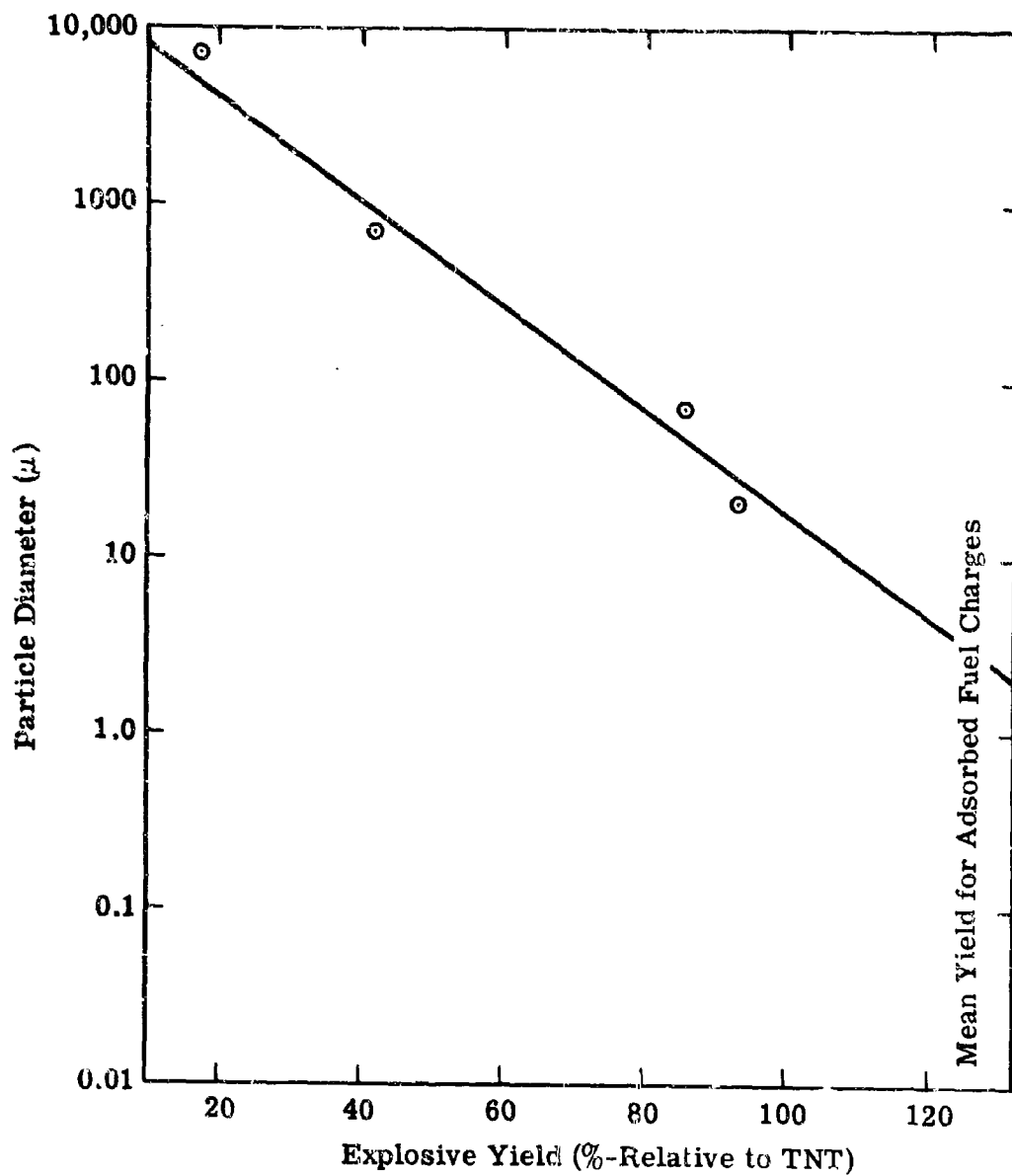


Fig. 7-3. Explosive Yield vs Fuel Particle Diameter for 3:1 Ratio of LO_2 -to-Fuel

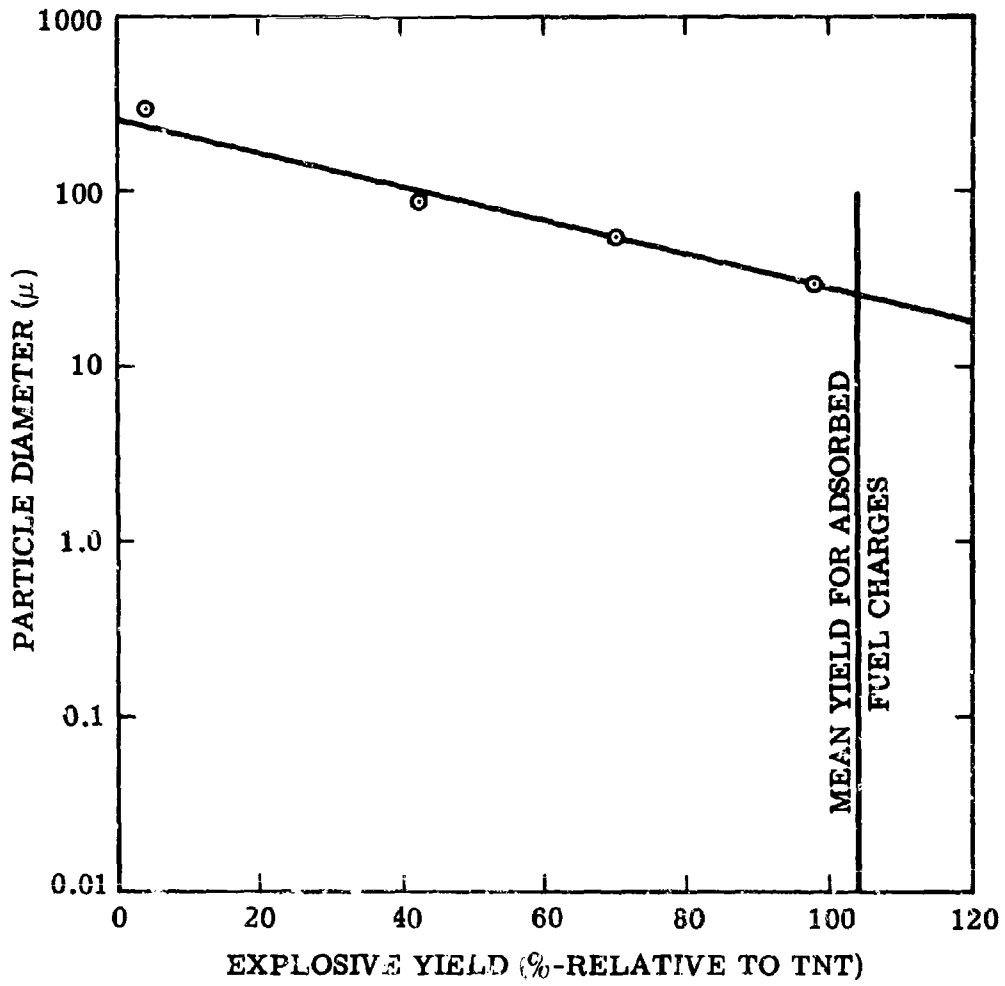


Fig. 7-4. Explosive Yield vs Fuel Particle Diameter for 5:1 Ratio of LO₂-to-Fuel

in terms of its ultimate end-point LO_2 -to-fuel ratio. As an example, let this concept be applied directly to calculate the yield, as a function of various end-point LO_2 -to-fuel ratios, that is to be expected from a charge initially composed of 13.5 gm each of LO_2 and fuel. In this computation, selection of an end-point ratio fixes the amount of fuel that does not enter the reaction. Table 7-1 indicates the net effect on explosive yield for such a situation. The data in Table 7-1 are summarized in the plot in Figure 7-5, which also shows the net effect of the change in ratio and the corresponding loss in active charge weight on yield. It is evident from both the table and the plot that the postulate of yield dependence on end-point LO_2 -to-fuel ratio results in an expected higher absolute yield if some of the fuel fails to enter the reaction. In fact, if anywhere up to four-fifths of all the fuel is, for some reason, unreacted, the yield is seen to be either equal to or higher than the case where 100 percent of the fuel reacts. At an end-point ratio between 2.5:1 and 3:1, the plot demonstrates that the mean yield has increased to its peak value, or about 88 percent TNT versus the 51 percent at 1:1.

Additional experiments were conducted at an initial LO_2 -to-fuel ratio of 1:1, using fuel particles in the 74- to 104- μ range (mean size by volume of 87 μ) and the 256- to 420- μ range (mean size by volume of 333 μ). Both sets of tests resulted, as postulated above, in mean yields higher than 57 percent TNT equivalence. The smaller, 87- μ , size produced a mean yield of 84 percent, and the 333- μ size produced a mean yield of 61 percent. Each of these yields could correspond to two different end-point ratios, as seen in Fig. 7-5. Nevertheless, it may be inferred that if the 333- μ size shows the lower mean yield of the two, it must therefore be at the higher of the two end-point ratios possible. This would correspond to an LO_2 -to-fuel ratio of 4.8:1 and 21 percent of the fuel reacted by volume. Hence, the surface layer burned off the radius would be about 7 percent or 11.7 μ . Consequently, it might be further inferred that a 23.4- μ particle ($2 \times 11.7 \mu$ layer burned off) must surely have the same end-point ratio if the initial ratio is 4.8:1, i.e., that a particle size at least 23.4 μ in diameter would be completely consumed if incorporated in a charge with an initial ratio of 4.8:1.* Examination of

* Actually a larger size should react completely at this initial ratio since at every stage of the reaction the effective ratio will be higher than for an initial ratio of 1:1 with the end-point ratio of 4.8:1.

Table 7-1
 CALCULATED EXPLOSIVE YIELDS (RELATIVE TO TNT) AS A FUNCTION OF THE END-POINT LO_2 -TO-FUEL RATIO FOR A 27-CM CHARGE INITIALLY AT A 1:1 RATIO (COMPOSED OF 13.5 GM LO_2 AND 13.5 GM OF PARAFFIN SPHERES)

| 1 | 2 | 3 | 4 | 5 | 6 |
|---------------------------------|---------------------------|--------------------------------|---------------------------------|---|------------------------------|
| End Point LO_2 -to-Fuel Ratio | Portion of Fuel Unreacted | Weight of Fuel Reacted(a) (gm) | Effective Charge Weight(b) (gm) | Yield per Gram of Reacting Material (c) (%) | Total Yield of Charge(d) (%) |
| 1.0 | 0 | 13.5 | 27.0 | 57 | 57 |
| 1.5 | 1/3 | 9.0 | 22.5 | 93 | 78 |
| 2.0 | 1/2 | 6.75 | 20.25 | 113 | 85 |
| 2.5 | 3/5 | 5.4 | 18.9 | 126 | 88 |
| 3.0 | 2/3 | 4.5 | 18.0 | 132 | 88 |
| 3.5 | 5/7 | 3.9 | 17.4 | 123 | 79 |
| 4.0 | 3/4 | 3.4 | 16.9 | 115 | 72 |
| 4.5 | 7/9 | 3.0 | 16.5 | 105 | 64 |
| 5.0 | 4/5 | 2.7 | 16.2 | 96 | 58 |
| 6.0 | 5/6 | 2.25 | 15.8 | 83 | 49 |
| 7.0 | 6/7 | 1.9 | 15.4 | 70 | 40 |
| 8.0 | 7/8 | 1.7 | 15.2 | 59 | 33 |

Notes:

- (a) 13.5 divided by column 1
- (b) 13.5 plus column 3
- (c) From Fig. 7-2
- (d) Product of columns 4 and 5 divided by 27

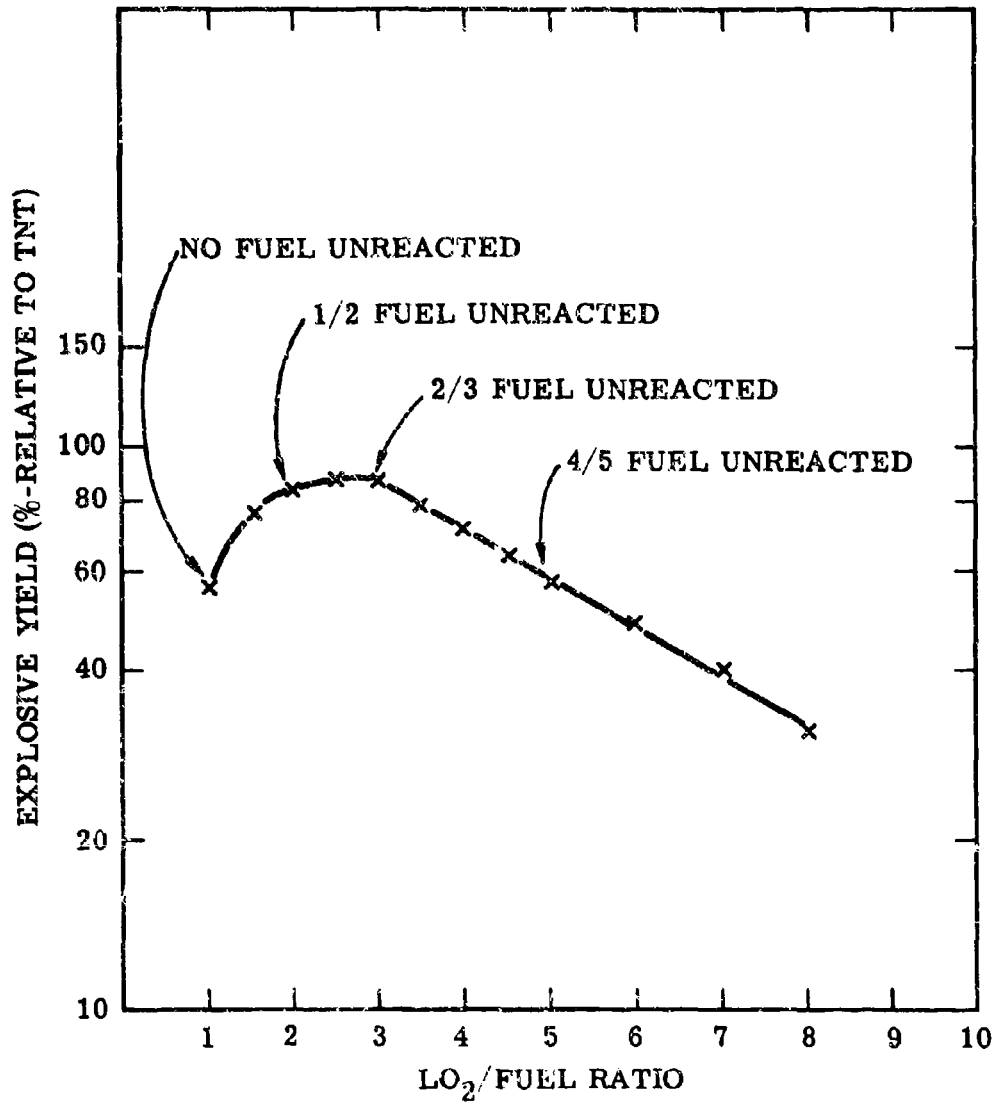


Fig. 7-5. Explosive Yield as a Function of End-Point LO₂-to-Fuel Ratio for an Initial LO₂-to-Fuel Ratio of 1:1

Fig. 7-4 shows that 28μ is the largest particle size at an initial ratio of 5:1 that is expected to have an end-point ratio of 5:1. Thus, the hypotheses and inferences are, so far, consistent.

For initial LO_2 -to-fuel ratios of 3:1 and 5:1, Table 7-2 presents layer thicknesses inferred to have reacted off the outside of the spherical fuel particles. The information was calculated on the basis of the fuel-limited-reaction concept by using information of the type presented in Table 7-1. Implicit in Table 7-1 is a relationship between yield (the information in Column 6) and reacted layer thickness as computed from the portion of fuel reacted (which can be determined from information on the amount of fuel unreacted, given in Column 2). Thus, the experimentally measured relationships between yield and particle size allows layer thickness reacted to be related to particle size for a given initial LO_2 -to-fuel ratio. It is rather surprising that within experimental limits of error, there appears to be a constant layer thickness reacted (i.e., completely independent of particle size) for the case where the initial LO_2 -to-fuel ratio is 5:1, but when the initial ratio is 3:1 (i.e., slightly less than stoichiometric), there appears to be an ever-increasing layer thickness reacting as particle size is increased. The apparent explanation for what appears at this time to be "anomalous" behavior in the fuel-limited-reaction concept is simply that the more sophisticated transport-limited concept involving limitations on the LO_2 transport seems to be required. The existence of an absolute limit on LO_2 transport would be more compatible with observations that indicate a constant layer thickness of fuel consumed when much excess LO_2 is available, as at the 5:1 initial-ratio condition.

Summary

A summary plot incorporating most of the available data and hypotheses and based on the various inferences discussed appears in Fig. 7-6 which is a plot of explosive yield vs initial LO_2 -to-fuel ratio for a range of fuel particle sizes. From it, rough predictions can be made with regard to test results anticipated from particulate-fuel charges in regions where extrapolation, rather than interpolation, is required (for example, at higher initial LO_2 -to-fuel ratios).

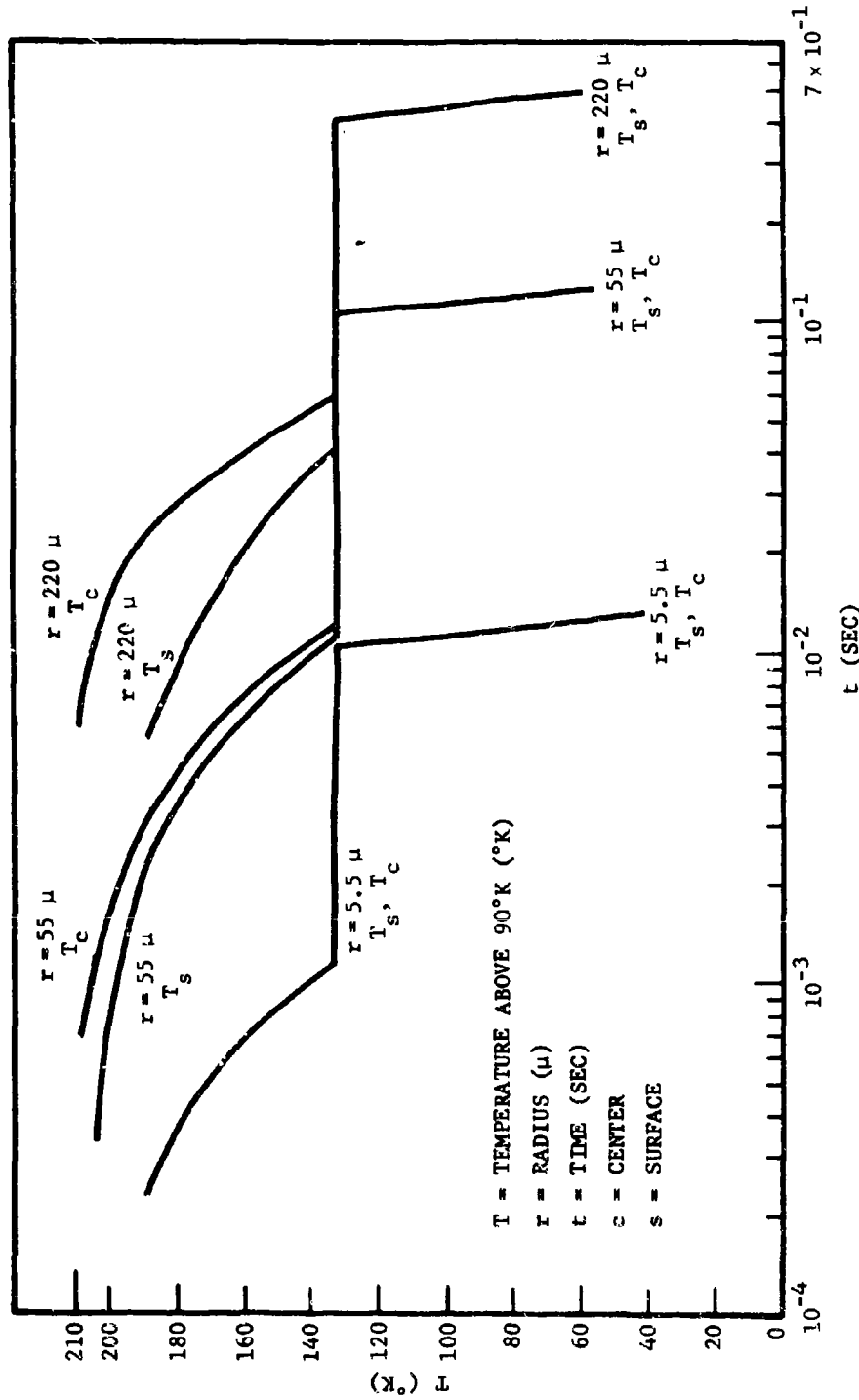


Fig. 7-8 Temperatures of the Surfaces and Centers of RP-1 Spheres as a Function of Time for Various Radii

transfer coefficient appropriate to the successive boiling regimes prevalent in the cryogenic at the particle surface. Boiling heat transfer coefficients reported in the literature have been measured under nontransient conditions at the surface of solid metal heaters immersed in cryogenic. These coefficients have varied in different investigations by factors of 2 to 5. In our calculation of particle cooling curves, the heat transfer surface (a spherical RP-1 droplet) is liquid during part of the cooling process, and the heat transfer process is transient rather than steady, so that the heat transfer coefficient assumed to prevail at the particle surface was even more uncertain than the reported experimental values.

To place the cooling curve calculations on a more secure basis, it was decided to obtain an experimental measurement of the boundary heat transfer coefficient under conditions directly applicable to the calculations, i.e., at the boundary of a cooling RP-1 fuel particle in cryogenic. The experimental method sought was the least sophisticated one capable of providing the desired measurement. The precision desired was only that required to determine whether the heat transfer coefficient at the boundary of cooling particles was within the range reported for steady boiling heat transfer from a solid heater in cryogenics or whether it was significantly outside this range.

Basically the measurement consisted of obtaining an approximation to the heat transfer rate by measuring the rate of vaporization of liquid cryogenic^{*} surrounding an initially liquid RP-1 particle that was injected under the cryogenic surface. Measurement of the bubble volume in time, together with the diameter of the responsible particle, was accomplished through photographic recording of the region.

While the particle density is greater than that of the cryogenic, the vapor formation surrounding the particle results in the ascension of both to the surface during film boiling. In order to maintain the particle's position below the surface and within camera view throughout a sufficient portion of the cooling period, an arrangement permitting an adjustable, downward fluid flow was used. This consisted of a vertical glass tube (1/2 in. in diameter) dilated at the upper end to form a reservoir and a coneshaped rod, which was

* Liquid nitrogen was used in place of liquid oxygen.

Table 7-2

CALCULATION OF LAYER THICKNESSES REACTED AS FUNCTIONS OF PARTICLE SIZE FOR TWO INITIAL RATIOS OF LO₂/FUEL

Part 1: Initial LO₂-to-Fuel Ratio of 3:1

| 1 | 2 | 3 | 4 | 5 | 6 | 7 | 8 |
|------------------------------------|--------------|-----------------------------------|-----------------------------------|-----------------------------|---------------------------|-----------------------|----------------------|
| REACTED WEIGHTS W _T (%) | LAYER* (% D) | END-POINT LO ₂ /FUEL** | TNT YIELD AT END-POINT RATIO† (%) | CHARGE WEIGHT REACTED†† (%) | TNT YIELD FOR CHARGE† (%) | PARTICLE SIZE D** (μ) | LAYER THICKNESS‡ (μ) |
| 100 | 100 | 3 | 132 | 100 | 132 | 2 | 1 |
| 80 | 41.5 | 3.75 | 122 | 95 | 116 | 5.2 | 1.07 |
| 64 | 28.8 | 4.7 | 102 | 91 | 93 | 27 | 3.7 |
| 51 | 21 | 5.9 | 80 | 97 | 70 | 130 | 13.7 |
| 40 | 15.7 | 7.5 | 59 | 85 | 50 | 520 | 41 |
| 32 | 12.2 | 9.4 | 42 | 83 | 35 | 1,400 | 85 |
| 25 | 9.2 | 12.0 | 25 | 81 | 20.4 | 3,900 | 180 |
| 20 | 7.3 | 15.0 | 15 | 80 | 12 | 7,100 | 260 |
| 16 | 5.7 | 18.75 | 8.3 | 79 | 6.5 | | |

NOTE: Both parts of table based on charge weight of 27 gm.

* $1 - (1 - W_T)^{1/3}$

† From Fig. 7-2

†† From Fig. 7-3

** $\frac{(3/W_T)}{1}$

‡ $(3 + W_T)^{1/4}$

□ $\frac{(Col. 2 \times 7)}{2}$

‡ Col. 4 x 5

inserted in the lower end of the tube and which provided a means of controlling the flow rate. (Particles entering the nucleate boiling region proceeded downward even though the flow was stopped.)

Vapor generation through heat transfer to the liquid by other than the particles required minimization to the extent that particle-generated bubbles could be distinguished. To this end, heat conduction was minimized by vacuum-jacketing the tube, including the reservoir, and immersing the lower portion of the tube in cryogenic. Radiation was minimized by suitably covering all but the camera viewing region.

Energy from the RP-1 particles is not entirely consumed in the nitrogen phase change. For example, volume changes of the gaseous envelope of a particle cause changes in the height of the center of mass of the liquid above it as well as causing changes in the liquid surface energy at the liquid-gas interface. Temperature rises of the gaseous envelope above the liquid nitrogen temperature also account for some energy. Estimates of the relative magnitudes of energy, however, indicate that most of the energy is consumed in the phase change.

To that approximation, then, the heat transfer rate is given as

$$q = \frac{Ln}{\pi D_p^2 \Delta t}$$

where q is the average heat per unit area per unit time over a time Δt from a particle of diameter D_p , n is the number of moles of liquid nitrogen that are vaporized in Δt (commencing at the beginning of bubble formation), and L is the heat of vaporization of nitrogen (1,330 cal/mole). Then n is

$$n = \frac{p\pi D_b^3}{6RT}$$

where D_b is the bubble diameter (assuming $D_b \gg D_p$) and p and T are the pressure and temperature of the gas. Thus

$$q = \frac{LpD_b^3}{6RT\Delta t D_p^2}$$

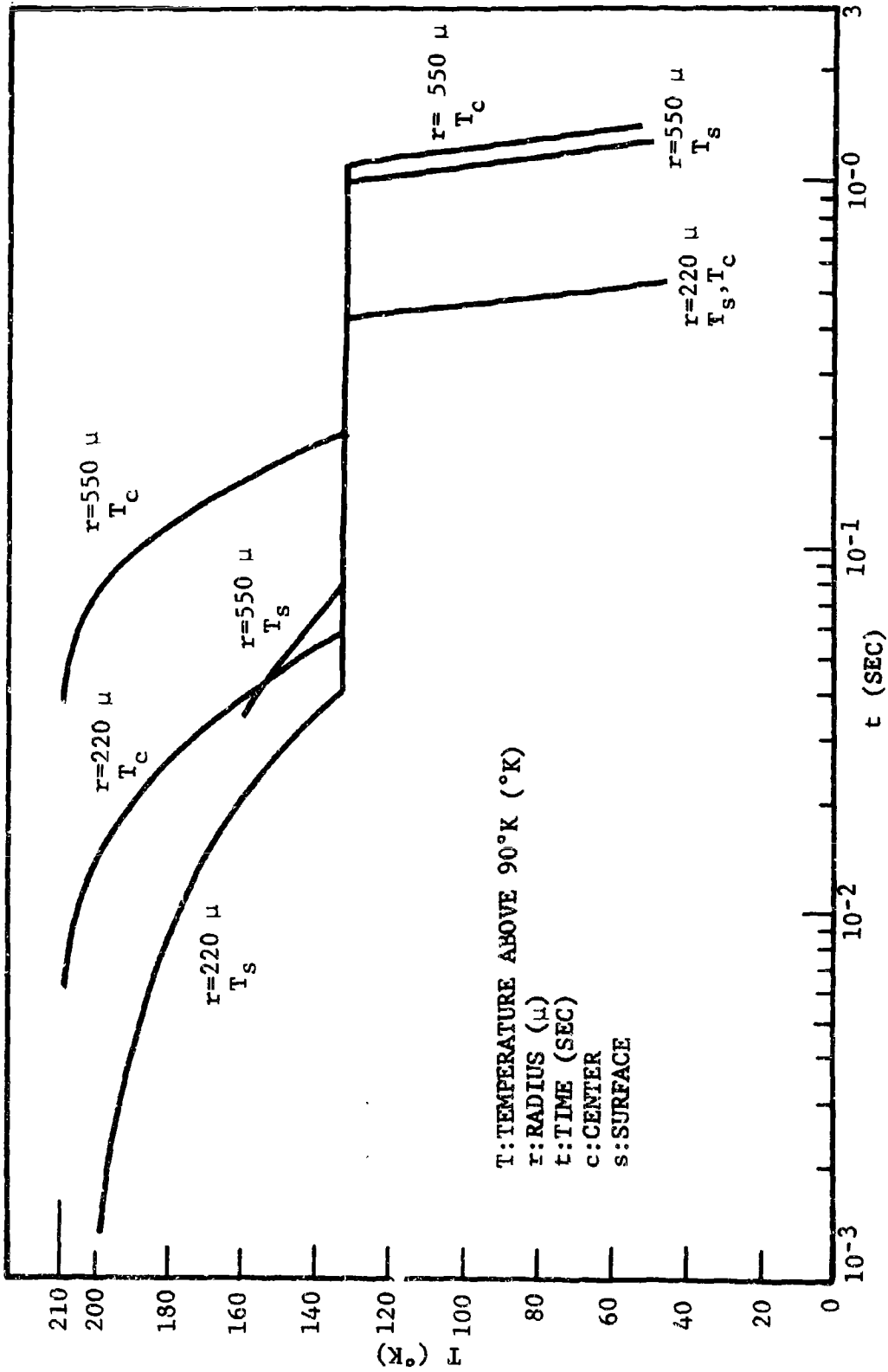


Fig. 7-9 Temperatures of the Surfaces and Centers of RP-1 Spheres as a Function of Time for Various Radii

Table 7-2, (cont.)

Part 2: Initial LO₂-to-Fuel Ratio of 5:1

| 1 | 2 | 9 | 10 | 11 | 12 | 13 | 14 |
|------------------------------------|--------------|-----------------------------------|-----------------------------------|---------------------|----------------------------|-----------------------|----------------------|
| REACTED WEIGHTS W _r (%) | LAYER* (% D) | END POINT LO ₂ /FUEL** | TNT YIELD AT END-POINT RATIO† (%) | CHARGE WEIGHT†† (%) | TNT YIELD FOR † CHARGE (%) | PARTICLE SIZE D†† (μ) | LAYER THICKNESS‡ (μ) |
| 100 | 100 | 5 | 96 | 100 | 96 | 28 | 14 |
| 80 | 41.5 | 6.25 | 72 | 97 | 69 | 55 | 11.4 |
| 64 | 28.8 | 7.8 | 52 | 94 | 49 | 88 | 12.7 |
| 51 | 21 | 9.8 | 35 | 92 | 32 | 130 | 13.7 |
| 40 | 15.7 | 12.5 | 21 | 90 | 19 | 175 | 13.7 |
| 32 | 12.2 | 15.6 | 12 | 89 | 10.6 | 210 | 12.8 |
| 25 | 9.2 | | | | | | |
| 20 | 7.3 | | | | | | |
| 16 | 5.7 | | | | | | |

NOTE: Both parts of table based on charge weight of 27 gm.

* $1 - (1 - W_r)^{1/3}$

** $\frac{(5/W_r)}{1}$

† From Fig. 7-2

†† $(5 + W_r)^{1/6}$

‡ From F. 7-4

□ $\frac{(\text{Col. 2 x 13})}{2}$

‡ Col. 10 x 11

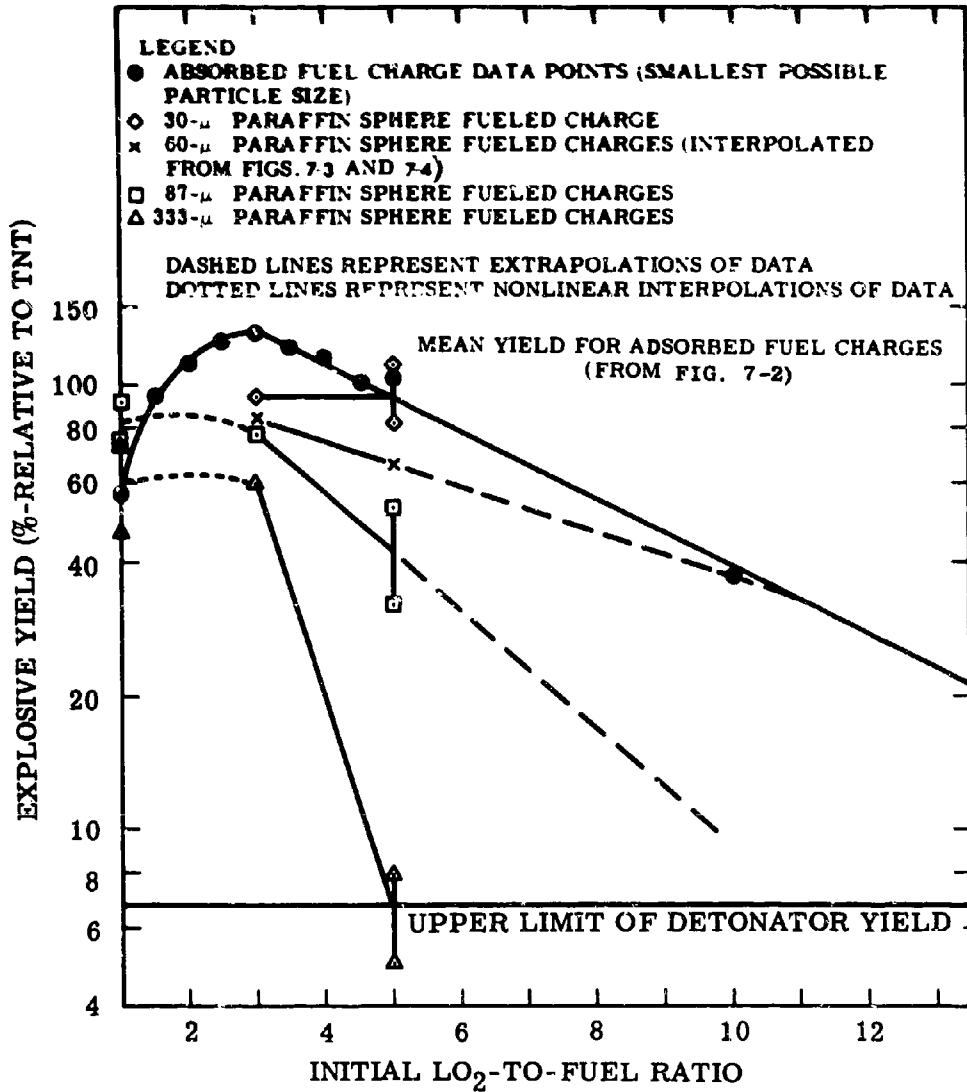


Fig. 7-6. Summary of Current Data and Hypotheses Regarding Yield Dependence on Fuel Particle Size

or if more than one bubble is generated,

$$q = \frac{L_p}{6RTD_p \Delta t} \sum D_b^3$$

The experimental results are indicated in Table 7-3 and a comparison is made in Fig. 7-10 of the measured heat transfer rates with those of the pool boiling region of the characteristic curves for LN₂ and LO₂ taken from Ref. 7-7, p. 157. The measurements were average heat transfer rates over an interval of time, which implies that they were average rates over a temperature interval (confined to the film boiling region). However, during the phase change of an RP-1 particle, the particle temperature remains relatively constant (the RP-1 freezing temperature) throughout a larger portion of the cooling

Table 7-3

EXPERIMENTAL HEAT TRANSFER COEFFICIENTS

| Particle Radius (μ) | q (cal/cm ² -sec) | Duration of Measurement (sec) | Number of Bubbles Generated |
|---------------------|------------------------------|-------------------------------|-----------------------------|
| 250 | 2.8 | 0.05 | 3 |
| 425 | 3.1 | 0.09 | 5 |
| 500 | 0.15* | 0.12 | 2 |
| 500 | 8.4 | 0.05 | 6 |
| 500 | 8.8 | 0.05 | 10 |
| 680 | 3.8 | 0.04 | 4 |
| 680 | 5.0 | 0.05 | 5 |
| 1,100 | 1.9 | 0.07 | 6 |

*The generation of only two bubbles over a relatively long period resulted in an unusually low heat transfer rate from this particle. This was apparently due to the particle having less motion through the fluid than others. Determining if such rates are common would require more extensive observations.

Ultimately, tests with a charge size 8 times larger by weight (8 oz), or 2 times larger in all linear dimensions than the 27-gr charges, are required to provide a preliminary verification of the hypothesis that particles twice as large will then be consumed to the same degree. If this hypothesis is verified at the 8-oz charge size, it would be desirable to determine if it can weather an additional factor of 5 in linear dimension scaling, or a total linear scaling range of 20 to 30.

HEAT TRANSFER STUDY

The explosive yield of LO_2 /RP-1 mixtures is known to depend on the particle size or interface area per unit volume of the propellants. Breakup of RP-1 is assumed to proceed by small drops being separated from the larger mass of fuel that is in contact with LO_2 . The small drops of RP-1 freeze rapidly in the LO_2 to form a fine-grained mixture capable of large explosive yield. The question arises as to whether a major proportion of the fuel particles in a fine-grained mixture in LO_2 could, at any time in the breakup process, be in that stage of their cooling process in which they are individually enclosed in gaseous oxygen envelopes. The possibility of such a situation developing will obviously depend on the cooling times of small RP-1 particles in LO_2 being of the same order of magnitude as the time required for the entire fuel mass to break up into drops. The importance of the situation, if it does, in fact, occur, is that charges composed of particles with gaseous envelopes (due to boiling at the particle surface) might have explosive properties different from those of charges in which the fuel-particle surfaces are in contact with oxygen in the liquid phase. A charge composed of particles of fuel enveloped in gaseous oxygen would necessarily be reduced in charge density because of the presence of the boiling films on the fuel-particle surfaces. It might also develop lower explosive yield, owing to the lower concentration of oxygen in the gaseous film at the fuel surface and the consequently less favorable conditions for a rapid reaction to support the detonation wave.

The cooling rates of small RP-1 particles in LO_2 have been calculated as a part of the study of this phenomenon. The objective was to determine whether the time required for small RP-1 particles to cool in LO_2 is of the

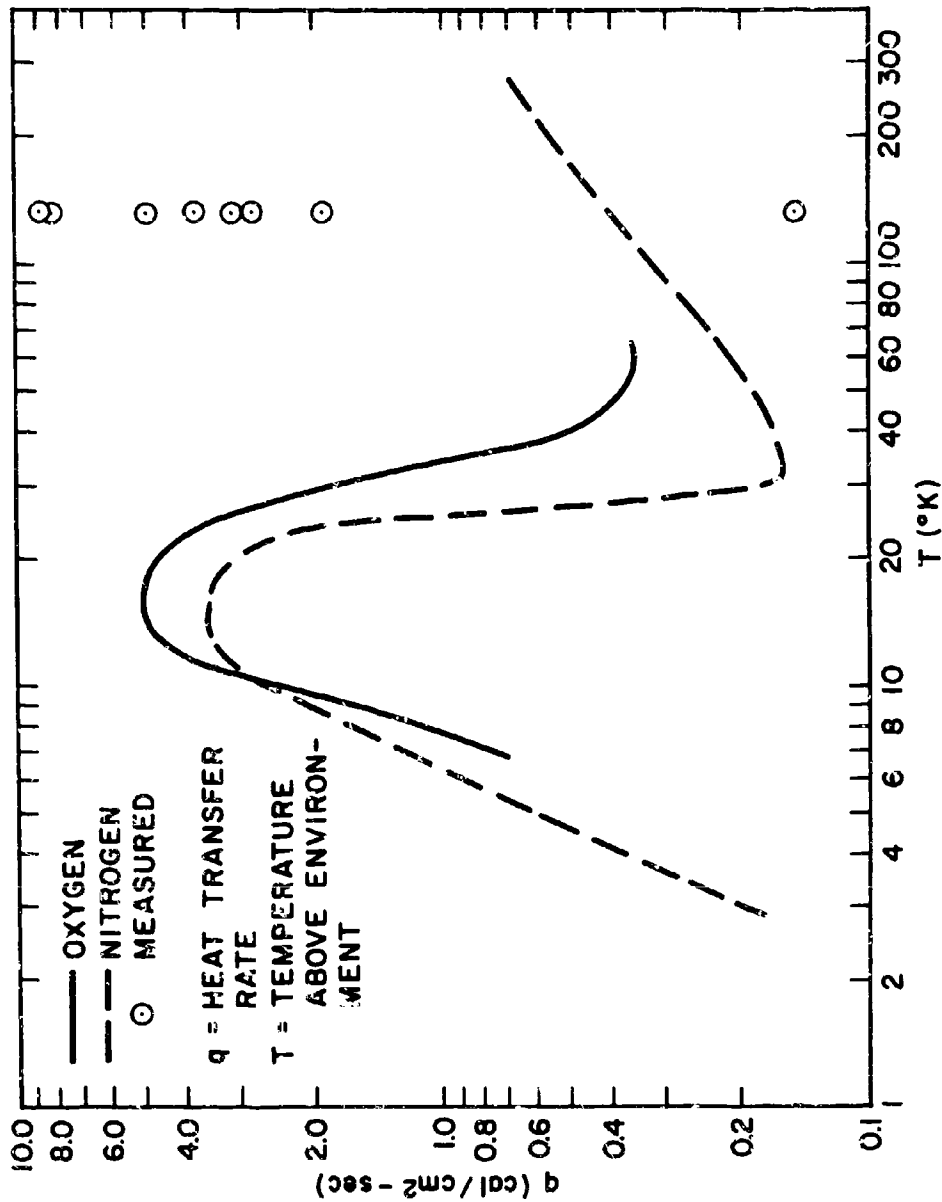


Fig. 7-10. Comparison of Measured Heat Transfer Rates and Typical Pool Boiling Curves

same order of magnitude or much less than the time required for large masses of propellants to break up into explosive mixtures. If the cooling times were an order of magnitude (or more) less than the time required for breakup of major propellant masses, it could be concluded that only an insignificant proportion of fuel drops capable of contributing to explosive yield could be in the cooling process at a given point in time, and hence, the problem of their possible effect on the yield could be ignored.

In addition to providing this insight, the cooling rate of small particles of RP-1 fuel in LO_2 is of interest because it is one aspect of the overall heat transfer process between RP-1 fuel and LO_2 . It is thought that the boiling heat transfer between the fuel and LO_2 may be the major source of agitation causing mixing of the two propellants in most cases.

The fuel-particle size range of interest was that capable of producing substantial explosive yields when charges so constituted were detonated when mixed with LO_2 . The determination of this range of fuel particle size and its variation with charge size was the objective of our program, described elsewhere in this section, to study the effect of fuel-particle size on the explosive yield of charges composed of LO_2 and paraffin particles. The latter investigation was not continued beyond the smaller charge size, so that no information was obtained as to what fuel-particle sizes could be expected to produce large explosive yields at the larger scale sizes.

The size of the largest particles of interest in the investigation of fuel-particle cooling rates is, therefore, not presently known. Owing to this fact, the calculation of particle cooling rates was limited to particles not larger than about 1 mm in diameter for the present.

In the course of calculating the particle cooling rates, it was found that the weakest point in the calculation was the value to be assigned to the boiling heat transfer coefficient for LO_2 boiling on the surface of RP-1 fuel particles. To strengthen this part of the work, an experimental measurement was made of this heat transfer coefficient.

period and also throughout most of the measurements. In Fig. 7-10, therefore, the measured values are plotted at the RP-1 freezing temperature. The RP-1 heat transfer rates are seen to be in the neighborhood of, but slightly higher than, those for other cases.

Complete agreement of the data is not to be expected, owing to the large number of variables known to affect boiling heat transfer and the fact that the RP-1 fuel particles constitute a heat transfer surface different from any for which heat transfer rates have been reported heretofore. A systematic presentation of the effect of variables affecting boiling heat transfer has been made by J. W. Westwater (Ref. 7-8), in which data are presented to show that a factor of two in boiling heat transfer coefficient can result from a change of material composing the heat transfer surface alone. Comparable effects attend changes in other significant variables.

Since the measurements were made in an effort to decrease the uncertainty of calculations of cooling times based on heat transfer coefficients reported for fixed, solid heater surfaces and since the results suggest that somewhat higher coefficients are more appropriate for RP-1 spheres, the effect of applying higher values on cooling times (ranging from 2.5 to 10 times) for particles having radii of 5.5 and 220 μ has been illustrated in Figs. 7-11 and 7-12. A relationship closely approximating an inverse is seen to exist between increase of heat transfer coefficient and time to reduce temperature difference between particle surface and LO_2 to a small value, say, 40°K. That is, increasing the heat transfer coefficient by a factor of ten requires one-tenth the time to reach $\Delta T = 90^\circ\text{K}$.

Throughout the experiment, there occasionally were particles observed in which the manner of bubble generation was distinctly different in that relatively small bubbles left the particle but at a relatively high rate. Further, the particle had no observable gaseous envelope, and on all occasions such particles descended through the liquid, suggesting that there was probably little or no gaseous envelope. These particles had radii less than about 450 μ (however, only four such particles were observed) and were observed at times greater than about 0.15 sec. They had apparently extended into the nucleate boiling region. In particular, two of the particles had radii of 425 and 340 μ

Calculation of Particle Cooling Rates

In the computation of the particle cooling rate, it is assumed that the particles remain spherical in shape. This assumption is valid for particles sufficiently small that forces due to surface tension greatly exceed those due to gravity and inertia. In the case of larger masses of fuel, for which gravity and inertial forces predominate, the agitation of the fluids due to boiling heat transfer between RP-1 and LO₂ can be expected to act on the mass of fuel, continuously deforming its shape, and temperature gradients within the mass at any time must also be a function of the changing shape of the mass of fuel. Any analytical approach to the calculation of the cooling rates of the larger masses of fuel in LO₂ would need to consider the changing shape of the fluid mass and its effect on the temperature gradients within the mass.

Within the particle of fuel, heat is transferred by conduction (molecular diffusion of heat) and possibly by convection as long as the fuel remains liquid within the particle. Similarity of systems undergoing free convection is measured in terms of Grashof's number:

$$G = \frac{\beta \Theta L^3 \rho^2}{\mu}$$

where

β = thermal coefficient of expansion

Θ = temperature

g = acceleration of gravity

L = a characteristic linear dimension of the system

ρ = density of the fluid

μ = fluid viscosity

The appearance of L to the third power in Grashof's number indicates that free convection within otherwise similar particles of fuel must decrease very rapidly as their diameter is reduced. It has therefore been assumed,

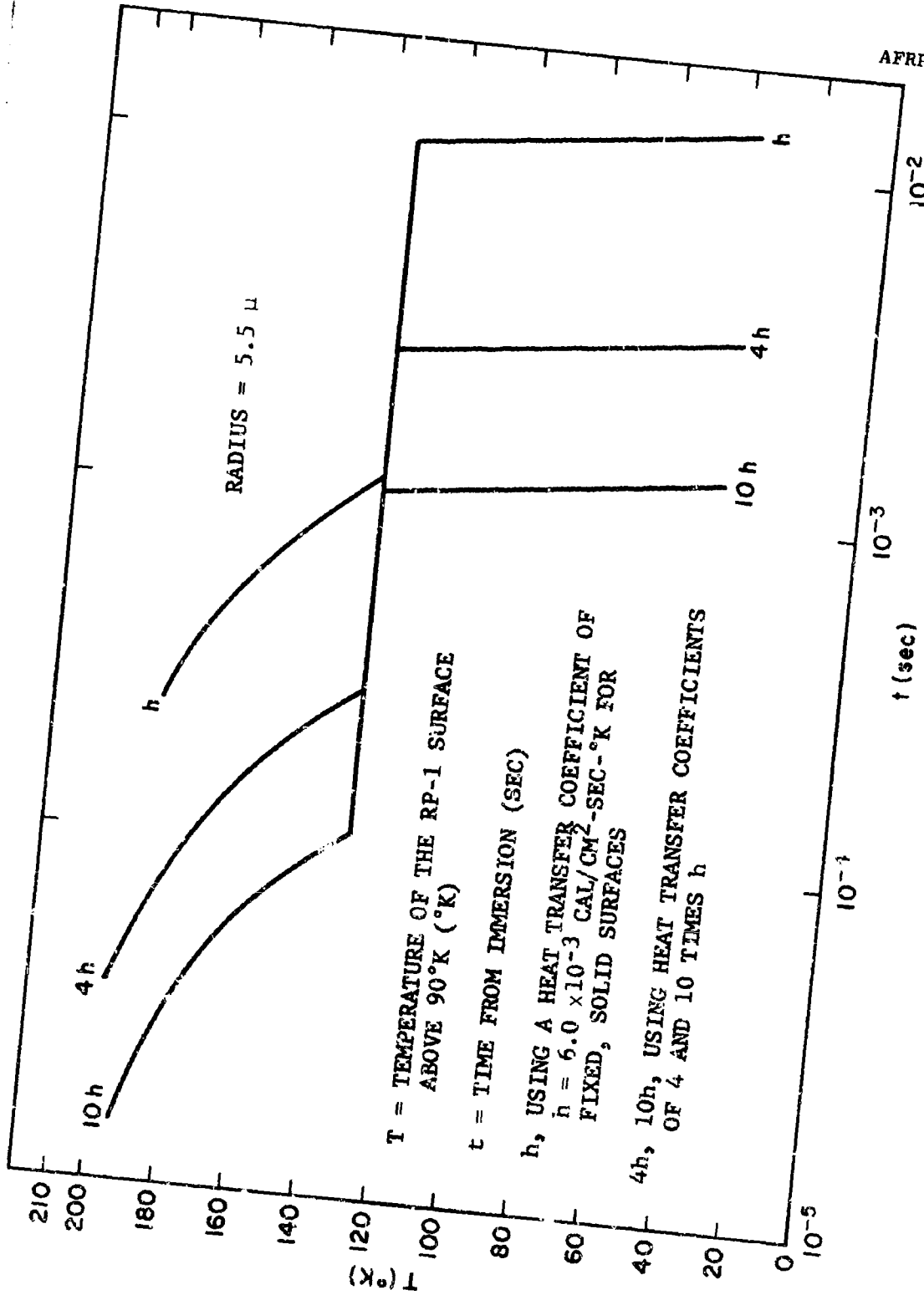


Fig. 7-11. Cooling Curves of 5.5- μ RP-1 Spheres in Liquid Oxygen

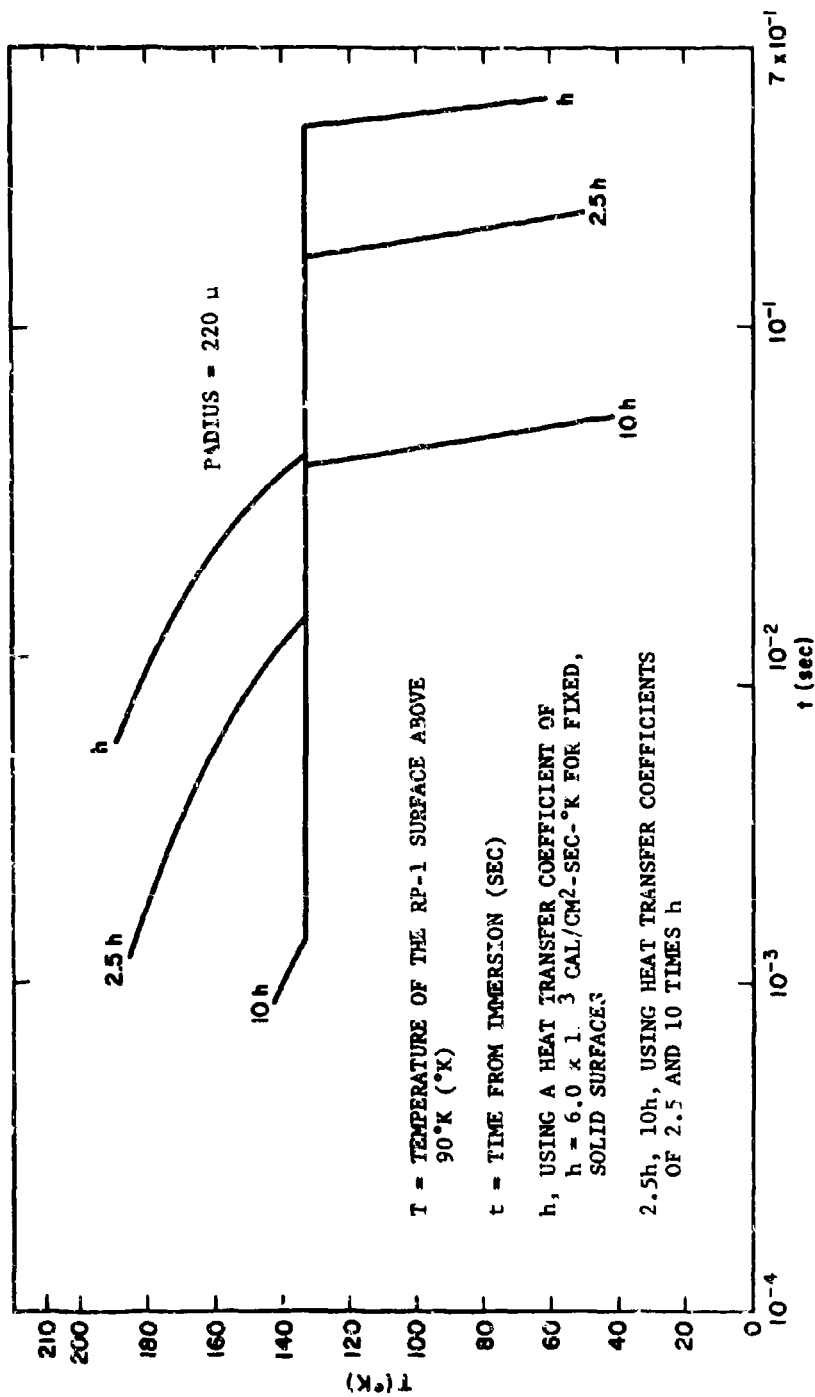


Fig. 7-12. Cooling Curves of 220 μ RP-1 Spheres in Liquid Oxygen

that fuel particles small enough to maintain their shape approximately constant will support negligible internal convection.

For these particles, the cooling process can be represented as conduction of heat within a sphere with a variable flux at the boundary. The boundary flux is that corresponding to the LC_2 boiling regime prevalent at the particle surface.

No satisfactory theoretical derivation of the flux obtained in boiling heat transfer is available, so that it is best to use experimentally determined values. Representative curves are shown in Fig. 7-7, in which the heat flux is plotted against the difference in temperature of the heat transfer surface and the saturation temperature of the boiling liquid. The curves are not single-valued, i.e., the same heat flux can be obtained at more than one value of temperature difference.

This effect is attributed to the existence of two different regimes of boiling heat transfer, nucleate and film boiling, with a third transition regime between them. It is worth noting that the boiling heat flux changes by a factor of 10 to 20 in passing from the nucleate boiling maximum to the minimum at the start of the film boiling region.

The heat transfer fluxes corresponding to the same value of temperature difference have varied in different investigations by factors of 2 to 5. The cause of this variation is not known, but is thought to be due to variations in the condition of the heat transfer surface. Richards, Steward, and Jacobs in National Bureau of Standards Technical Note No. 122, dated October 1961, have surveyed the literature on heat transfer from solid surfaces to cryogenic fluids. Their conclusion states, in part, "The existing experimental data on heat transfer between solid surfaces and cryogenic liquids (helium, hydrogen, nitrogen, and oxygen) vary appreciably between experimenters, even when heater geometries and orientations, pressures, etc. are comparable. The variations are both in the magnitude of the heat flux and in the shape of the heat-flux-versus-temperature-difference curves, and are possibly due to uncontrolled parameters such as surface roughness and contamination."

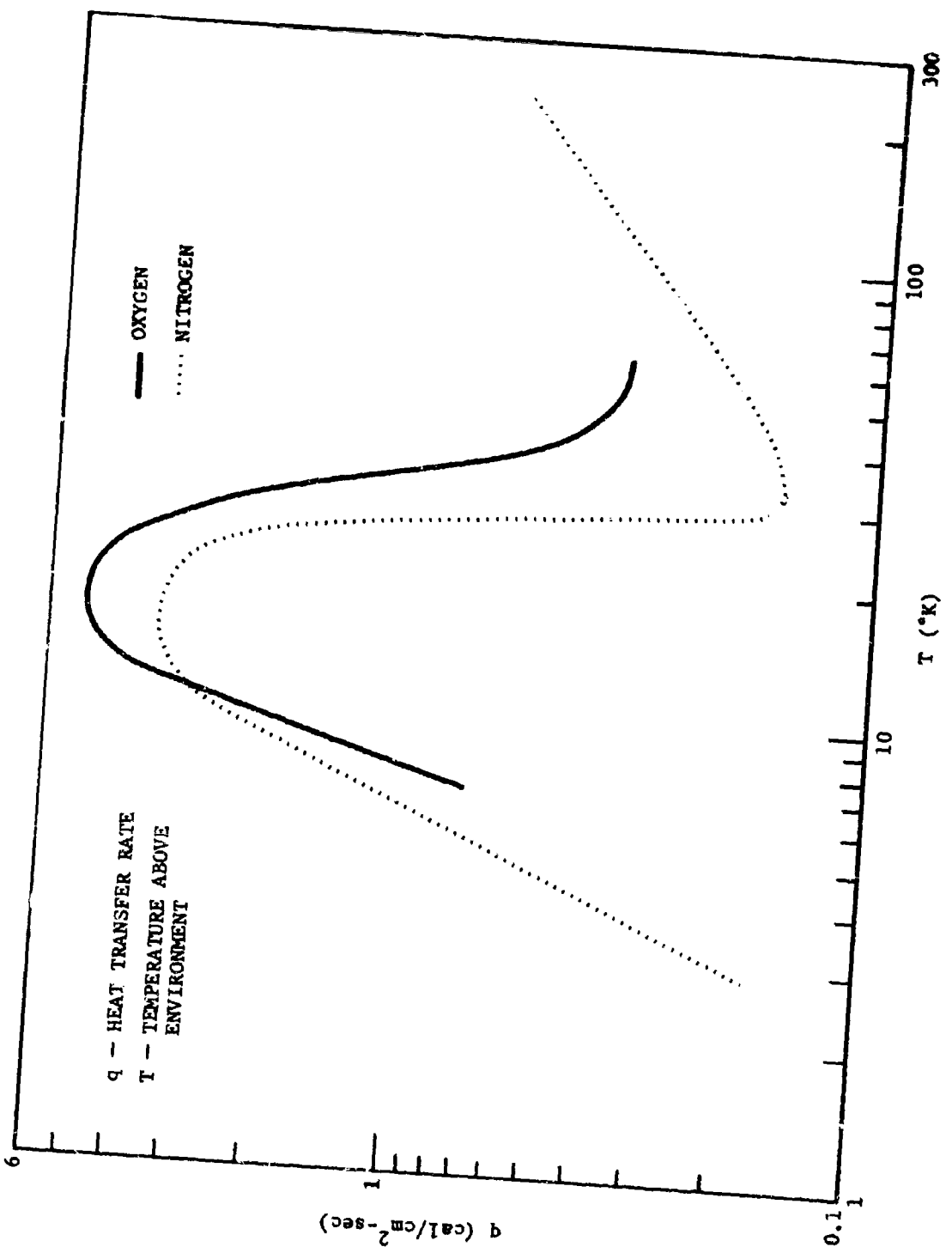


Fig. 7-7 Representative Curves Relating the Flux Obtained as a Function of the Difference in Temperature Between the Heat Transfer Surface and the Saturated Liquid for LO₂ and LN₂

and were observed beginning 0.27 and 0.54 sec after injection. Two other particles, too small to resolve, left bubble trails commencing at 0.16 and 0.21 sec.

Another phenomenon clearly observed only once was the sudden disintegration of a particle. This was accompanied by the rapid generation of a large bubble apparently due to "new" surfaces at relatively high temperatures (for sufficiently large particles) suddenly being exposed to the cryogenic, with a consequent increase in area and heat transfer coefficient.

Summary

Application of the results of the fuel-particle cooling rate calculations to deciding the question of whether a significant fraction of the fuel particles composing a high-yield LO_2 /RP-1 mixture can be in the film boiling, regime at any instant of time is not presently possible because it is not yet known what fuel particle sizes contribute to large explosive yield in charge sizes greater than 27 gm. Until this information is available and the cooling rate calculations are applied it is not known whether this study needs to be pursued further. If it is found that film boiling can occur simultaneously on a major fraction of the fuel particles capable of contributing to explosive yield, development of a test charge of known LO_2 /RP-1 ratio and particle size will be needed that would be capable of being formed with or without a boiling regime present at its LO_2 - fuel interfaces. The relative explosive yield in these two conditions would then be indicative of the importance of film boiling in affecting explosive yield.

MIXING STUDY

It is generally recognized that the mixing of propellants after a propellant spillage is an important process affecting the explosive yield of the mixture. The specific properties of such mixtures that determine their explosive yield are not yet well established, so that the term "mixing" can not presently be defined and quantified, but must instead simply be synonymous with potential explosive yield.

At present, the mechanism by which spilled propellants form explosive mixtures is not understood. In the case of cryogenic propellants, it is not known to what extent, and under what conditions, the mixing of the propellants is due to flow phenomena, such as turbulence and spattering on impact, or to the effects of boiling heat transfer, whereby the agitation due to boiling of the cryogenic may break up the warmer propellant of the combination to produce a larger heat transfer interface and, hence, more violent boiling.

The importance of understanding the mechanism by which mixing proceeds in various circumstances stems from the fact that the variables affecting the mixing process can not be deduced without some knowledge of the mechanism involved. Thus, if one sought to assess the effect on its explosive potential of changing one of the variables of the propellant spillage process, it would be necessary to know the principal mechanism by which mixing was proceeding in order to deduce the effect of that variable on the process and on explosive yield. In addition it is necessary to know the important variables affecting the mixing process in each of the configurations in which explosive testing is conducted so that correlation of yield data between scale sizes may be accomplished.

The following proposes a method by which effects of mixing due to flow conditions can be separated from those due to heat transfer effects. The method would apply only to the study of those effects in the $\text{LO}_2/\text{RP-1}$ propellant combination; however, the results should, by analogy, aid in understanding the mixing process in other cryogenic combinations.

The method depends on the use of a simulant for RP-1 capable of forming explosive mixtures with LO_2 to give the same yield as RP-1, but differing from RP-1 by remaining liquid at 90.1°K , the normal boiling point of LO_2 . The explosive yield of spillage explosions using the simulant fuel, precooled to 90.1°K , would be compared with those of RP-1/ LO_2 spillages under otherwise identical conditions, but with the RP-1 initially at room temperature. The simulant fuel, having been cooled to 90.1°K before the spill, would transfer no heat to the LO_2 , so that any mixing would be due to phenomena other than heat transfer, such as flow or impact conditions. The RP-1/ LO_2 spillage in the same conditions could mix by both a heat transfer and/or flow mechanism.

No experimental data on the film boiling of liquid oxygen under free convection were found. It was assumed that the curve for liquid oxygen in the film boiling region would be parallel that for liquid nitrogen in this region. All of the experimental measurements of boiling heat transfer appear to have employed solid heat transfer surfaces, whereas fuel particles are liquid in part of the cooling process.

In the case of small fuel particles in LO_2 , the uncertainties in the boiling heat flux (indicated above) are increased because the heat flux at the particle surface changes rapidly in time, whereas the experimental measurements of boiling heat transfer referred to above have been made under nontransient conditions.

The uncertainties in the boiling heat transfer rate limit the precision it is profitable to seek in calculating the cooling rate of fuel particles. Inspection of the curve for liquid nitrogen in Fig. 7-7 indicates that the boiling heat transfer coefficient, defined as the boiling heat flux divided by the corresponding difference in temperature between particle surface and saturated liquid, changes by about 50 percent as the particle surface cools from room temperature to the lower end of the film boiling regime. Within this film boiling region, therefore, a constant heat transfer coefficient, taken as the average over the region, represents the boiling heat transfer coefficient as well as it is known. A constant heat transfer coefficient was, therefore, assumed for LO_2 within the film boiling region.

In obtaining the heat flow within the particle, it is necessary to account for the freezing of the RP-1 during the cooling process. Analytical solutions of the diffusion of heat with change of phase do not include the case of constant flux or constant heat transfer coefficient at the boundary for spherical geometry (Ref. 7-5, p. 291). A solution is available, however, for diffusion of heat in a sphere with constant heat transfer coefficient at the boundary if no change of phase occurs (Ref. 7-5, p. 237). This solution has been presented as a chart by P. J. Schneider (Ref. 7-6, p. 97). In applying this solution to the case of RP-1 fuel particles in the period prior to the onset of freezing in the particle, it was found that within particles of diameter up to about 1 mm, the temperature gradients were very

The relative explosive yields measured for the two kinds of fuels in the same spillage geometry would constitute a measure of the fraction of total mixing due to flow conditions alone for that geometry. One would expect this fraction to be a maximum under violent flow conditions, such as impact, and very low for in-tank mixing.

Butene-1 ($\text{CH}_3\text{-CH}_2\text{-CH}=\text{CH}_2$) appears to be an optimum RP-1 simulant for the method outlined above. Some properties of butene-1 are tabulated below along with those of RP-1.

| Fuel | <u>Hydrogen Carbon Ratio</u> | <u>Heat of Combustion (Kcal/gm) (H₂O liq., CO₂ gas)</u> | <u>Freezing Point (°C)</u> | <u>Boiling Point (°C)</u> |
|----------|----------------------------------|---|------------------------------------|-----------------------------------|
| RP-1 | 1.9 | 10,370 | -40 max. | 185 to 274 |
| Butene-1 | 2.0 | 11,490 | -185.35 | -6.25 |

The hydrogen-to-carbon ratio and heat of combustion, which largely determine the explosive yield of hydrocarbons in perfect mixtures with LO_2 , differ by 5 and 11 percent, respectively. The explosive yield does not change linearly with these variables, however, so that the difference in explosive yield between charges of otherwise identical composition containing RP-1 and butene-1 should be much less than 11 percent. This question can be resolved experimentally by comparing the explosive yield of kieselguhr charges containing butene-1 with that of charges containing RP-1.

Because the butene-1 would be cooled to the close vicinity of 90.1°K (normal LO_2 boiling point) before being spilled to form the mixture with LO_2 , its viscosity in this temperature range is of importance with respect to its simulation of the flow properties of RP-1 at room temperature and slightly below when spilled in the same geometry.

Some preliminary measurements have been made of the viscosity of butene-1 near the boiling temperature of LO_2 . These measurements are tabulated below.

first, i.e., at any time after the start of cooling, the centers of the spheres differed by only a few degrees from the temperatures of their surfaces. Thus, all parts of the spheres reached the freezing temperature of the fuel nearly simultaneously. For this range of sizes, negligible error resulted if the cooling of the particle to the bottom of the film boiling regime of LO_2 was calculated in three separate stages. In the first stage, the cooling curve of the liquid particle was obtained from room temperature to the freezing point of RP-1 fuel, by means of the solution for constant boundary heat transfer coefficient. In the second stage, the time for the latent heat of fusion of the fuel contained in the particle to be removed was computed by means of the rate corresponding to the boiling heat flux prevalent at the particle surface at the fuel freezing temperature. In the third stage, the cooling curve for the solid particle was obtained from the fuel freezing point to the bottom of the film boiling regime of LO_2 by the same method used in the first stage.

The results* are presented in Figs. 7-8 and 7-9, in which the temperatures at the surfaces and centers of spheres of RP-1 fuel of various sizes up to about 1 mm diameter are shown as a function of time after immersion in LO_2 . The time difference between the cooling curve for the surface and center of each particle becomes greater as particle size increases, indicating steepening of the temperature gradients within the particle as diameter increases. The error of the method of calculation should be negligible as long as the time difference between the cooling curves for the center and surface of the particle is small compared with the time the particle temperature remains at the RP-1 freezing temperature.

Experimental Measurement of Boiling Heat Transfer Coefficients of LN_2 on RP-1 Particles

As indicated in the foregoing, the major uncertainty in the calculation of the cooling curves by any method is in specifying the magnitude of heat

* Values of heat transfer coefficient and RP-1 thermal properties used in the calculation are: heat transfer coefficient = 6.0×10^3 cal/cm²-sec-°K; heat of fusion = 50 cal/gm; thermal conductivity = 3.32×10^{-4} cal/cm²-sec-°K; density = 0.8 gm/cm³; and heat capacity = 0.2 cal/gm-°K.

| <u>Temp</u> <u>(°C)</u> | <u>Viscosity</u> <u>(cp)</u> | |
|----------------------------|---------------------------------|-------------------|
| -182 | 620 | |
| -180 | 200 | 99% pure butene-1 |
| -169 | 6.3 | |
| -180 | 116 | |
| -170 | 10.7 | 95% pure butene-1 |

Boiling Point $LO_2 = -183.0^\circ C$

The viscosity of RP-1 is specified as not greater than 13.2 cp at $-34.4^\circ C$, and is about 3 cp at room temperature.

Summary

It appears that butene-1, precooled near $90.1^\circ K$, can be made to simulate the flow of RP-1 to a satisfactory approximation for spillages in the range of 40 lb or greater. A positive statement in this regard is not easy to make, because it must be expected that the viscosity of RP-1 increases during the course of its flow due to its being cooled by LO_2 , while the viscosity of precooled butene-1 should either remain constant or decrease during the flow. Much light could be shed on this question by examining high-speed photos of butene-1 and RP-1 in the same flow condition. If the flow velocities and patterns at the same time after spillage were substantially the same for the two cases, it could be assumed that viscosity differences had negligible effect on the flow.

The logical next steps in pursuing this portion of the mixing study are listed below.

- a. Conduct tests of butene-1 with LO_2 in kieselguhr charges to confirm its explosive equivalence to LO_2 /RP-1 charges when perfectly mixed.
- b. Photograph spill tests of cold butene-1 and of RP-1 at room temperature to establish similarity of flow of the two fuels.
- c. Conduct scaled explosive tests in appropriate mixing modes with cold butene-1 as fuel simulant for comparison with otherwise identical tests using RP-1 at room temperature.

- (i) Choose at least one mixing mode having low kinetic energy in fluid flow, but having easily measurable yield with RP-1 as fuel.
- (ii) Choose at least one mixing mode having high kinetic energy in fluid flow, but with measurable yield with RP-1 as fuel.

RECOMMENDED ADDITIONAL WORK

As discussed in the introduction to this section the studies described above constituted the initial phase of an overall program to investigate the basic phenomena involved in propellant mixing and explosion processes. To properly pursue this program requires proceeding as indicated with the above areas of work and initiating certain additional studies as described below.

For determining the properties of the propellant mixture that determine its explosive characteristics, two additional study areas are of prime concern:

- Uniformity of Mix. Another aspect of spatial distribution besides charge geometry proposed as a possible definitive property of propellant spillages is nonuniformity of $LO_2/RP-1$ ratio and particle size through the mixture. If the explosive yield of propellant mixtures as a function of their particle size and $LO_2/RP-1$ ratio is known from the particle size studies, the effect of nonuniformity of $LO_2/RP-1$ ratio or particle size on explosive yield could be estimated by adding up the yield contributions from each part of the nonhomogeneous charge according to its local $LO_2/RP-1$ ratio and particle size. Measurement of the explosive yields of charges having known nonuniform distributions of paraffin particles can then be made in several charge sizes to test the accuracy of the estimated yields.
- Gross Interface Geometry (Layered-Charge Study). The possibility that distributions of the two propellants may commonly occur in which the explosion of well-mixed portions of the spillage could provide the impulse to force the mixing of adjacent volumes of bulk propellant in time for these previously unmixed propellants to contribute to the explosive effects has been suggested. It has been tacitly assumed heretofore that any development of interface between the two propellants, to be capable of affecting the explosive yield of the mixture, must occur prior to ignition. This assumption was based on the premise that explosive reaction occurring at propellant interfaces would generally drive the two propellants apart, thus terminating any further development of interface between them.

In certain mixing modes tested in Project PYRO, viz., the diaphragm-rupture tests with hypergolic propellants and the early-ignition diaphragm-rupture tests with cryogenic propellants, such an explosively driven termination of the mixing process does indeed appear to prevail, as evidenced by the very low yields obtained.

For mixing modes which lead to highly convoluted or multilayered boundaries, however, the available evidence suggests that significant additional mixing can occur after ignition. Presumably this would occur because reaction at one interface, although tending to separate the propellants in that region, can force them together at another interface.

Examples of mixing modes in this category included the Aerojet high-interface-area, low-velocity drop tests (Ref. 7-9) and the hypergolic high-velocity impact tests conducted under the PYRO program at NOTS.

The definitive properties of propellant mixtures of concern in regard to post-ignition mixing should be relatively easy to identify, and it should be relatively straightforward to determine their effects experimentally. Such a phenomenon, if it occurs, would probably be found in convoluted, interlayered or multiply connected spatial distributions of the propellants. A series of tests have, therefore, been planned in which the explosive yields of such charges can be measured. The charges would be composed of alternate masses of fuel and LO_2 , with conventional explosive in the form of sheets intervening.

The explosive sheets will provide a convenient simulation of explosive propellant mixtures at the interface between the bulk propellant masses, with the advantage that the explosive yield of the sheet explosive will be accurately known. Any measured explosive yield of such charges that is significantly greater than the known yield of the sheet explosive will be evidence of a contribution by the propellants due to post-ignition mixing.

The future course of the investigations of mechanisms leading to the development of the definitive properties in a propellant spillage will depend to some extent on the results obtained from the cold-fuel mixing study outlined earlier and thus cannot be defined in detail. However, the work would generally be directed toward study of fuel break up and study of more general flow processes.

In the fuel breakup study, once it has been determined what mechanism supplies the energy to break up RP-1 fuel in spillages with LO_2 , it should be possible to deduce the variables likely to be important in the process. Thus, if the breakup was due only to fluid friction, variables such as fluid viscosity, velocity, and characteristic length would be dominant. If boiling heat transfer supplied the breakup energy, variables such as thermal conductivity, heat transfer coefficient, interface area, etc. would be important. In any case, the tentative identification of controlling variables depends on the construction of a simplified theoretical model of the breakup process, in which the interactions of the various forces leading to breakup of the fuel are developed conceptually.

Following the development of a simplified theoretical model (or models) of the breakup process in the more important mixing modes tested in Project PYRO, a series of experimental tests would be conducted to find the dependence of fuel breakup on each of the hypothetical controlling variables predicted by the simplified theoretical model.

These experiments would employ a method by which the final surface area and other particulate properties of the fuel can be measured after the fuel breakup process is complete. The problem of representative sampling can be disposed of by collecting all of the fuel in each test for surface area measurement. The planned experimental approach involves use of a model regime in which the temperatures of all important processes would be about 75 to 100°C higher than in the actual event, but the temperature differences and the heat transfer phenomena would, as nearly as possible, be maintained the same as in the actual process. In this kind of model, all the heat transfer phenomena and the turbulence leading to fuel breakup should be substantially the same as in the actual process, but the melting point of the fuel would be slightly

above room temperature so that the examination and measurement of its particulate properties would be greatly facilitated. Molten wax, at an initial temperature about 65 to 75°C above its melting point, would simulate the fuel in the model. A mixture of two Freon compounds (Freon 14; CF_4 ; b.p. 145°K and Freon 23; CHF_3 ; b.p. 191°K) having a boiling point of about 165 to 180°K could be used to simulate LO_2 . The use of this simulant for LO_2 would provide for the initial temperature difference - as well as the temperature difference at the fuel freezing point between fuel surface and cryogenic - to be the same as the corresponding quantities in the LO_2 /RP-1 interaction being modeled. The heat of vaporization per unit weight of the LO_2 simulant would be nearly the same as that of LO_2 , so that the boiling heat transfer and the turbulence thereby produced should simulate the LO_2 /RP-1 interaction very well, assuming that the boiling heat transfer rate as a function of the temperature difference between fuel and cryogenic is the same for the Freon as for LO_2 .

The first use of the Molten wax results would be to establish whether the hypothetical controlling variables do, in fact, exert a major influence on the breakup of the fuel and, hence, to test the theoretical model from which they were deduced. The test results would ultimately form the basis of a correlation formula expressing the fuel particle size (or whatever aspect of particle size had previously been found to be definitive with respect to explosive yield) as a function of its controlling variables in dimensionless form, from which the fuel breakup scaling relationship would be obtainable.

The following steps are anticipated.

- a. Develop simplified theoretical models of fuel breakup in various mixing modes.
- b. Conduct measurements of interface area developed in chosen mixing modes by means of molten wax techniques, varying parameters suspected as controlling variables from simple theoretical models developed in item (a).
- c. Develop correlation formulae from data obtained in (b), expressing interface per unit volume as a function of controlling variables in dimensionless form.

The additional flow process study would primarily be concerned with those processes leading to propellant contact. The objective would be to find

whether the fraction of propellants that arrive in sufficiently close juxtaposition to one another to participate in the development of potential explosive yield change with scale and - if such a change is found - to correlate the fractions of propellants so engaged with the variables controlling their change.

The approach to be followed would be to develop simplified theoretical hydrodynamic models that best represent the major features of the fluid flow in each mixing mode, using as a guide, appropriate photography (taken at several scale sizes) of flow in the more important mixing modes tested in Project PYRO. From these theoretical models, expressions relating the fraction of each propellant to its maximum distance from an LO_2 /RP-1 interface as a function of time will be derived. The effect of scale changes on these relationships will be deduced.

In some geometries, the flow instabilities either due to fluid friction or boiling heat transfer may play a dominant role in determining the total fraction of propellants that participate in development of potential explosive yield. In such cases, it will be necessary to investigate the effect of scale changes on these processes.

Section 7
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Section 8
SUMMARY AND CONCLUSIONS

PURPOSE

The objective of Project PYRO was to develop a reliable philosophy for predicting the credible damage potential which may be experienced from the accidental explosion of liquid propellants during launch or test operations of missiles or space vehicles.

Such information is required for the siting of static test stands and launch facilities, for vehicle and payload design, for launch operations, etc. The propellant combinations of $N_2O_4/50\%$ UDMH - 50% H_2 , $LO_2/RP-1$, and LO_2/LH_2 were investigated.

METHOD OF APPROACH

The PYRO program included experimental determination of the blast and thermal environments resulting from various types of propellant mixtures for these liquid propellant combinations. Propellant weights up to about 100,000 lb were used for the cryogenic combinations and up to 1,000 lb for the hypergolic combination.

The generalized test conditions were selected to simulate the important classes of propellant interaction, i.e., the manner in which the two propellants come into contact with each other and mix during an accidental failure. The ways in which the propellants can mechanically interact with each other are dependent on the initial conditions of the propellants at the start of the interaction and on the nature of the boundary conditions which control or confine the flow of propellants during the spillage and mixing process. The two major boundary conditions selected for testing were confinement-by-the-missile (CBM) and confinement-by-the-ground-surface (CBGS).

The CBM condition is intended to simulate the general case where failure occurs in the intertank bulkhead and propellant mixing is confined within the tankage. The parameters of primary concern for this case are the ignition time, the size of the opening in the intertank bulkhead, the length-to-diameter ratio of the tankage, the ullage volume, and the pressure rise to cause tank rupture.

The CBGS condition simulates the situation where the propellants spill out of the tankage and mix on the ground surface (such as could occur in fallback of a vehicle on the launch pad). Major emphasis in the program has been placed on a flat ground surface, although a limited amount of data was obtained for other conditions. The parameters of primary concern for this boundary condition are the ignition time and the velocities, shapes, and relative orientation of the propellant masses at the start of their interaction.

Conceptually, propellant mixing can also occur without confinement, i.e., after the propellants spill out of the tankage but before they reach the ground, (such as could occur with a flight destruct system). Such free-fall mixing was not included in the program, however, because of the small amount of mixing anticipated. Unless there is a large velocity difference between the two propellants (which is unlikely for massive failures near the ground), there are no significant forces holding the two masses together, and even a small pressure generated by vaporization or reaction at the interface between the two masses will be sufficient to separate them and minimize mixing.

Although the generalized test conditions used resemble some actual failure modes, the intent in the program was not to investigate all credible combinations of tankage configuration, failure mode, and site geometry. There is an almost infinite number of such combinations. Rather, it was reasoned that many of these combinations would lead to similar propellant interactions and that study of several basic propellant interaction modes would be sufficient to provide a basis for evaluation or prediction under a variety of failure conditions.

The basic blast data obtained from these tests were peak overpressure and positive-phase impulse, both as a function of distance from the propellant explosion. Equivalent explosive weights at each measurement distance were

determined separately for peak overpressure and positive-phase impulse by means of standard TNT surface burst reference curves. Thermal data obtained from the tests included total heat flux, gas temperatures, and radiant heat flux.

SUMMARY OF TECHNICAL FINDINGS

The overall results obtained from the PYRO program are far too extensive in scope to fully summarize in this section, and the reader is referred to the main body of the report and the prediction method in Volume 3 for complete information. Certain of the findings, however, are sufficiently important to be emphasized here, and are discussed below.

Blast and Thermal Environment Prediction Method

The major result of Project PYRO was the development of a scientific method for predicting the blast wave and fireball characteristics of liquid propellant explosions. This method is based on the results of the basic test program in conjunction with the analytical studies and prior information regarding liquid propellant explosive behavior. The method can be applied to any given missile or space vehicle system for a wide range of specified failure modes.

In the prediction method, the thermal environment is given only as a function of propellant type and weight, while the blast environment is given as a function of a number of controlling parameters, including time of ignition for the cryogenic propellant combinations. The selection of appropriate values of the parameters needed to predict the blast environment for a specific system is considered to be the responsibility of the users of the information and in general requires a failure mode analysis of the system.

The derivation of the blast prediction method for the hypergolic propellant combination is given in Section 4 and for the cryogenic combinations in Section 5. The thermal prediction method for all propellant combinations appears in Section 6. The detailed procedures for using all prediction methods are given in Volume 3 of this report.

In general, the blast yields from hypergolic propellants are less than 2%. Exceptions to this occur for the high-velocity impact condition, or when an explosive donor weighing more than 15% of the propellant quantity is employed. With cryogenic propellants ($\text{LO}_2/\text{RP-1}$ or LO_2/LH_2) the yields will vary widely as a function of the failure mode, ignition delay time, and several other factors. Thus, use of a single TNT equivalent yield value (such as the current 20% for $\text{LO}_2/\text{RP-1}$; 60% for LO_2/LH_2) may actually be far too high or too low, depending on the particular system and failure conditions under consideration.

Liquid Propellant Blast Phenomena

Characteristically, the TNT equivalent weights computed from the measured data vary both as a function of the shock wave parameter used (peak overpressure or positive-phase impulse) and the distance from the explosion. At long distances, however, the equivalent weights tend to approach an equal and constant value, which has been defined as the terminal equivalent weight (when expressed as a percent of the total propellant weight).

The differences between the blast waves from liquid propellants and conventional high explosives tend to get larger as the distance from the explosion decreases. Characteristically the peak pressures from propellant explosions tend to get smaller and impulses larger than for a similar quantity of TNT. These differences can be large enough to significantly affect damage prediction. Methods of modifying the blast characteristics to account for these differences are included in the prediction method.

Effects of Factors

Ignition Time. In general, for the cryogenic combinations, the most critical parameter is the time of ignition. TNT equivalent yield values can vary by as much as a factor of 5 between the conditions under which ignition occurs early in the failure process and when it occurs at later times. This clearly points out the desirability of establishing, whenever possible, that ignition will occur at or near the earliest possible time.

Impact Velocity. Impact velocity was found to be a very significant factor in determining the yields of propellants impacting and mixing on the ground surface (CBGS and high-velocity-impact mixing modes). Even with the storable propellants, yields can become very high if the impact velocity exceeds several hundred feet per second. With the cryogenic propellants, yields increase very rapidly at much lower impact velocities, and very high yields can be obtained from only a 100-ft drop height.

Geometry. The geometry of the tankage (L/D ratio) is a moderately significant factor for the cryogenic combinations and must be considered in the CBM failure condition. However, the yield does not vary over a wide range in going from an L/D of 1.8 to 5.

Bulkhead Opening Size. The size of the bulkhead opening relative to the tank diameter (D_o/D_t ratio) also is important in the CBM failure condition. However, so long as the opening is less than 20% of the tank cross-sectional area, the effect is relatively small. The maximum yield values for the 100% open case are significantly larger than those for the 20% case, but obtaining openings larger than 20% seems somewhat unlikely.

Ullage Volume. The large effect of ullage volume on yield was one of the interesting results obtained from the LO₂/RP-1 CBM case. For this case it was shown that the maximum yield (in pounds of TNT) expected from a given vehicle when it was fully fueled was significantly less than that expected when it was about one-half to three-fourths full. The explanation is that the much larger ullage volume for the partially full case leads to a longer time for propellant mixing (and thus more complete mixing) prior to tank rupture caused by the pressure buildup resulting from vaporization of the LO₂. (The time of tank rupture is considered to be the maximum credible ignition time.)

Tank Rupture Pressure Differential. The tank rupture pressure differential (difference between tank burst pressure and initial pressure) was also found to be significant for essentially the same reason that the initial ullage volume was important. The larger the rupture pressure differential, the longer the maximum available mixing time and, thus, the larger the maximum yield.

Quantity. The basic blast prediction method for full-scale quantities of cryogenic propellants was initially developed using data obtained from the 200-lb weight scale and from postulated scaling relationships based on available information regarding the propellant mixing and explosion phenomena. Specifically, the initially postulated scaling relationship was that the explosive yield (in percent of the total propellant weight) would be independent of the propellant quantity and that the time of ignition to obtain equal explosive yields would scale as the cube root of the propellant weight. The validity of the postulated scaling for the cryogenic propellant combination was tested first on the 1,000-lb weight scale and later by using propellant quantities in the range of 25,000 to 100,000 lb. In only one case did these larger scale tests result in a modification to the scaling relationships, and that was for the LO₂/RP-1 CBM condition. The results for this case showed that the yield tended to decrease with increasing weight but that the rate of decrease also tended to decrease with increasing weight, so that the yield was roughly constant above 10,000 lb and only a factor of 1.2 greater at 1,000 lb.

PROBLEM AREAS

The major problem area still remaining in regard to predicting the blast environment from liquid propellant explosions is in establishing credible ignition times for the cryogenic propellant combinations. As noted previously, very much smaller yield values can be used if it can be demonstrated that ignition will occur very early in the failure process.

Although early ignition is generally believed to be reasonably likely, at the present time no generally applicable method exists for quantitatively predicting the credible ignition time. The scope of Project PYRO did not include an investigation of the probability of ignition; and the fact that it was possible to get long times of ignitions for most of the test conditions in the PYRO program tends to make it even more difficult, without further study, to justify the assumption of early ignition. Admittedly, special precautions were taken in the test program to minimize the probability of accidental ignition and to obtain a wide range of ignition delay times. The real question is, what is the

probability of ignition as a function of time for full-scale missile and space vehicle systems? Because of the large yield reductions possible with early ignition, it is clear that this question deserves study by those responsible for design of missile and space vehicle systems.

Two basic approaches are possible to capitalize on the relatively low yields obtained from early ignition:

1. Develop a generally applicable method for predicting ignition times in the hope that the intuitive belief that it is likely to occur early will be verified.
2. Develop automated ignition systems that will ensure early ignition.

The first approach has the advantage that if it turns out as postulated, early ignition can be assured without development of new hardware. It may turn out, however, that early ignition cannot be assured in all cases, and then it would still be necessary to use the second approach for these cases. The second approach clearly offers greater assurance of providing the desired result, but it is likely to be significantly more expensive in the long run.

Since there is no clear-cut advantage within the present state of knowledge for one approach over the other, it is recommended that a two-phase program be conducted, with the first phase a preliminary study of both approaches in order to evaluate their relative potential, and the second phase the development of the approach showing the greatest potential.

Section 9
GLOSSARY AND SYMBOLS

GLOSSARY

Blast Scaling Laws: Scaling laws formulated from the general laws of similitude relating blast and environmental parameters. The most common blast scaling laws (termed "cube root scaling") relate blast wave parameters (e.g., blast pressure, P, positive-phase impulse, I, and positive-phase duration, t^+) to distance from an explosion d, and explosion weight W, as follows:

$$P = f(d/W^{1/3}) = f(\lambda)$$

$$I/W^{1/3} = h(d/W^{1/3}) = h(\lambda)$$

$$t^+/W^{1/3} = g(d/W^{1/3}) = g(\lambda)$$

The quantities $d/W^{1/3}$ and $t^+/W^{1/3}$ are commonly referred to as scaled distance and scaled time, respectively. (See SACHS SCALING LAW.)

Blast Wave: A pressure pulse (or wave) in air, propagated continuously from an explosion and characterized by an initial generally rapid rise of pressure above ambient values. The air within a blast wave moves in the direction of propagation, causing winds. (See SHOCK WAVE.)

Confinement-by-the-Missile (CBM): Condition in which an internal failure occurs and one propellant falls down onto the other. Duration of case is limited to the time the propellants are confined within the walls of the missile.

Confinement-by-the-Ground-Surface (CBGS): Test condition in which propellants interact subsequent to tank rupture, when propellants are impacting on flat horizontal surface. Both vertical and horizontal flow directions were investigated.

Vertical Flow Test Condition: Propellants impact the ground surface together.

Horizontal Flow Test Condition: Lower propellant is allowed to impact on ground surface, creating a pool prior to impact of top propellant.

Explosive Yield: The explosive potential of propellants is usually expressed in terms of their TNT equivalent yield, i.e., the amount of TNT which if put at the position of the propellant explosion would produce the same value of a particular shock wave parameter at the same distance as for the propellant explosion. The explosive yield of a given propellant explosion can be given in equivalent pounds

of TNT, although it is more common to express it in terms of the percent of the total weight of propellants involved. The term explosive yield is usually modified by the shock wave parameter used in the calculation, e.g., peak overpressure yield or positive-phase impulse yield.

Free-Air Overpressure (or Free-Field Overpressure): The unreflected pressure, in excess of the ambient atmospheric pressure, created in the air by the blast wave from an explosion.

Impulse (per unit area): The integral, with respect to time, of the overpressure in a blast wave at a given point, the integration being carried out between the time of arrival of the blast wave and that at which the overpressure returns to zero at the given point. Impulse dimensions are the product of overpressure and time, e.g., psi-seconds.

Overpressure: The transient pressure, usually expressed in pounds per square inch, exceeding the ambient pressure, manifested in the shock (or blast) wave from an explosion. The variation of overpressure with time depends on the energy yield of the explosion, the type of explosive or propellant, the distance from the point of burst, and the medium in which the explosive propellants are detonated. The peak overpressure is the maximum value of the overpressure at a given location and is generally experienced at the instant the shock (or blast) wave reaches that location. (See SHOCK WAVE.)

Sachs Scaling Law: Scaling laws relating blast and environmental parameters that include the effects of changes of ambient pressures. These scaling laws are summarized below:

$$P/P_0 = f' \left[d / (W/P_0)^{1/3} \right] = f'(\lambda')$$

$$I / (W/P_0)^{1/3} = h' \left[d / (W/P_0)^{1/3} \right] = h'(\lambda')$$

$$t^+ / (W/P_0)^{1/3} = g' \left[d / (W/P_0)^{1/3} \right] = g'(\lambda')$$

where P = shock pressure

P_0 = ambient pressure

d = distance from the charge

W = charge weight

t^+ = duration of the positive-pressure phase

I = positive-phase impulse

$$\lambda' = d/(W/P_0)^{1/3}$$

The quantities $d/(W/P_0)^{1/3}$ and $t^+/(W/P_0)^{1/3}$ are commonly referred to as Sachs scaled distance and Sachs scaled time, respectively.

Shock Front (or Pressure Front): The fairly sharp boundary between the pressure disturbance created by an explosion (in air, water, or earth) and the ambient atmosphere, water, or earth, respectively. It constitutes the front of the shock (or blast) wave.

Shock Wave: A continuously propagated pressure pulse (or wave) in the surrounding medium, which may be air, water, or earth, initiated by the expansion of the hot gases produced in an explosion. A shock wave in air is often referred to as a blast wave. The duration of a shock (or blast) wave is distinguished by two phases. First there is the positive (or compression) phase, during which the pressure rises very sharply to a value that is higher than ambient and then decreases to the ambient pressure. The duration of the positive phase increases and the maximum (peak) pressure decreases with increasing distance from an explosion of a given energy yield. In the second phase, the negative (or rarefaction) phase, the pressure falls below ambient and then returns to the ambient value. Deviations from the ambient pressure during the negative phase are never large. (See OVERPRESSURE.)

Terminal Yield: The value of the explosive yield in the region where the explosive yield becomes independent of distance from the explosion or the shock wave parameter used in the calculation. (See EXPLOSIVE YIELD.)

BLAST SYMBOLS

D = Ground distance from point of explosion (ft)

$$D^* = D/W^{1/3} = \text{Scaled ground distance}$$

D_o/D_t = Intertank bulkhead opening ratio (ratio of effective circular diameter of opening in intertank bulkhead to tank diameter)

I = Positive-phase impulse (psi/msec)

$$I/W^{1/3} = \text{Scaled positive-phase impulse}$$

k = Ratio of the terminal yield for a given V_u and ΔP_r to that for the standard condition of $V_u = 10\%$ and $\Delta P_r^u = 85$ psi

L = Propellant loading fraction (fraction of propellant tanks filled with fuel - assumes that L = 1 corresponds to a $V_u = 10\%$)

L/D = Propellant length-to-diameter ratio

P = Peak overpressure (psi)

ΔP_r = Difference between the tank burst pressure and the initial tank pressure

t = Propellant mixing time (for CBM case corresponds to time from initial propellant contact to ignition - for CBGS case corresponds to time from initial propellant contact on ground surface to ignition)

$t^* = t/W^{1/3}$ = Scaled propellant mixing time

t_{\max}^* = Maximum scaled propellant mixing time (applicable to CBM case - corresponds to time from initial propellant contact to time for the tanks to burst due to pressure buildup from vaporization of cryogenic propellants)

t_{crit}^* = Critical scaled propellant mixing time (applicable to $LO_2/RP-1$ and LO_2/LH_2 CBGS case - corresponds to time from initial propellant contact on ground surface to time of maximum yield)

v = Propellant impact velocity on ground surface (or other target)

V_u = Tank ullage volume in percent of total tank volume

$V_{u\text{-eff}} = \frac{\Delta P_r}{85} V_u$ = Effective ullage volume for constant ΔP_r

W = Total propellant weight (lb)

W_t = Propellant weight in top tank

W_b = Propellant weight in bottom tank

Y = Explosive yield (percent of total propellant weight which acts like TNT, assuming surface burst conditions)

Y_I = Explosive yield computed from impulse data

Y_p = Explosive yield computed from peak overpressure data

Y_s = Specific explosive yield (yield per pound of propellants)

Other Notation

Empirically derived constants ($a, a', b, b', c, c', n, m, \alpha, \alpha', \alpha'', \alpha''', \beta, \beta_0, \beta_1, \beta_2, \delta_1, \gamma$)

Functions of variables [f(), g(), h() . . .]

THERMAL SYMBOLS

c = Specific heat (watt-sec/gm-°C)

D = Diameter (ft)

K = Thermal conductivity (watt/cm-°C)

$K' = (K\rho c/\pi)^{1/2}$

q = Heat flux density (watt/cm²)

T_s = Temperature change (°K)

t = Time (seconds)

W = Total propellant weight (lb)

ρ = Mass density (gm/cm³)

τ = Fireball duration (seconds)

Appendix A
BLAST INSTRUMENTATION SYSTEM

This section describes the blast instrumentation system installed at test pad 1-91, AFRPL, for the main portion of the PYRO test series. (Thermal instrumentation is described in Appendix C.) Included are a discussion of the rationale for the design of the system, the layout of the measuring stations, description of the blast sensors and mounts, and a description of the test program conducted to evaluate the blast instrumentation. Descriptions of the instrumentation systems installed at the Naval Ordnance Test Station for the high-velocity impact tests are presented in the section discussing the results from those tests.

EXPERIMENTAL ARRANGEMENT

Gauge Station Array

To provide adequate documentation of the explosive characteristics of propellant explosions, it is desirable to measure the pertinent blast characteristics as a function of both distance and azimuth. To provide this capability, the blast gauges were placed on three radial lines passing through ground zero and oriented 120 deg with respect to each other.

The basic blast-gauge system consisted of overpressure sensors on each line at several radii for each propellant weight. Since the peak overpressure decreases approximately as the negative 1.8 power of the distance over the pressure range of primary concern, 1 to 100 psi, it was convenient to space the stations at distances differing by a factor of approximately 1.8. This resulted in pressures differing by a factor of 2.5 from station to station, or a total difference in pressure over five stations of a factor of about 40. This arrangement not only provides a reasonable spacing of stations over the pressure range of interest, but also permits use of most of the same stations when the propellant weight used in the testing is increased. At the time the system was

designed, the planned propellant weights were 200, 1000, 5000, and 25,000 lb, each size differing from the previous one by a factor of 5. Since the pressure depends on the ratio of the distance to the cube root of the charge weight and since the pressure decreases approximately as the negative 1.8 power of the distance, the pressure at a given distance will vary approximately as the 0.6 power of the charge weight, i.e., it will increase by a factor of about 2.5 with an increase of charge weight by a factor of 5. Since this is the same as the difference in pressure from station to station, the same pressure range can be covered for a change in propellant weight by a factor of 5 by deleting the closest station and adding one new station at a distance of approximately 1.8 times the distance of the last station. The actual propellant weights used during the program were 200, 1000, 25,000 and 100,000 lb. To accommodate the 100,000 lb propellant weight, an additional ring of gauges was installed using the same 1.8 distance factor.

Since a variety of explosive yields was expected for each scale of testing, ranging from as low as 1 percent to nearly 100 percent, an intermediate yield level of 10 percent was selected for establishing the specific station location. (A 100-percent yield would give pressures about a factor of 4 higher than those for a 10-percent yield, while a 1-percent yield would give pressures about a factor of 4 lower.)

In addition to the foregoing considerations, selection of the actual pressure values for the stations was guided by the following: the pressure region of primary interest is between 1 and 100 psi, and a minimum of three of the stations should be in the region below about 15 psi, where the shock wave would be expected to be unsupported, i.e., classical in nature, for a 10-percent explosive yield situation.

To supplement the basic overpressure measurement system, the following additional measurements were also installed:

- Two overpressure-time measurements as close to the explosion as practical.

- Four measurements of stagnation pressure, spanning the range from near ground zero to the 32-psi level. (These gauges were also used as time-of-arrival gauges.)

Additional details of the instrumentation and station layout are given in Fig. A-1 and Table A-1.

Transducers and Amplifying and Recording System

The basic factors which were considered in the choice of an overpressure sensor for the primary blast-measuring system for project PYRO were:

- Gauges which would be unaffected by the ambient environment expected at the test site over the several-year test period. The most important factor to consider here is probably temperature stability.
- Gauges which would be small in diameter ($< 1/2$ in.) to fit in the thin probe required for the head-on overpressure measurements and also to have a very short air-shock travel time (rise time) for the side-on overpressure measurements. The gauge and preamp system should also have a high natural frequency (≥ 20 kHz) to accurately measure the air blasts from the smaller scale tests.
- Gauges which would survive the thermal and blast environment, which could be in excess of $3,000^{\circ}\text{K}$ and 3,000 psi at the close-in stations.
- A gauge and preamp system which had been used in a similar environment and preferably one which we and other investigators had used in the field.

The system selected to satisfy all of the above requirements was manufactured by the Kistler Instruments Corporation and consisted of natural quartz piezoelectric transducers (Models 601A, 601H, 603A, 606A, and 701A) combined with a charge-amplifier-type preamp (Model 566M109). These transducers are small, approximately $1/4$ in. in diameter, with a $1/8$ -in. sensitive area. They were enclosed in a stainless steel housing, had a flush-mounted stainless steel diaphragm, and were available with overpressure ranges in excess of 15,000 psi. The frequency response of the sensor-preamplifier system was rated from near DC to approximately 70 kHz. The sensors had good temperature stability and, with a protective thermal covering of silicone grease, had been used successfully in a similar propellant-blast environment by URS and by other investigators.

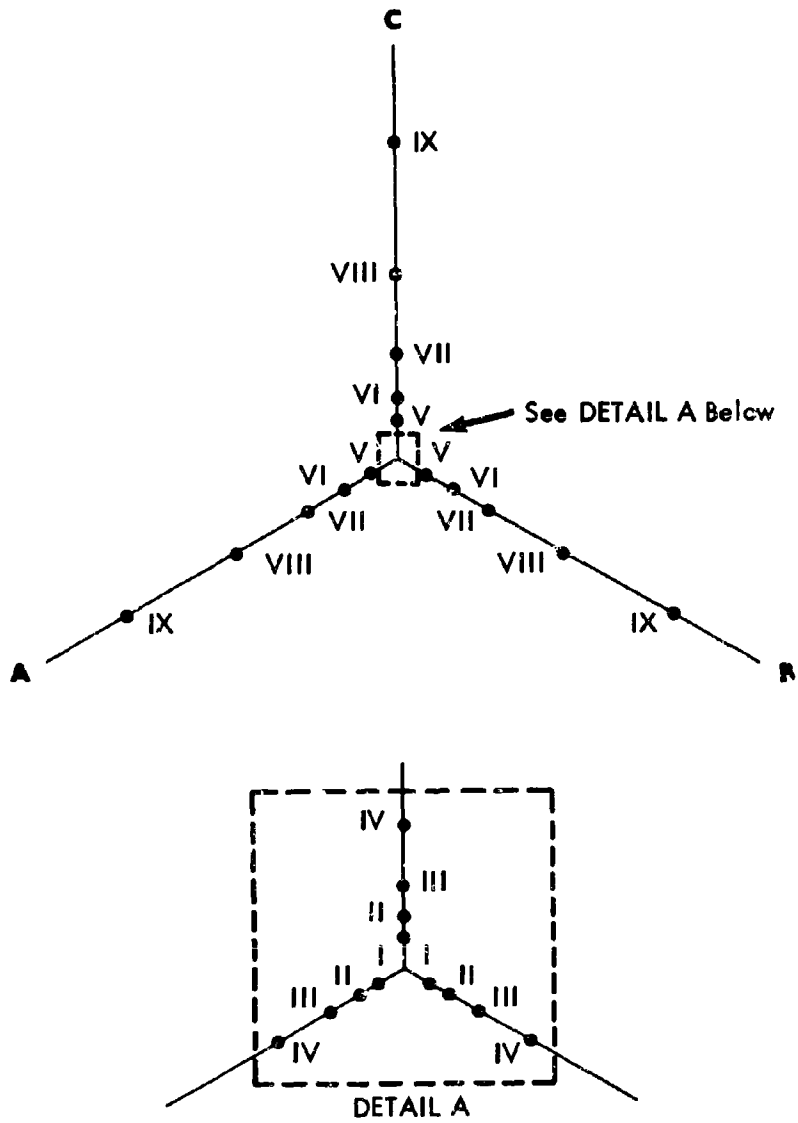


Fig. A-1. Instrumentation Layout

Table A-1
SUMMARY OF BLAST INSTRUMENTATION

| NO. | GAUGE LINE | NOMINAL DISTANCE (ft) | 200 LB | | 1000 LB | | 25000 LB | | 100,000 LB | |
|-----|-------------|-----------------------|------------------|-------------------|----------------|----------------|----------------|----------------|----------------|----------------|
| | | | P _s * | P _o ** | P _s | P _o | P _s | P _o | P _s | P _o |
| I | A B C | 2.8 | | X | | | | | | |
| II | A B C | 4.5 | X | X | | X | | | | |
| III | A B C | 7.5 | X X | X X X | X | X | | | | |
| IV | A B C | 13 | X | X X X | X X | X X X | | X | | X*** X*** |
| V | A B C | 23 | | X X X | X | X X X | X | X | | X X |
| VI | A B C | 38 | | X X X | | X X X | X X | X X X | | X X |
| VII | A B C | 67 | | X X X | | X X X | X | X X X | | X X X |
| IX | A B C | 200 | | | | | | X X X | | X X X |
| X | A B C | 335 | | | | | | X X X | | X X X |
| XJ | A B C | 600 | | | | | | X X X | | X X X |

* Head-on-oriented stagnation pressure sensor.

** Side-on-oriented overpressure sensor.

*** A single gauge at the 13-ft distance was alternately located along gauge lines A and C.

The charge amplifiers for each sensor were mounted in protective junction boxes located 20 to 50 ft further away from ground zero than the sensors. These separation distances between the sensors and the amplifiers were necessary to eliminate any possible air-induced ground shock from affecting the amplifiers during the time of interest. In all cases the air shock pulse would have passed over the sensor before the shock front reached the location of the amplifier junction boxes.

The control and recording equipment was located in a "blockhouse" approximately 1000 ft from the test pad. The recording system consisted of three Model FR-1200 and three Model FR-100, 14-track Ampex instrumentation tape recorders.

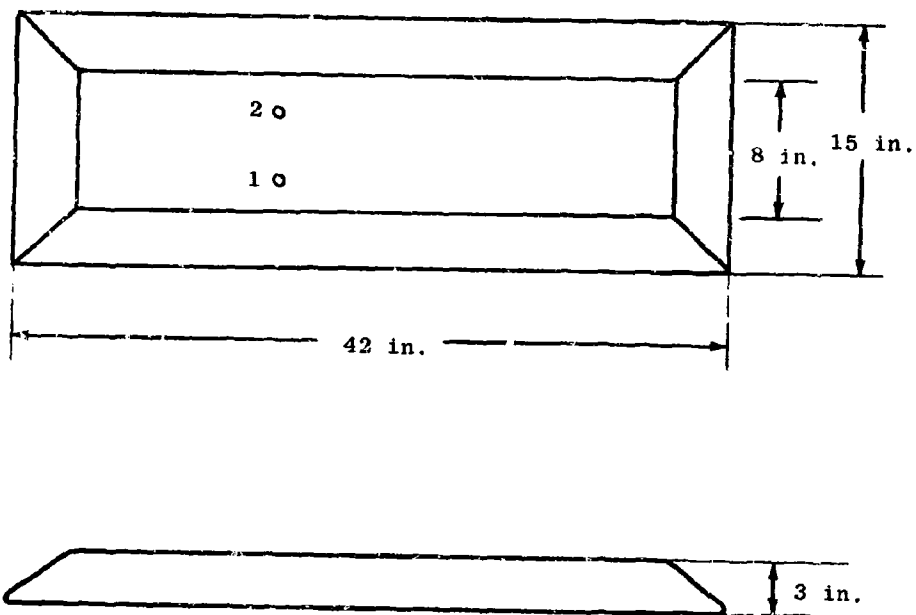
All pressure channels were recorded on the three FR-1200's, which were run at 120 ips. The bandwidth of these records was DC to 20 kHz. The thermal instrumentation was recorded on the FR-100's, which had a bandwidth of DC to 10 kHz. Tape speed for the FR-100's was 60 ips.

Mounting of Transducers

The three basic sensor mount designs used for the blast instrumentation are shown in Figs. A-2 through A-4. The type A mounts, Fig. A-2, were fabricated from 3-in.-thick steel and were used in the close-in environment. The configuration shown, located 4.5 ft* from ground zero, contains a side-on overpressure sensor and a stagnation pressure sensor. Another version of this sensor mount, with the same basic design and containing only a single overpressure sensor, was located at 2.8 ft from ground zero.

The type B mount, Fig. A-3, located 7.5 ft from ground zero, was fabricated from 3-in.-diameter heavy-wall stainless steel tubing. The front nose of this mount was removable to allow use of either the pointed nose containing the

* In this discussion, the distances quoted refer to the 200-lb test series, as described in the discussion of blast instrumentation design. These distances were increased by a factor of approximately 1.8 for each increase in propellant weight of a factor of 5.



1. Side-on overpressure gauge
2. Stagnation gauge

Fig. A-2. Type A Sensor Mount

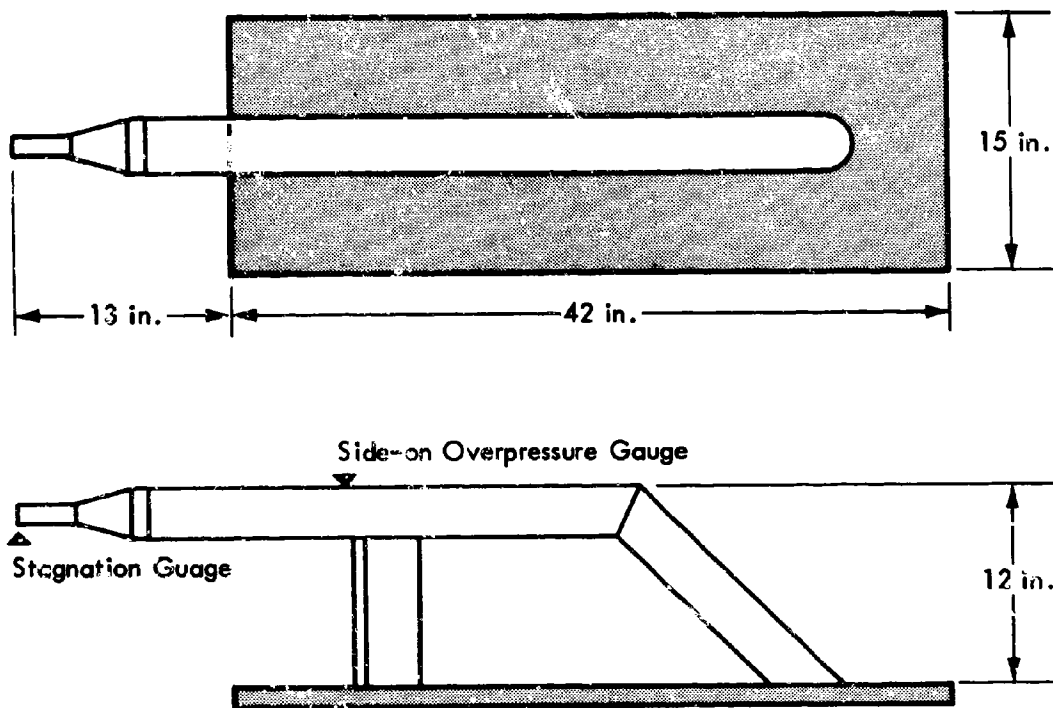


Fig. A-3. Type B Sensor Mount

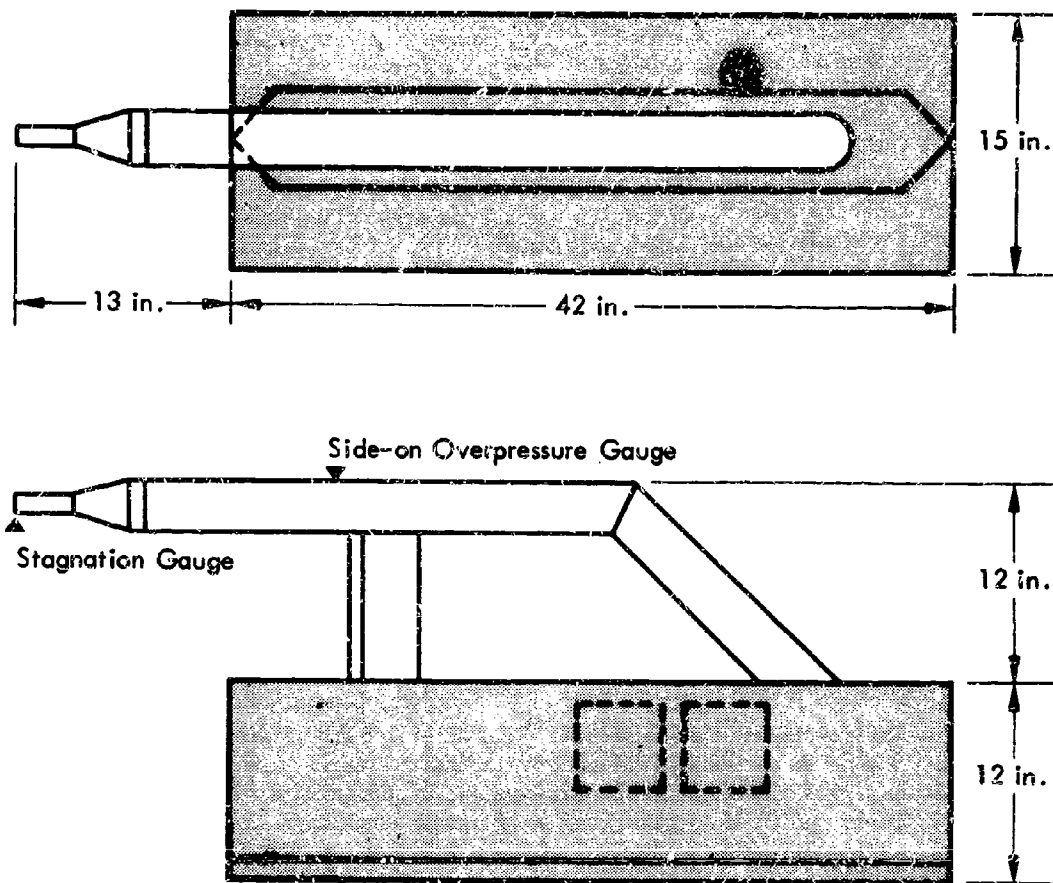


Fig. A-4. Type C Sensor Mount

stagnation sensor or a blunt protective nose. A side-on-pressure sensor was located on top of the mount, as noted in the figure.

The type C mount, Fig. A-4, combined the type B with a 12-in.-high raised pedestal. At the stations off the test pad, the additional height of the raised pedestal helped to prevent ground surface irregularities and the dust created by the explosions from influencing the overpressure readings.

CALIBRATION

The blast instrumentation system was calibrated by a variety of methods, including periodic laboratory calibration of individual components, a calibration of electrical components during the countdown that preceded each propellant and high-explosive (H.E.) test, a periodic end-to-end field calibration of individual channels, and complete system calibrations using standard H.E. charges.

Calibration by Other Than High Explosives

The laboratory calibrations of electrical components and piezoelectric pressure transducers were periodically conducted by standard procedures, the latter by standard shock tube techniques.

Calibration of the charge amplifiers in the field preceding each propellant test was conducted by applying a measured charge signal to the input and measuring the output voltage for the test gain setting, thus obtaining the gain ultimately required for test data evaluation.

An end-to-end calibration of all blast instrumentation channels was conducted by dynamically applying a known pressure step to each pressure gauge in its field position by means of a small portable shock tube.

The calibration procedure for the amplifiers used with the thermal instruments was somewhat less involved, since only the external feedback circuits needed to be measured. These circuits were constructed on printed circuit cards

for easy removal from the test area to the control station for calibration. For calibration, these circuits are inserted into a special calibration panel which simulates the test configuration.

It should be mentioned also that AFRPL instrumentation personnel conducted an accuracy study of the laboratory and electrical calibration procedures used during the program. This study is reported in Ref. A-1.

High-Explosive (H.E.) Calibration Tests

General Description

Calibration of the instrumentation system was performed throughout the program by detonating spherical charges of high explosives whose explosive properties are well known (TNT and Pentolite). The purpose of these tests was to verify the correct overall operation of the blast instrumentation system, both initially and throughout the program, and to provide information on the system reproducibility.

A summary of the calibration tests is given in Table A-2 showing their division into test series, the first series being conducted to test the system initially, and each of the remaining series being associated with a given set of propellant tests.

The spherical charges were detonated at a scaled height of burst (HOB) of about $1 \text{ ft/lb}^{1/3}$ for all tests. This charge height was selected because it was high enough to minimize cratering and the subsequent hazard to instrumentation from flying debris and still low enough to ensure a well-developed shock front at the closest above-surface transducer. Furthermore, this charge height is high enough so that the particular nature of the ground surface would not significantly influence the results. (Much of the available reference data are for natural ground surfaces, such as hard-packed clay, while the surface in the vicinity of the explosion for the PYRO tests is concrete.)

Table A-2
SUMMARY OF H.E. CALIBRATION TESTS

| NUMBER OF TESTS | CHARGE | | TEST SERIES DESCRIPTION |
|-----------------|-------------|-----------|---|
| | WEIGHT (lb) | TYPE | |
| 6 | 18 | Pentolite | Initial series; conducted prior to propellant testing. |
| 18 | 18 | Pentolite | Conducted periodically throughout the propellant testing at the 200- and 1,000-lb levels. |
| 3 | 500 | Pentolite | Conducted in conjunction with the 25,000-lb tests and the Titan I test. |
| 2 | 1,000 | TNT | Conducted in conjunction with the S-IV test. |

Calibration Data

The individual gauge readings of peak overpressure and impulse for each of the four test series listed in Table A-2 are given in Tables A-3 through A-6.* It can be seen from the tables that various gauge changes were implemented, usually between test series. For instance, subsequent to the initial six tests one of the three gauges at both the 6.6- and 12.8-ft distances was deleted. In addition, starting with Test 221, three gauges were added at both 117 and 200 ft. The system was modified for the 500-lb pentolite tests (see Table A-5) as follows: three gauges (one for each gauge line) were added at both the 335- and 600-ft distances, two gauges were deleted at 13 ft, leaving one gauge at this distance along either gauge line A or C; and at both the 23- and 37-ft distances, a gauge was deleted along gauge line A, leaving seven gauges along gauge lines B and C.

* These data have not been corrected for the difference in ambient pressure between the test pad (about 13.4 psi) and sea level (14.7 psi), since the net effect of the correction (which has to be made in both the overpressure and scaled distance) is so small as not to influence any of the conclusions drawn from the data.

Table A-3
PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
(PSI-MSEC) FROM 18-LB PENTOLITE SPHERES

| Shot No. | Pressure-Impulse | Gauge Line | 6.56 | 12.8 | 22.5 | 37.1 | 66.6 |
|----------|------------------|------------|------|-------|------|------|------|
| 018 | P | A | - | 59.0 | 20.0 | - | 2.92 |
| | | B | - | 46.2 | 18.3 | 7.4 | 2.66 |
| | | C | 220 | 65.0 | 16.0 | 6.0 | 3.30 |
| 019 | I | A | - | 13.8 | 12.4 | - | 4.06 |
| | | B | - | - | 19.7 | 7.61 | 3.72 |
| | | C | - | 14.8 | 10.7 | 6.78 | 4.43 |
| 020 | P | A | - | 54.0 | 17.6 | 6.00 | 3.08 |
| | | B | - | 42.0 | 16.8 | 6.85 | - |
| | | C | 300 | 57.6 | 12.0 | 4.95 | 2.92 |
| 021 | I | A | - | 13.5 | 10.9 | 6.17 | 4.27 |
| | | B | - | - | 10.1 | 6.55 | - |
| | | C | 27.5 | 13.2 | 9.11 | 5.20 | 4.24 |
| 022 | P | A | - | 56.8 | 18.0 | 6.20 | 3.20 |
| | | B | 350 | 66.4 | 15.0 | 6.20 | - |
| | | C | 292 | 54.4 | 14.2 | 5.28 | 2.92 |
| 023 | I | A | 23.5 | 13.5 | 11.1 | 6.00 | 4.32 |
| | | B | 28.4 | - | 9.43 | 6.48 | - |
| | | C | 27.6 | 13.1 | 9.85 | 5.93 | 4.03 |
| 024 | P | A | 333 | 55.0 | 17.2 | 6.32 | 3.12 |
| | | B | 230 | 59.0 | 15.4 | 5.70 | - |
| | | C | 274 | 54.0 | 13.0 | 4.96 | 2.58 |
| 025 | I | A | 26.9 | 13.9 | 10.8 | 6.16 | 4.22 |
| | | B | 30.1 | - | 9.78 | 6.02 | - |
| | | C | 26.6 | 13.4 | 9.27 | 5.33 | 3.72 |
| 026 | P | A | 320 | 56.0 | 15.8 | 6.0 | 2.64 |
| | | B | 236 | 44.17 | 16.5 | 5.4 | - |
| | | C | 260 | 52.8 | 11.8 | - | 2.90 |
| 027 | I | A | 24.3 | 12.0 | 10.9 | 5.81 | 3.69 |
| | | B | 31.1 | - | 9.23 | 6.10 | - |
| | | C | 26.7 | 13.3 | 7.76 | - | 4.23 |
| 028 | P | A | - | - | - | 6.1 | 2.72 |
| | | B | - | - | - | 5.84 | - |
| | | C | - | - | - | - | 3.00 |
| 029 | I | A | - | - | - | 6.18 | 3.88 |
| | | B | - | - | - | 6.21 | - |
| | | C | - | - | - | - | 4.26 |

Table A-4
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
 (PSI-MSEC) FROM 16-LB PENTOLITE SPHERES

| Shot No. | Pressure-Impulse | Gauge Line | Distance (ft) | | | | | | |
|----------|------------------|------------|---------------|------|------|------|------|-----|-----|
| | | | 6.5 | 12.8 | 23 | 37 | 67 | 117 | 200 |
| 082 | P | A | - | 57.4 | - | 6.80 | 2.41 | - | - |
| | | B | 226 | 68.3 | 17.5 | 6.24 | 2.38 | - | - |
| | | C | 268 | 63.2 | 15.6 | 6.40 | 2.51 | - | - |
| 083 | I | A | - | 42.8 | - | 18.7 | 9.22 | - | - |
| | | B | 74.7 | 42.7 | 26.0 | 15.9 | 8.97 | - | - |
| | | C | 81.3 | 45.2 | 25.9 | 15.8 | 9.09 | - | - |
| 089 | P | A | - | 59.8 | - | 6.81 | 2.58 | - | - |
| | | B | 206 | 65.0 | - | 6.29 | 2.34 | - | - |
| | | C | 281 | 59.8 | 16.2 | 6.14 | 2.40 | - | - |
| 111 | I | A | - | 42.9 | - | 18.8 | 9.42 | - | - |
| | | B | 73.0 | 42.8 | - | 16.2 | 9.19 | - | - |
| | | C | 80.7 | 43.7 | 26.0 | 15.5 | 9.10 | - | - |
| 112 | P | A | - | 58.8 | 17.5 | 6.20 | 2.97 | - | - |
| | | B | 197 | 57.1 | 13.1 | 5.96 | 2.19 | - | - |
| | | C | 232 | 65.6 | 16.3 | 5.63 | 2.65 | - | - |
| 113 | I | A | - | 39.6 | 24.1 | 17.8 | 10.4 | - | - |
| | | B | 95.2 | 37.6 | 21.7 | 16.4 | 8.96 | - | - |
| | | C | 76.4 | 43.8 | 26.4 | 14.9 | 9.32 | - | - |
| 114 | P | A | - | 71.7 | 17.6 | 6.41 | 2.88 | - | - |
| | | B | 187 | 70.5 | 17.9 | 6.69 | 2.57 | - | - |
| | | C | - | 76.0 | 17.5 | 6.99 | 2.56 | - | - |
| 115 | I | A | - | 42.4 | 29.4 | 19.2 | 11.6 | - | - |
| | | B | 81.7 | 45.1 | 28.3 | 19.3 | 10.7 | - | - |
| | | C | 76.2 | 43.1 | 26.6 | 18.5 | 10.4 | - | - |
| 116 | P | A | - | 55.7 | - | 6.37 | 2.96 | - | - |
| | | B | 159 | 67.5 | 15.6 | 6.52 | 2.47 | - | - |
| | | C | 261 | 73.9 | 16.9 | 7.12 | 2.62 | - | - |
| 117 | I | A | - | 41.2 | - | 20.3 | 11.8 | - | - |
| | | B | 94.7 | 46.9 | 28.2 | 13.4 | 9.81 | - | - |
| | | C | 93.4 | 44.8 | 29.2 | 18.4 | 10.4 | - | - |

Table A-4 (Cont.)
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
 (PSI-MSEC) FROM 18-LB PENTOLITE SPHERES

| Shot No. | Pressure-Impulse | Gauge Line | Distance (ft) | | | | | | |
|----------|------------------|------------|---------------|------|------|------|------|-----|-----|
| | | | 6.5 | 12.8 | 23 | 37 | 67 | 117 | 200 |
| 128 | P | A | - | - | 15.0 | 6.03 | 2.37 | - | - |
| | | B | 237 | - | 16.5 | 6.52 | 2.56 | - | - |
| | | C | 242 | 73.2 | 16.3 | - | 2.53 | - | - |
| 129 | I | A | - | - | 22.2 | 18.7 | 9.59 | - | - |
| | | B | 96.5 | - | 22.9 | 18.9 | 9.89 | - | - |
| | | C | 79.4 | 38.7 | 28.9 | - | 9.53 | - | - |
| 129 | P | A | - | - | 16.5 | 6.19 | 2.44 | - | - |
| | | B | - | 77.4 | 15.0 | 6.57 | 2.60 | - | - |
| | | C | 211 | 73.4 | 16.0 | - | 2.57 | - | - |
| 149 | I | A | - | - | 23.0 | 18.2 | 9.62 | - | - |
| | | B | - | 47.2 | 24.6 | 18.0 | 9.97 | - | - |
| | | C | 100 | 41.1 | 27.9 | - | 9.76 | - | - |
| 149 | P | A | - | - | 16.5 | 6.25 | 2.52 | - | - |
| | | B | 255 | 64.4 | 18.0 | 6.54 | 2.71 | - | - |
| | | C | 245 | 65.1 | 16.4 | 6.43 | 2.63 | - | - |
| 187 | I | A | - | - | 26.3 | 19.0 | 9.06 | - | - |
| | | B | 71.9 | 39.6 | 31.5 | 16.9 | 9.28 | - | - |
| | | C | 68.7 | 44.9 | 27.4 | 16.5 | 9.17 | - | - |
| 187 | P | A | - | - | 16.9 | 6.88 | 2.78 | - | - |
| | | B | 255 | 78.9 | 19.0 | 6.27 | 2.86 | - | - |
| | | C | 282 | 59.0 | 17.0 | 7.03 | 2.79 | - | - |
| 188 | I | A | - | - | 26.2 | 20.1 | 9.79 | - | - |
| | | B | 77.8 | 38.8 | 28.8 | 18.1 | 10.8 | - | - |
| | | C | 76.2 | 40.8 | 26.3 | 19.0 | 10.5 | - | - |
| 188 | P | A | - | - | 13.2 | 6.93 | 2.67 | - | - |
| | | B | 251 | 77.4 | 18.3 | 6.11 | 2.78 | - | - |
| | | C | 263 | 58.9 | 15.7 | 6.62 | 2.57 | - | - |
| 188 | I | A | - | - | 22.9 | 18.7 | 9.32 | - | - |
| | | B | 83.7 | 39.7 | 29.1 | 17.8 | 10.4 | - | - |
| | | C | 65.1 | 39.0 | 26.5 | 19.4 | 9.40 | - | - |

Table A-4 (Cont.)
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
 (PSI-MSEC) FROM 18-LB PENTOLITE SPHERES

| Shot No. | Pressure-Impulse | Gauge Line | Distance (ft) | | | | | | |
|----------|------------------|------------|---------------|------|------|------|------|------|------|
| | | | 6.5 | 12.8 | 23 | 37 | 67 | 117 | 200 |
| 221 | P | A | - | - | 16.8 | 6.32 | 3.02 | 1.15 | .535 |
| | | B | 250 | 80.3 | 20.4 | 5.66 | 2.23 | 1.45 | .608 |
| | | C | 259 | 67.2 | 18.9 | 6.50 | 2.52 | 1.30 | .684 |
| I | A | - | - | 23.3 | 19.7 | 11.1 | 5.91 | 3.30 | |
| | B | 90.6 | 31.4 | 28.7 | 17.0 | 9.24 | 7.24 | 3.23 | |
| | C | 63.6 | 49.6 | 27.0 | 16.2 | 9.75 | 6.47 | 3.53 | |
| 222 | P | A | - | - | 17.1 | 6.11 | - | 1.11 | .622 |
| | | B | 311 | 74.4 | 18.5 | 6.50 | 2.35 | 1.34 | .652 |
| | | C | 330 | 76.5 | 17.8 | 6.56 | 2.57 | 1.25 | .575 |
| I | A | - | - | 24.0 | 20.1 | - | 5.53 | 3.35 | |
| | B | 73.8 | 38.2 | 27.9 | 17.0 | 9.62 | 5.72 | 3.24 | |
| | C | 95.2 | 58.6 | 22.9 | 16.3 | 10.1 | 6.24 | 3.73 | |
| 234 | P | A | - | - | 18.8 | 6.59 | 2.30 | 1.17 | .625 |
| | | B | 248 | - | 18.3 | 6.40 | 2.41 | 1.47 | .655 |
| | | C | 231 | 67.0 | 23.1 | 7.09 | 2.67 | 1.25 | .683 |
| I | A | - | - | 20.1 | 18.7 | 10.6 | 5.61 | 3.41 | |
| | B | - | - | 22.8 | 17.7 | 9.37 | 6.50 | 3.56 | |
| | C | 86.1 | 40.9 | 33.2 | 16.8 | 9.64 | 5.87 | 3.99 | |
| 235 | P | A | - | - | 16.4 | 6.25 | 2.80 | 1.19 | .639 |
| | | B | 169 | - | 21.5 | 6.34 | 2.43 | 1.39 | .688 |
| | | C | 223 | 62.9 | 24.0 | 7.39 | 2.73 | 1.26 | .737 |
| I | A | - | - | 26.8 | 19.4 | 10.5 | 6.13 | 3.61 | |
| | B | - | - | 30.3 | 17.9 | 9.95 | 5.92 | 3.58 | |
| | C | 93.0 | 39.2 | 31.1 | 18.7 | 11.0 | 6.43 | 3.60 | |
| 246 | P | A | - | - | 18.1 | 5.91 | 2.77 | 1.04 | .606 |
| | | B | 148 | 60.4 | 17.8 | 6.90 | 2.44 | 1.21 | .729 |
| | | C | 250 | 64.2 | 18.9 | 6.76 | 2.55 | 1.21 | .518 |
| I | A | - | - | 27.0 | 15.4 | 9.99 | 5.49 | 3.24 | |
| | B | 43.1 | 33.0 | 27.4 | 17.8 | 9.48 | 5.37 | 3.48 | |
| | C | 96.3 | 40.4 | 28.5 | 18.1 | 10.2 | 6.11 | 3.52 | |

Table A-4 (Cont.)
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
 (PSI-MSEC) FROM 18-LB PENTOLITE SPHERES

| Shot No. | Pressure-Impulse | Gauge Line | Distance (ft) | | | | | | |
|----------|------------------|------------|---------------|------|------|------|------|------|------|
| | | | 6.5 | 12.8 | 23 | 37 | 67 | 117 | 200 |
| 247 | P | A | - | - | 18.3 | 5.67 | 2.81 | 1.10 | .596 |
| | | B | - | 88.7 | 19.1 | 5.68 | 2.41 | 1.13 | .691 |
| | | C | 221 | 85.6 | 17.8 | 5.88 | 2.48 | 1.28 | .697 |
| | I | A | - | - | 25.5 | 18.2 | 10.1 | 5.67 | 3.74 |
| | | B | - | 40.8 | 28.0 | 18.0 | 9.30 | 5.23 | 3.41 |
| | | C | 67.1 | 46.2 | 28.9 | 16.5 | 10.6 | 6.20 | 3.92 |
| | P | A | - | - | 18.8 | 5.57 | 1.09 | 1.15 | .571 |
| | | B | 236 | 76.6 | 21.2 | 7.21 | 2.13 | 1.19 | .674 |
| | | C | 289 | 72.1 | 21.3 | 7.08 | 2.61 | 1.28 | .670 |
| 255 | I | A | - | - | 28.4 | 17.6 | 4.26 | 6.08 | 3.35 |
| | | B | 115 | 44.9 | 30.9 | 18.3 | 9.99 | 5.62 | 3.43 |
| | | C | 89.9 | 44.5 | 35.1 | 18.1 | 10.8 | 6.11 | 4.02 |
| | P | A | - | - | 18.8 | 5.87 | 1.01 | 1.16 | .578 |
| | | B | 214 | 68.7 | 23.1 | 7.00 | 2.50 | 1.22 | .693 |
| | | C | 278 | 72.8 | 20.2 | 6.71 | 2.63 | 1.27 | .710 |
| 256 | I | A | - | - | 28.3 | 18.7 | 3.74 | 5.91 | 3.15 |
| | | B | 93.6 | 39.7 | 27.8 | 18.5 | 9.75 | 5.23 | 3.45 |
| | | C | 89.2 | 40.1 | 31.2 | 17.0 | 10.8 | 6.23 | 3.52 |

Table A-5
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE
 (PSI-MSEC) FROM 500-LB PENTOLITE SPHERES

| Shot No. | Pressure- Impulse | Gauge Line | Distance (ft) | | | | | | | |
|----------|-------------------|------------|---------------|-----|------|------|------|------|-------|-------|
| | | | 13 | 23 | 37 | 67 | 117 | 200 | 335 | 600 |
| 274 | P | A | 694 | - | - | 14.5 | 5.14 | 2.38 | 0.995 | 0.563 |
| | | B | - | 178 | 81.2 | 19.9 | 8.08 | 2.45 | 1.19 | 0.679 |
| | | C | - | - | 47.9 | 17.0 | 6.68 | 2.35 | 0.901 | - |
| 274 | I | A | 233 | - | - | 64.3 | 53.7 | 34.2 | 16.5 | 10.2 |
| | | B | - | 173 | 109 | 85.0 | 63.1 | 32.1 | 16.0 | 11.3 |
| | | C | - | 115 | 83.9 | 82.7 | 61.8 | 29.9 | 21.3 | 10.2 |
| 276 | P | A | - | - | - | 13.2 | 5.19 | 2.12 | 1.02 | 0.532 |
| | | B | - | 210 | 53.0 | 20.1 | 5.52 | 2.41 | 1.26 | 0.654 |
| | | C | 345 | 269 | 69.5 | - | - | 2.33 | 1.05 | 0.522 |
| 276 | I | A | - | - | - | 51.3 | 57.0 | 30.0 | 16.7 | 9.69 |
| | | B | - | 175 | 115 | 96.5 | 46.4 | 28.6 | 16.7 | 9.64 |
| | | C | 258 | 209 | 142 | - | 32.2 | 30.4 | 18.3 | 10.5 |
| 280 | P | A | - | - | - | 16.6 | 5.10 | 2.39 | 1.01 | 0.561 |
| | | B | - | 212 | - | 15.8 | 5.31 | 2.29 | 0.958 | 0.451 |
| | | C | 479 | 259 | 64.9 | 16.2 | 5.80 | 1.79 | 1.03 | 0.520 |
| 280 | I | A | - | - | - | 65.3 | 48.7 | 28.6 | 17.3 | 9.20 |
| | | B | - | 189 | 131 | 70.1 | 49.8 | 24.1 | 17.4 | 8.65 |
| | | C | 178 | 203 | 93.7 | 81.4 | 49.7 | 20.4 | 15.3 | 10.1 |

Table A-6
 PEAK OVERPRESSURE (PSI) AND POSITIVE-PHASE IMPULSE (PSI-MSEC)
 FROM 1,000-LB TNT SPHERES

| Shot No. | Pressure- Impulse | Gauge Line | Distance (ft) | | | | | | |
|----------|-------------------|------------|---------------|-------|-------|------|------|------|------|
| | | | 22.5 | 37.1 | 65.6 | 117 | 200 | 335 | 600 |
| 063 | P | 1 | - | - | 25.9 | - | 3.4 | 1.5 | 0.6 |
| | | 2 | - | - | - | 8.7 | 3.2 | 2.2 | 0.6 |
| | | 3 | 284.3 | 82.2 | - | 7.7 | - | - | 0.6 |
| 063 | I | 1 | - | - | 112.0 | - | 42.5 | 23.6 | 13.3 |
| | | 2 | - | - | - | 64.9 | 43.8 | 31.6 | 14.0 |
| | | 3 | 327.5 | 186.6 | - | 61.3 | 54.5 | - | 14.9 |
| 064 | P | 1 | - | - | 28.6 | - | 4.4 | 1.8 | 0.6 |
| | | 2 | - | - | - | 8.8 | 3.7 | 1.6 | 0.7 |
| | | 3 | 300.9 | 91.4 | - | 6.9 | - | - | 0.6 |
| 064 | I | 1 | - | - | 129.6 | 71.5 | 53.4 | 28.0 | 14.1 |
| | | 2 | - | - | - | 68.7 | 48.8 | 33.1 | 13.0 |
| | | 3 | 371.3 | 206.8 | - | 63.6 | - | - | 15.9 |

For the system as it existed for the Saturn S-IV test, two 1000-lb TNT spheres were detonated immediately after the propellant test at the usual $1\text{-ft}/\text{lb}^{1/3}$ scaled height of burst. It can be seen from Table A-6 that the gauge array for these tests was much like that for the 25,000-lb and Titan I tests, although there were no 13-ft gauges and only one each at the 22.5-, 37.1- and 66.6-ft distances. It can also be seen from the table that the agreement between the pressure and impulse results from gauges at the same distance and for each particular test is quite good, but that the close-in data for test 064 are generally higher than the data for shot 063. While this difference is not unlike the normal test-to-test reproducibility at those distances, it is unusual for all the data to have the same trend. A possible explanation for this is that the detonation conditions for the two tests were not the same. The charge for Test 063 was detonated at the top surface, using a C4 booster, and the charge for Test 064 was detonated at the center, using the internally cast pentolite booster. While very little is known about the effect of point of initiation on blast-wave parameters measured close to a charge, it appears reasonable to assume that at least some of the data spread can be attributed to this change in detonation conditions.

Results of High-Explosive Calibration Tests

The calibration test data are presented in this section in terms of peak pressure and positive-phase impulse versus scaled distance graphs; for purposes of comparison, standard reference curves are included. Also presented are the results of statistical analysis of both the initial 18-lb pentolite tests and the subsequent series of eighteen 18-lb pentolite tests; no comparable analyses were performed for the 500- and 1000-lb series due to the small number of tests conducted.

The surface burst reference curves included with the calibration data plots are based on TNT surface burst data given in Refs. A-2 and A-3 for pressure and impulse, respectively. For use in these plots, these reference curves have been adjusted to a pentolite base. This adjustment entails the assumption that 1.18 lb of TNT is equivalent to 1 lb of pentolite.

Also included on some of the calibration data figures are reference curves based on spherical charges of Composition A detonated at a scaled HOB of 1.4 ft/lb^{1/3} (from Ref. A-4). As would be expected, the surface burst data tend to lie somewhat below the calibration data collected at a scaled HOB of 1 ft/lb^{1/3} and closer to the Composition A data collected at a scaled HOB of 1.4 ft/lb^{1/3}.

18-lb Pentolite Tests. Plots indicating the results of the twenty-four 18-lb pentolite tests are given for peak pressure and scaled impulse on Figs. A-5 and A-6, respectively. The plotted points represent the mean pressure or impulse averaged over all three gauge lines and all 24 tests; data are occasionally missing for one or more of the three gauges at a given distance, and the data from each test -- rather than each gauge -- were weighted equally. The brackets associated with each plotted point indicate the estimated standard deviation of the measurements, where the standard deviation is of the distribution of pressure or scaled impulse means over the gauge lines; thus, for pressure (or impulse) at 67 ft, the distribution consists of 24 pressure (or 24 scaled impulse) values, and since gauges were installed for the last eight tests only at the 117- and 200-ft distances, the distribution at these distances consists of eight pressure (or eight scaled impulse) values.

One of the major purposes of the calibration tests was to obtain statistical information that would be useful in the analysis of the propellant test data, e.g., estimates of the standard deviation which can be attributed to the instrumentation system. The desired statistical information was obtained from the results of the tests by means of analysis of variance techniques. Separate analyses were made for the pressure and impulse data at each distance; a two-way classification was used, the factors being the shot (test) and the gauge, and interactions were assumed negligible.

The results of the analysis for the initial six tests are given in Table A-7. For this test series a 5-percent level of significance was used. Under the column labeled "effect," a "yes" (or "no") indicates that there was (or was not) a statistically significant difference between the mean values for that

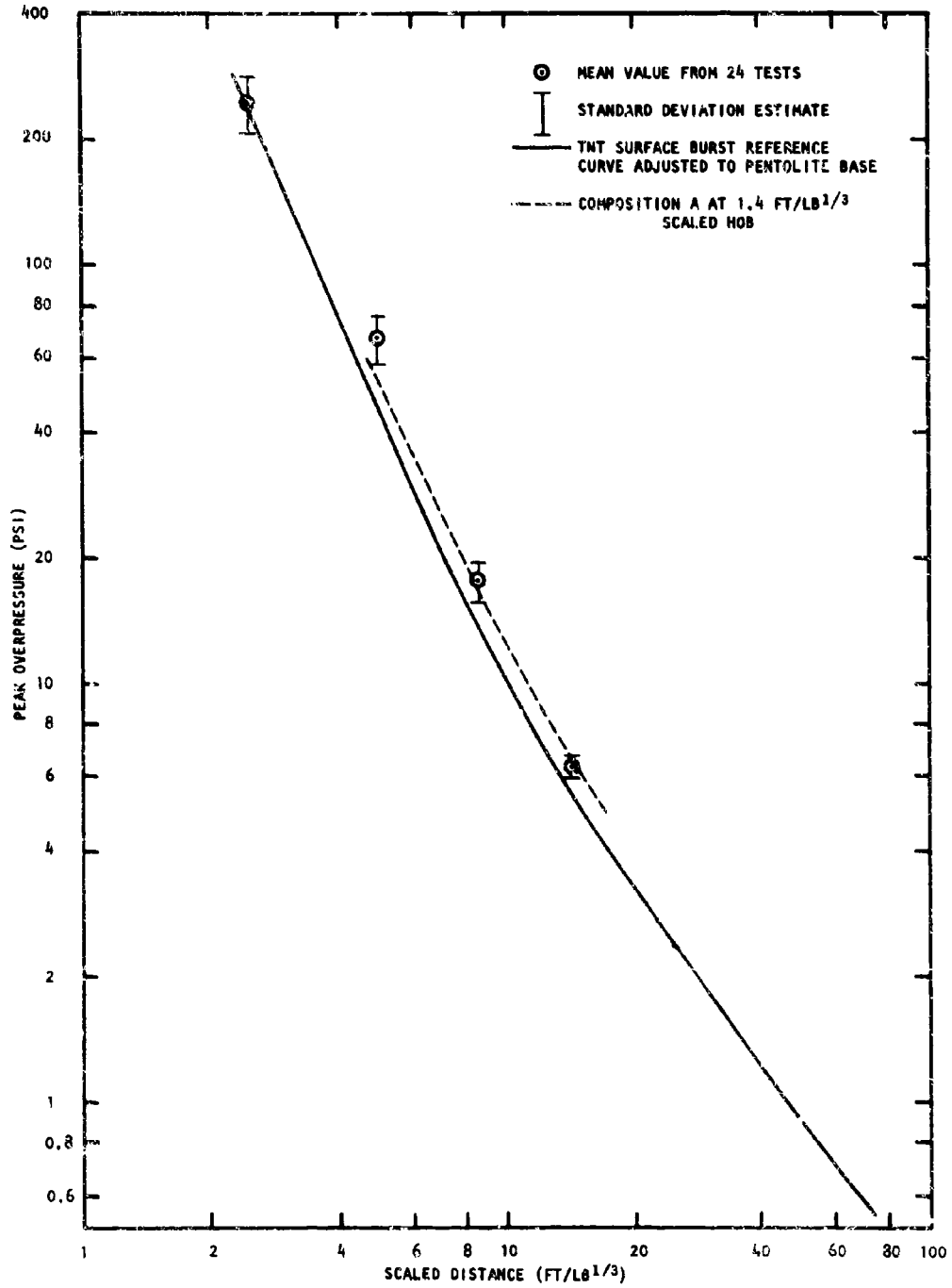


Fig. A-5. Peak Overpressure vs Scaled Distance from Twenty-four Pentolite Spheres at a Scaled HOB of 1.0 ft/lb^{1/3}

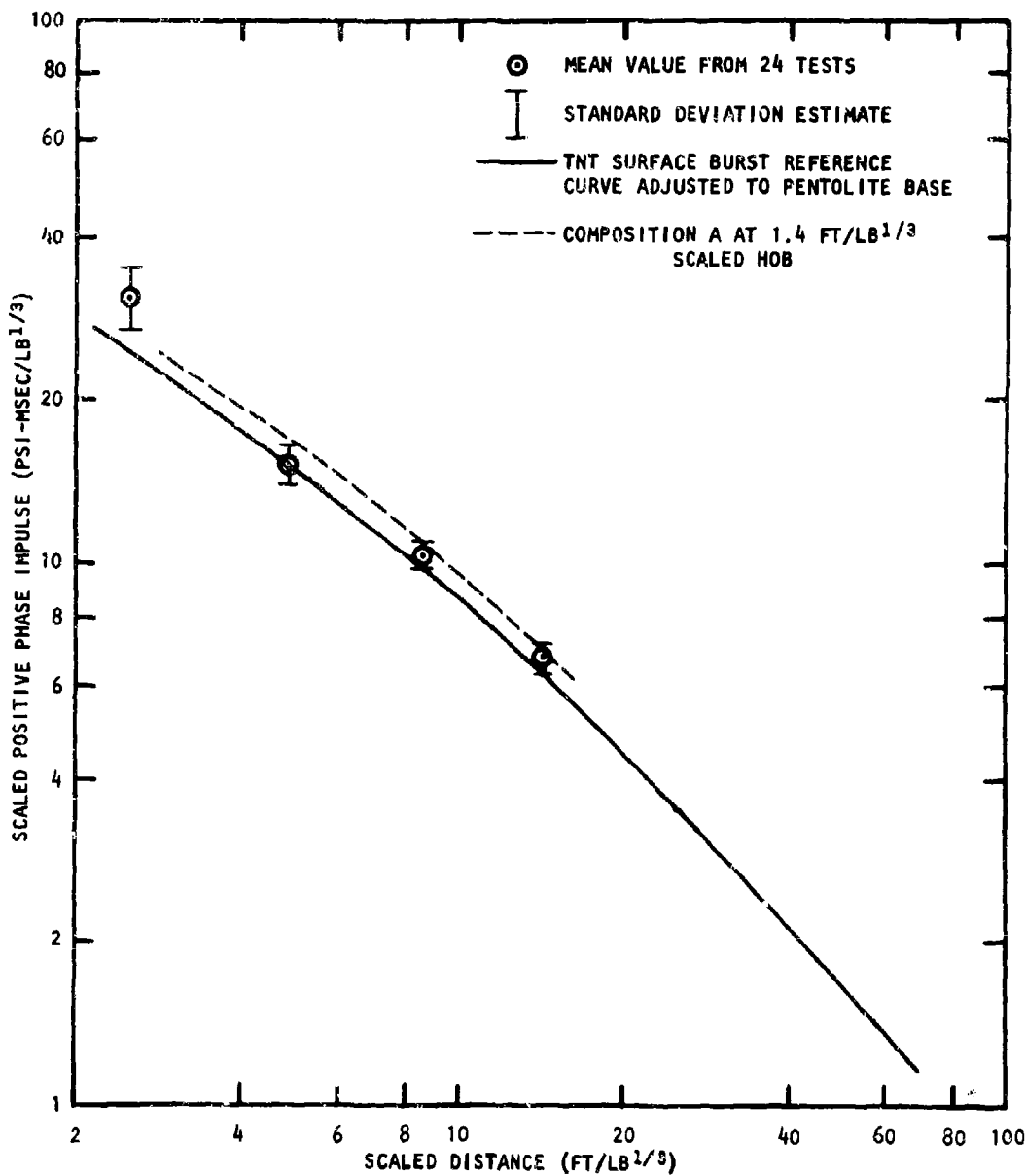


Fig. A-6. Scaled Positive-Phase Impulse vs Scaled Distance from Twenty-four 18-lb Pentolite Spheres at a Scaled HOB of 1.0 ft/lb^{1/3}

parameter. For example, a "yes" under the gauge column indicates that the mean values from the various gauges (averaged over all shots) were significantly different. Similarly a "no" under the shot column indicates that the mean values from the various shots (averaged over all gauges) were not significantly different. Also given in Table A-7 is an estimate of the standard deviation of an individual reading computed from all the data. This standard deviation includes shot and/or gauge effects, if they are present.

Table A-7

RESULTS OF STATISTICAL ANALYSIS OF THE INITIAL SIX 18-1b PENTOLITE TESTS

| PARAMETER | DISTANCE (ft) | EFFECT | | ESTIMATE OF % STANDARD DEVIATION OF INDIVIDUAL READING |
|-----------|------------------|--------|-------|--|
| | | SHOT | GAUGE | |
| Pressure | 6.6 | No | Yes | 15.5 |
| | 12.8 | No | No | 12.2 |
| | 22.5 | Yes | Yes | 14.8 |
| | 22.5* | No | Yes | 13.9 |
| | 37.1 | No | Yes | 11.2 |
| | 66.6 | No | No | 7.4 |
| Impulse | 6.6 | No | Yes | 9.0 |
| | 12.8 | No | No | 5.4 |
| | 22.5 | Yes | Yes | 10.8 |
| | 22.5* | No | Yes | 9.9 |
| | 37.1 | No | No | 7.7 |
| | 66.6 | No | No | 5.7 |

* Eliminating Shot 018

From Table A-7 it can be seen that, except at the 22.5-ft station, there are no significant shot effects for either pressure or impulse. Furthermore, it may be seen that if shot 18 is eliminated from consideration, the shot effect at the 22.5-ft station disappears. Since shot 18 was the first of the six calibration shots and testing procedures were still being developed, it is believed that its results should not be weighted very heavily. Accordingly, it is concluded that the inevitable shot-to-shot variations in the actual blast

wave from the high-explosive charges do not significantly inflate the total standard deviations over and above those that are attributable to the instrumentation system alone.

The analyses, however, did indicate significant gauge effects at the 6.6- and 22.5-ft stations for both pressure and impulse and for pressure at the 37.1-ft station.

The results given in Table A-7 indicate that the system as initially installed typically gave a pressure standard deviation value of about 12 to 13 percent and an impulse deviation of about 8 to 9 percent.

An analysis of variance similar to that for the initial six tests was also performed for the eighteen remaining 18-lb calibration tests, the latter differing only in that a 2.5-percent (rather than 5-percent) level of significance was used due to the increased quantity of data. The results are quite similar to those from the initial test series; for instance, shot effect was present only for impulse at 23 ft, and gauge effects were present in 9 of 14 cases, as opposed to 5 of 10 for the initial series. Gauge line effects, however, are ordinarily of little consequence in evaluating terminal yield as described above, since gauge line averages are used. In the infrequent instances of missing data at the outer distances, adjustments to the data in order to compensate for gauge effects are sometimes necessary. Any investigation of azimuthal variations in the blast wave would of course, require consideration of gauge line effects. However, blast wave asymmetries for the propellant tests (at the AFRPL test site) are not pronounced. Therefore such an investigation, beyond that required to establish the extent of blast asymmetries, is not included in this program. In the interests of indicating the general magnitude of the gauge line effects that are present in the instrumentation system, however, Table A-8 is presented to enable ready comparison of the gauge line mean values of pressure and impulse with the total mean value at each distance for the 18 calibration tests. Where the analysis of variance has indicated that the gauge line differences are significant, the total mean value is followed by an asterisk (*). It can be seen from the table that the mean value for a given gauge line is not grossly different from the corresponding total mean value.

Table A-8
GAUGE LINE COMPARISONS

| PRESSURE- IMPULSE | DISTANCE (ft) | MEAN VALUE OVER GAUGE LINE | | | MEAN VALUE OF ALL MEASUREMENTS |
|----------------------|------------------|----------------------------|-------|-------|--------------------------------------|
| | | A | B | C | |
| Pressure | 6.6 | - | 224 | 262 | 243 |
| | 12.8 | - | 71.7 | 68.9 | 70.3 |
| | 23 | 17.1 | 18.5 | 18.5 | 18.0* |
| | 37 | 6.31 | 6.44 | 6.63 | 6.46* |
| | 67 | 2.72 | 2.49 | 2.59 | 2.60* |
| | 117 | 1.13 | 1.30 | 1.26 | 1.23* |
| | 200 | 0.596 | 0.674 | 0.659 | 0.643* |
| Impulse | 6.6 | - | 83.2 | 80.8 | 82.0 |
| | 12.8 | - | 40.6 | 43.0 | 41.8 |
| | 23 | 25.2 | 27.4 | 26.8 | 26.4 |
| | 37 | 19.0 | 17.6 | 17.2 | 18.0* |
| | 67 | 10.1 | 9.69 | 9.68 | 9.89* |
| | 117 | 5.79 | 5.85 | 6.21 | 5.95 |
| | 200 | 3.39 | 3.42 | 3.72 | 3.51* |

The analysis of variance of these eighteen tests indicated that the pressure standard deviation was about 8.7 percent, and the impulse deviation about 8.7 percent. These numbers are similar to those obtained in the analysis of the initial series of six 18-lb tests, and as before, these values are inflated somewhat due to gauge line and, to a lesser extent, shot effects.

Series of Three 500-lb Pentolite Tests. Overpressure and scaled impulse versus distance plots for the three 500-lb pentolite tests are presented in Fig. A-7. In this case, each point represents the average of three gauge readings (one for each gauge line) at a given distance for a given test. As for the previous test series, the TNT surface burst curves adjusted to the pentolite base are included in the figure (continuous line), along with the curves from Composition A detonated at a scaled HOB of $1.4 \text{ ft/lb}^{1/3}$.

Due to the limited number of tests in the 500-lb series, the analysis of variance was not performed.

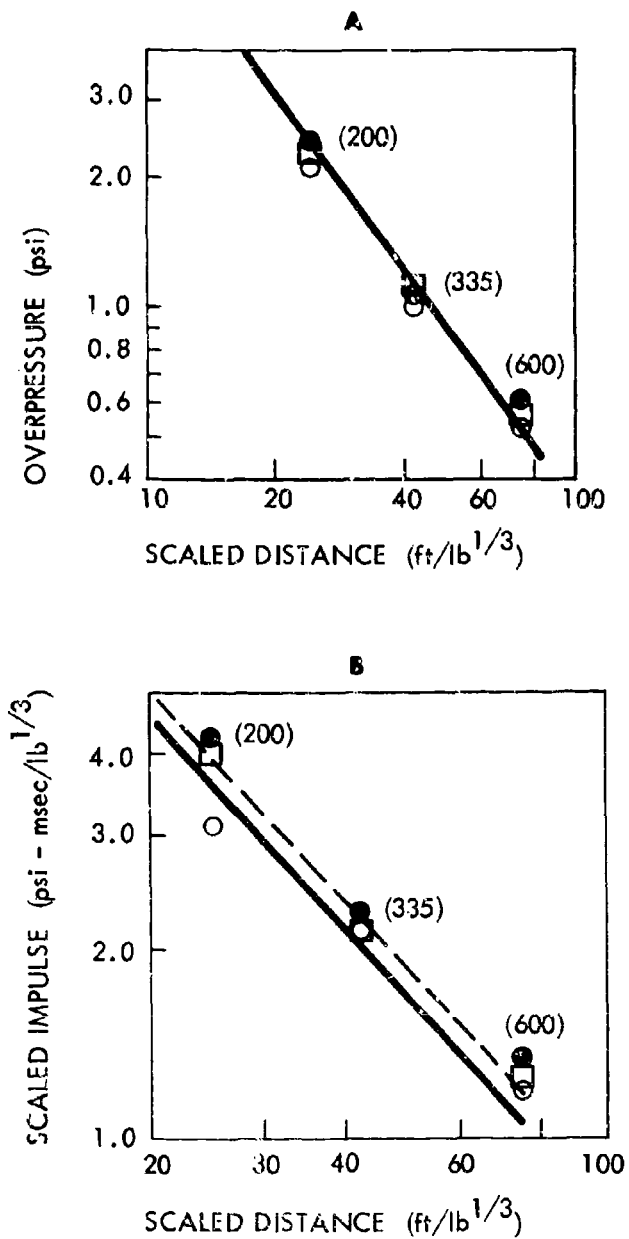


Fig. A-7. Peak Overpressure and Scaled Impulse at Outer Gauge Distances from 500-lb Pentolite Calibration Tests

| STEP NUMBER | VARIABLE | | MULTIPLE | | INCREASE |
|-------------|----------|---------|----------|------------|----------|
| | ENTERED | REMOVED | R | RSQ | IN RSQ |
| 1 | DV^2 | | 0.7272 | 0.5288 (3) | 0.5288 |
| 2 | D^2V^2 | | 0.7564 | 0.5721 (4) | 0.0433 |

Fig. B-1b. Partial Output from Computer Program BMD 02 R

Note that contribution of the second variable was 4 percent. If in our judgment this increase in explained variation was too small, then that variable was omitted from the final form of the regression equation. Such an elimination of terms enabled us to simplify the starting equation given in Section 5 to that used in this section.

The coefficients of the best fitting yield relationships are given in Fig. B-1a (10) and thus

$$\tilde{Y} = 18.4 + 0.0031 D^* v^2 - 0.00015 D^{*2} v^2 \quad (B.1)$$

The residual variance which is needed to estimate the upper prediction interval is 206.4 (8).

Prediction Interval Estimation

To obtain a "reasonable" bound for a future yield observation, an upper prediction interval was computed. For $0 < \alpha < 1$, an upper prediction interval is an interval bounded above which will bracket a single future observation with probability $1-\alpha$. The decision to consider only an upper limit was based on interest in maximum credible yields, rather than minimum. The α level upper prediction interval for an observation Y resulting from a choice of independent variables $(Z_1, Z_2, \dots, Z_{p-1})$ is given by the equation

$$Y_u = \tilde{Y} + \left(t_{n-p}^\alpha \right) S \left(1 + \sum_{i=0}^{p-1} \sum_{j=0}^{p-1} A^{ij} Z_i Z_j \right)^{1/2} \quad (B.2)$$

Where the symbols have the following definitions

\tilde{Y} is the value of the regression equation computed at the point $(Z_1, Z_2, \dots, Z_{p-1})$

t_{n-p}^α is a constant obtained from a table of Students t distribution with $n-p$ degrees of freedom

n is the number of sample points and p is the number of parameters. The quantity $n-p$ can be found easily in Fig. B-1a (9).

S is the square root of S^2 , the residual variance identified earlier

A^{ij} is the ij^{th} element in the inverse to the matrix (A_{ij}) whose ij^{th} element is:

$$\sum_{l=1}^n X_{il} X_{jl}$$

where

X_{ij} is the i ($i = 1, \dots, p-1$)th element of the j^{th} observation of the vector X and for all j $X_{0j} = 1$.

For the LO_2/LH_2 CBGS-V case, we can make the following identifications:

\tilde{Y} is given by Eq. (B.1)

α was chosen to be 0.1 for all cases

$n = 20$, $p = 3$, and $n-p = 17$

$$t_{n-p}^{\alpha} = 1.33$$

$$S = \sqrt{206.4}$$

$$(A_{ij}) = \begin{pmatrix} 20 & 1.705445 \times 10^5 & 8.101516 \times 10^5 \\ 1.705445 \times 10^5 & 2.346231 \times 10^9 & 1.214744 \times 10^{10} \\ 8.101516 \times 10^5 & 1.214744 \times 10^{10} & 7.912697 \times 10^{10} \end{pmatrix}$$

This matrix, when inverted, yields

$$A^{ij} = \begin{pmatrix} 1.374266 \times 10^{-1} & -1.318130 \times 10^{-5} & 6.165114 \times 10^{-7} \\ -1.318130 \times 10^{-5} & 3.341659 \times 10^{-9} & -3.780473 \times 10^{-10} \\ 6.165114 \times 10^{-7} & -3.780473 \times 10^{-10} & 6.436284 \times 10^{-11} \end{pmatrix}$$

Series of Two 1,000-lb TNT Tests. Pressure and impulse data from the two 1,000-lb TNT tests that followed the S-IV propellant test are plotted in Figs. A-8 and A-9, respectively. Each data point represents the gauge line average at that distance for a particular test. The TNT surface burst reference curves from Refs. A-2 and A-3 are included in the figures (continuous line); these curves are thus identical with those used to evaluate yields. The Composition A curves at the scaled HOB are also included (dashed line), although in this case they have been adjusted to a TNT base by the assumption that 1.18 lb of TNT is equivalent to 1.0 lb of Composition A.

It can be seen from the figures that the data once again tend to lie between the surface burst and scaled HOB curves.

Due to the small number of tests, no statistical analysis of the data was conducted.

Upon identifying

$$Z_0 = 1$$

$$Z_1 = D^* v^2$$

$$Z_2 = D^{*2} v^2$$

Eq. (B.2) can be rewritten as

$$Y_u = 18.4 + 0.0031 D^{*2} v^2 - 0.00015 D^{*2} v^2 + 1.33 \sqrt{206.4} (1 + 1.374266 \times 10^{-1} \\ + 3.341659 \times 10^{-9} D^{*2} v^4 + 6.436284 \times 10^{-11} D^{*4} v^4 - 2.636260 \times 10^{-5} \\ D^{*2} v^2 + 12.330228 \times 10^{-7} D^{*2} v^2 - 7.560946 \times 10^{-10} D^{*3} v^{4.1/2})$$

LO₂/RP-1 CBGS - VERTICAL

The form of the generalized equation selected for the regression analysis of this case was

$$\frac{Y}{v^n} = a + \frac{bD^*}{v^m} \text{ for } 0 \leq \frac{D^*}{v^m} \leq \left(\frac{D^*}{v^m}\right)_{\text{crit}}$$

$$\frac{Y}{v^n} = a' + b' \left(\frac{D^*}{v^m}\right)^{-1} \text{ for } \frac{D^*}{v^m} > \left(\frac{D^*}{v^m}\right)_{\text{crit}}$$

where a, b, a', b', n, and m are constants.

In this analysis the data (given in Table B-2) were divided into two sets, set number 1 consisting of those having D^*/v^m values less than the critical and set 2 consisting of those values of D^*/v^m greater than critical. Various trial plots of the data using a range of n and m values were used to make the initial grouping of data. Regression analyses were then made using the above equations and the data in Table B-2 for all combinations of n and m ranging from 0.1 to 1.0 in 0.1 increments.

Although the minimum residual error for each part of the equation did not occur for the identical set of n and m values, the two minimums were quite close

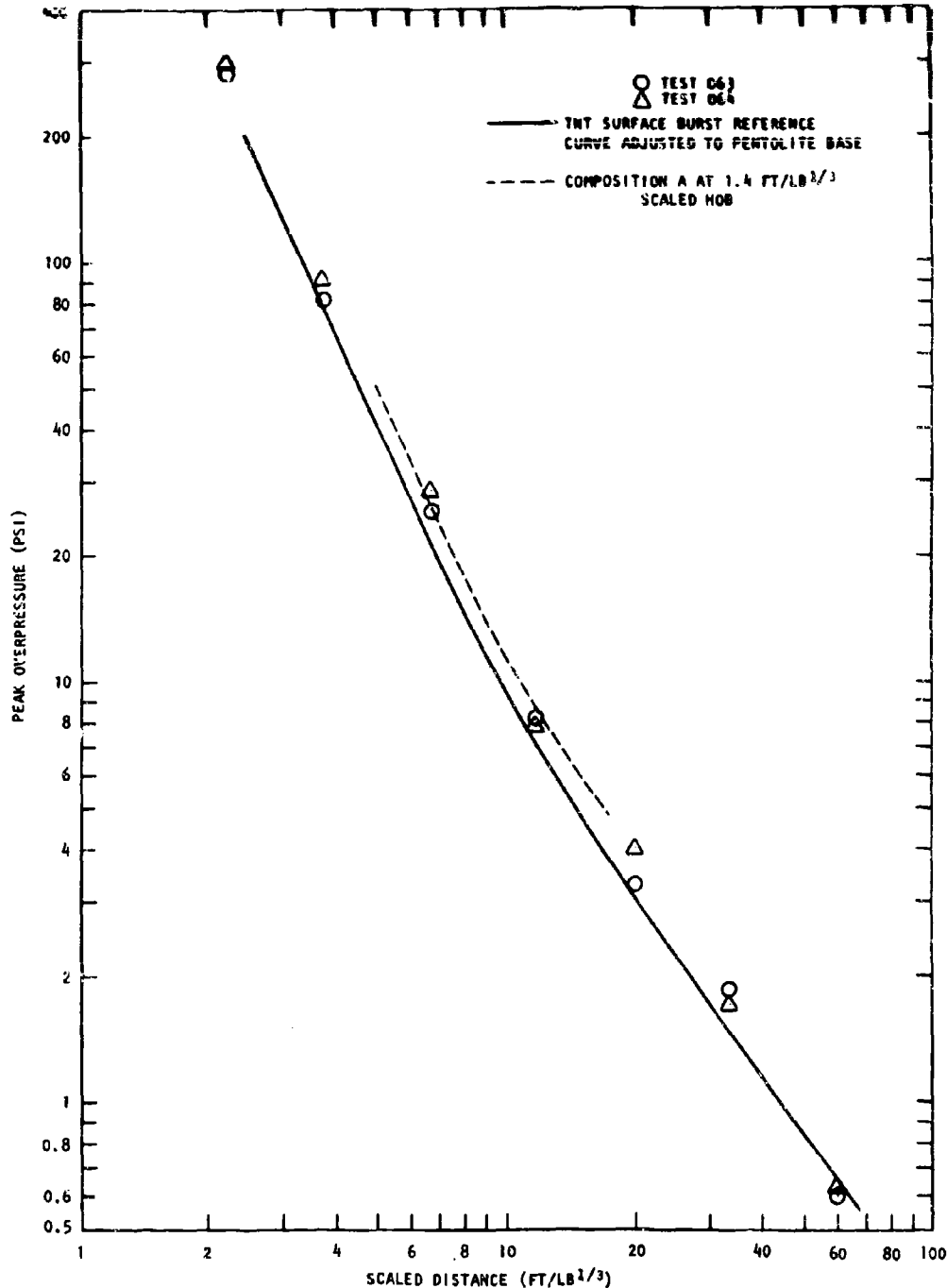


Fig. A-8. Peak Overpressure vs Scaled Distance from 1000-lb TNT Spheres at a Scaled HOB of 1.0 ft/lb^{1/3}

Table B-2
 LO_2 /RP-1 CBGS - VERTICAL DATA USED IN ANALYSIS

| L/D | Weight (lb) | Velocity (ft/sec) | Test Number | Terminal Yield (%) | Ignition Time (msec) |
|-----|----------------|----------------------|----------------|--------------------------|----------------------------|
| 1.8 | 200 | 23 | 144 | 24 | 190 |
| 1.8 | 200 | 23 | 202 | 42 | 870 |
| 1.8 | 200 | 23 | 249 | 25 | 210 |
| 1.8 | 200 | 44 | 208 | 62 | 460 |
| 1.8 | 200 | 44 | 232 | 30 | 1220 |
| 1.8 | 200 | 44 | 249 | 50 | 710 |
| 1.8 | 200 | 44 | 250 | 52 | 200 |
| 1.8 | 1000 | 44 | 267 | 64 | 1170 |
| 1.8 | 1000 | 44 | 268 | 70 | 340 |
| 1.8 | 1000 | 44 | 220 | 96 | 525 |
| 1.8 | 1000 | 44 | 219 | 14 | 1835 |
| 1.8 | 200 | 77 | 110 | 26 | 35 |
| 1.8 | 200 | 77 | 205 | 41 | 40 |
| 1.8 | 200 | 77 | 206 | 85 | 350 |
| 1.8 | 200 | 77 | 207 | 38 | 28 |
| 1.8 | 200 | 77 | 236 | 74 | 720 |
| 1.8 | 1000 | 77 | 190 | 96 | 570 |
| 1.8 | 1000 | 77 | 269A | 44 | 77 |

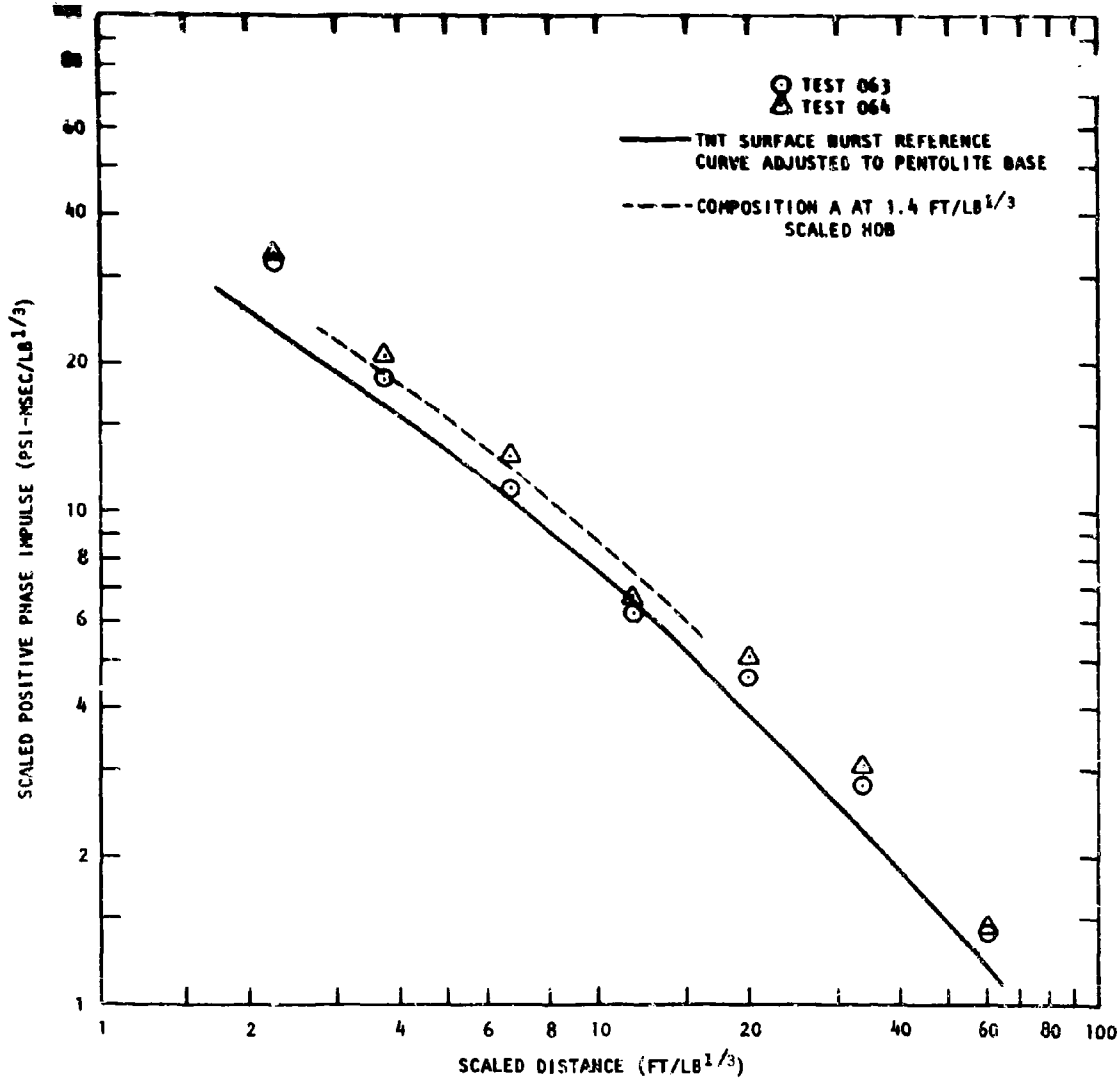


Fig. A-9. Scaled Positive-Phase-Impulse vs Scaled Distance from 1000-lb TNT Spheres at a Scaled HOB of 1.0 ft/lb^{1/3}

Appendix A
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- A-3. Defense Atomic Support Agency, Operation SNOW BALL Project Descriptions, Volume 1 (U), DASA Data Center Special Report 24-1, DASA 1516-1 (AD 441 974)
- A-4. Wilton, C., K. Kaplan, and N. Wallace, Study of Channeling of Air Blast Waves Final Report, URS 170-19, DASA-1605, URS Corporation for the Defense Atomic Support Agency, Burlingame, California, December 1964

together and the errors in the vicinity were changing rather slowly. The general region of the minimums for both parts of the equation occurred for n values of 0.8 and 0.9, and m values of 0.1 to 0.3.

The specific values of n = 0.9 and m equal to 0.2 were selected as suitable values near the minimum of each part of the equations and used in determining the following final form of the equations:

$$\left(\frac{Y}{v^{0.9}}\right) = 0.22 + 2.7 \frac{D^*}{v^{0.2}}$$

$$\text{for } \frac{D^*}{v^{0.2}} \leq 1.1$$

$$\left(\frac{Y}{v^{0.9}}\right) = 0.33 + 3.2 \frac{v^{0.2}}{D^*}$$

$$\text{for } \frac{D^*}{v^{0.2}} > 1.1$$

The upper prediction intervals calculated for these equations are:

$$\left(\frac{Y}{v^{0.9}}\right)_u = \left(\frac{Y}{v^{0.9}}\right) + (0.20)(1.42) \left(1.38 - 1.18 \frac{D^*}{v^{0.2}} + 1.28 \frac{D^{*2}}{v^{0.4}}\right)^{1/2}$$

$$\text{for } \frac{D^*}{v^{0.2}} \leq 1.1$$

$$\left(\frac{Y}{v^{0.9}}\right)_u = \left(\frac{Y}{v^{0.9}}\right) + (0.37)(1.46) \left(1.64 - 2.33 \frac{v^{0.2}}{D^*} + 2.49 \frac{v^{0.4}}{D^{*2}}\right)^{1/2}$$

$$\text{for } \frac{D^*}{v^{0.2}} > 1.1$$

LO₂/LH₂ CRM

The generalized form of the equation selected for the regression analysis was

$$Y = a + bt^* + \frac{c}{W} + d(L/D) + e(D_o/D_t)$$

where a, b, c, d, and e are constants and $t^* = t/W^{1/3}$.

The data for the D_o/D_t of 1.0 and L/D of 1.8 condition were excluded from the general analysis because their general pattern was significantly different from that of the remainder of the data, and the condition is of little interest, because D_o/D_t values greater than 0.45 are considered highly unlikely.

The remainder of the data indicated that the yield increased with time up to a scaled time of about 17 msec/lb^{1/3} and then became independent of time. Accordingly, the data shown in Table B-3 were divided into two groups for analysis with this equation.

Set 1 consists of 9 points corresponding to time values less than or equal to 100 msec. Set 2 consists of 11 points for which the ignition time was greater than or equal to 82 msec.

For set 1, the regression equation was

$$\tilde{Y} = 2.75 + 0.82 t^* \quad (B.3)$$

The other independent variables, L/D and D_o/D_t , do not explain any appreciable amount of variability and hence were not included in the regression equation.

For set 2, the regression equation was computed to be

$$\tilde{Y} = 20.0 + 0.00034 t^* \quad (B.4)$$

Appendix B
STATISTICAL ANALYSIS PROCEDURES

Data collected in the basic cryogenic test program were analyzed using the methodology described in this section. The two propellant combinations, $\text{LO}_2/\text{RP-1}$ and LO_2/LH_2 , and the two basic test configurations (CBM and CBGS) used gave rise to the four cases described in Section 5. To simplify the presentation of the statistical procedures, the analysis of the data arising in each of the four cases is presented in the following order:

1. LO_2/LH_2 CBGS - Vertical
2. $\text{LO}_2/\text{RP-1}$ CBGS - Vertical
3. LO_2/LH_2 CBM
4. $\text{LO}_2/\text{RP-1}$ CBM

The analysis was accomplished in two steps. In the first, regression techniques* were employed to obtain a relationship between yield and factors affecting yield. The process of establishing the form of the yield equation is described for each case in Section 5. Regression techniques were used to compare alternative forms for the yield equation, to select factors which could best explain observed variation in yield, and to determine values for the unknown parameters in the final equation.

In the second step, prediction interval estimation* techniques were used to determine an upper bound on a future yield observation.

These two steps will be described in detail and illustrated with the data from the LO_2/LH_2 CBGS-Vertical test series. For the other cases, the two steps are described briefly and variations in the analysis procedure are noted.

* See R. G. Miller, Simultaneous Statistical Inference, McGraw Hill, 1966, New York, for description of these techniques.

Table B-3
TEST DATA FOR LH₂/LO₂ CBM

| L/D | D _o /D _t | Weight (lb) | Test Number | Ignition Time (msec) | Terminal Yield (%) | Set Number |
|-----|--------------------------------|----------------|----------------|----------------------------|--------------------------|---------------|
| 1.8 | 0.45 | 200 | 053 | 1 | 4 | 1 |
| 1.8 | 0.45 | 200 | 090 | 35 sec | 29 | 2 |
| 1.8 | 0.45 | 200 | 118 | 82 | 20 | 1,2 |
| 1.8 | 0.45 | 200 | 199 | 816 | 8 | 2 |
| 1.8 | 0.45 | 200 | 200 | 417 | 17 | 2 |
| 1.8 | 0.45 | 1000 | 210 | 20 | 7 | 1 |
| 1.8 | 0.45 | 1000 | 212 | 1366 | 27 | 2 |
| 1.8 | 0.45 | 1000 | 213 | 708 | 35 | 2 |
| 1.8 | 0.45 | 1000 | 265 | 750 | 10 | 2 |
| 1.8 | 0.083 | 200 | 169 | 318 | 15 | 2 |
| 1.8 | 0.083 | 200 | 173 | 56 | 13 | 1 |
| 5 | 1 | 200 | 052 | 83 | 7 | 1 |
| 5 | 1 | 200 | 057 | 12 | 1 | 1 |
| 5 | 1 | 200 | 092 | 3 min | 26 | 2 |
| 5 | 0.45 | 200 | 054 | 17 | 6 | 1 |
| 5 | 0.45 | 200 | 055 | 1 | 1 | 1 |
| 5 | 0.45 | 200 | 094 | 329 | 25 | 2 |
| 5 | 0.45 | 200 | 138 | 100 | 17 | 1,2 |

LO_2/LH_2 CBGS - VERTICAL

Yield Estimation by Regression Analysis

In simple regression, one has a dependent variable, Y, and an independent variable, X. A set of observation pairs collected on Y and X is used to fit a line relating Y to X. In multiple regression, one has a dependent variable, Y, and several independent variables, X_1, X_2, \dots, X_p . Sets of data consisting of $p + 1$ numbers are collected from which is obtained a set of coefficients $\beta_0, \beta_1, \dots, \beta_p$ enabling the yield to be expressed in terms of the independent variables according to the equation

$$Y = \beta_0 + \beta_1 X_1 + \beta_2 X_2 \dots + \beta_p X_p$$

The relationship, though seemingly linear, can be made nonlinear by replacing an independent or dependent variable with some nonlinear function of that variable. Thus, for example, Y can be replaced by $Y^{1/2}$ or X_1 can be replaced by X_1^2 , etc. Thus, many different functional forms of the regression equation can be examined to find that one which does the best job in explaining the variation.

For the LO_2/LH_2 CBGS-Vertical case, the final form of the regression equation was found (Section 5) to be of the form

$$Y = \beta_0 + \beta_1 D^* v^2 + \beta_2 v^2$$

where

$$D^* = \frac{Tv}{W^{1/3}}$$

and the symbols are defined as follows:

v denotes velocity (fps)

T denotes ignition time (sec)

W denotes weight (lb)

which for all intents and purposes can be considered independent of t .

The regression relation for yield was then taken to be Eq. (B.3) up to the time, t_c , at which this relation intersected the constant 20.0 and constant thereafter.

Thus,

$$\hat{Y} = \begin{cases} 2.75 + 0.82 t^* & t^* \leq 21.1 \\ 20.0 & t^* > 21.1 \end{cases}$$

An upper prediction interval for Eq. (B.3) was computed to be $Y_u = 2.75 + 0.82 t^* + (1.415) (4.10) (1.24 - 0.00374 t^* + 0.00270 t^{*2})^{1/2}$, where 1.415 is the value of the t distribution of $\alpha = 0.1$ and seven degrees of freedom, and 4.10 is the residual standard deviation, S .

An upper prediction interval for Eq. (B.4) is

$$Y_u = 20.0 + (1.39) (8.46) (1.10)^{1/2}$$

It is possible to join the two upper prediction intervals by finding the point in which they intersect in the same manner as described for the regression equations.

LO₂/RP-1 CBM

The data for this case consist of the 23 tests described in Table B-4. The data can be observed to divide into two overlapping sets. One set consists of the sixteen 200-lb tests in which the parameter L/D assumes the values of 1.8 and 5 and the parameter D_o/D_t assumes the values 0.45 and 1. The other set consists of the 13 tests for which $L/D = 1.8$ and $D_o/D_t = 0.45$. In these 13 tests, the propellant weight parameter takes on the values of 200, 1,000 and 25,000 lb.

For each test the data consisted of yield, ignition time, impact velocity, and propellant weight. For the 20 tests used in the analysis, these data are shown in Table B-1.

Computations were done on a CDC 3800 computer system using a stepwise regression program, BMD 02 R, developed as part of the biomedical programs package by the UCLA Health Sciences Computing Facility. A partial output from this program is shown in Fig. B-1a.[†]

Note that the regression equation is developed stepwise. The contribution of each variable in reducing the unexplained variation in the data is computed and the variable showing the greatest reduction is entered first.

In this case D^*v^2 was the first variable entered (1)^{††} and $D^{*2}v^2$ followed (2) in Step 2 as the remaining variable.

In Fig. B-1b, also obtained as output from the computer program, the contribution of each variable in reducing the variation is observable. The entry of D^*v^2 explained 53 percent (3) of the original variation, leaving 47 percent yet unexplained. Variable $D^{*2}v^2$ explained an additional 4 percent (4), increasing the explained variation to 57 percent and leaving an unexplained variation of 43 percent of the original variation in the data.

In Fig. B-1a, the total variation is 8199, (5), and the unexplained variation is 3863 (6), leaving an explained variation of 4336 (7). The ratio

$$\frac{4366}{8199} = 53\%$$

is the percentage of variation explained in the variable entered in the first step.

[†] Note the actual output has been retyped and edited to improve its clarity.

^{††} The numbers in parentheses are keyed to the corresponding points of interest in the figures. The figure has been reproduced in upper case. Thus v appears in the figure as cap V. Also D* appears as D.

Table B-4
 SUMMARY OF TEST CONDITIONS AND TERMINAL YIELDS
 FOR $\text{I.O}_2/\text{RP-1}$ CONFINEMENT BY THE MISSILE TESTS

| L/D | D_o/D_t | Propellant Weight (lb) | Test Number | Ignition Time (msec) | Terminal Yield (%) |
|-----|-----------|------------------------------|----------------|----------------------------|--------------------------|
| 1.8 | 1 | 200 | 042 | 290 | 48 |
| 1.8 | 1 | 200 | 058 | 200 | 27 |
| 1.8 | 1 | 200 | 086 | 100 | 14 |
| 1.8 | 0.45 | 200 | 044 | 120 | 18 |
| 1.8 | 0.45 | 200 | 087a | 70 | 16 |
| 1.8 | 0.45 | 200 | 095a | 120 | 17 |
| 1.8 | 0.45 | 200 | 101 | 145 | 35 |
| 1.8 | 0.45 | 200 | 237 | 127 | 32 |
| 1.8 | 0.45 | 200 | 238 | 85 | 19 |
| 1.8 | 0.45 | 200 | 239 | 156 | 32 |
| 1.8 | 0.45 | 1000 | 192 | 216 | 14 |
| 1.8 | 0.45 | 1000 | 193 | 222 | 20 |
| 1.8 | 0.45 | 1000 | 209 | 121 | 10 |
| 1.8 | 0.45 | 1000 | 270a | 225 | 13 |
| 5 | 1 | 200 | 047 | 120 | 10 |
| 5 | 1 | 200 | 049 | 316 | 12 |
| 5 | 1 | 200 | 085 | 380 | 12 |
| 5 | 0.45 | 200 | 046 | 143 | 17 |
| 5 | 0.45 | 200 | 088 | 60 | 4 |
| 5 | 0.45 | 200 | 100 | 220 | 23 |
| 1.8 | 0.45 | 25000 | 275 | 515 | 4 |
| 1.8 | 0.45 | 25000 | 278 | 530 | 13 |
| 1.8 | 0.45 | 25000 | 282 | 540 | 13 |

Table B-1
 LO_2/LH_2 CBQS - VERTICAL DATA USED IN ANALYSIS

| L/D | Weight (lb) | Velocity (ft/sec) | Test Number | Terminal Yield (%) | Ignition Time (msec) |
|-----|----------------|----------------------|----------------|--------------------------|----------------------------|
| 1.8 | 200 | 23 | 152 | 14 | 480 |
| 1.8 | 200 | 23 | 184 | 17 | 810 |
| 1.8 | 200 | 23 | 201 | 26 | 1524 |
| 1.8 | 200 | 23 | 225 | 34 | 933 |
| 1.8 | 200 | 23 | 153 | 14 | 121 |
| 1.8 | 200 | 44 | 254 | 32 | 533 |
| 1.8 | 200 | 44 | 197 | 19 | 500 |
| 1.8 | 200 | 44 | 231 | 24 | 525 |
| 1.8 | 200 | 44 | 203 | 31 | 800 |
| 1.8 | 200 | 44 | 251 | 64 | 775 |
| 1.8 | 200 | 44 | 204 | 42 | 317 |
| 1.8 | 200 | 44 | 252 | 38 | 325 |
| 1.8 | 200 | 44 | 229 | 53 | 1374 |
| 1.8 | 1000 | 44 | 217 | 33 | 1490 |
| 1.8 | 1000 | 44 | 262 | 42 | 900 |
| 1.8 | 200 | 77 | 114 | 54 | 74 |
| 1.8 | 200 | 77 | 150 | 35 | 40 |
| 1.8 | 200 | 77 | 151 | 46 | 167 |
| 1.8 | 200 | 77 | 195 | 104 | 292 |
| 1.8 | 200 | 77 | 226 | 51 | 283 |

The final form of the yield equation of interest is

$$\frac{Y}{t^*} = (1 + \beta/W) (\beta_0 + \beta_1 L/D + \beta_2 D_0/D_t)$$

where $t^* = t/W^{1/3}$ and t is time in milliseconds.

Yield Estimation by Regression Analysis

The regression coefficients β , β_0 , β_1 , and β_2 were obtained in three steps.

Step 1

Starting with the equation

$$\left(\frac{Y}{t^*}\right) = \beta_0 + \beta_1 (L/D) + \beta_2 (D_0/D_t)$$

the 200-lb data were used to estimate β_0 , β_1 , and β_2 . The resulting estimates were

$$\hat{\beta}_0 = 1.792$$

$$\hat{\beta}_1 = 0.190$$

$$\hat{\beta}_2 = 0.570$$

Step 2

The estimates for β_0 , β_1 , and β_2 were then substituted into the equation

$$\frac{Y}{t^*} = (\alpha + \beta/W) [\hat{\beta}_0 + \hat{\beta}_1 (L/D) + \hat{\beta}_2 (D_0/D_t)]$$

Using the data for which $L/D = 1.8$ and $D_0/D_t = 0.45$, the factor

| | | | |
|-------------------------------|-----------------------------------|---------------------|-------------|
| DEPENDENT VARIABLE | Y | | |
| STEP NUMBER | 1 | | |
| VARIABLE ENTERED | DV ² (1) | | |
| MULTIPLE R | 0.7272 | | |
| STD.ERROR OF EST. | 14.6496 | | |
| ANALYSIS OF VARIANCE | | | |
| | DF | SUM OF SQUARES | MEAN SQUARE |
| REGRESSION | 1 | 4335.561 (7) | 4335.561 |
| RESIDUAL | <u>18</u> | <u>3862.989</u> (6) | 214.610 |
| TOTAL | 19 | 8198.550 (5) | |
| VARIABLES IN EQUATION | | | |
| VARIABLE | COEFFICIENT | | |
| CONSTANT | 19.85061 | | |
| DV ² | .00221 | | |
| STEP NUMBER | 2 | | |
| VARIABLE ENTERED | D ² V ² (2) | | |
| MULTIPLE R | 0.7564 | | |
| STD.ERROR OF EST. | 14.3657 | | |
| ANALYSIS OF VARIANCE | | | |
| | DF | SUM OF SQUARES | MEAN SQUARE |
| REGRESSION | 2 | 4690.220 | 2345.110 |
| RESIDUAL | <u>17</u> (9) | <u>3508.330</u> | 206.372 (8) |
| TOTAL | 19 | 8198.550 | |
| VARIABLES IN EQUATION | | | |
| VARIABLE | COEFFICIENT | | |
| CONSTANT | 18.40333 (10) | | |
| DV ² | .00310 (10) | | |
| D ² V ² | -.00015 (10) | | |

Fig. B-1a. Partial Output from Computer Program BMD 02 R

$$[\beta_0 + \beta_1 (L/D) + \beta_2 (D_o/D_t)]$$

is constant, and estimates $\hat{\alpha}$ and $\hat{\beta}$ can be computed for α and β . This computation yielded

$$\hat{\alpha} = 0.4850$$

$$\hat{\beta} = 105.236$$

Step 3

An equation of the desired form can now be obtained by dividing the first factor, $(\alpha + \beta/W)$ by α and simultaneously multiplying the second factor, $\beta_0 + \beta_1 (L/D) + \beta_2 (D_o/D_t)$, by α . This computation yields the final equation:

$$\left(\frac{\widetilde{Y}}{t^*}\right) = \left(1 + \frac{217.0}{W}\right) [0.8693 + 0.0919 (L/D) + 0.2763 (D_o/D_t)] \quad (B.5)$$

It is recognized that this is not a standard regression computation and the slight overlap in the data will produce some error in the equation. It is felt, however, that the error is not as large as might be the result of using some less desirable form of the equation.

Upper Prediction Interval Estimation

By restricting our interest to large weights, the above equation is approximately

$$\left(\frac{\widetilde{Y}}{t^*}\right) \approx 0.8693 + 0.0919 (L/D) + 0.2763 (D_o/D_t)$$

We can then use all the data to set up the matrix (A_{ij}) and obtain the confidence interval. The value for S^2 was obtained by taking the sum of the squared deviation of each data point from the value of Eq. (B.5) and dividing by 23 minus 3, to adjust for the effect of the number of parameters estimated.

This yielded the upper prediction interval

$$\begin{aligned} \left(\frac{Y}{t^*}\right)_u &= \left(1 + \frac{217.0}{W}\right) [0.8693 + 0.0919 (L/D) + 0.2763 (D_o/D_t)] \\ &+ 1.33 \left(\frac{217.0}{W}\right) 0.29 [1 + 0.3627 + 0.02459 (L/D)^2 \\ &+ 0.8326 (D_o/D_t)^2 - 0.07466 (L/D) - 0.7442 (D_o/D_t) \\ &- 0.09258 (L/D) (D_o/D_t)]^{1/2} \end{aligned}$$

Appendix C
THERMAL INSTRUMENTATION

Appendix C consists of discussions of the thermal instruments, the errors of the corresponding measurements, and the experimental arrangement and instrument mounting. The following list of measurements and the associated instruments are considered:

- Heat flux density computed from measurements of the surface temperature of slabs
- Fireball temperature from a photographic recording Pyrometer (a Sandia Corporation instrument)
- Radiant flux density within and external to the fireball from Gardon-type radiometers
- Gas temperature from thermocouple probes (Sandia Corporation instruments)

The general organization of Appendix C is given in the following paragraphs.

The measurement of heat flux density is considered first, starting with a description of the method of its evaluation from the surface temperature records. This is followed by an analysis of errors that are uniquely associated with this method and that are not related to the errors in heat flux density which are a consequence of errors in slab temperature measurements. The slab temperature transducers or instruments are then considered, and this is followed by a discussion of heat flux density errors that result from errors in the slab temperature measurements.

A brief description is then given of the photographic recording pyrometer. This instrument indicates blackbody equivalent temperatures through a radiation intensity comparison of an image of the fireball with corresponding images of several tungsten filaments, each of which is at a different temperature.

The radiometers are next described, and this is followed by an analysis of radiometer data correction factors, including an indication of their uncertainties, which are required in conjunction with intrafireball measurements.

A brief description of the thermocouple probes is then given. Finally, the experimental arrangement and instrument mounting is described.

Appendix C
THERMAL INSTRUMENTATION

HEAT FLUX DENSITY: METHOD OF EVALUATION, INSTRUMENTS, AND ERRORS

Computational Method of Evaluating Heat Flux Density from Surface-Temperature Data

The basic relationship from which the heat flux density history at the surface of a slab is evaluated from the slab surface-temperature history is*

$$q(t) = K' \int_0^t \frac{\partial T_s(\tau)}{\sqrt{t-\tau}} d\tau \quad (C.1)$$

where q is the heat flux density; $K' = (K\rho c/\pi)^{1/2}$; K , ρ , and c are the conductivity, density, and specific heat, respectively, of the slabs; T_s is the slab surface temperature; and t is time. This relationship is valid provided the heat flux density is uniform over the surface of the slab, the thermal properties, K , ρ , and c , are constant, and the slab is semi-infinite, that is, it occupies, or effectively occupies, half of space.

The evaluation of $q(t)$ from the surface temperature in data-trace form through application of Eq. (C.1) may be accomplished by any of numerous procedures. For convenience, we have chosen to modify Eq. (C.1) to a form which particularly accommodates temperature data in digital form, and this modification is described in the following paragraph.

Integration of Eq. (C.1) over the time range $t = 0$ to $t = t$ is first divided into arbitrary intervals, so that for t in the interval $t_{i-1} \leq t \leq t_i$, $q(t)$ is given by

* A derivation of Eq. (C.1) is given starting p. 132 in Ref. C-1.

$$\frac{q(t)}{K'} = \sum_{n=1}^{n=i-1} \int_{t_{n-1}}^{t_n} \frac{\frac{\partial T_s(\tau)}{\partial \tau}}{(t-\tau)^{1/2}} d\tau + \int_{t_{i-1}}^t \frac{\frac{\partial T_s(\tau)}{\partial \tau}}{(t-\tau)^{1/2}} d\tau, \quad (t_0 = 0) \quad (C.2)$$

Then an expression representing the temperature in the n^{th} interval $t_{n-1} \leq t \leq t_n$ is assumed and substituted into Eq. (C.2); in particular, the linear expression $T_{ns}(t) = a_n + b_n t$ has been chosen. Equation (C.2) then reduces to

$$\frac{q(t)}{K'} = 2 \left[b_1 t^{1/2} + \sum_{n=1}^{n=i-1} (b_{n+1} - b_n)(t - t_n)^{1/2} \right] \text{ for } t_{i-1} \leq t \leq t_1 \quad (C.3)$$

Eq. (C.3) is further modified by writing the constants b_n in terms of the temperatures and corresponding times at the ends of the time intervals, that is, by $b_n = (T_n - T_{n-1}) / (t_n - t_{n-1})$, giving

$$\frac{q(t)}{K'} = 2 \left[\frac{T_1}{t_1} t^{1/2} + \sum_{n=1}^{n=i-1} \left(\frac{T_{n+1} - T_n}{t_{n+1} - t_n} - \frac{T_n - T_{n-1}}{t_n - t_{n-1}} \right) (t - t_n)^{1/2} \right] \quad (C.4)$$

for $t_{i-1} \leq t \leq t_1$

Eq. (C.4) is, finally, the working equation, that is, q is evaluated at t by the substitution of appropriate temperature values (T_{n+1}, T_n, \dots, T_1) and corresponding times (t_{n+1}, t_n, \dots, t_1) that are evaluated from the data records.

Errors Associated with the Heat Flux Density Computational Method

Errors that are encountered in evaluating the heat flux density are, for convenience, separated into those associated with the instrumentation (that is, those due to errors in the thermal properties of the slabs [K' in Eq. (C.1)] and to errors in the surface temperature-time record due to the transducer, amplifier,

and recording system) and into those that are not associated with the instruments. Those related to the instruments are considered later. The remaining errors, which concern the application of Eq. (C.4), are considered in the present section. This discussion commences with the assumption that a true or correct surface temperature-time record (trace form) is available. Under this circumstance, there are two types of errors encountered in the evaluation of the heat flux.

The first error type is that which results from the misrepresentation of the true surface temperature-time curve which occurs when this curve is replaced, in effect, by a sequence of line segments. By itself, this error can, of course, be minimized to any desired degree by decreasing the time interval over which the temperatures are evaluated; however, its elimination is precluded by the fact that errors of the second type tend to increase with decreasing interval sizes.

Errors of the second type result from the inability to exactly evaluate the "true" temperatures at the ends of the intervals, there being the natural tendency for the measured values to scatter above and below the true values. The effect of these temperature errors on the computed heat flux density is illustrated on Fig. C-1. On Fig. C-1a is a portion of a true temperature curve

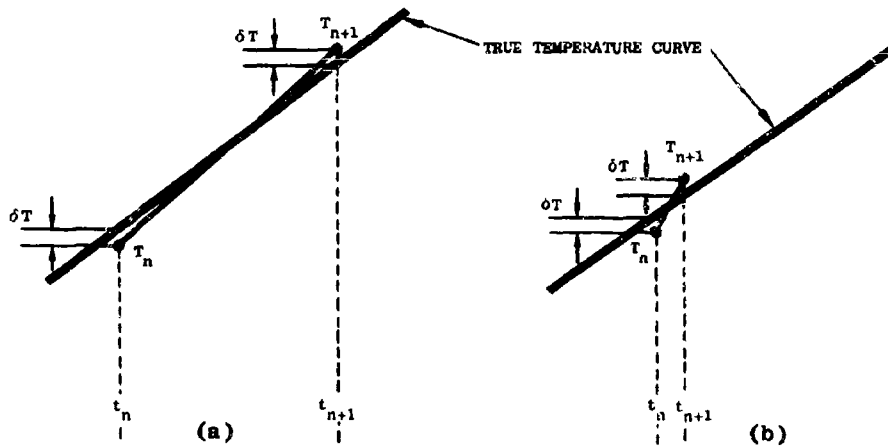


Fig. C-1. Illustration of Origin of Errors of Second Type

along with a line segment representing the curve over a single interval, where there is a temperature error, indicated as δT , at both ends of the interval, the evaluation being less than the true value at the early end and greater at the late end. For the same set of temperature errors over a smaller interval, as shown in Fig. C-1b, the error in the slope of the temperature-time curve is larger. From Eq. (C.4), the heat flux density is seen to depend on the slope over the interval (and all preceding intervals), and thus the flux density error due to temperature errors of this type will tend to increase as the size of the interval is decreased, as implied above.

Thus, with the first type error decreasing and the second type increasing with decreasing interval size, there is an optimum interval length, although the optimum length will vary along a given temperature curve, for instance, as the curvature of the temperature curve varies. A more complicating circumstance, however, is that an error in the heat flux evaluated within a given interval depends not only on the temperature-time slope error in the interval, but in all preceding intervals, as examination of Eq. (C.4) indicates. Because of this latter circumstance and the fact that there is a great variation in the temperature-time records obtained, a general and analytical error analysis is prohibitive and, in light of the objectives, unnecessary. In consequence, the analysis is confined to the evaluation of errors for temperature curves that resemble typical temperature data forms that have been obtained. In addition, because the maximum values of the heat flux that are obtained during heat transfer surges are given relative importance in the presentation of results, errors of the maximum values are emphasized in the error analysis.

The method of evaluating errors consists of comparing an exact heat flux density-time solution for a given surface-temperature-time function (equation) with the solution when Eq. (C.4) is applied to the same temperature function but with, in some cases, the insertion of deliberate and likely temperature errors in Eq. (C.4). The exact solutions are obtained in two ways, the first being simply to utilize existing solutions that are appropriate. When suitable solutions are not available, they are obtained by applying the basic relationship, Eq. (C.1), to temperature-time functions which both resemble typical data forms and which render Eq. (C.1) readily integrable. Examples of the heat flux

errors of both kinds that are encountered for various temperature curves are presented in the following paragraphs.

An example of a temperature-time function which is representative of "early time" data that are obtained is presented in Fig. C-2, along with the "true" flux density-time curve corresponding to that temperature for a K' [Eq. (C.1)] of $0.455 \text{ watt}/^\circ\text{C-cm}^2\text{-sec}^{1/2}$, corresponding approximately to some of the stainless steels. The heat flux was evaluated "exactly" through Eq. (C.1), and the flux computed numerically by applying Eq. (C.4). This example is presented primarily as an illustration of concepts and certain features of the two error types, but the associated magnitudes are also useful since the temperature curve is representative of data obtained.

It is convenient to introduce a "pseudo-curvature," hereafter referred to simply as the curvature, which is defined as

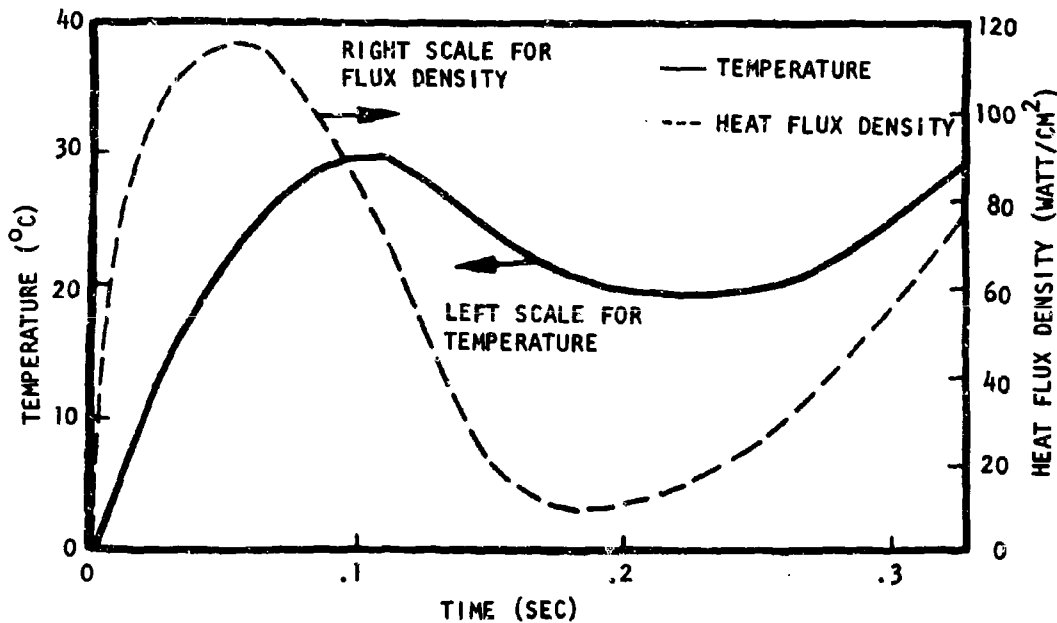


Fig. C-2. Heat Flux Density from Example Temperature-Time Trace

$$C = \frac{d^2T}{dt^2} / \left[1 + \left(\frac{dT}{dt} \right)^2 \right]^{3/2}$$

where C is the curvature, T is the temperature, and t is time. This is an extension of the ordinary or geometric curvature in that space or dimensional variables have been replaced by those representing temperature and time.

Errors of the first type have been evaluated for this temperature curve by applying Eq. (C.4) with a specific and uniform time interval for each computation, with the correct values of the temperature being used. This computation has been done for various computational time intervals, and the resultant error is plotted with the time interval on Fig. C-3 (continuous curve). Errors of this type tend to increase with increasing curvature of the temperature curve, and errors for other ranges of curvature will be presented later.

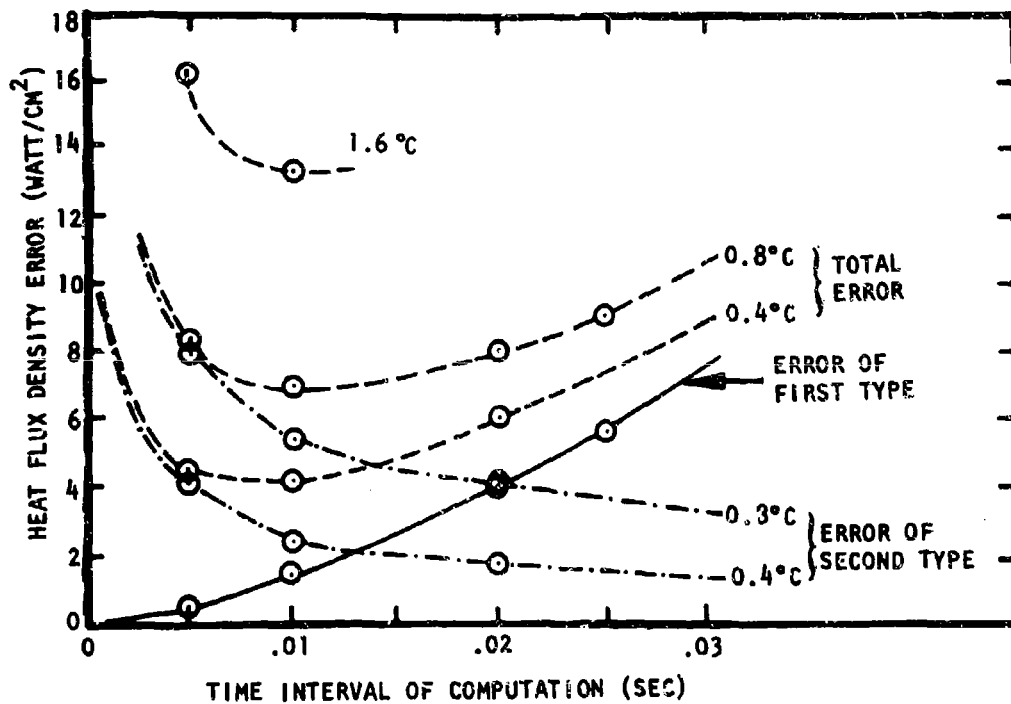


Fig. C-3. Heat Flux Density Errors for a Representative Temperature-Time Curve

Equation (C.4) was subsequently applied to the same temperature curve using the same time intervals, but with deliberate sequences of error in the temperature values, thereby revealing the total flux error, that is, the error which includes errors of both types.* The temperature errors used correspond to those that are likely to have occurred with the data processing procedures that have been used. The temperature error distribution used consists of temperatures at the ends of time intervals that are alternately above and below the correct value by a fixed magnitude. Thus, the slope of the temperature curve over a time interval is alternately (from time interval to time interval) above and below the correct slope, and while this particular distribution of errors is highly improbable, it is diagnostically useful. Total errors obtained from this computation are plotted on Fig. C-3 (dashed line) for a range of time intervals of computation and for various magnitudes of temperature errors. A temperature of 0.4, 0.8, or 1.6 °C is listed adjacent to a curve to indicate the total temperature error inserted at the ends of each interval, that is, the magnitude that the temperature is above the true value at one end of a given time interval plus the magnitude that it is below at the other end. As before, the curves are plotted for errors obtained at 0.1 sec, and also as before, the error is for practical purposes invariant for the range of curvatures present in the temperature curve of Fig. C-2. It can be seen that the total error curves reach minimum values for time intervals in the vicinity of 10 msec, with least total errors of about 4, 7, and 13 watts/cm² for temperature errors of 0.4, 0.8, and 1.6 °C, respectively. At the peak of the flux curve, where the magnitude is approximately 114 watts/cm² (at about 0.05 sec on Fig. C-2), errors of 4, 7, and 13 watts/cm² correspond to 3.5, 6, and 10 percent. From estimates of the standard deviation of the temperature evaluation, the error curve that most nearly corresponds to actual errors is the curve labeled "0.8 °C," thus indicating that a flux error of about 6 percent can be expected for this circumstance. It should be noted, however that this will vary somewhat with the quality of the temperature data. More

* Errors of the second type may be isolated through certain applications of Eq. (C.1). However, the effort is more extensive than is felt to be justified.

importantly, it should be understood that the value of 0.8 °C for a typical temperature error is the result of both the techniques used for measuring the displacement (or height) of the data trace and the particular scale of the data trace. Regarding the latter, a temperature-time trace with a different number of degrees (temperature) per inch on the paper would result in typical temperature errors other than 0.8 °C, and perhaps well outside the 0.4 to 1.6 °C range mentioned above. For a given scale, improving the accuracy of the trace displacement measurement would of course decrease the temperature error. The value of 0.8 °C is an estimate for the methods of displacement measurement used with the data at the graphical scale that was available. Referring once again to the typical flux error of 7 watts/cm², for periods during which the flux is comparatively low, the percentage error can be exceedingly large; for instance, in the vicinity of 0.2 sec where the flux is approximately 12 watts/cm², the error is about 60 percent. It is not correct, however, that all relatively small flux values have percentage errors of this order. Roughly speaking, low but steady flux magnitudes will be comparatively accurate, and the larger errors in flux (on a percentage basis) are associated with the minimum value of a sharp depression or trough in a flux curve.

Estimates and trends of errors of the second type are also given on Fig. C-3 (dash-dot curve), which were obtained by subtraction of the error of the first type (continuous curve) from the errors which are due to errors of both types (dashed curve)*. With this additional curve, Fig. C-3 serves as a useful guide. It is known, for instance, that for temperature curves having less curvature than the curve of Fig. C-2 at 0.1 sec, the errors of the first kind will not increase as rapidly with time interval as the curve presented

* For convenience, the errors of the first and second type are treated as though their sum always represents the total error. For approximately one-half of the intervals, however, the first and second error types are in opposing directions, that is, the error of one type tends to negate the error of the other type. Thus, the plots indicate worst or bounding case errors.

on Fig. C-3, and therefore, the flux error for a given temperature error can be greatly reduced by increasing the time interval of the computation from 10 msec. If, however, it is necessary to maintain or improve the accuracy for conditions of greater curvature, the only recourse is to improve the temperature-data-evaluating procedure.

The effect of variation in the curvature of the temperature curves on errors of the first type is given to a limited extent in Fig. C-4, where the error is plotted vs time interval of computation for three values of

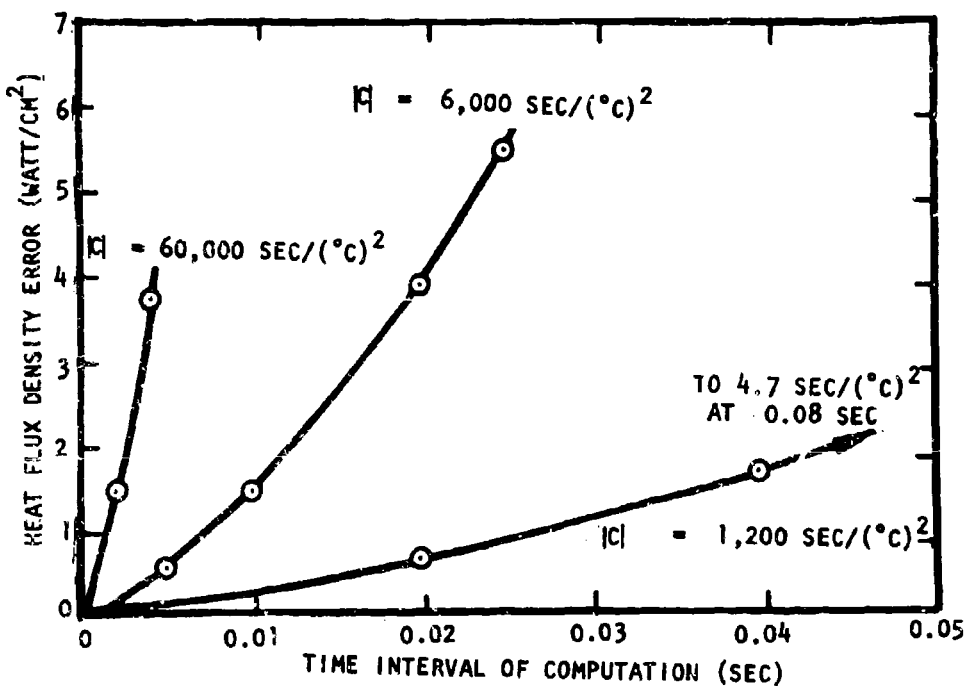


Fig. C-4. Heat Flux Density Errors of the First Type

curvature. Since errors of the second type depend only on the time interval of computation and temperature evaluation error, that is, they are independent of curvature*, the curves for errors of the second type on Fig. C-3 and those for the first type on Fig. C-4 may be combined so as to reveal curves representing the total error for a range of curvatures. A family of curves obtained from this process for the anticipated temperature errors of 0.8 °C are given on Fig. C-5.** These curves provide guidance in selecting the computational interval that will minimize the errors, and provide an indication of the errors that can be expected for a given set of conditions.

It should be recognized that the error within a given interval depends not only on the error in the slope of the temperature curve for that interval but in the corresponding errors for all preceding intervals, and that the sequence of temperature errors that has thus far been considered has consisted of errors of a fixed magnitude that are alternately (from interval to interval) above and below the true value. The error curves that have been presented are, then, representative of this particular distribution of temperature errors, while the actual distributions are, of course, infinite in their variety. The effect of modifying the distribution was investigated by imposing several different and intuitively probable temperature error distributions on the same temperature function. The variation in the errors for a given interval from among the various distributions was never in excess

* The independence of errors of the second type with curvature can be proven analytically by applying Eq. (C.4) to two dissimilar but mathematically arbitrary temperature functions which have identical but arbitrary temperature error distributions imposed on them.

** Since the values of the errors of the second type on Fig. C-3 are very uncertain at times less than 0.04 sec and these values are required over this time range for the construction of total error curves for curvatures of $60,000 \text{ sec}/(^{\circ}\text{C})^2$, an estimate of the error curve of the first type for a curvature of $12,000 \text{ sec}/(^{\circ}\text{C})^2$ was obtained and the total error curve corresponding to this curvature is given in Fig. C-5.

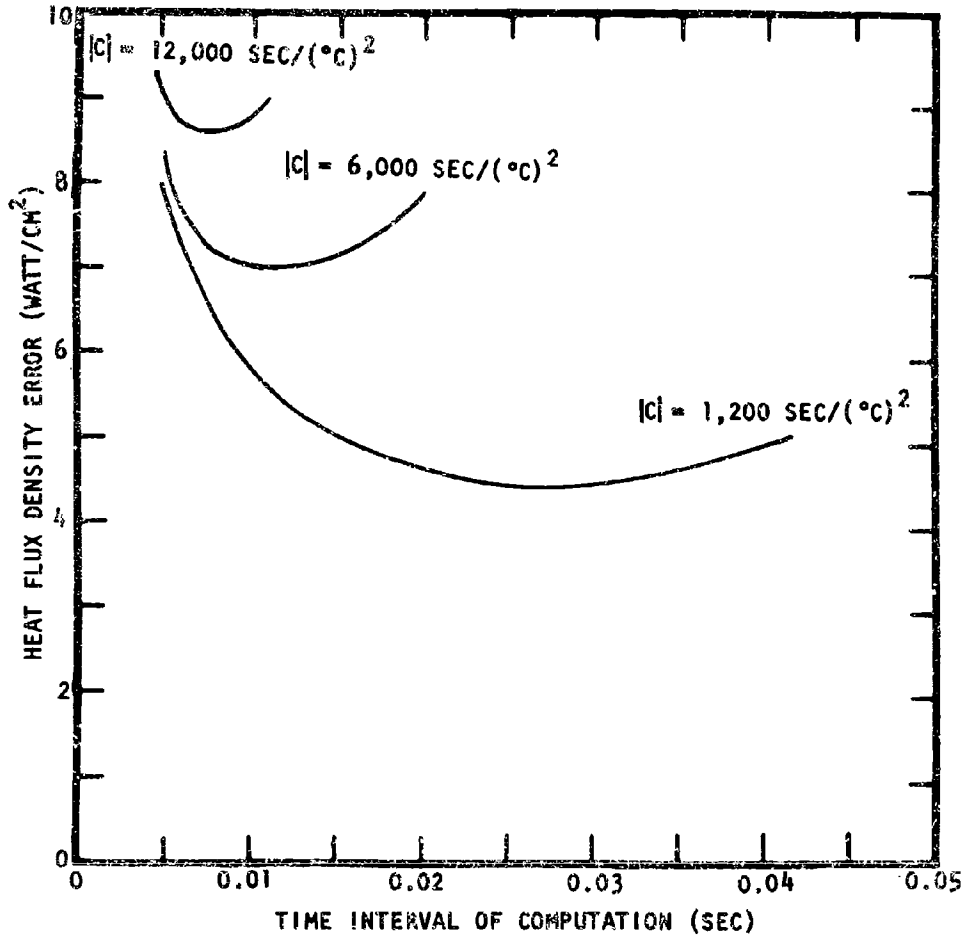


Fig. C-5. Heat Flux Density Error with Curvature and Time Interval of Computation

of 5 percent, and the error curves given for the special-case distribution in the figures above are representative of the errors for these distributions. The distributions that were considered by no means represent a thorough sampling, however, and distributions which result in variations in excess of 5 percent may occur.

The general errors as illustrated on Fig. C-5 have been generally verified for numerous conditions, and the results from a further example are presented primarily in the interest of illustrating certain points. One of the conditions of error that is of particular interest is that associated with the peak values for comparatively short-duration heating pulses. This was investigated by analytically imposing various short-duration heating pulses at various times on an otherwise constant and comparatively low-magnitude heating cycle. In the usual way, exact solutions were obtained and compared with numerical evaluations for which various appropriate errors had been introduced into the temperature values. For the example presented, a heating pulse of 40 msec duration centered about 0.1 sec* was superimposed on a constant heat flux density of 40 watts/cm², with the pulse maximum reaching approximately 132 watts/cm². Temperature errors covering their expected range were introduced at approximately the time corresponding to the peak of the heating pulse. The errors for this case are presented (in terms of percentage) on Fig. C-6. For a temperature error of 0.8°C corresponding to the most likely magnitude, the error from Fig. C-6 is about 15 percent, which corresponds to an error of about 6 watts/cm². This error is substantially greater than the minimal error (about 8.6 watts/cm²) given for this curvature [about 12,000 sec/(°C)²] on Fig. C-5 because a time interval of computation of 0.004 sec rather than the optimum interval of approximately 0.007 sec was used. However, since the 'dip or trough' in the total error curves of Fig. C-5 is comparatively 'sharp' for curvatures of this and larger magnitudes, this minimal error (cited above) is perhaps common for this curvature unless inordinately refined data analysis procedures are

* Precisely, the corresponding temperature 'pulse' rather than the heating pulse was centered about 0.1 sec, the peak of the heating pulse slightly preceding this time.

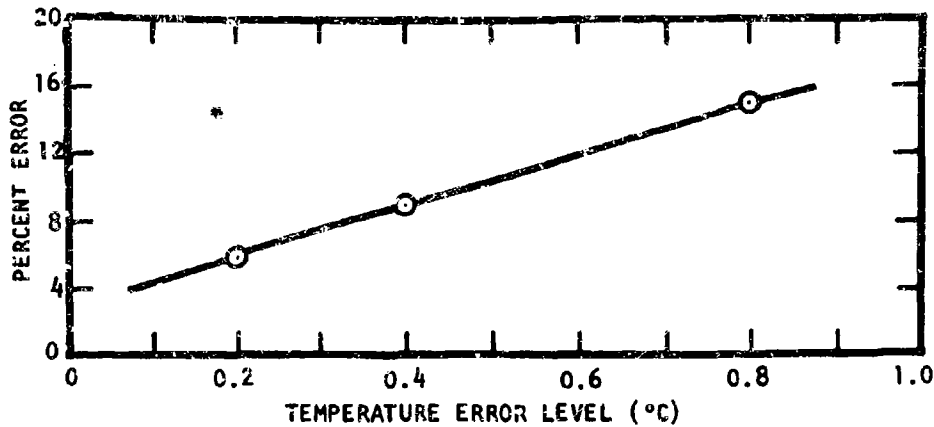


Fig. C-6. Error at the Peak of Heat Flux Density Pulse

adhered to. Moreover, misjudgments of a given magnitude would tend to more substantially inflate the error in this case compared to cases where the curvature of the temperature curve is smaller.

Finally, it is important to recognize that the errors presented above are representative of those associated with a single computational interval, and that they will tend to scatter from interval to interval above and below the true value by such magnitudes. In the case above, ten computational intervals would be associated with the 40-msec pulse. An erroneously high value over a given interval will tend to be compensated by a correspondingly low value in the subsequent interval. Thus, a smooth curve through the computed values over the 40-msec pulse will be substantially more certain than indicated by the errors plotted on Fig. C-5 above. By way of illustration, the exact and computed cumulative flux density values over, say, 20 to 30 computational intervals for typical temperature curves and errors, never differ by more than 0.5 percent.

Surface-Temperature Transducers

The method employed to measure the temperature within a solid material, hereafter referred to as the parent material, is standard and described or referred to in several articles, for example in Refs. C-2, C-3, and C-4. Briefly, the transducer, illustrated in Fig. C-7, consists of a fine wire which is contained within a cavity in the parent material and which is electrically insulated from the walls of the cavity except at an end, where it is joined (welded) to form a thermocouple junction just below one exterior surface of the parent material. Thus, any change of temperature of the junction will introduce a corresponding change in the electrical potential difference between the wire and parent material.

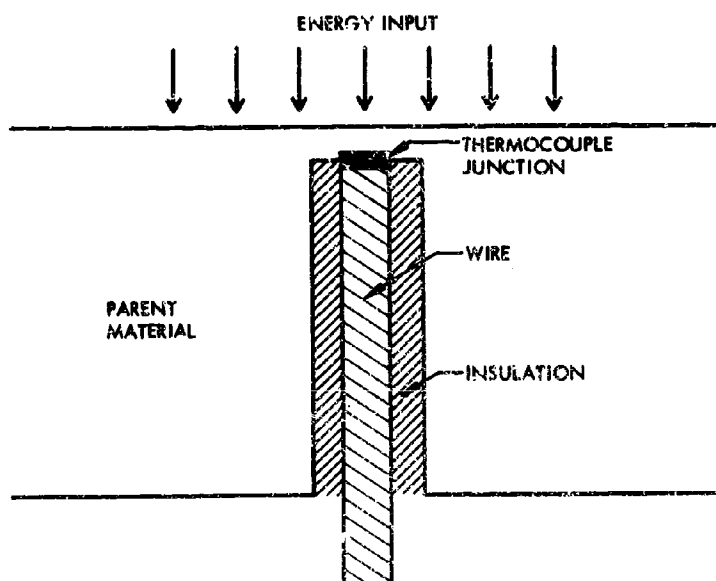


Fig. C-7. Generalized Configuration of a Surface-Temperature transducer

Instruments of two somewhat different designs are used. The details of the first type,* the transducer that was predominantly employed, are presented in a cross-sectional cutaway view, along with an enlargement of the thermocouple junction region, in Fig. C-8.** This transducer incorporates two wires so that only the molecular composition of the wires, not the parent material, enters into the electromotive force per unit temperature rise characteristic of the transducer.

Three slightly different versions of the transducer type shown on Fig. C-8 were used, differing only in the parent material and a single dimension. The parent material of the instrument presented in Fig. C-8 is copper, and the depths of the thermocouple junctions are 0.005 in. For the other two, the parent material is stainless steel 303 and 309, with junction depths for each of 0.002 in. The three versions are otherwise identical, including the thermocouple wire combination of chromel and alumel.

The important features of the second type of transducer*** are presented on Fig. C-9, which is a cross-sectional view of just the thermocouple junction region, analogous to the region that was enlarged on Fig. C-8. This is a single-cavity (and wire) system, so that the parent material, in this case iron, enters into the electromotive force per unit temperature change characteristic. The depth of the thermocouple junction is 0.0002 in., and unlike the previous example, the material between the junction and the exposed surface does not match the parent material, the thin exposed layer being a copper deposit on an iron surface, as illustrated on Fig. C-9. The particular application planned for this transducer called for the parent material to be a comparatively poor heat conductor, more specifically, that the thermal diffusivity be low, in order to minimize the distance that a spatial non-uniformity in heat input along the surface of the parent material can effect

* Manufactured by Advanced Technology Laboratories, Mountain View, California.

** Unless tolerances are noted on the figures, the distances shown were obtained by measurement of one transducer that has been cross-sectionally sliced and are uncertain to perhaps 0.003 in.

*** Manufactured by Heat Technology Laboratories, Huntsville, Alabama.

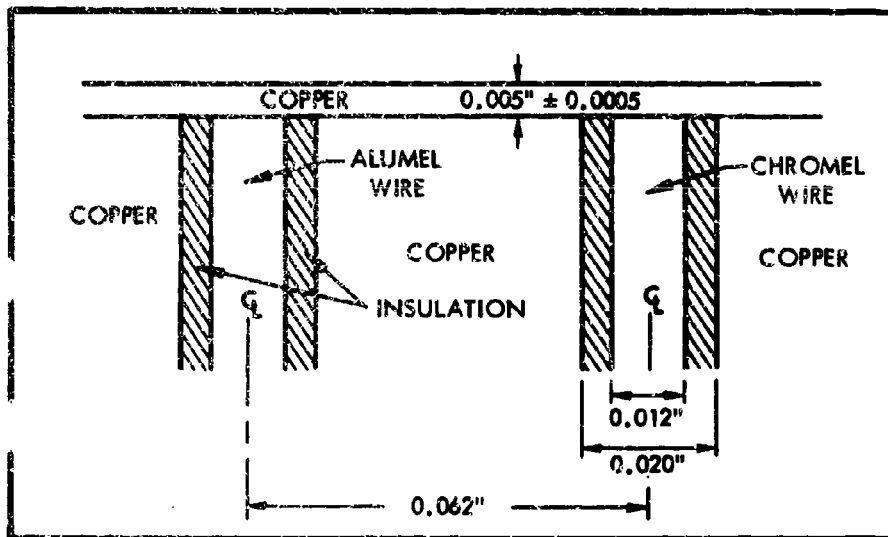
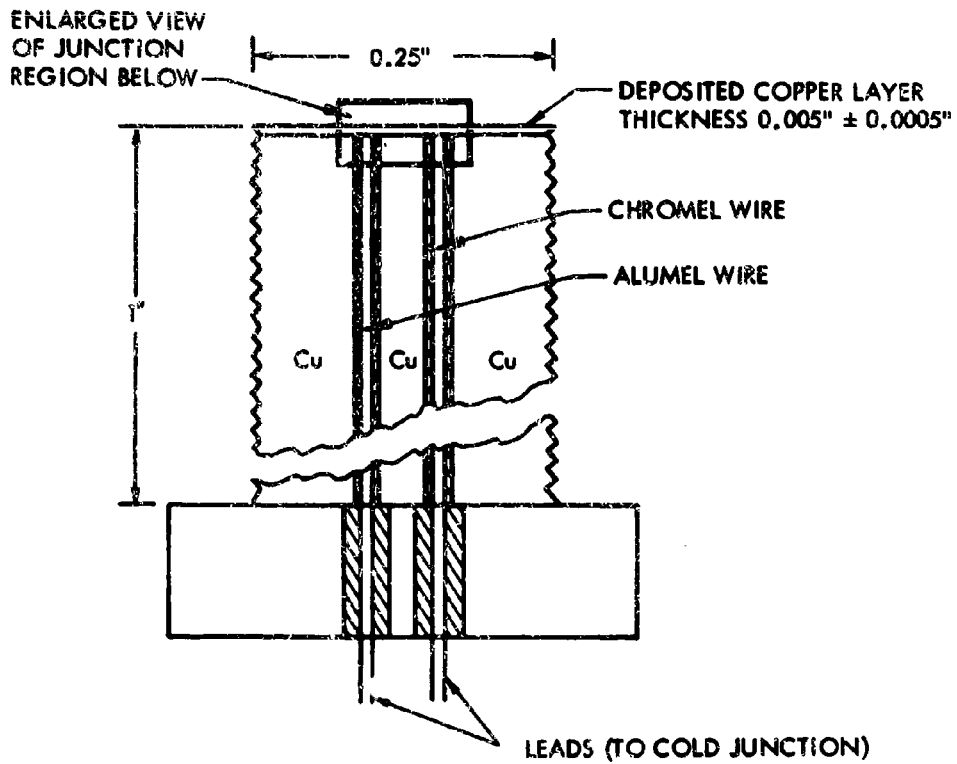


Fig. C-8. ATL Surface-Temperature Transducer

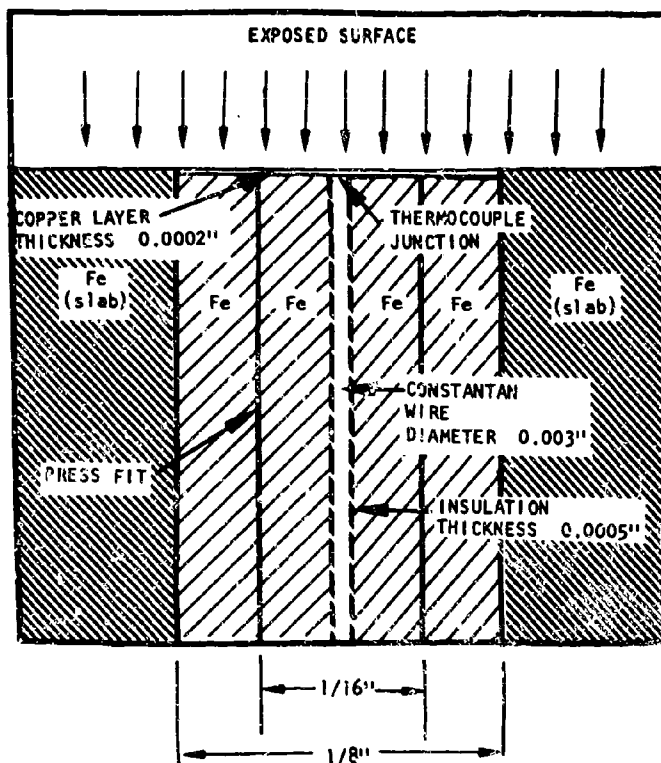


Fig. C-9. Thermocouple Junction Region of HTL Surface-Temperature Transducer

the temperature of the thermocouple junction in a given time interval. Of the materials immediately available at that time, iron had the best thermal properties. Because of the pronounced tendency of pure iron to oxidize in a high temperature oxidant atmosphere, thereby changing the thermal properties, the layer was made of copper. The replacement of this thin iron region with a copper layer, however, does not for practical purposes, alter the temperature measurement for a given energy input.

The surface temperature instruments as thus far described are inserted into larger objects, hereafter called slabs since for a time period they are thermally representative of semi-infinite slabs, which match the parent material of the given transducer. The surface of the transducer that is exposed to the

fireball, the surface nearest the thermocouple junction, is always flush with the surface of the slab it is inserted into. The exposed surface of the slab in most instances is planar, with the exception of a few measurements, described in more detail below, taken on a surface having some curvature, e.g., a radius of curvature of 6 in.

The thickness and lateral dimensions of the slabs are given below in the section on Instrument Mounting. These dimensions are of importance in that they govern the length of time that the slabs are thermally representative of semi-infinite slabs, or consequently, the time throughout which Eq. (C.1) may be properly applied.

The thermal properties of the slabs that have been applied in evaluating the heat flux are given in Table C-1 below, where the column on the extreme right is the value of the constant, $K' = (K\rho c/\pi)^{1/2}$, required in Eq. (C.1). These values were obtained from several sources including, for instance, Ref. C-5. The values listed for copper and iron are comparatively accurate since pure grades were used (see error analysis) whereas lot to lot variations can be expected for the stainless steels.

Table C-1
SLAB THERMAL PROPERTIES USED IN HEAT FLUX COMPUTATION

| Slab Material | Conductivity (watt/cm-°C) | Mass Density (gm/cm ³) | Specific Heat (watt-sec/gm-°C) | K' of Eq. (C.1) (watt-sec ^{1/2} /cm ² -°C) |
|---------------------|---------------------------|------------------------------------|--------------------------------|--|
| Copper | 3.81 | 8.94 | 0.389 | 2.05 |
| Iron | 0.652 | 7.85 | 0.418 | 0.825 |
| Stainless Steel 303 | 0.147 | 7.8 | 0.503 | 0.437 |
| Stainless Steel 309 | 0.155 | 7.8 | 0.586 | 0.475 |

Heat Flux Density Errors That Are Due to Instrument Errors

Heat flux density errors associated with the method of its computation from the surface-temperature of slabs were discussed above, starting with the assumption that a true temperature curve was available. There are, of course, errors in the temperature-time traces, and errors that are propagated to the heat flux density evaluation from these temperature errors are considered in this section, along with the error that depends on the error in the properties of the slab, i.e., on K' of Eq. (C.1). Consideration is given first to the four sources of systematic errors, that is, errors that can be corrected, and then to random errors.

The equation through which the heat flux density is evaluated from the slab temperature [Eq. (C.4)] is applicable to the surface temperature, while the slab thermocouple junctions are slightly below the surface. In order to evaluate the effect of this on heat flux density, temperature-time functions were first evaluated at the surface and then at depths corresponding to the thermocouples for various heating pulses applied to the surface of the slabs, and a comparison was then made of the heat flux densities from the two temperature functions. The heating pulses or the slab boundary conditions applied to both copper and stainless steel slabs were the so called radiation boundary conditions using a gas temperature of $2,000^{\circ}\text{K}$ and heat transfer coefficients of both 0.34 and $0.042 \text{ watt/cm}^2\text{-}^{\circ}\text{K}$. The resultant fractional errors are presented on Fig. C-10. The curves indicate that large initial errors rapidly converge to tractable values. The curves are somewhat misleading in the following manner. Roughly speaking, the temperature slightly below the surface tends to follow the surface temperature but with a small time delay. Thus, in the analysis, there is a time period when the embedded temperature is essentially zero while the surface temperature is rising appreciably, with the consequence that infinitely large fractional errors are

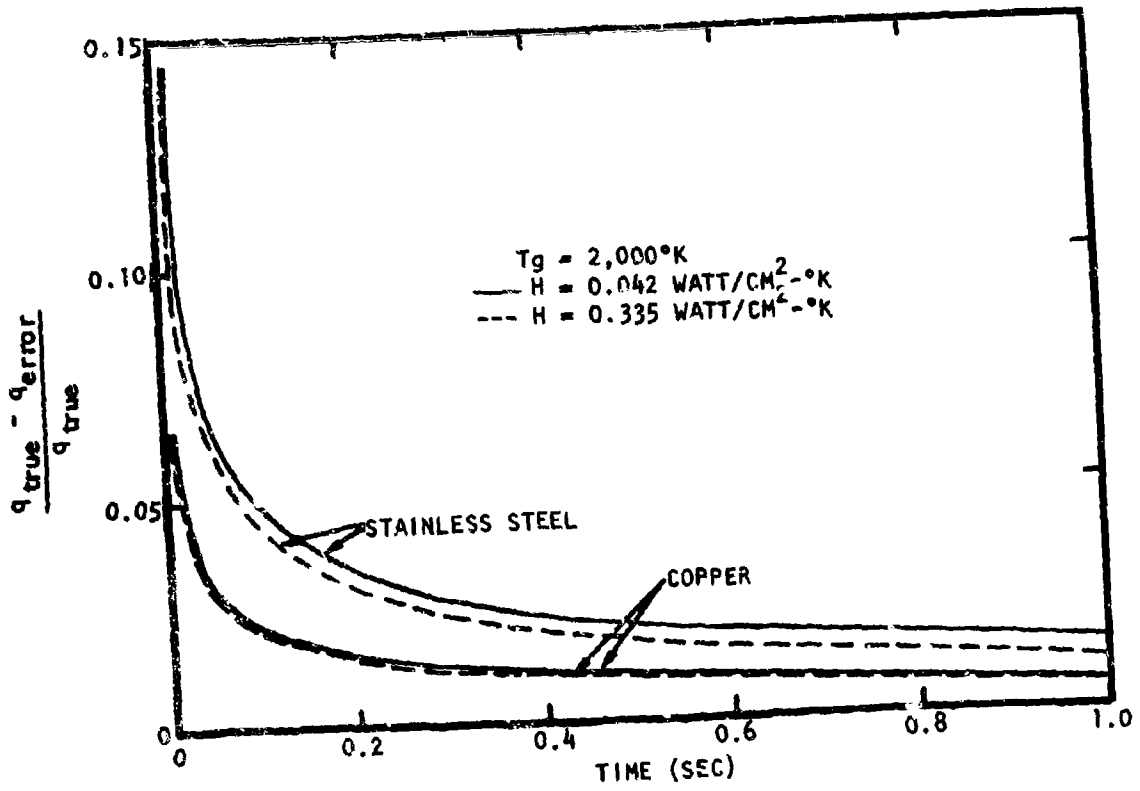


Fig. C-10. Heat Flux Density Errors which Result from Using Temperatures at Depths which Correspond to Those of the Thermocouple Junctions

indicated at early times. In actuality, zero time for an embedded measurement would be designated as the instant the temperature commences to rise, thus tending to minimize the magnitude of the initial error. The curves of Fig. C-10 tend, in this sense, to bound the errors. However, the curves are also misleading in another sense. The heating pulses applied to obtain these curves vary gradually with time, while the corresponding pulses for propellant tests are often comparatively erratic. Consequently, erratic behavior in the heating pulse can occur before the errors indicated in Fig. C-10 have decreased sufficiently. The effect here is perhaps best summarized as follows. Sharp or erratic behavior of a surface temperature will tend to 'smooth' slightly at the thermocouple depth, with the result that the measured heat flux density is also a slightly 'smoothed' version of the true input. To an extent, this is desirable. Extremely high resolution is not required, and the expense of the heat flux computation for precise representations of the surface temperature would be unjustifiably increased.

The second systematic error pertains to the effect on the temperature within the slab due to the presence of the thermocouple (including leads) and the cavity which contains the thermocouple. The analysis of this error is extremely involved, and evidently as a consequence few thorough analyses appear in the literature even though this is a standard instrument. While the ultimate errors in heat flux density are not expected to be of particular concern, it is difficult to numerically specify the uncertainty for a given circumstance. A series of analytical studies* for slab and transducer conditions, i.e., depth to thermocouple, cavity dimensions, properties of both the slab and thermocouple leads, etc., that differ in some respects from the cases of interest suggest minor errors can be expected. For instance, the effect of heat conduction down the thermocouple lead for comparatively large diameter leads and for lead to slab conductivity ratios of ten indicate corrections of the order of 25 percent. With comparatively high conducting leads,

* A relevant part of this study is given in Ref. C-4.

it is not surprizing that errors of this magnitude would be attained. For the instruments used, however, the conductivity ratios were no greater than 20 percent, and the errors from this effect would be substantially less. An experimental study* in which known heat pulses were applied to slabs with very similar instruments was conducted and a comparison made between the measured temperature and that which should result from the heating pulse. In such a study, the effects of all the systematic errors are inherently in the temperature differences observed, and it is not clear which individual error(s) are predominantly responsible. Yet the systematic error in question is the only one that cannot be readily estimated, so that conclusions can be drawn with regard to this error. In any case, errors obtained were of the order of a 5 percent or less, thereby suggesting that the effect of the systematic error in question was not grossly in excess of this figure.

While it does not appear that the effects of the presence of the cavity and thermocouple leads are of serious concern, being perhaps no larger than a few percent, these effects are the least certain of the systematic errors leading to errors in heat flux density measurements.

The third and fourth types of systematic errors result from the non-linearity of the amplifying and recording systems and the nonlinearity between the thermocouple emf and the temperature of the junction. These errors are similar and relatively simple to estimate or correct.

The deviation from linearity of the amplifying and recording systems was determined by applying a sequence of step voltages to the amplifier, where the ratios of steps are accurately known (0.5 percent), and measuring the magnitudes of the corresponding step deflections on the final data record. Such measurements were taken for all thermal data channels over several tests. Then for each channel, fractions of various step deflections to the highest step deflection on the final data record were obtained and compared with the corresponding fractions of the input step voltages. The

* Ref. C-6.

deviation, in each case, of the fraction on the final record from the fraction of the input is equal to the deviation, or error, of the temperature evaluation along the trace from zero to maximum temperature. An erroneous temperature curve that is representative of the error indicated by the fractions was constructed, and the heat flux was computed for the erroneous curve and a corresponding true temperature curve. The representative erroneous temperature curve was constructed using the criterion that, at each step, two-thirds of the deviations from linearity mentioned above were between the true and erroneous curves, with the remaining one-third outside this range. The resultant fractional error in heat flux density is presented on Fig. C-11, and as can be noted, the effect of nonlinearity in the amplifying and recording system tends to be minor.

Consideration of the nonlinearity of the appropriate emf vs temperature curves indicates that the associated errors are negligible over typical temperature ranges of the slabs (50 and 200°C for copper and stainless steel, respectively).

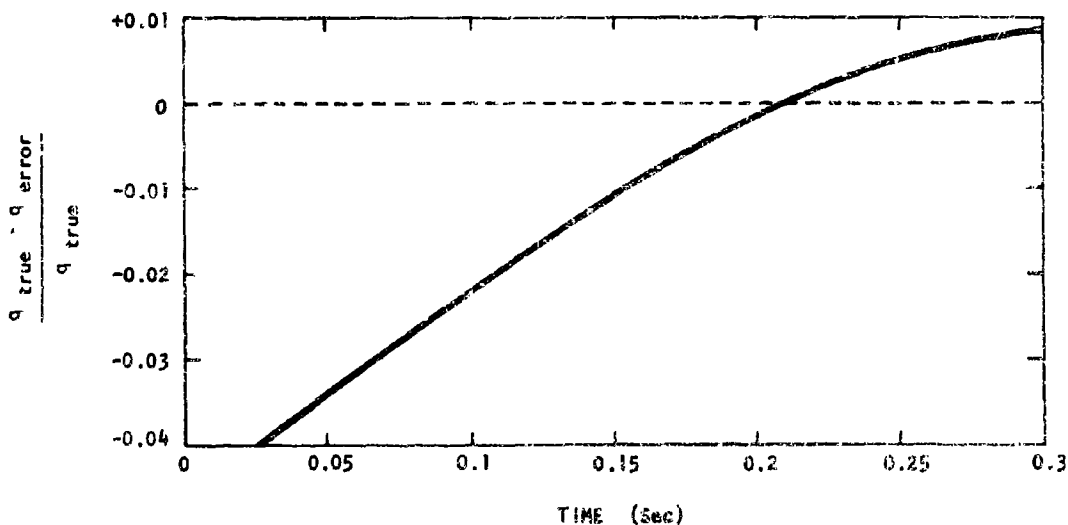


Fig. C-11. Heat Flux Density Error Due to the Nonlinearity of the Amplifying and Recording System

Random errors in the slab temperature measurements will be separated into two different types. The first type has a fixed value for a given data trace, while the second type fluctuates with time over the course of acquiring a data trace. Errors of the first type are considered first.

The slab temperatures are, in effect, evaluated using the equation

$$T(t) = \frac{Vd(t)}{EG}$$

where T is the temperature above ambient at any time t , E is the emf (volts) generated by the temperature transducer per unit temperature rise, G is the amplifier gain, V is the voltage applied to the recording system per unit deflection on the final data record, and d is the trace deflection on the final record. The uncertainty in E is specified by the manufacturer to be 0.5 percent, and G is periodically measured to within an accuracy of 1 percent. V is obtained by measuring the step calibration voltage applied to the recording system immediately before each test and dividing this voltage by the measurement of the corresponding deflection on the final record. It is estimated that the step deflection, preset to approximately match the maximum expected trace deflection, can be evaluated to about 0.5 percent, while the voltage is evaluated to about 5 percent; the uncertainty in the quotient V is then approximately 5 percent. This error, or its component errors, remain constant over the entire data trace for a given channel. It can be seen from Eq. (C.4) that the error in heat flux due to a particular error in temperature of this kind are equal to the temperature error, so that the flux and temperature uncertainties are about 5 percent.

Fluctuations of the measured temperature about the true value that occur throughout the duration of the data traces (noise) are introduced by the transducer, amplifier, recording system, playback system, and so on. Some of these fluctuations are cyclic, while others vary in a randomlike fashion with time. The cyclic fluctuations can be readily identified as

noise because of their presence on the constant value voltage input to each data channel immediately prior to each test. The random fluctuations are evidently comparatively negligible; they are imperceptible during the constant-value voltage input (due account being made for the superimposed cyclic fluctuations).

Regarding the uncertainty in K' , the uncertainty in measurements of the dependent parameters K and C (assuming the uncertainty in ρ is relatively small) is typically 5 percent, so that the uncertainty in a measurement of K' would be

$$\frac{\Delta K'}{K'} = \frac{\sqrt{2}}{2} \times 0.05 = 0.035$$

Since the copper and iron slabs are of comparatively pure grade material (99.9-percent pure for copper), the uncertainty in the properties specified for these materials should be about equal to the typical measurement uncertainty. It is likely to be somewhat higher for the stainless steel slabs (specified as stainless steel 303 and 309) since there is a greater lot-to-lot variability in the constituents of stainless steels.

A further but temporary source of error occasionally arises due to a somewhat sinusoidal high-frequency oscillation in the initial portion of the surface temperature-time data trace, which evidently results from passage of the shock wave and/or the impact of high-speed fragments of the tankage hardware on the instrument or its mounting. This signal attenuates rapidly with time and generally decays to negligible proportions in less than 0.1 sec, and the amplitude for the most extreme and rare cases is about 25 percent of the true signal. Since the period of vibration is usually orders of magnitude smaller than the period of typical changes of the true signal, "smoothing" the initial portion of the trace to eliminate the vibration induced noise can usually be accomplished with reasonable accuracy.

One additional systematic error should also be considered. The relationship from which the heat flux density is computed from the slab surface temperatures Eq. (C.1) assumes that the slab is effectively semi-infinite (occupies half

of space), while the instrumented slabs are finite. The surface temperature - time curve from a finite slab will eventually commence to diverge from that of a corresponding semi-infinite slab, and thus, an error will be introduced into the computed heat flux unless Eq. (C.1) is modified to accommodate a finite slab. This modification is somewhat involved, however, and the alternate approach was taken of selecting the slab materials and dimensions such that the errors in applying Eq. (C.1) are minor. The worst case was in connection with copper slabs having thicknesses of about 1-1/2 in. (detailed slab dimensions are given below in the discussion of instrument mounting). Comparative examination of first time derivatives of the temperature - time curves for copper slabs of this thickness and corresponding semi-infinite slabs indicate, for instance, that errors in heat flux density as large as 10 percent can occur at 5 sec, but that they are negligible at 2.5 sec. Iron slabs of similar thickness and stainless steel slabs 1 in. thick were also used, although the corresponding errors are comparatively small.

PHOTO-RECORD PYROMETER

Blackbody equivalent temperature measurements using a photographic recording pyrometer at a remote location were made by the Sandia Corporation. This instrument is shown in Fig. C-12. Basically, the measurement consists of matching in the visible spectrum the intensity of an image of a region of the fireball with the intensity of a similar image of one of several tungsten filaments, each of which is at a known and preset temperature. Since the intensity of the tungsten in the optical spectrum approximates that of a blackbody, the measured fireball temperature is near the blackbody equivalent temperature.

A more specific description is as follows. A camera is positioned to photograph a region of the fireball to be measured. A lamp box containing several tungsten lamps is placed so that the lamp filaments are in the lower portion of the camera field of view and are in focus at the film plane. The lamps are then adjusted to temperatures bracketing the expected temperature of the fireball. The lamp temperatures are read with an optical pyrometer. A



Fig. C-2. Photographic Recording Pyrometer

Wratten 29 filter is placed in front of the camera lens, and the exposure set so that the lamp filament images will give a maximum density on the film of about 1.8. The lamps are burning during the operation, so that each data frame, when processed, contains an image of the fireball, and the images of the lamp filaments, each of the latter at a different calibrated temperature. The density of the calibration lamps is plotted against temperature, and the temperature of the fireball taken from this curve.

RADIANT FLUX: INSTRUMENTS, CORRECTION FACTORS AND ERROR ANALYSIS

Basic Description of Radiometers

The radiant flux measurements are made with an instrument ordinarily referred to as the Gardon-type radiometer. Detailed descriptions of the principles of operation of this instrument are given in Refs. C-7 and C-8. The following is a brief summary from these references.

The basic radiant-energy-receiving element of the instrument is a circular metallic foil, as illustrated in Fig. C-13. If energy is absorbed at a constant rate uniformly* over the surface of a sufficiently thin circular foil (or heat is generated at a constant rate uniformly within the foil) whose circumferential edge is maintained at its initial temperature, a steady-state temperature field is such that the temperature differential between the center and circumferential edge of the foil is, to an approximation, proportional to the rate of radiant energy absorption by the foil** or rate of heat generation within the foil). Specifically, an approximate equation relating the temperature differential to the constant heat flux (watt/cm²) is

$$q = 4KS \Delta T/R^2 \quad (C.5)$$

* Uniform irradiance of the foil may not be a necessary condition for the flux to be proportional to the temperature differential upon reaching steady state, but it is known to be a sufficient condition, and thus we have made efforts to maintain uniform irradiance.

** Some of these statements are subject to conditions too complex to consider in this brief summary.

where K , δ , and R are the thermal conductivity, thickness and radius of the foil, q is the heat flux, and ΔT is the temperature difference between the center and edge of the foil.

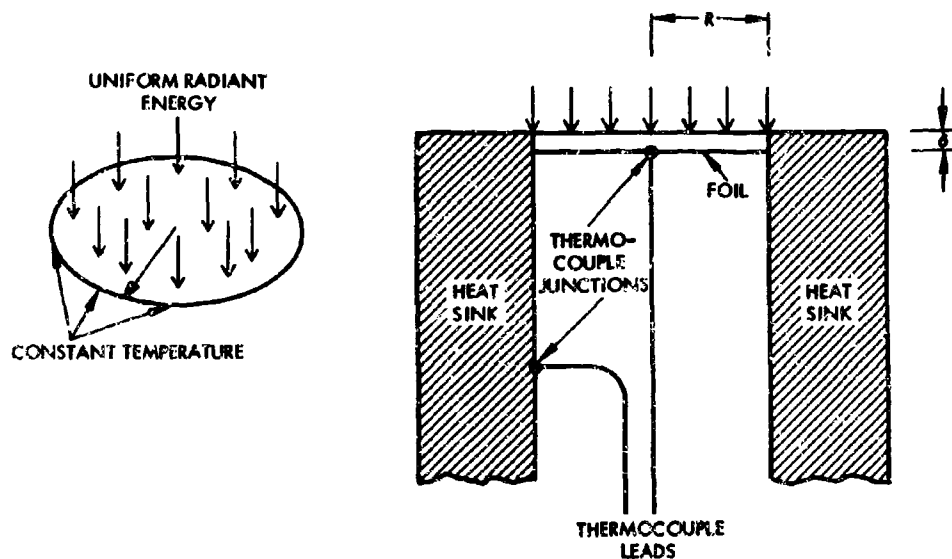


Fig. C-13. Configuration of Gardon-Type Radiometer

In practice, the condition that the temperature at the edge of the foil be maintained constant is approximately met by welding the foil to a highly conducting heat sink, as illustrated in cross section in Fig. C-13. Finally a differential thermocouple, whose voltage output is proportional to ΔT , is made by welding wires of the same material (but different from that of the foil) to the center and edge of the foil (the latter wire may be joined at any region of the heat sink). Then in terms of the electrical potential difference ΔV across the thermocouple leads, Eq. (C.5) becomes

$$q = 4Ke\delta \Delta V/R^2$$

where e is the voltage difference per unit temperature difference across the thermocouple junctions.

An order of time τ for steady state to be approached after initiating the constant flux is given by

$$\tau = R^2/4\kappa$$

where κ is the thermal diffusivity of the foil.

Description of Radiometers

Radiometers of three slightly different types were used for measurements within the fireball designated as the Sandia, RU-1, and HTL.* radiometers, and a fourth type for all remote measurements, designated the external radiometer. All four, however, are Gardon-type radiometers and function as in the basic description above. The differences are primarily in the materials used, for instance, for the sensitive foil and heat sink, and in dimensions, such as the foil diameter and thickness and the thickness of the windows.

A cross-sectional sketch of the HTL radiometer is given in Fig. C-14, with an enlargement of the aperture and foil region in the lower portion of the figure. The foil for this particular radiometer is 0.001-in.-thick constantan with a diameter of 0.89 in. The thermocouple junction wires are copper, and with this combination of materials and dimensions, the instrument has a time response of 50 msec and a sensitivity of 10 watts/cm²/mV.

A feature of the HTL instrument that is not present in the other intra-fireball radiometers is an annular disc which restricts the aperture to approximately the diameter of the foil, as illustrated in the lower portion of Fig. C-14. The upper surface of the disc is coated with a thin, light-absorbing layer to minimize reflection, and energy that is absorbed by the disc is conducted to a region of the heat sink that is remote from the foil. This disc, along with an external annular ring illustrated on the upper sketch of Fig. C-14,

* The basic energy-sensing element of the radiometer was manufactured by Heat Technology Laboratories, Huntsville, Alabama. Fabrication of the supplementary hardware and assembly was done at the AFRPL shop, Edwards Air Force Base, California.

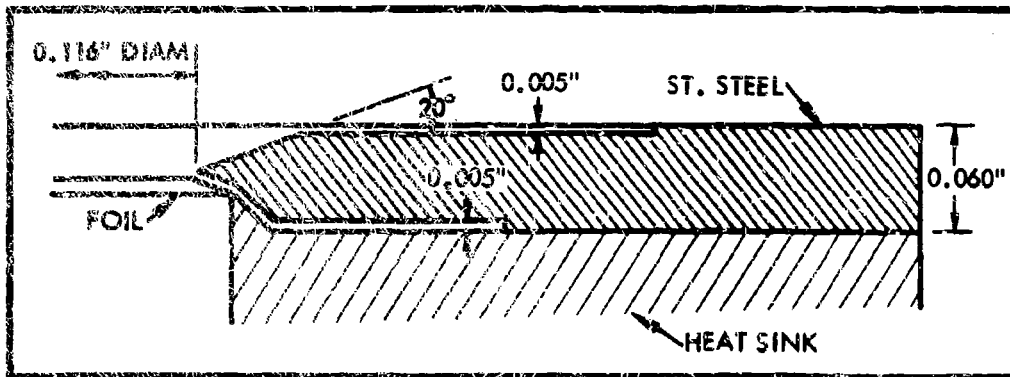
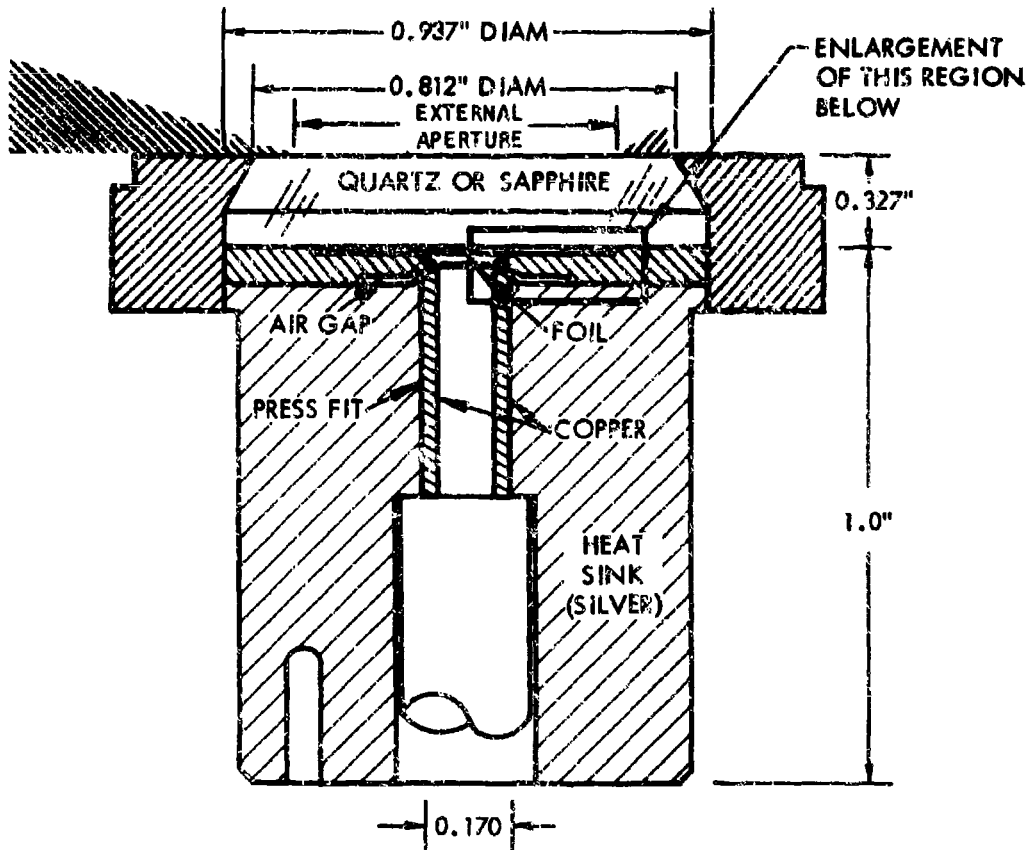


Fig. C-14. HTL Radiometer

restricts energy having angles of incidence between 70 and 90 deg from arriving at the foil. The diameter of the external aperture is just sufficient, accounting for refraction at the window surfaces, to restrict energy incident between 70 and 90 deg from arriving at the foil. No such restriction, either internally or externally, existed for the remaining two intrafireball radiometers.

The relevant data and characteristics of all four radiometers are listed in Table C-2.

• Table C-2
DIMENSIONS AND CHARACTERISTICS OF THE RADIOMETERS

| | | HTL | SANDIA | RU-1 | EXTERNAL |
|--|-----------------|--------------------|------------|--------------------|------------|
| FOIL | Thickness (in.) | 0.001 | 0.005 | 0.001 | 0.001 |
| | Diameter (in.) | 0.089 | 0.25 | 0.25 | 0.25 |
| | Material | Constantan | Silver | Silver | Silver |
| Thermocouple Junction Wire Material | | Copper | Constantan | Constantan | Constantan |
| WINDOW | Thickness (in.) | 3/16 | 3/8 | 3/16 | 1/4 |
| | Diameter (in.) | 0.94 | 3.2 | 0.94 | 1.75 |
| | Material | Quartz or Sapphire | Quartz | Quartz or Sapphire | Quartz |
| Field-of-View (deg) | | 70 | 90 | 90 | 45 |
| Time Response (msec) | | 50 | 60 | 60 | 60 |
| Sensitivity (watt/cm ² /mV) | | 10 | 50 | 8 | 10 |

Correction Factors for Radiometer Measurements Within the Fireball

Questions for interpreting radiometer data that are obtained within the fireball arise, and a correction factor permitting proper interpretation must be obtained. Evaluation of the correction factor is necessary because the intention is to evaluate the radiant energy incident on an immersed object

while the energy-detecting element of the radiometers is behind a window. Energy is lost enroute to the element by reflection at both surfaces of the window and by absorption within the window, and adjustment of the measured flux requires knowledge of the relationship between the flux at an exposed surface to that at the sensitive elements.

As will be described below, rather significant uncertainties exist regarding two properties of the fireball that are used in evaluating the radiometer-data correction factor. While these properties can be measured, a rather substantial effort is required, and in light of the secondary nature of the measurements, as mentioned in the general discussion, an effort to lessen these uncertainties did not appear to be warranted, nor was any made. In addition, no thorough analysis which would indicate the level of the uncertainty has been performed, although limited consideration suggests uncertainties of perhaps 20 or 30 percent. The data, however, are considered of value in supporting, at this level of uncertainty, the general magnitudes of other measurements, and consequently estimates of the correction factors based on the best available information have been evaluated. The origin and nature of the uncertainties will become more evident in the following paragraphs, which describe the process of evaluating the correction factors. The correction factor consists of the ratio of the radiant energy incident on an exposed surface area to that on the same area which is separated from the gas by a window, and the discussion commences with the evaluation of the energy on the exposed area.

Consider in Fig. C-15 the energy emitted from an elemental volume ΔV of gas which intersects the elemental area ΔA . Letting the energy emitted per unit volume of gas be e_1 , the energy emitted from ΔV in all directions is $e_1 \Delta V$, and the fraction emitted in a direction so as to intersect the area ΔA is $(\Delta A \cos \theta) / 4\pi r^2$, where r is the distance between ΔA and ΔV and θ is the angle between the line intersecting ΔV and ΔA and the normal to ΔA . Some of this energy is absorbed by the gas. In particular, the fraction of the energy not absorbed is e^{-kr} , where k is an absorption coefficient. The energy emitted by ΔV that arrives on ΔA then is

$$\frac{e_1 \Delta A}{4\pi} \sin \theta \cos \theta e^{-kr} d\theta dr d\phi \quad (C.6)$$

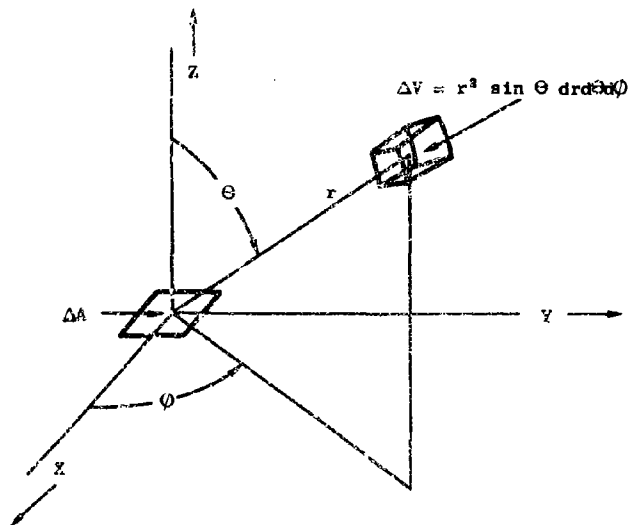


Fig. C-15. Basic Geometry for Computation of Radiant Flux Density Correction Factor

In summing the contributions of energy from the entire gas, where it is assumed that the gas extends indefinitely in both the lateral (X and Y) and vertical (Z) directions, it is convenient to identify for later use the angular energy distribution on ΔA by summing first over the ranges of the variables r and ϕ giving

$$e_{\theta} = \frac{e_1 \Delta A}{2k} \sin \theta \cos \theta d\theta \quad (C.7)$$

Thus e_θ is the total energy arriving at ΔA with an angle of incidence between θ and $\theta+d\theta$. Continuing the summation over the range of the angle θ , the total energy arriving on ΔA is

$$e = \frac{e_1 \Delta A}{4k} \quad (C.8)$$

It is this quantity that must be compared with the energy on ΔA when a window is inserted between the radiating gas and ΔA .

For the case where a window separates ΔA from the radiating gas, it is convenient to first evaluate the energy incident on ΔA for a window having infinite lateral dimensions and to subsequently show that the energy is approximately the same for windows that are appropriately finite laterally.

Commencing with the assumed laterally infinite window and assuming that the radiating gas occupies all of the space on one side of the window, the angular distribution of energy on the upper exposed surface of the window is given by Eq. (C.7) (now interpreting the angle θ as simply the angle of incidence) and the energy is uniformly distributed over this surface since the gas extends indefinitely. Since the irradiance of the upper surface is uniform, the energy emerging from the lower surface of the windows is also uniform, although the angular distribution of the emerging energy will not be described by Eq. (C.7) since energy is lost by reflection at both the upper and lower surfaces of the window (absorption within the window is momentarily ignored) and the fraction of the energy reflected depends on the angle of incidence. However, since the radiant energy emerging from the lower surface of the window is uniform, evaluating the emergent radiant flux density (or the energy on ΔA below the window) is simply a matter of evaluating the energy that is transmitted through the window for an angular distribution of incident energy given by Eq. (C.7). That is, the complexities of computation that arise for cases where the emergent energy is not uniform can be avoided. The reflected energy at the exposed window surface depends on the angular distribution of energy at that surface and on the index of refraction of the window material relative to the radiating gas. (The fundamental relationships permitting evaluation of reflected energy are given, for instance, in Chapter 1 of Ref. C-9 and Chapter 25 of

Ref. C-10). At the lower or unexposed surface, the reflected energy depends on the index of refraction of the window material relative to air and on the angular distribution of energy at the lower surface. This distribution is different from the above distribution due both to refraction (or bending) at the upper surface and to the different quantities of energy that were reflected at each angle at the upper surface. The index of refraction associated with the upper surface is comparatively uncertain due to the uncertainty that exists in the properties of the radiating gas, and this lack of knowledge represents one of two significant weaknesses of the intrafireball radiometer measurements. In order to provide an approximation to the correction factor, however, computations were made for the single case where the index of refraction of the window relative to the radiating gas is the same as that of the window relative to air (the same as the index associated with the lower window surface). For this case, the fraction of energy with an angle of incidence of θ that is transmitted through both surfaces, ignoring multiple reflections at a given surface, is given by

$$T(\theta) = 8(n \cos \theta \cos \theta')^2 \left\{ \frac{1}{(\cos \theta + n \cos \theta')^4} + \frac{1}{(n \cos \theta + \cos \theta')^4} \right\} \quad (C.9)$$

where n is index refraction of window relative to surrounding gas and θ is related to θ' by $\sin \theta = n \sin \theta'$. The angular distribution of energy on ΔA beneath the window is then

$$e_{\theta} = \frac{e_1 \Delta A}{2k} T(\theta) \sin \theta \cos \theta d\theta \quad (C.10)$$

The summation of Eq. (C.10) gives the energy on ΔA and, as will be shown below, has been computed for quartz and sapphire over various appropriate ranges of θ . Finally, the data correction factor (thus far excluding the correction for absorbed energy) is given by

$$\begin{aligned} F &= \frac{e_1 \Delta A}{4k} \bigg/ \frac{e_1 \Delta A}{2k} \cdot \int_0^{\theta_1} T(\theta) \sin \theta \cos \theta d\theta \\ &= 1/2 \cdot \int_0^{\theta_1} T(\theta) \sin \theta \cos \theta d\theta \end{aligned} \quad (C.11)$$

where θ_1 is the maximum angle of incidence permitted in the particular instrument. Eq. (C.11) is the ratio of energy incident on ΔA without a window Eq. (C.8) to that with a window, so that the measured radiant flux density should be multiplied by F in order to determine the radiant flux density on an exposed surface. For the RU-1 and Sandia radiometers, θ_1 was 90 deg. whereas it was usually 70 deg for the HTL radiometer due to presence of an aperture arrangement both on the exposed and underneath side of the window.

Thus far, a laterally infinite window has been assumed. Consideration of the path of light "beams" which arrive on ΔA from any possible direction, as illustrated in cross section with incident beams at 0, 45, 90 deg on Fig. C-16a, indicates that because of refraction, only a window section of finite diameter D is involved. That is, the window, except for the disk of diameter D , could be removed without any effect on the energy at ΔA . For the radiometers, ΔA , the sensitive element of the radiometer, is slightly below the lower window surface, and for radiation at a given angle, the diameter must be increased, as

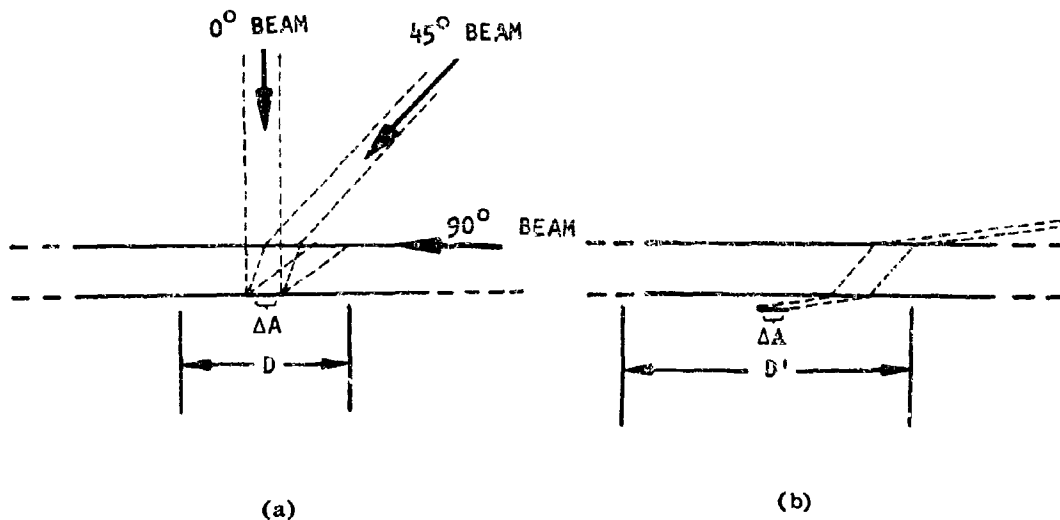


Fig. C-16. Illustration of the Useful Dimensions of Radiometer Windows

illustrated in Fig. C-16b, to D'. In addition, energy incident at and approaching 90 deg is restricted from arriving at ΔA for a finite window. For the radiometer dimensions used, however, this energy loss is negligible; for instance, for the HTL radiometers where 3/16-in.-thick windows with an effective diameter of 0.81 in. were used and the sensitive element was 0.003 in. below the window surface, the fraction of the energy lost due to its finite diameter is estimated to be about 1/10,000 for quartz, and similar numbers occur for other materials and radiometers.

Evaluating the energy absorbed within the window requires knowledge of the wavelength and angular distribution of the incident radiant energy and the absorption coefficient of the window material as a function of wavelength. The wavelength distribution is not precisely known, although the general form for black or grey bodies is known, given the temperature. The distribution for fireballs from $LO_2/ RP-1$ explosions probably reasonably approximates that from a blackbody. This uncertain wavelength distribution, however, does create an uncertainty in the evaluation of the absorbed energy, although it is not particularly serious at the expected temperature levels. The window materials have been chosen so that for temperatures obtained, the absorbed energy will be minor for any probable wavelength distribution. For instance, special grades of fused quartz* are used which are excellent transmitters beyond (at greater wavelength than) the 2.7- μ water band region to about 3.5 μ , along with sapphire,** which transmits to the 4.5- μ region. To illustrate, the fraction of the energy from a blackbody at 2300°K that is beyond 3.5 and 4.5 μ is about 14 and 8 percent, respectively. This percentage can be somewhat higher for probable wavelength distributions of the fireball, however. In addition, as the gas temperature decreases, the fraction of the energy that is in the wavelength region beyond the transmission cutoff increases, that is, the fraction absorbed increases, and the error in the estimated energy loss tends to have a more serious effect on

* A grade referred to as "Infrasil" from Englehard Industries, Inc., Amerisil Quartz Division, Hillsdale, N.Y., and grade G-103 from General Electric Comp., Lamp Glass Dept., Cleveland, Ohio.

** Optical grade synthetic sapphire from Linde Division, Union Carbide Corp., Torrance, California.

the total correction factor. In addition to the energy absorbed that is beyond transmission cutoff at 3.5 and 4.5 μ , some energy is also absorbed in the transmission region of quartz and sapphire. For the window thicknesses involved, this represents a small fraction of the energy, and evaluating the energy in this case does not depend critically on the wavelength distribution since the absorption properties of quartz and sapphire are for practical purposes constant over this portion of the spectrum. The absorption properties are more uncertain here and increase in uncertainty as the absorption coefficients become negligibly small; however, since this entails a large error of the negligible energy loss, the correction factor is not seriously effected by the error.

An estimate of the fraction of the energy absorbed for the various window thicknesses and materials and radiometer fields-of-view was obtained, and the corresponding correction factors associated with absorption alone (as well as those associated with the combined effect of reflection and absorption) are presented below. For the absorption estimate, the wavelength distribution from a 2200°K blackbody was assumed, and the absorption properties used were those specified by the manufacturer of the windows, supplemented by data from the technical literature.* Also, while the distance a given beam of energy traverses through the window varies with the angle of incidence, the estimate was based on an averaged distance. The absorption losses should properly have been evaluated in conjunction with reflection losses. However, the error resulting from their separate treatment is comparatively insignificant.

The final correction factors are listed in Table C-3. In order to indicate the relative influence of reflection and absorption, the factors due to reflection alone and absorption alone have been included along with the results from their combined effect. It should be noted that these factors will tend to become larger as the gas temperature decreases from 2200°K.

* Absorption properties of sapphire are given in Refs. C-11 and C-12. The properties of fused quartz silica (SiO_2) are available in numerous handbooks and infrared reference books.

Table C-3
RADIOMETER CORRECTION FACTORS

| INSTRUMENT DESIGNATION | WINDOW MATERIAL | FIELD-OF-VIEW (deg) | CORRECTION TO: | | |
|------------------------|-----------------|---------------------|------------------------|------------------------|----------------------------------|
| | | | DUE TO REFLECTION ONLY | DUE TO ABSORPTION ONLY | DUE TO REFLECTION AND ABSORPTION |
| Sandia | Quartz | 90 | 1.19 | 1.19 | 1.43 |
| RU-1 | Quartz | 90 | 1.19 | 1.18 | 1.41 |
| HTL | Quartz | 70 | 1.28* | 1.18 | 1.51 |
| HTL | Sapphire | 70 | 1.37* | 1.18 | 1.60 |

THERMOCOUPLE PROBE

Measurements of the fireball gas temperature at instrument locations within the fireball were made by the Sandia Corporation with a metallic sheathed thermocouple junction, or thermocouple probe. The probe consists of a 1-mil-wire-thickness tungsten/tungsten 26% rhenium thermocouple sheathed with 16-mil-O.D. molybdenum disulfate-coated tantalum. The thermocouple wires are insulated from the sheath and, except at the junction, from each other by beryllium oxide.

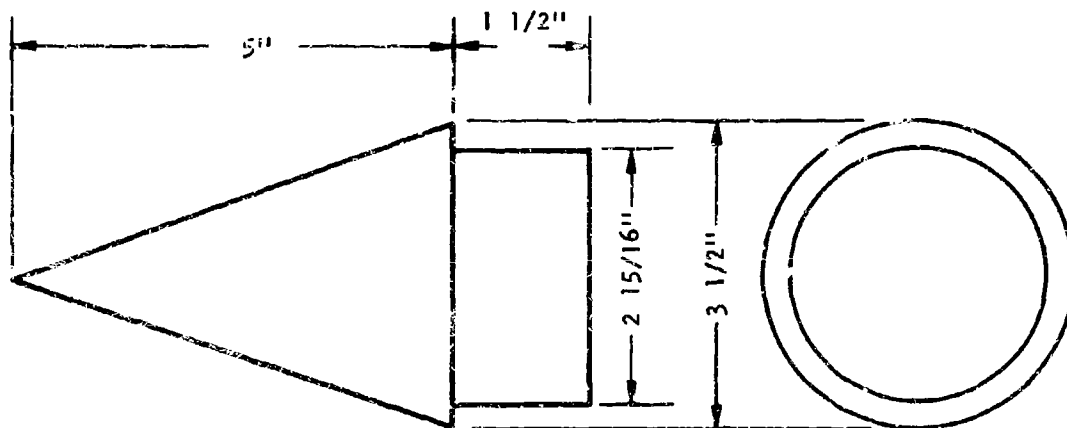
While the thermocouple probe is a standard instrument, for the temperatures and molecular constituents that occur in the fireball, a comparatively large uncertainty in the indicated temperature of its environment occurs. Upon immersion in the fireball, the temperature of the instrument commences to increase rapidly toward that of its environment, and at temperatures well below the melting temperature of the sheath, a chemical reaction between the sheath material and the nearby oxidants of the fireball gas commences. The energy from the reaction can contribute substantially to the temperature elevation of the probe and its ultimate temperature can exceed that of its environment. The rate of

* Includes loss due to field-of-view restriction.

reaction depends, of course, on the type and concentration of available oxidants, the flow velocities, and the sheath material, and it is difficult to establish whether the reaction is or is not inducing a false indication of significance. The resultant data, however, should be used with caution.

Passive Sensor

A piece of test hardware inadvertently became a passive thermal sensor for Test 285. This sensor consists of a solid aluminum cone, as illustrated in Fig. C-17. At the time of ignition, it was located at or within a few feet of the point of initial propellant ignition, and after the test was located within a few feet of its initial location. Approximately 0.1 to 0.2 in. of material had been uniformly ablated from the surface of the sensor. No thorough analysis of the possible heating pulses that could have induced such an ablation has been performed, nor is it clear if chemical reaction was involved in the process of ablating the surface.



SCALE = 1/2

MATERIAL = 6061 - T6 Aluminum

Fig. C-17. Dimensions of Aluminum Cone

INSTRUMENT MOUNTING

Within the Fireball

There are four intrafireball instrument mounts which are designated as the H, S, P, and Sandia Stations. This section contains a description of these stations, their location, and the position of the instruments on the stations.

A sketch of station "H" is given in Fig. C-18, showing the location of two slabs and four radiometer positions along the upper surface of the mount. Also along the upper surface is a "wing-like" projection (or wing), which extends beyond the main body of the mount both laterally and toward the center of the explosion. The wing is 1 in. thick (steel), with two adjacent slabs (3-by 3-by 1-in.-thick stainless steel 303) mounted with their exposed surface flush with the upper surface of the wing. The upper wing surface is entirely planar, and the leading edge and wing tips are tapered in toward the main body from the perimeter of the upper surface at an angle of 30 deg from the horizontal. The function of the wing is to induce similarity of gas flow along the surface of the two slabs even though the flow direction may be somewhat askew of the vertical plane of symmetry of the mount and/or of the horizontal plane.

Positions for four radiometers were included in order that gross differences could be detected between simultaneous measurements from dissimilar instruments, similar instruments with dissimilar window materials, etc., although differences exceeding the measurement uncertainty were not obtained. The radiometers of this station are provided, starting with test No. 279, with a helium purge (apparatus not included in sketch) along the exposed surface of their windows, the purpose being to minimize particle deposition and window heating, the latter because it tends to alter the reflection and absorption properties of the window. Through appropriate shaping of the purge-gas orifice and control of the velocity of the purge gas at the orifice, the purge-gas flow along the upper surface of the window was confined to a thickness of about a millimeter, and thus did not tend to repel the fireball gas from the window surface to an undesirable extent. The

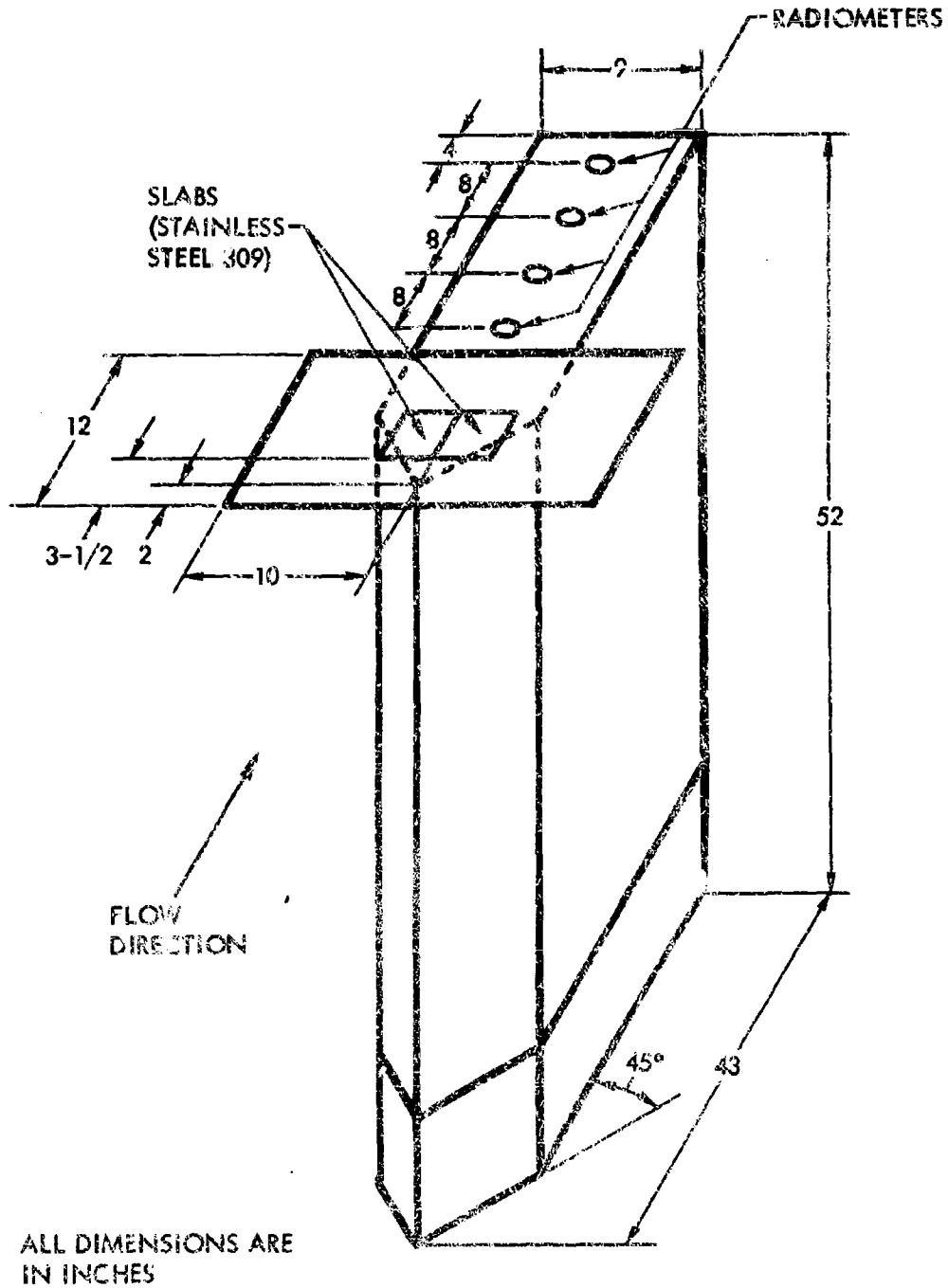


Fig. C-18. Intrafireball Thermal Instrument Station H

purge, however, did successfully eliminate deposition, although difficulty was experienced in maintaining the required purge-orifice alignment.

A sketch of station S is given on Fig. C-19, showing locations of two slabs, two thermocouple probes, and a radiometer, although no radiometer data were successfully acquired at this station. Station S was generally located approximately 13 ft above the ground surface and as near to the center of the explosion as practical. During Tests 275 through 285 it was located at a ground distance from the center of the test pad of about 23 ft approximately along gauge line A. Due to the difficulty of maintaining the station at this distance, it was relocated for the remaining tests to a ground distance of 32 ft along a radial line about half way between gauge lines A and B. A sketch of the station and its final support structure is given in Fig. C-20.

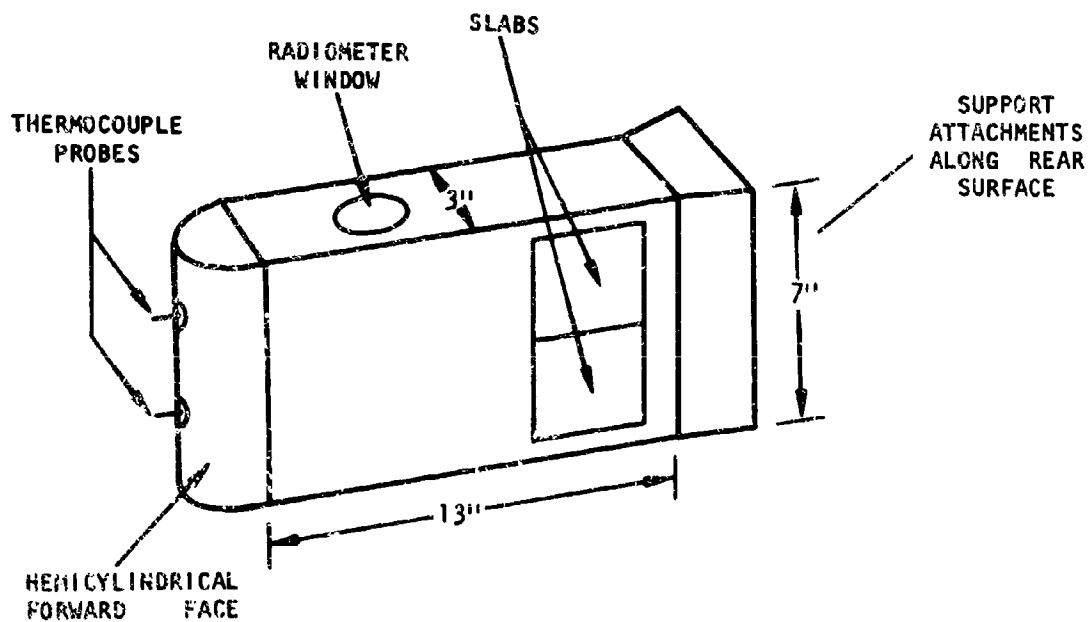


Fig. C-19. Intrafireball Thermal Instrument Station S

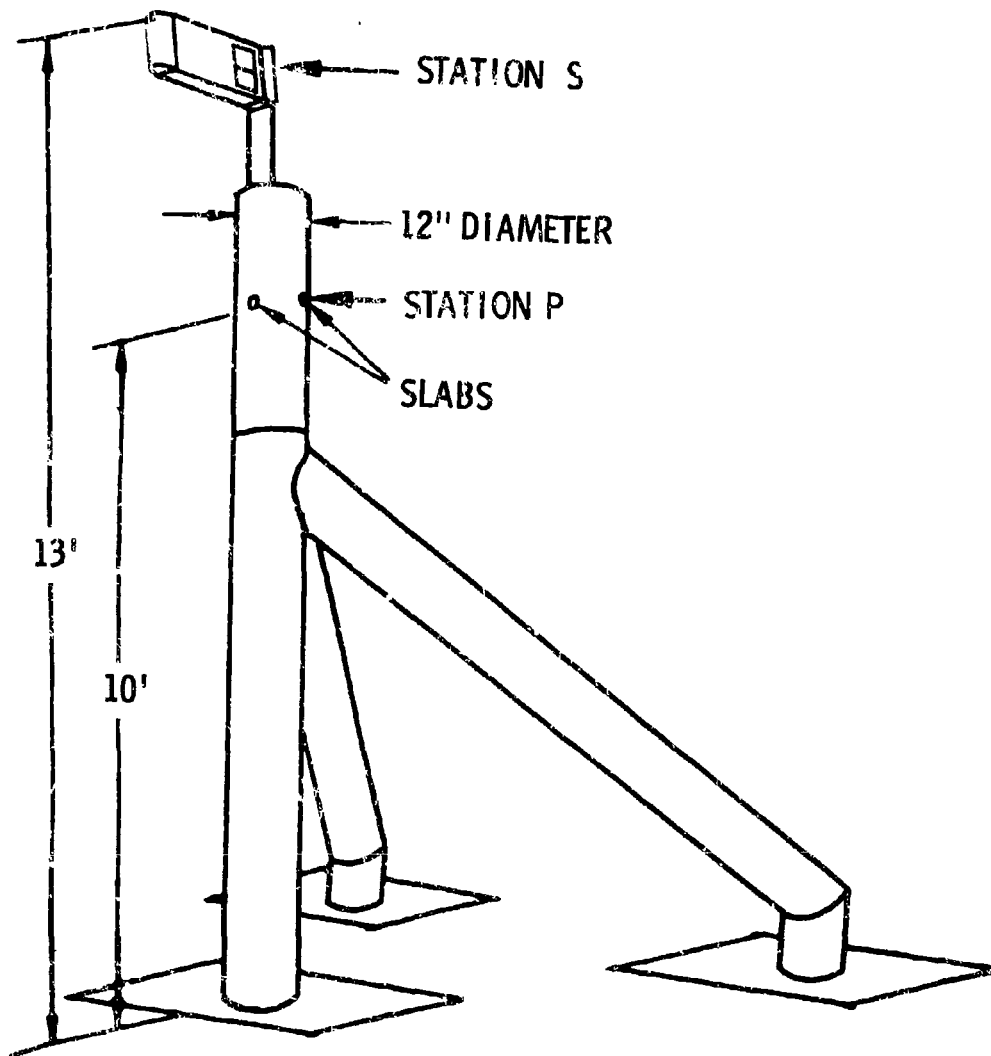


Fig. C-20. Thermal Instrument Stations P and S

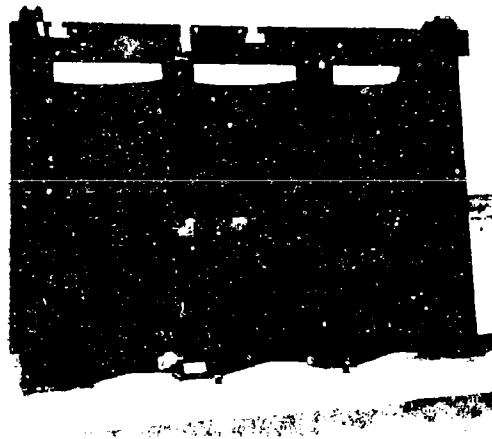
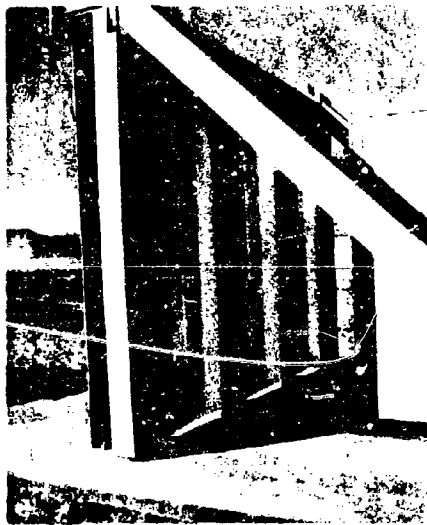
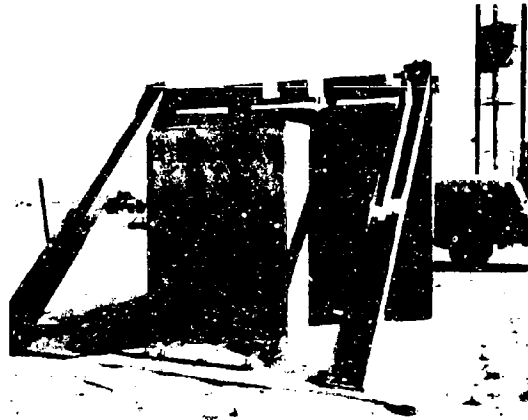


Fig. D-3. NASA/MSFC Blast Shields



Fig. D-4. Typical Fireball Cloud

The slabs on the S station from Tests 275 through 279 were 3 by 3 by 1-in.-thick stainless steel 309. While slabs having comparatively small thermal diffusivities such as stainless steel are preferable for the longer duration tests, the loss of several temperature transducers during the first few 25,000-lb tests required their replacement with transducers and slabs of copper. In order to increase the time during which negligible errors occur in the evaluation of heat flux, slabs having a thickness of 1-1/2 in. were installed, where as usual, the exposed slab surface was flush with the external surface of the mount.

The location of station P is shown in Fig. C-20 just below station S, station P being actually part of the support structure for station S. Station P, installed after test 285, consists of a series of instrumented slabs distributed at 60-deg-angle intervals along the circumference of a 12-in.-diameter, vertically oriented pipe. A more detailed cross-sectional cutaway view showing the slab arrangement is given in Fig. C-21, along with a dimensional sketch of an individual slab in the lower right hand corner of the figure. The primary purpose of the station was to determine if there are pronounced and consistent variations of flux density with position on such an object.

It was planned to use iron transducers and slabs in station P. However, iron transducers were not available until the Titan I test (Test 301) and copper transducers and slabs were substituted. The use of copper with these comparatively small slab dimensions will result in small errors in the evaluation of heat flux toward the end of the heating pulse as noted in the discussion of errors above. Iron slabs were installed for the Titan I test at station P positions 1, 3, and 5 (position is designated by numbers in parentheses on Fig. 21).

The Sandia station was elevated above the ground surface approximately 12 ft and supported by a structure similar to that for station S shown on Fig. C-20. It was located about 30 ft from the test pad center at a point approximately intersecting gauge lines A and C.



D-5a



D-5b

Fig. D-5. University of Florida Experiment

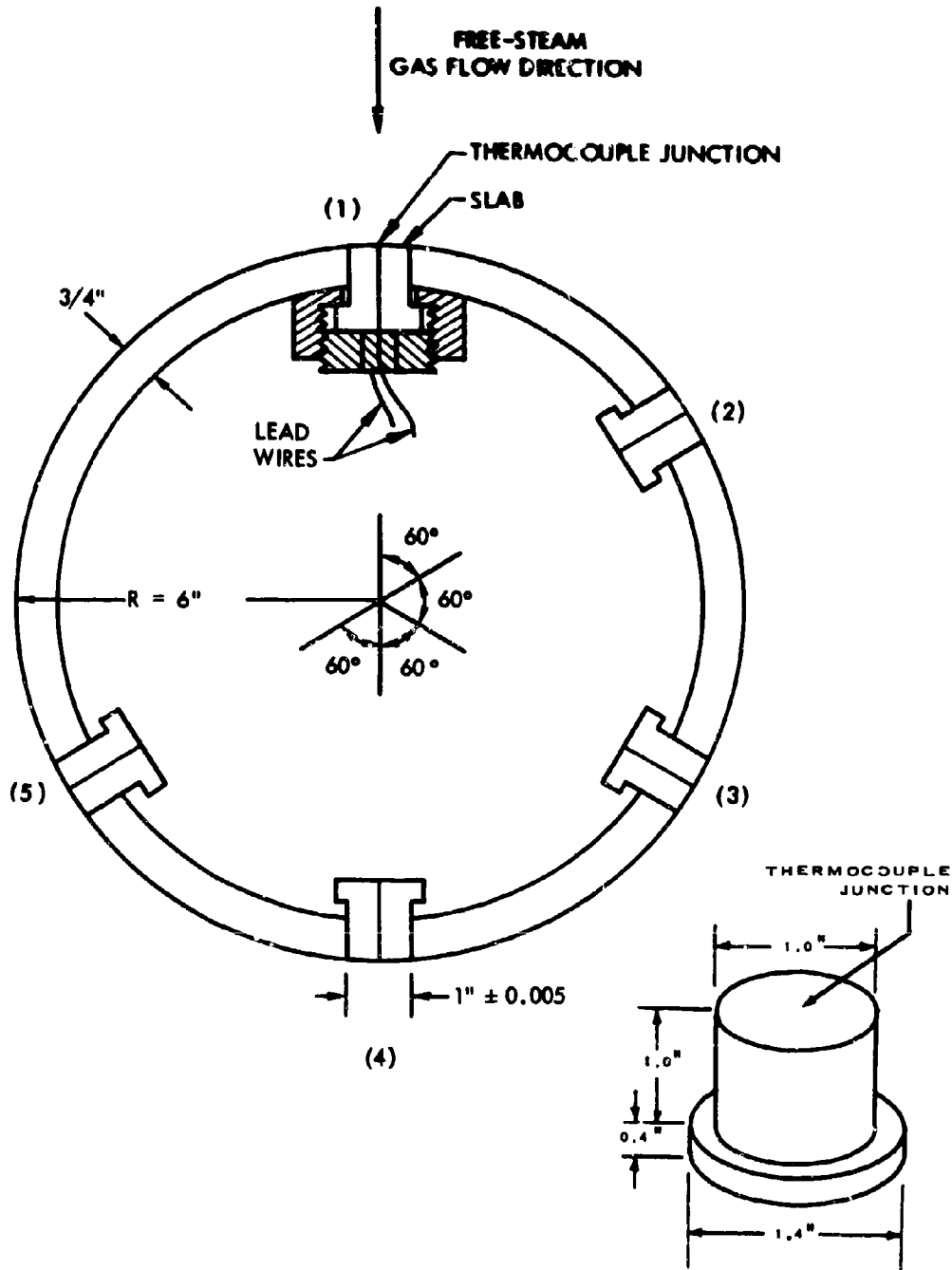


Fig. C-21. Cross-sectional Cutaway View of Thermal Instrument Station P and a Perspective View of the Associated Slabs

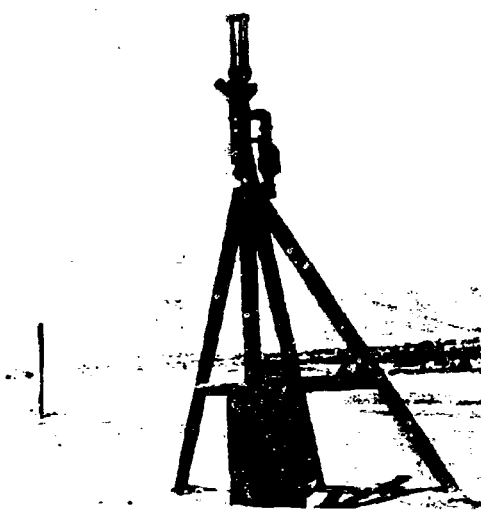


Fig. D-6. Sandia Snap 27, Heat Transfer Experiment

Outside the Fireball

Measurements of radiant flux density and fireball temperature are made from remote locations. Generally three radiometers were used per test, two at the same distance (335 ft) from ground zero along radial gauge lines (A and B) 120 deg from each other, with the third more remotely located (600 ft) along gauge line A. These distances are such that with the existing field-of-view of the instruments, radiant energy originating in any region of the fireball prior to its rise is not restricted from falling incident on the sensitive foil of the instrument.

The photo-recording pyrometer (a Sandia Corporation instrument) was located about 450 ft from the test pad along gauge line A.

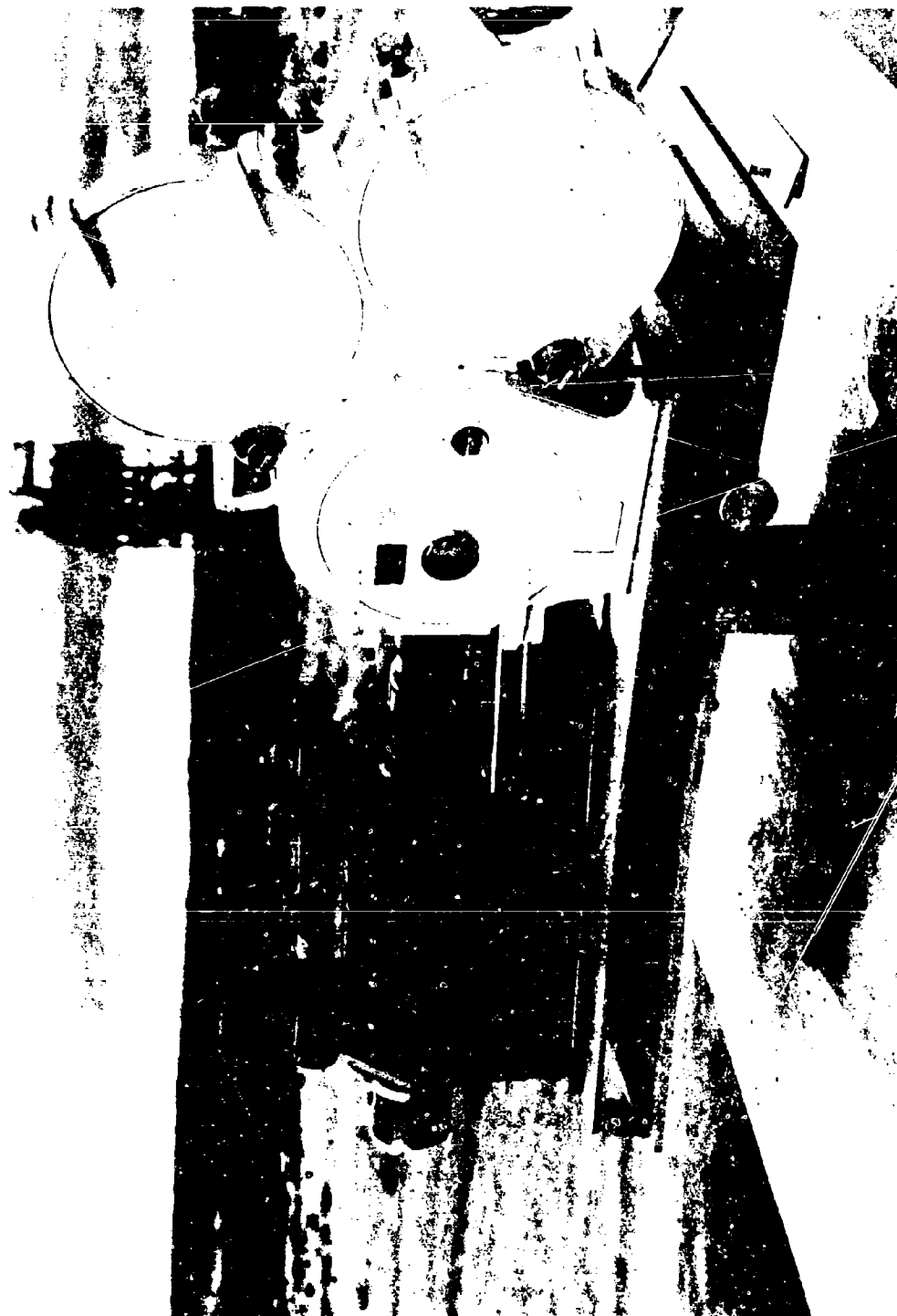


Fig. D-7. Pan American Cinespectrograph System

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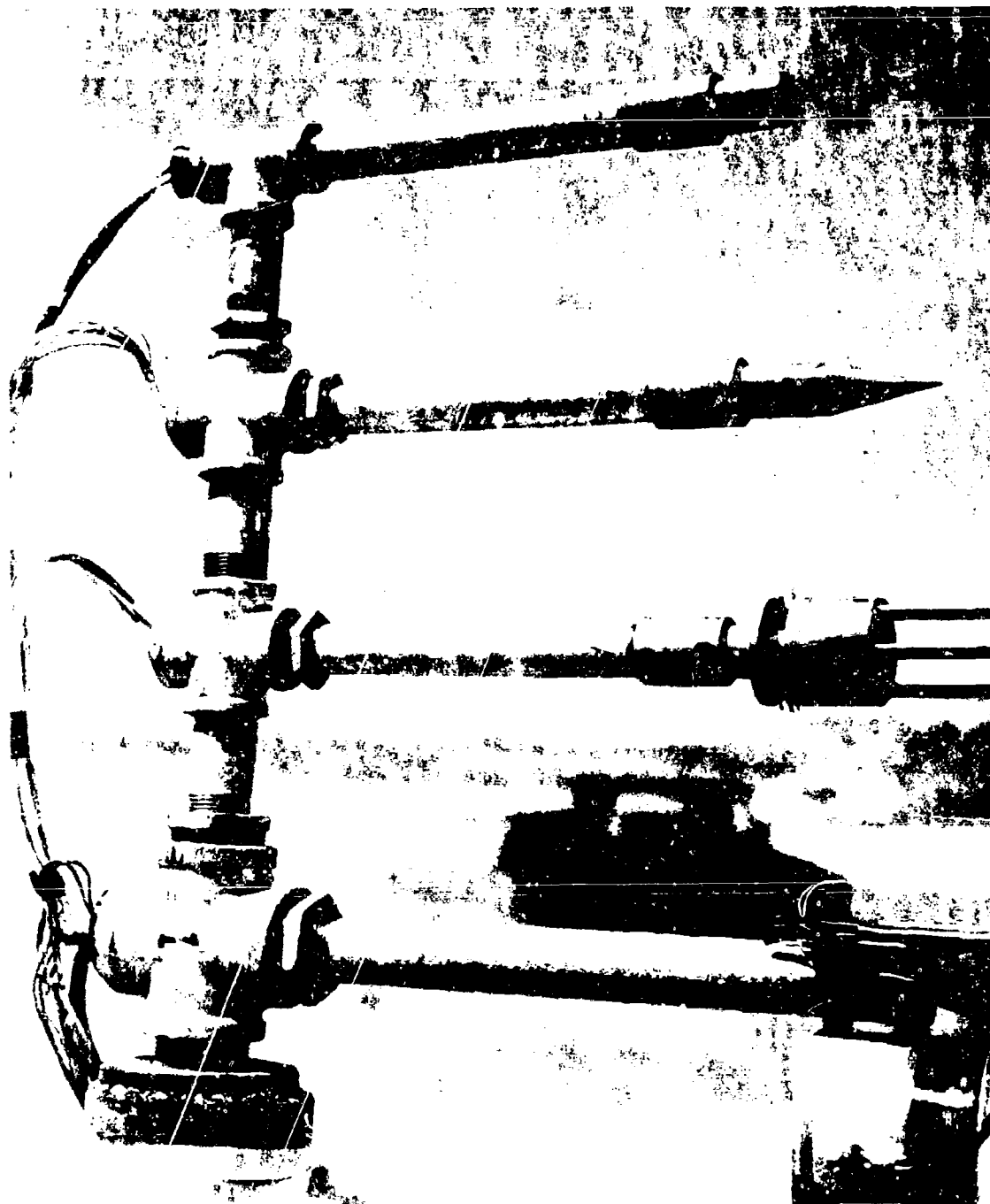


Fig. D-8. Midwest Research Institute Thermocouples

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Appendix D
"Ride-Along" Programs *

Included in the test phase of Project PYRO were several experiments conducted by outside agencies which utilized the thermal and blast environment provided by the liquid propellant explosions. The data generated in these experiments will be reported by the conducting agency. The following is a summary description of each experiment and the technical contact.

The U.S. Army Corps of Engineers built and installed aluminum structures in the PYRO test area (see Figure D-1) to measure structural response to the liquid propellant blast wave. Figure D-2 shows the damage received by a structure during a test. The technical contact for this program was Mr. M. J. Rosenfield, Ohio River Division Laboratories, 5851 Mariement Avenue, Cincinnati, Ohio 45227.

NASA/MSFC installed a series of blast shields 100 ft from ground zero during the 25,000-lb test series to evaluate these shields for use on test stands and launch pads to protect ground support equipment. Figure D-3, shows typical blast shields as installed for the tests. The technical contact for this program was Mr. Walter V. Medenica, NASA/MSFC (R-Test-B) Huntsville, Alabama 35812.

BSD in conjunction with TRW Corporation tracked the fireball cloud of three 25,000-lb tests to determine the feasibility of measuring the turbulence velocity in and near the cloud. Figure D-4 shows a typical fireball cloud. The technical contact for this program was Dr. Chris Bush, 1 Spruce Park, Building R-4, Room 1166, Redondo Beach, California 90278.

* This portion of the report was authored by Mr. R. Thomas, AFRPL.

Appendix E
EXPERIMENTAL TESTS

Presented in this appendix is a chronological listing of all the experimental tests conducted under this program. Included in this listing is the following information:

- Test Number
- Description of test conditions, including: Propellant or explosive type; LO₂/RP-1 (liquid oxygen/RP-1). LO₂/LH₂ (liquid hydrogen), hypergolic (nitrogen tetroxide/50% unsymmetrical dimethylhydrazine - 50% hydrazine), High-explosive Calibration Test (a pentolite charge unless otherwise noted); Flow direction V - (vertical), H - (horizontal); Impact Velocity, LV - low velocity, MV - medium velocity, HV - high velocity; Propellant orientation normal, i.e., LO₂ on top of RP-1, LH₂ on top of LO₂, N₂O₄ on top of 50-50 unless marked RPO (reversed propellant orientation). All propellant weights are 200 lb unless otherwise noted.
- Yield - in percent TNT
- Ignition time in milliseconds
- Data Bank - indicates that the data are or are not in Volume 2, the data bank. A "no" in this column usually indicates that no data were recorded or that the data couldn't be reduced.
- Prediction method - indicates the data which were used either in the development of the prediction method or as large-scale confirmation of the method.

The University of Florida, under contract to NASA KSC, installed a thermocouple grid in the fuel tank of one 200-lb and two 25,000-lb $LO_2/HP-1$ CBM tests to relate explosive yield to propellant mixing as measured by the thermocouple grid. Figure D-5-A shows the grid as installed, and Figure D-5-B shows the oscillograph recording equipment used. The technical contact for this program is Dr. Farber of the University of Florida, Gainesville, Florida 32603.

Sandia Corporation installed two thermal experiments during the 25,000-lb test series. The PAMS (Pad Abort Measuring System), which is used at Cape Kennedy, was evaluated by comparison with the basic PYRO thermal data. The second instrument installed by Sandia, shown in Figure D-6, was the SNAP-27 Heat Transfer Experiment. The data obtained from the SNAP-27 was used to evaluate the thermal model employed in the design of the SNAP-27 power reactor capsule. The technical contact for these experiments is Mr. F. D. Kite of Sandia Corporation, P.O. Box 5800, Albuquerque, New Mexico 32925.

Pan American, under contract to AFETR, installed a cine-spectrograph system, shown in Figure D-7, to provide data regarding the products of the fireball and to measure the fireball temperature. The technical contact for this project is Mr. Cy Golub of Pan American Airways, Mail Unit 706, Building 989, Patrick AFB, Florida 32925.

The Midwest Research Institute, under contract to NASA/MSC, installed pre-heated thermocouples of various designs (see Figure D-8) to determine their survivability under blast wave loading. The purpose of these pre-heated thermocouples is to measure directly the peak temperature of the fireball. The technical contact for this experiment is Mr. R. F. Fletcher of NASA/MSC (STED), Houston, Texas 77001.

NOTES FOR TEST TABLE

1. Propellant spilled to 20-ft diameter from rupture in bottom of tank.
2. Pressure reached 95 psi, 25 msec after diaphragm break. At this point, the top of the tank blew out, and a fire at the top of the tank was observed.
3. Detonator blew hole in tank, which tipped over. Propellant spilled on pad and spread until ignition.
4. Test similar to 087 except tank did not tip over.
5. Top part of tank flew to about the height of the apex of the tower before falling back on the pad. Ignition occurred after it had essentially come to rest.
6. Ignition at impact of top tank.
7. Comparison of tank pressure for test 275 with those for tests 278 and 282, which had very similar ignition time but considerably larger yield, showed a much slower and more linear pressure rise for 275 than for either 278 or 282.
8. Fire visible at bottom of tank about 12 msec before ignition.
9. Propellants not in contact at time of ignition.

The U.S. Coast and Geodetic Survey Agency installed ground shock instrumentation for several 25,000-lb tests to relate ground shock data from nuclear explosions to liquid propellant explosions. The technical contact for this work is Mr. W. B. Mickey, U.S. Coast and Geodetic - ESSA, Washington Science Center, Rockville, Maryland 20852.

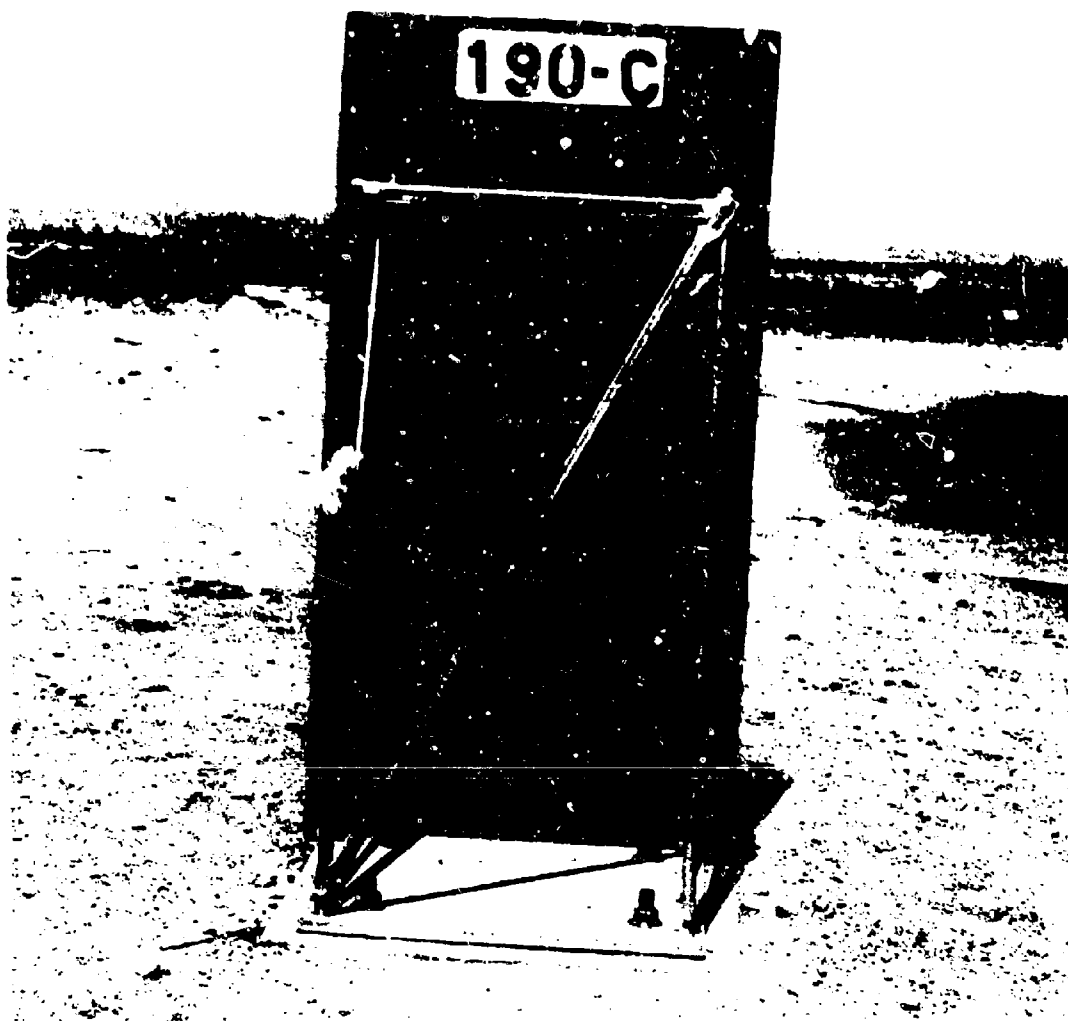


Fig. D-1. Pretest Photo, U.S. Army Corps of Engineers Structure

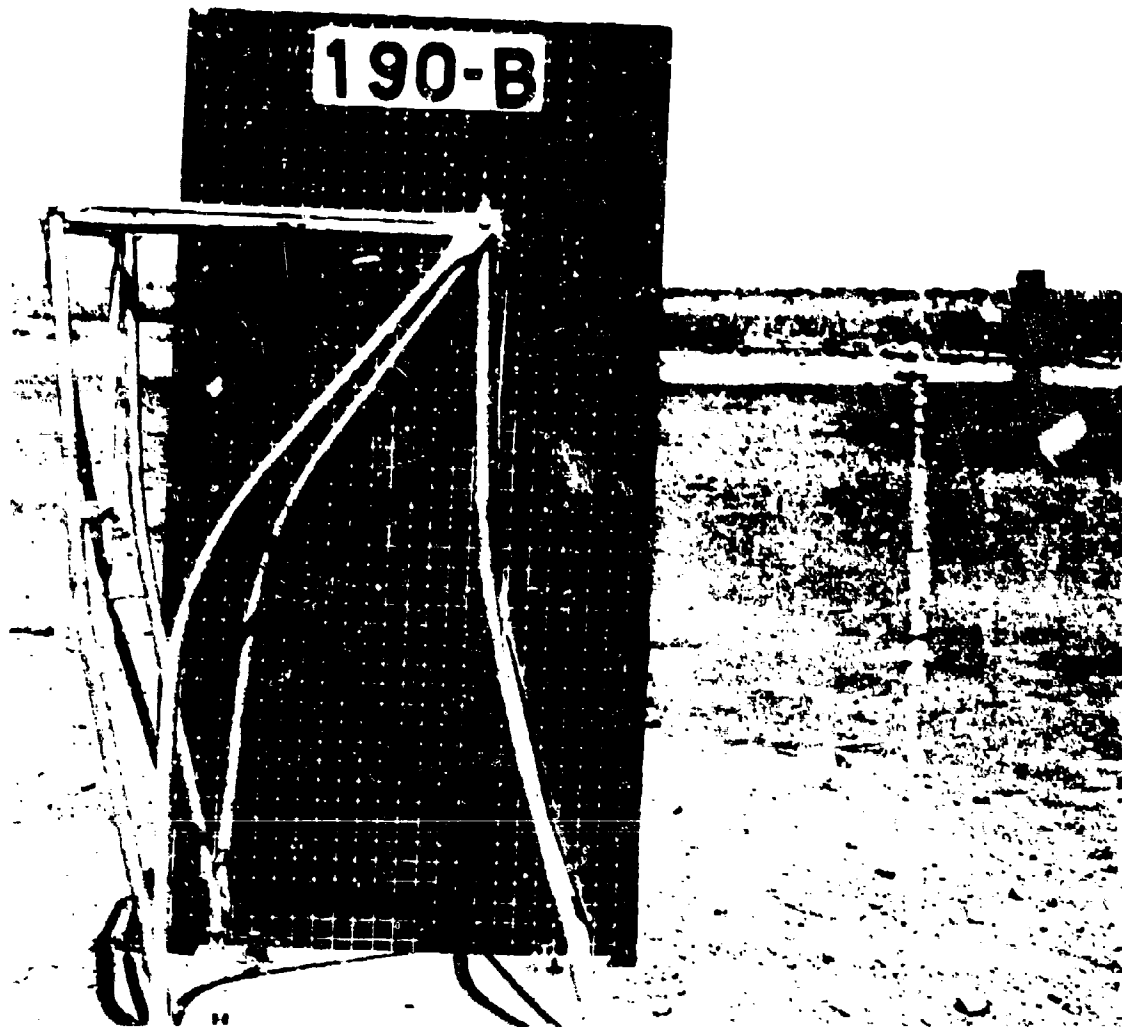


Fig. D-2. Posttest Photo, U.S. Army Corps of Engineers Structure

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|--|-----------|---------------|-----------|--------------|------------------------------|
| 001C | NWC 18-1b CAL. | -- | -- | -- | -- | Data presented in Section 4 |
| 002C | NWC 18-1b CAL. | -- | -- | -- | -- | Data presented in Section 4 |
| 001 | NWC N ₂ O ₄ /50-50 Flat Wall | 13 | -- | Yes | Yes | -- |
| 002 | NWC N ₂ O ₄ /50-50 Shallow Wall | 53 | -- | Yes | Yes | -- |
| 003C | NWC 18-1b CAL. | -- | -- | -- | -- | Data presented in Section 4 |
| 003 | NWC N ₂ O ₄ /50-50 Deep Hole | 29.5 | -- | Yes | Yes | -- |
| 004 | NWC N ₂ O ₄ /50-50 Parallel Wall | 40 | -- | Yes | Yes | -- |
| 005 | N ₂ O ₄ /50-50 30-lb Spherical Shaped Charge Donor | -- | -- | -- | -- | Data presented in Section 4 |
| 006 | NWC 18-1b CAL. | -- | -- | -- | -- | Data presented in Section 4 |
| 007 | NWC N ₂ O ₄ /50-50 Flat Wall | 5 | -- | Yes | Yes | -- |
| 008 | NWC N ₂ O ₄ /50-50 Deep Hole | 38 | -- | Yes | Yes | -- |
| 009 | NWC N ₂ O ₄ /50-50 Deep Hole | 56 | -- | Yes | Yes | -- |
| 010 | NWC N ₂ O ₄ /50-50 Parallel Wall | 26 | -- | Yes | Yes | -- |
| 011 | NWC 18-1b CAL. | -- | -- | No | No | Data presented in Section 4 |
| 012 | NWC 1000-1b N ₂ O ₄ /50-50 Flat Wall | 15 | -- | Yes | Yes | -- |
| 013 | NWC 1000-1b N ₂ O ₄ /50-50 Deep Hole | 28 | -- | Yes | Yes | -- |
| 014 | NWC 216-1b CAL. | -- | -- | No | No | Data presented in Section 4 |
| 015 | NWC 216-1b CAL. | -- | -- | No | No | Data presented in Section 4 |
| 016 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 017 | N ₂ O ₄ /50-50 Test Article Filled With Water Using 30-lb Spherical Charge Donor | -- | -- | No | No | Data presented in Section 4 |
| 018 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 019 | N ₂ O ₄ /50-50 30-lb Spherical Shaped Charge Donor | 3.4 | -- | Yes | Yes | -- |
| 020 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 021 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 022 | N ₂ O ₄ /50-50 Test Article Filled With Water Using 30-lb Spherical Charge Donor | -- | -- | No | No | Data presented in Section 4 |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|--------------|--|
| 023 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 024 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 025 | N ₂ O ₄ /50-50 Linear Charge Donor | 0.35 | -- | Yes | Yes | -- |
| 026 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 027 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 028 | N ₂ O ₄ /50-50 Conical Charge Donor | 1.2 | -- | Yes | Yes | -- |
| 029 | N ₂ O ₄ /50-50 Conical Charge Donor | 0.8 | -- | Yes | Yes | -- |
| 030 | N ₂ O ₄ /50-50 30-lb Spherical Charge Donor | 4 | -- | Yes | Yes | -- |
| 031 | N ₂ O ₄ /50-50 In-Tank Failure | 0.20 | -- | Yes | Yes | -- |
| 032 | N ₂ O ₄ /50-50 In-Tank Failure | 0.08 | -- | Yes | Yes | -- |
| 033 | Frozen N ₂ O ₄ /50-50 | 8.8 | -- | Yes | Yes | -- |
| 034 | Frozen N ₂ O ₄ /50-50 | -- | -- | No | No | Failure of propellant transfer to test tank |
| 035 | N ₂ O ₄ /50-50 In-Tank Failure | 0.15 | -- | Yes | Yes | -- |
| 036 | N ₂ O ₄ /50-50 Linear Charge Donor | 0.30 | -- | Yes | Yes | -- |
| 037 | Frozen N ₂ O ₄ /50-50 | 13.7 | -- | Yes | Yes | -- |
| 038 | N ₂ O ₄ /50-50 In-Tank Failure | -- | -- | No | No | No data test article failure |
| 039 | N ₂ O ₄ /50-50 30-lb Spherical Charge Donor | -- | -- | No | No | -- |
| 040 | LO ₂ /RP-1 Time vs Pressure | 43 | -- | No | No | No pressure on LO ₂ tank before test. Cap ignition at 100 psi tank pressure |
| 041 | LO ₂ /LH ₂ Time vs Pressure | -- | -- | No | No | (10) |
| 042 | LO ₂ /RP-1 CEM | 48 | 290 | Yes | Yes | Tank rupture ignition |
| 043 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Negligible explosion. Suspect no hydrogen |
| 044 | LO ₂ /RP-1 CEM | 18 | 120 | Yes | Yes | Tank rupture ignition |
| 045 | LO ₂ /RP-1 CEM | 5.6 | 123 | No | No | Suspect diaphragm breaker, cap ignition |
| 046 | LO ₂ /RP-1 CEM | 17 | 143 | Yes | Yes | Squib ignition |
| 047 | LO ₂ /RP-1 CEM | 10 | 120 | Yes | Yes | Cap ignition |
| 048 | LO ₂ /RP-1 CEM | 0.8 | 310 | No | No | Suspect diaphragm breaker, cap ignition |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | % BANK | PRED. METHOD | REMARKS |
|---------|---|-----------|---------------|--------|--------------|--|
| 049 | LO ₂ /RP-1 CEM | 12 | 316 | Yes | Yes | Squib ignition |
| 050 | LO ₂ /RP-1 CEM | 86 | 180 | Yes | -- | Tank rupture ignition 2 - LO ₂ /LH ₂ |
| 051 | LO ₂ /LH ₂ CEM | 22 | 80 | Yes | -- | Cap ignition at tank rupture |
| 052 | LO ₂ /LH ₂ CEM | 7 | 83 | Yes | Yes | Squib ignition |
| 053 | LO ₂ /LH ₂ CEM | 4 | 1 | Yes | Yes | Diaphragm rupture ignition |
| 054 | LO ₂ /LH ₂ CEM | 6 | 17 | Yes | Yes | Diaphragm rupture ignition |
| 055 | LO ₂ /LH ₂ CEM | 1 | 1 | Yes | Yes | Diaphragm rupture ignition |
| 056 | LO ₂ /LH ₂ CEM | 70 | 144 | No | No | Breaker charge caused fire on top of tank. (1) |
| 057 | LO ₂ /LH ₂ CEM | 1.3 | 12 | Yes | Yes | Diaphragm rupture ignition |
| 058 | LO ₂ /RP-1 CEM | 26 | 201 | Yes | -- | Squib ignition |
| 059 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 060 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 061 | 105-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 062 | SATURN S-IV LO ₂ /LH ₂ | 6 | 183 | No | No | Data presented in Section 5 |
| 063 | 1000-lb TNT CAL. | -- | -- | No | No | Data presented in Appendix A |
| 064 | 1000-lb TNT CAL. | -- | -- | No | No | Data presented in Appendix A |
| 065 | FWC 18-lb CAL. | -- | -- | No | No | Data presented in Section 4 |
| 066 | NWC CTF/Solid Deep Hole | 0.6 | -- | No | No | Impact Velocity 590 ft/sec |
| 067 | NWC 18-lb CAL | -- | -- | No | No | Data presented in Section 4 |
| 068 | NWC N ₂ O ₄ /50-50 Flat Wall | 15 | -- | Yes | Yes | Impact Velocity 568 ft/sec |
| 069 | NWC N ₂ O ₄ /50-50 Deep Hole | 37 | -- | Yes | Yes | Impact Velocity 555 ft/sec |
| 070 | NWC 18-lb CAL. | -- | -- | No | No | Data presented in Section 4 |
| 071 | NWC 105-lb CAL. | -- | -- | No | No | Data presented in Section 4 |
| 072 | NWC 1000-lb N ₂ O ₄ /50-50 Flat Wall 23 | 23 | -- | Yes | Yes | Impact Velocity 585 ft/sec |
| 073 | NWC 1000-lb N ₂ O ₄ /50-50 Flat Wall 21 | 21 | -- | Yes | Yes | Impact Velocity 410 ft/sec |
| 074 | NWC 1000-lb N ₂ O ₄ /50-50 Deep Hole 34 | 34 | -- | Yes | Yes | Impact Velocity 557 ft/sec |
| 075 | NWC 1000-lb LO ₂ /RP-1 Flat Wall 21 | 21 | -- | Yes | Yes | Impact Velocity 526 ft/sec |
| 076 | NWC 1000-lb LO ₂ /RP-1 Deep Hole 57 | 57 | -- | Yes | Yes | Impact Velocity 523 ft/sec |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PREL. METHOD | REMARKS |
|---------|--|-----------|---------------|-----------|--------------|---|
| 077 | NWC 1000-lb LO ₂ /RP-1 Flat Wall | 20 | -- | Yes | Yes | Impact Velocity 523 ft/sec |
| 078 | NWC 1000-lb LO ₂ /RP-1 Deep Hole | 77 | -- | Yes | Yes | Impact Velocity 518 ft/sec |
| 079 | NWC 1000-lb LO ₂ /LH ₂ Flat Wall | 141 | -- | Yes | Yes | Impact Velocity 597 ft/sec |
| 080 | NWC 1000-lb LO ₂ /LH ₂ Deep Hole | 163 | -- | Yes | Yes | Impact Velocity 519 ft/sec |
| 081 | NWC 216-lb CAL. | -- | -- | No | No | Data presented in Section 4 |
| 082 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 083 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 084 | LO ₂ /RP-1 CHEM | 10 | 312 | No | No | Test tank leaked, Cap Ignition |
| 085 | LO ₂ /RP-1 CHEM | 12 | 380 | Yes | Yes | Cap Ignition, 108 psi internal pressure |
| 086 | LO ₂ /RP-1 CHEM | 14 | 100 | Yes | Yes | Cap Ignition |
| 087 | LO ₂ /RP-1 CHEM | -- | -- | No | No | No Ignition (3) |
| 087A | LO ₂ /RP-1 CHEM | 16 | 70 | Yes | Yes | Cap Ignition |
| 088 | LO ₂ /RP-1 CHEM | 8 | 60 | Yes | Yes | Cap Ignition |
| 089 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 090 | LO ₂ /LH ₂ CHEM | 29 | 35 sec | Yes | Yes | Cap Ignition at 100 psi internal pressure |
| 091 | LO ₂ /LH ₂ CHEM | 13 | -- | Yes | No | Diaphragm rupture ignition |
| 092 | LO ₂ /LH ₂ CHEM | 26 | 3 min | Yes | Yes | Cap Ignition at 100 psi internal pressure |
| 093 | LO ₂ /LH ₂ CHEM | 34 | 147 | Yes | -- | Cap Ignition |
| 094 | LO ₂ /LH ₂ CHEM | 25 | 329 | Yes | Yes | Ignition at 100 psi internal pressure |
| 095 | LO ₂ /RP-1 CHEM | -- | -- | No | No | No Ignition (4) |
| 095A | LO ₂ /RP-1 CHEM | 17 | 120 | Yes | Yes | Cap Ignition |
| 096 | LO ₂ /RP-1 V - LV | 14 | 50 | Yes | -- | Cap Ignition |
| 097 | LO ₂ /RP-1 V - LV (RPO) | 32 | 240 | Yes | -- | Cap Ignition |
| 098 | LO ₂ /RP-1 V - LV | 14 | 300 | Yes | -- | Cap Ignition |
| 099 | LO ₂ /RP-1 V - LV (RPO) | 23 | 140 | Yes | -- | Fire on pad before ignition |
| 100 | LO ₂ /RP-1 CHEM | 23 | 220 | Yes | Yes | Cap Ignition |
| 101 | LO ₂ /RP-1 CHEM | 35 | 245 | Yes | Yes | Cap Ignition |
| 102 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 103 | LO ₂ /LH ₂ V - LV (RPO) | -- | -- | No | No | No ignition, ordnance wires reversed |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. BANK METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|-------------------|---|
| 103A | LO ₂ /LH ₂ V - LV (RPO) | 39 | 208 | Yes | -- | Cap Ignition |
| 104 | LO ₂ /LH ₂ V - LV | 6 | 258 | Yes | -- | Squib Ignition |
| 105 | LO ₂ /LH ₃ V - LV | 7 | -- | Yes | -- | Cap Ignition |
| 106 | LO ₂ /LH ₂ V - LV (RPO) | -- | -- | No | No | No ignition, tank dropped late, propellant spilled on pad |
| 106A | LO ₂ /LH ₂ V - LV (RPO) | 22 | 325 | No | No | Tank hit breaker |
| 107 | LO ₂ /RP-1 V - HV (RPO) | 29 | 42 | Yes | -- | Cap Ignition |
| 108 | LO ₂ /RP-1 V - HV (RPO) | 31 | 60 | Yes | -- | Cap Ignition |
| 109 | LO ₂ /RP-1 V - EV | 10 | 40 | Yes | -- | Cap Ignition |
| 110 | LO ₂ /RP-1 V - HV | 26 | 35 | Yes | Yes | Cap Ignition |
| 111 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 112 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 113 | LO ₂ /LH ₂ V - HV (RPO) | 52 | 77 | Yes | -- | Cap Ignition |
| 114 | LO ₂ /LH ₂ V - HV | 54 | 74 | Yes | Yes | Cap Ignition |
| 115 | LO ₂ /LH ₂ V - HV (RPO) | 15 | 93 | Yes | -- | Cap Ignition |
| 116 | LO ₂ /LH ₂ V - EV | 10 | 18 | Yes | -- | Ignition on impact with breaker |
| 117 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Breaker charge caused fire and early ignition |
| 118 | LO ₂ /LH ₂ CEM | 20 | 82 | Yes | Yes | Cap Ignition at 100 psi |
| 119 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Test aborted (2) |
| 119A | LO ₂ /LH ₂ CEM | -- | -- | No | No | Test aborted, no data recorded |
| 119B | LO ₂ /RP-1 H LV | -- | -- | No | NC | Tanks dropped in reverse order, bad calibrations Cap Ignition. Data not usable. |
| 120 | LO ₂ /RP-1 H LV | 30 | 238 | No | No | NC propellant out of bottom tank until impact of top tank |
| 121 | LO ₂ /RP-1 H LV | 30 | 184 | Yes | Yes | RP-1 dumped before start of film, cap ignition |
| 122 | LO ₂ /RP-1 H LV | 28 | 338/139 | Yes | Yes | Cap Ignition |
| 123 | LO ₂ /RP-1 H LV | 25 | 362/53 | Yes | Yes | Cap Ignition |
| 124 | LO ₂ /RP-1 H HV | 5 | 238/0 | Yes | -- | Ignition at impact of top tank |
| 125 | LO ₂ /RP-1 H HV | 52 | 142/76 | Yes | Yes | Cap Ignition |
| 126 | LO ₂ /RP-1 H TV | -- | -- | No | No | Ignition at impact of top tank |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PREL. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|--------------|--|
| 127 | LO ₂ /RP-1 H HV | 42 | 510/55 | Yes | Yes | Fluid at 7-8' at ignition, tank breakup ignition |
| 128 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 129 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 130 | LO ₂ /LH ₂ CHM | -- | -- | No | No | Test aborted when tank pressure dropped |
| 130A | LO ₂ /LH ₂ H - LV | -- | -- | No | No | Top tank did not drop, no data |
| 130B | LO ₂ /LH ₂ H - LV | -- | -- | No | No | Top tank caught on tower, no data |
| 130C | LO ₂ /LH ₂ H - LV | -- | -- | No | No | Self-ignition, LH ₂ trench destroyed |
| 131 | LO ₂ /LH ₂ H - LV | 6 | 441/125 | Yes | Yes | Cap Ignition |
| 132 | LO ₂ /LH ₂ H - LV | 5 | -- | Yes | Yes | Bottom tank did not release fluid until after impact of top tank, cap ignition |
| 133 | LO ₂ /LH ₂ H - LV | -- | -- | No | No | Top tank did not drop |
| 133A | LO ₂ /LH ₂ H - LV | 6 | 284/286 | Yes | Yes | Cap Ignition |
| 134 | LO ₂ /LH ₂ H HV | 8 | -- | No | No | Cap Ignition, all propellant not on ground at ignition |
| 135 | LO ₂ /LH ₂ H HV | 5.5 | -- | No | No | Ignition at impact of top tank |
| 136 | LO ₂ /LH ₂ H HV | -- | -- | No | No | Ignition at impact of top tank |
| 137 | LO ₂ /LH ₂ H HV | 6.4 | -- | No | No | Ignition at impact of top tank |
| 138 | LO ₂ /LH ₂ CHM | 17 | 100 | Yes | Yes | Cap Ignition |
| 139 | LO ₂ /RP-1 H HV | 4.2 | -- | No | No | Ignition at impact of top tank |
| 140 | LO ₂ /RP-1 H HV | 12 | 420/10 | Yes | Yes | Cap Ignition |
| 141 | LO ₂ /RP-1 V HV | 5 | -- | Yes | -- | Probable diaphragm rupture ignition (8) |
| 142 | LO ₂ /RP-1 V HV | 14 | 33 | Yes | -- | Cap Ignition |
| 143 | LO ₂ /RP-1 V LV | 34 | 210 | No | No | Top and bottom tanks impacted together |
| 144 | LO ₂ /RP-1 V LV | 24 | 190 | Yes | Yes | Cap Ignition |
| 145 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 146 | 16-lb Functional | -- | -- | No | No | Instrumentation checkout only, data not reduced |
| 147 | 16-lb Functional | -- | -- | No | No | Instrumentation checkout only, data not reduced |
| 148 | Hypergolic V HV | -- | -- | No | No | Caught fire on top of tower, no data |
| 149 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 150 | LO ₂ /LH ₂ V HV | 35 | 40 | Yes | Yes | Cap Ignition |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|---------------------------------------|-----------|---------------|-----------|--------------|--|
| 151 | LO ₂ /LH ₂ V HV | 46 | 167 | Yes | -- | Cap Ignition, tank fell through stopper |
| 152 | LO ₂ /LH ₂ V LV | 14 | 480 | Yes | Yes | Cap Ignition |
| 153 | LO ₂ /LH ₂ V LV | 14 | 121 | Yes | Yes | Cap Ignition, bad timing |
| 154 | LO ₂ /RP-1 V LV | 18 | 470 | Yes | -- | Cap Ignition |
| 155 | LO ₂ /RP-1 H LV (RPO) | 55 | 314/202 | Yes | Yes | Cap Ignition |
| 156 | LO ₂ /RP-1 H LV (RPO) | 63 | 182/230 | Yes | Yes | Cap Ignition |
| 157 | Hypergolic V HV | 0.59 | 99 | Yes | Yes | -- |
| 158 | Hypergolic V HV | 0.2 | -- | Yes | Yes | -- |
| 159 | Hypergolic V HV | 0.3 | -- | Yes | Yes | -- |
| 160 | LO ₂ /LH ₂ V HV | 32 | 67 | Yes | -- | Cap Ignition |
| 161 | LO ₂ /LH ₂ V HV | 5 | -- | Yes | -- | Cap Ignition |
| 162 | LO ₂ /RP-1 H HV | 24 | -- | No | No | Bottom tank did not drop (6) |
| 163 | LO ₂ /RP-1 V HV | -- | -- | No | No | Two explosions, one early, one correct time |
| 164 | LO ₂ /LH ₂ V LV | 4 | 125 | Yes | No | Tank leaked, self ignition |
| 165 | LO ₂ /LH ₂ V LV | 8 | 325 | Yes | -- | Cap ignites after propellant on pad, fire goes out, detonation at time given |
| 166 | LO ₂ /LH ₂ CEM | -- | -- | -- | -- | 0.7% open, cold flow test (10) |
| 167 | LO ₂ /LH ₂ CEM | 24 | 8.74 sec | Yes | -- | Partial full cold flow, ignition caused by poor venting (10) |
| 168 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Cold flow test (10) |
| 169 | LO ₂ /LH ₂ CEM | 15 | 318 | Yes | Yes | Cap Ignition |
| 170 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Cold flow test (10) |
| 171 | LO ₂ /LH ₂ CEM | -- | -- | No | No | Cold flow test (10) |
| 172 | LO ₂ /LH ₂ CEM | 32 | 730 | Yes | -- | Fire on tank before ignition |
| 173 | LO ₂ /LH ₂ CEM | 15 | 56 | Yes | Yes | Fire on tank before ignition |
| 174 | LO ₂ /RP-1 CEM | 51 | 186 | Yes | -- | Tank ruptured before ignition |
| 175 | LO ₂ /RP-1 CEM | -- | -- | No | No | Tank came apart, deflagration at much later time. No data. |
| 176 | LO ₂ /RP-1 H LV | 65 | 488/374 | Yes | Yes | Cap Ignition |
| 177 | LO ₂ /RP-1 H LV | 30 | 111/203 | Yes | Yes | Cap Ignition |
| 178 | LO ₂ /RP-1 H HV | 3.2 | -- | No | No | Bottom tank did not drop (6) |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (S) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|--------------|---|
| 179 | LO ₂ /RP-1 H HV | 12 | 787/23 | Yes | Yes | Probable impact ignition |
| 180 | LO ₂ /RP-1 H HV (RPO) | -- | -- | No | No | LO ₂ cutran RP-1, no ignition |
| 181 | LO ₂ /RP-1 H HV (RPO) | -- | -- | No | No | No tape recorder |
| 182 | LO ₂ /LR ₂ H HV | 2.7 | -- | No | No | Ignition at impact of top tank |
| 183 | LO ₂ /LR ₂ H HV | 15 | 448/0 | Yes | -- | Probable impact ignition |
| 184 | LO ₂ /LR ₂ V LV | 17 | 810 | Yes | Yes | Cap Ignition |
| 185 | LO ₂ /LR ₂ H LV | 8 | 469/0 | Yes | -- | Probable impact ignition |
| 186 | LO ₂ /LR ₂ H LV | 9 | 587/61 | Yes | No | Ignition source unknown |
| 187 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 188 | 18-1b CAL. | -- | -- | No | No | Data presented in Appendix A |
| 189 | Hypergolic 1000-1b | 0.35 | -- | Yes | Yes | -- |
| 190 | LO ₂ /RP-1 V - HV 1000-1b | 96 | 570 | Yes | Yes | Cap fired early, propellant found other ignition source |
| 191 | LO ₂ /RP-1 V MV 1000-1b | 13 | 0 | No | No | Tank broke stopper and hit ground |
| 192 | LO ₂ /RP-1 CEM 1000-1b | 14 | 216 | Yes | Yes | Probable Cap Ignition |
| 193 | LO ₂ /RP-1 CEM 1000-1b | 20 | 222 | Yes | Yes | Probable Cap Ignition |
| 194 | LO ₂ /RP-1 CEM 1000-1b | 5.5 | 5 sec | No | No | Tank separated at diaphragm (S) |
| 195 | LO ₂ /LR ₂ V - HV | 104 | 282 | Yes | Yes | Cap Ignition |
| 196 | LO ₂ /LR ₂ V HV | 17 | 639/77 | Yes | Yes | Cap Ignition |
| 197 | LO ₂ /LR ₂ V MV | 19 | 500 | Yes | Yes | Cap Ignition |
| 198 | LO ₂ /LR ₂ H LV | 17 | 507/121 | No | No | Top tank pushed bottom tank to ground |
| 199 | LO ₂ /LR ₂ CEM | 8 | 816 | Yes | Yes | Cap Ignition |
| 200 | LO ₂ /LR ₂ CEM | 17 | 100 | Yes | Yes | Cap Ignition |
| 201 | LO ₂ /LR ₂ V LV | 26 | 1524 | Yes | Yes | Cap Ignition |
| 202 | LO ₂ /RP-1 V LV | 42 | 870 | Yes | Yes | Cap Ignition |
| 203 | LO ₂ /LR ₂ V MV | 31 | 800 | Yes | Yes | Cap Ignition |
| 204 | LO ₂ /LR ₂ V MV | 42 | 317 | Yes | Yes | Cap Ignition |
| 205 | LO ₂ /RP-1 V HV | 41 | 40 | Yes | Yes | Tank on fire on the way down |
| 206 | LO ₂ /RP-1 V HV | 85 | 350 | Yes | Yes | Cap Ignition |
| 207 | LO ₂ /RP-1 V HV | 38 | 28 | Yes | Yes | No obvious ignition source |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|--------------|--|
| 208 | LO ₂ /RP-1 V MV | 39 | 460 | Yes | Yes | Cap Ignition |
| 209 | LO ₂ /RP-1 CEM 1000-lb | 10 | 121 | Yes | Yes | Cap Ignition |
| 210 | LO ₂ /LH ₂ CEM 1000-lb | 7 | 20 | Yes | Yes | Either tank rupture or diaphragm rupture ignition |
| 211 | LO ₂ /LH ₂ V MV 1000-lb | 12 | 0 | Yes | No | Ignition at impact |
| 212 | LO ₂ /LH ₂ CEM 1000-lb | 27 | 1366 | Yes | Yes | Cap Ignition |
| 213 | LO ₂ /LH ₂ CEM 1000-lb | 35 | 708 | Yes | Yes | Cap Ignition |
| 214 | LO ₂ /LH ₂ V HV 1000-lb | NG | -- | No | No | No data, tank caught fire 50' up tower |
| 215 | LO ₂ /LH ₂ V HV 1000-lb | 20 | 23 | Yes | No | Tank broke apart at impact |
| 216 | LO ₂ /LH ₂ V HV 1000-lb | 9 | 0 | Yes | No | Ignition at impact |
| 217 | LO ₂ /LH ₂ V MV 1000-lb | 33 | 1490 | Yes | Yes | Cap Ignition |
| 218 | LO ₂ /RP-1 V MV 1000-lb | 4.5 | -- | Yes | No | Ignition at impact |
| 219 | LO ₂ /RP-1 V MV 1000-lb | 14 | 1835 | Yes | -- | Cap Ignition |
| 220 | LO ₂ /RP-1 V MV 1000-lb | 96 | 525 | Yes | Yes | Cap Ignition |
| 221 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 222 | 18-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 223 | LO ₂ /LH ₂ H LV | 18 | 708/157 | Yes | Yes | Cap Ignition |
| 224 | LO ₂ /LH ₂ H LV | 16 | 899/621 | Yes | Yes | Cap Ignition |
| 225 | LO ₂ /LH ₂ V LV | 34 | 933 | Yes | Yes | Cap Ignition |
| 226 | LO ₂ /LH ₂ V HV | 51 | 283 | Yes | Yes | Fire on pad before ignition |
| 227 | LO ₂ /LH ₂ H HV | -- | -- | No | No | Tank on fire at impact |
| 228 | LO ₂ /LH ₂ H HV | 34 | 560/42 | Yes | Yes | Unknown ignition source |
| 229 | LO ₂ /LH ₂ V MV | 52 | 1374 | Yes | Yes | Cap Ignition |
| 230 | LO ₂ /LH ₂ V MV | 21 | 24 | Yes | -- | Fire visible at top of tank 10-15 msec before ignition |
| 231 | LO ₂ /LH ₂ V MV | 24 | 525 | Yes | Yes | Cap Ignition |
| 232 | LO ₂ /RP-1 V MV | 30 | 1220 | Yes | Yes | Cap Ignition |
| 233 | LO ₂ /LH ₂ H LV Pool | 109 | 2182 | Yes | Yes | Cap ignition, maximum yield trial |
| 234 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 235 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 236 | LO ₂ /RP-1 V HV | 74 | 720 | Yes | Yes | Cap Ignition |

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRD. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|-------------|--|
| 237 | LO ₂ /RP-1 CM | 33 | 127 | Yes | Yes | Tank Rupture |
| 238 | LO ₂ /RP-1 CM | 19 | 85 | Yes | Yes | Repeat of 87. Cap Ignition |
| 239 | LO ₂ /RP-1 CM | 32 | 156 | Yes | Yes | Tank rupture ignition probable, could be cap |
| 240 | LO ₂ /RP-1 CM | 60* | 156 | Yes | Yes | Tank only 2/3 full, yield based on full tank, Cap Ignition |
| 241 | 30-lb Comp B - Water | -- | -- | No | No | High explosive charge standard |
| 242 | LO ₂ /LE ₂ H LV Pool | 107 | 800 | Yes | Yes | Maximum yield trial, Cap Ignition |
| 243 | Hybrid | -- | -- | No | No | ** |
| 244 | Hybrid | -- | -- | No | No | ** |
| 245 | LO ₂ /LE ₂ H LV | -- | -- | No | No | Tape malfunction, no data |
| 246 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 247 | 18-lb High Explosive Cal. | -- | -- | No | No | -- |
| 248 | LO ₂ /RP-1 V LV | 25 | 210 | Yes | Yes | Cap Ignition |
| 249 | LO ₂ /RP-1 V LV | 50 | 710 | Yes | Yes | Cap Ignition |
| 250 | LO ₂ /RP-1 V MV | 52 | 200 | Yes | Yes | Cap Ignition |
| 251 | LO ₂ /LE ₂ V MV | 64 | 775 | Yes | Yes | Cap Ignition |
| 252 | LO ₂ /LE ₂ V MV | 38 | 325 | Yes | Yes | Cap Ignition |
| 253 | LO ₂ /LE ₂ H MV | 57 | 561/108 | Yes | Yes | Cap Ignition |
| 254 | LO ₂ /LE ₂ V MV 200-lb | 33 | 533 | Yes | Yes | Cap Ignition |
| 255 | 18-lb High Explosive Cal. | -- | -- | No | No | Data presented in Appendix A |
| 256 | 18-lb High Explosive Cal. | -- | -- | No | No | -- |
| 257 | H ₂ O ₄ /50-50 V HV 1000-lb | .3 | -- | Yes | Yes | -- |
| 258 | H ₂ O ₄ /50-50 V HV 1000-lb | .25 | -- | Yes | Yes | -- |
| 259 | Hybrid 30-lb Donor | -- | -- | No | No | ** |
| 260 | Hybrid drop | -- | -- | No | No | No data |
| 261 | Hybrid drop | -- | -- | No | No | No data |
| 262 | LO ₂ /LE ₂ V MV 1000-lb | 42 | 900 | Yes | Yes | Cap Ignition |
| 263 | LO ₂ /LE ₂ V MV 1000-lb | -- | -- | No | No | Ignition before impact |
| 264 | LO ₂ /LE ₂ V MV 1000-lb | 21.5 | 20 | Yes | No | Self ignition, suspect diaphragm break |

* Pressure yields from AFRPL

** Data reported in Investigation of the Explosive Potential of the Hybrid Propellant Combinations N₂O₄/PBAN and CTE/PBAN, TR No. AFRPL-TR-67-124, URS 652-26, March 1967

| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|---|-----------|---------------|-----------|--------------|---|
| 265 | LO ₂ /LF ₂ CEM 1000-lb | 10 | 750 | Yes | Yes | Cap Ignition |
| 266 | LO ₂ /LF ₂ V MV 1000-lb | 14 | -- | Yes | No | Fire at top of tank |
| 267 | LO ₂ /RP-1 V MV 1000-lb | 64 | 1770 | Yes | Yes | Cap Ignition |
| 268 | LO ₂ /RP-1 V MV 1000-lb | 70 | 340 | Yes | Yes | Cap Ignition |
| 269 | LO ₂ /RP-1 V HV 1000-lb | -- | -- | No | No | Test aborted |
| 269A | LO ₂ /RP-1 V HV 1000-lb | 44 | 77 | Yes | Yes | Midsection of tank fails at impact |
| 270 | LO ₂ /RP-1 CEM 1000-lb | -- | -- | No | No | Test aborted, no data |
| 270A | LO ₂ /RP-1 CEM 1000-lb | 13 | 225 | Yes | Yes | Cap Ignition |
| 271 | 105-lb CAL. | -- | -- | No | No | Data presented in Appendix A |
| 272 | 105-lb CAL. | -- | -- | No | No | -- |
| 273 | 100-lb Functional | -- | -- | No | No | A, B, C tests as well |
| 274 | 500-lb CAL. | -- | -- | No | No | -- |
| 275 | LO ₂ /RP-1 CEM 25,000-lb | 4 | 515 | Yes | Yes | Suspect only small hole in diaphragm (7) |
| 276 | 500-lb CAL. | -- | -- | No | No | -- |
| 277 | LO ₂ /LF ₂ CEM 25,000-lb | 0.2 | 31 | Yes | No | Diaphragm rupture ignition |
| 278 | LO ₂ /RP-1 CEM 25,000-lb | 13 | 530 | Yes | Yes | Tank rupture ignition |
| 279 | LO ₂ /LF ₂ CEM 25,000-lb | 0.2 | 33 | Yes | No | Diaphragm rupture ignition |
| 280 | 500-lb CAL. | -- | -- | No | No | -- |
| 281 | LO ₂ /LF ₂ CEM 25,000-lb | 0.1 | 0 | Yes | No | Diaphragm rupture ignition |
| 282 | LO ₂ /RP-1 CEM 25,000-lb | 13 | 540 | Yes | Yes | Tank rupture before ignition |
| 283 | LO ₂ /RP-1 V HV 25,000-lb | -- | -- | No | No | Tank dropped early, no data |
| 284 | LO ₂ /RP-1 V MV 25,000-lb | 2 | 89 | Yes | No | Diaphragm rupture ignition, ignition before fluid hits ground |
| 285 | LO ₂ /RP-1 V MV 25,000-lb | 37 | 465 | Yes | No | Ignition source unknown |
| 286 | 500-lb CAL. | -- | -- | No | No | -- |
| 287 | 500-lb CAL. | -- | -- | No | No | -- |
| 288C | LO ₂ /LF ₂ V MV 25,000-lb | 13 | 365 | Yes | No | 288A and B were aborted. Ignition source unknown |
| 289 | LO ₂ /LF ₂ V MV 25,000-lb | 4 | 166 | Yes | No | Ignition source unknown |
| 290 | LO ₂ /LF ₂ V MV 25,000-lb | 4 | 105 | Yes | No | Fire at top of tank after diaphragm rupture, ignition at bottom of tank |

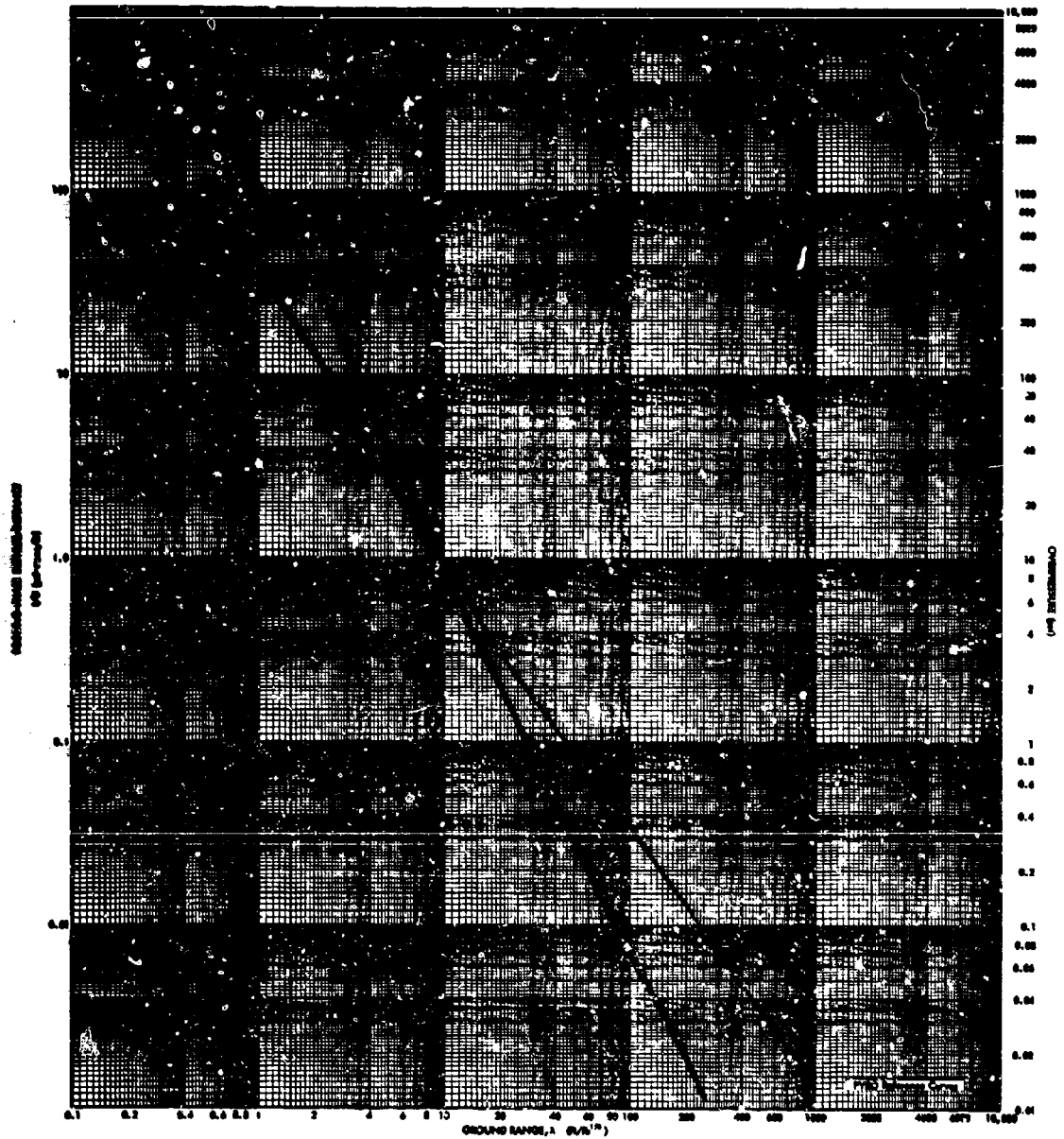
| TEST NO | DESCRIPTION TEST CONDITIONS | YIELD (%) | IGNITION TIME | DATA BANK | PRED. METHOD | REMARKS |
|---------|--|-----------|---------------|-----------|--------------|--|
| 291 | LO ₂ /LH ₂ V MV 1000-lb | 1.2* | -- | No | No | No data reduced at URS, ignition at impact |
| 292 | LO ₂ /LH ₂ V MV 1000-lb | 3.6* | -- | No | No | No data reduced at URS, ignition at impact |
| 293 | LO ₂ /LH ₂ V MV 1000-lb | 3.9* | -- | No | No | No data reduced at URS, ignitor at impact |
| 294 | LO ₂ /LH ₂ -LO ₂ /RP-1 V MV 1200-lb | 5.6* | 0 | No | No | No data reduced at URS, fire on tank caused premature ignition |
| 295 | LO ₂ /LH ₂ -LO ₂ /RP-1 V MV 1200-lb | 70 | 544 | Yes | No | Cap Ignition |
| 296 | LO ₂ /RF-1 V MV 1000-lb | 22 | 740 | No | No | Tank dropped to ground, slowing propellant release |
| 297 | LO ₂ /LH ₂ V MV 200-lb | 18* | 2 sec | No | No | -- |
| 298 | 500-lb Pentolite CAL. | -- | -- | No | No | Data presented in Appendix A |
| 299 | LO ₂ /RP-1 CEM 200-lb | 17 | 73 sec | No | No | AFRPL figures, data not reduced at URS |
| 300 | Titan LO ₂ /RP-1 | -- | -- | No | No | Premature ignition, no data |
| 301 | Titan LO ₂ /RP-1 | 3.7 | 840 | No | No | Source of ignition unknown |
| 302 | LO ₂ /LH ₂ CEM 25,000-lb | -- | -- | No | No | No data reduced at URS |
| 303 | 1000-lb Pentolite CAL. | -- | -- | No | No | -- |

* Pressure yields from AFRPL

Appendix F

**PEAK OVERPRESSURE versus DISTANCE
AND POSITIVE-PHASE IMPULSE versus
DISTANCE REFERENCE CURVES**





PEAK OVERPRESSURE and POSITIVE-PHASE IMPULSE vs SCALED DISTANCE for the SURFACE BURST

UNCLASSIFIED

Security Classification

DOCUMENT CONTROL DATA - R & D

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| 13. ABSTRACT | | | |
| <p>Project PYRO consisted of a comprehensive program to determine the blast and thermal characteristics of the three liquid propellant combinations in most common use in military missiles and space vehicles; liquid oxygen-RP-1 (LO₂/RP-1), liquid oxygen - liquid hydrogen (LO₂/LH₂), and nitrogen tetroxide/50% unsymmetrical dimethylhydrazine - 50% hydrazine (N₂O₄/50% UDMH - 50% N₂H₄). During the course of the program some 270 tests were conducted with these propellant combinations on weight scales ranging from 200 lb to 100,000 lb. This basic explosive test program was supplemented by analytical and statistical studies, laboratory-scale experimental studies, simulation tests with inert propellant combinations and a series of high-explosive tests for calibration and evaluation purposes.</p> <p>The basic test program was designed to investigate the explosive characteristics of the three propellant combinations for the most credible ways that the propellants might accidentally come into contact with each other and result in a significant explosion.</p> <p>The results of the basic test program in conjunction with the analytical studies and prior information regarding liquid propellant explosive behavior were used as the basis for developing methods for predicting the blast and thermal environment that would be expected for any given missile or space vehicle system and any specified failure mode. ()</p> <p>In the prediction method the thermal environment is given only as a function of propellant type, while the blast environment is given as a function of a number of controlling parameters. A failure mode analysis is required to select the appropriate values of the parameters needed to predict the blast environment for a specific system.</p> <p>This report is presented in three volumes: Volume 1, Technical Documentary Report; Volume 2, Test Data; Volume 3, Prediction Methods.</p> | | | |

DD FORM 1 NOV 68 1473

REPLACES DD FORM 1473, 1 JAN 64, WHICH IS OBSOLETE FOR ARMY USE.

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|---|--------|----|--------|----|--------|----|
| | ROLE | WT | ROLE | WT | ROLE | WT |
| Liquid propellants Thermal measurements Propellant hazards Hazard analysis Propellant blast environment | | | | | | |

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