2
RECEIVED BY TIC FEB 24 1977

BMI-X-676

Heat Source Component Development Program

QUARTERLY REPORT FOR OCTOBER-DECEMBER, 1976

Report Date: January, 1977

MAST

Battelle

Columbus Laboratories
505 King Avenue
Columbus, Ohio 43201

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Energy Research and Development Administration, nor any of the employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

HEAT SOURCE COMPONENT DEVELOPMENT PROGRAM

Quarterly Report for October-December, 1976

**Compiled by
William M. Pardue**

**Prepared for the Division of Nuclear Research and Applications,
U.S. Energy Research and Development Administration
Under Contract No. W-7405-eng-92, Task 94**

Report Date: January, 1977

**Columbus Laboratories
505 King Avenue
Columbus, Ohio 43201**

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

FOREWORD

This is the second in a series of quarterly reports describing the results of several experimental programs being conducted at Battelle-Columbus to develop components for advanced radioisotope heat source applications. The heat sources will for the most part be used in advanced static and dynamic power conversion systems. These reports replace the informal monthly technical letter reports previously prepared and are being utilized so that more cohesive presentation of results can be achieved. In addition, a series of summary management monthly reports was initiated in July of 1976 to permit NRA assessment of contractual progress.

The activities covered in this new series of reports are conducted under Contract No. W-7405-eng-92, Task 94. The specific components development efforts which are described herein are: improved selective and non-selective vents for helium release from the fuel containment; an improved reentry member and an improved impact member, singly and combined. The unitized reentry-impact member (RIM) is under development to be used as a bifunctional ablator. Finally, thermochemical supporting studies are reported.

TABLE OF CONTENTS

	<u>Page</u>
IMPROVED IMPACT MEMBER.	1
Introduction	1
Summary.	1
Work This Period	1
Development of Preferred Impact-Member Materials.	3
Effect of Elevated Temperature on Energy Absorption by Impact-Member Materials.	10
Instrumented High Loading Rate Test for Impact- Member Materials.	14
Energy Absorption by Bulk Graphite.	25
IMPROVED REENTRY MATERIALS.	31
Introduction	31
Summary.	32
Work This Period	32
Evaluate Bifunctional Component Configurations.	32
Surface Chemistry Studies	51
Thermal Stress Failure of C/C Composites.	59
THERMODYNAMIC STUDIES	62
Introduction	62
Summary.	62
Work This Period	62
HELIUM VENT DEVELOPMENT	70
Introduction	70
Summary.	70
Work This Period	70

APPENDIX

NOMENCLATURE GLOSSARY FOR GAS GUN GLOSSARY.	A-1
---	-----

HEAT SOURCE COMPONENT DEVELOPMENT PROGRAM

IMPROVED IMPACT MEMBER (W. H. Duckworth - Task Leader)

Introduction

The objective of this program is to obtain a basic understanding of the mechanisms of high strain rate failure in materials for impact protection of isotopic heat sources intended for static and dynamic power conversion systems. From this information, a preferred materials system for significantly improved impact members will be selected, or developed and demonstrated. Not only is the behavior of the impact member itself of significance, but also the mechanical interactions between the fuel, containment shell, and impact member are complex and critical to overall performance.

Summary

Research efforts during the past three months on materials to obtain information relative to improved performance for impact members of isotope heat sources were directed primarily to determining effects of fiber variations, temperature, and high rate loading on energy absorption characteristics of "golf-ball" wound carbon composites.

The basic purpose of the impact member is to limit deformation of the fuel so that its metal cladding does not rupture from excessive stretching at any location in the event of earth impact. General deformation of the fuel will be minimized by increasing the proportion of the impacting heat source's kinetic energy that converts to mechanical work in the impact member at the expense of that which dissipates in the fuel. Hence, materials with highest energy absorption capacities are being sought for impact member use, and for this and other reasons carbon composites are considered to be the prime candidate materials.

In order to dissipate an interesting amount of kinetic energy in an impact member made from a brittle material, it must undergo multiple fracturing during the impact and crush as a consequence. Our past research

has indicated that the "golf-ball" wound carbon composite is superior to more isotropic carbon composites and bulk graphites, having both a greater tendency to crush and a higher energy absorption associated with crushing.

For crushing to occur, of course, the impact material must be softer than the fuel. Otherwise, the fuel will deform initially and the impacting heat source's kinetic energy can dissipate to an undesirable extent in the fuel, preventing full use of the impact member's energy absorption capability; i.e., the fuel "cushions" the impact member rather than obtaining the required opposite result. Service tests to date of MHW heat sources at other laboratories have not provided adequate information to judge the extent of utilization of the impact member's energy absorption capacity that is available through crushing. Thus, a critical problem confronts the development and evaluation of impact member materials in ensuring that crushing will occur during impact, and this problem cannot be resolved analytically without detailed knowledge of the dynamic flow properties of the fuel, which is to be developed by Los Alamos Scientific Laboratory.

A possibility exists, however, for resolving this critical problem experimentally through carefully conceived service-type impact tests of special "golf-ball" wound carbon composites in which the crushing strength (i.e., "softness") is varied. Work is described below directed to developing information regarding the special materials for such experiments. Previously, it was hypothesized that tensile strength of the carbon fiber was a principal factor determining composite crushing strength. Major attention in the work now being reported was on determining the validity of this hypothesis and in determining other material variables, if any, that affect crushing strength. Since external lateral constraint of fuel flow during impact is also important to ensuring that the impact member crushes, it could be counter-productive if the crushing strength is lowered at the expense of constraint imposed by the impact member. Future investigations will give attention to the constraint of lateral fuel flow provided by various experimental composite materials.

Work This PeriodDevelopment of Preferred Impact-Member Materials
(G. K. Bansal)

The concepts that have been developed for preferred impact-member materials with yielding or unyielding ("hard" and "soft") fuel address the question of what might be done in tailoring a composite that would increase energy absorption in an impact event. The concepts focus on possible effects on crushing strength and energy absorption from changes in fiber characteristics. Fiber strength is indicated to be the major factor controlling composite performance, with fiber size, fiber spacing, and fiber modulus being less important factors.

Eight experimental "golf-ball" wound carbon composite specimens featuring principally controlled variations in fiber strength were obtained from HITCO. These are described in Table 1. Three specimens, Nos. 4, 5, and 6, were made from fibers having different properties than those of the usual Thornel-50 WYG130 fiber. Four specimens, Nos. 1, 2, 3 and 8, were made with Thornel-50 fiber, with Nos. 1 and 8 the same as the standard MHW impact member except that the No. 1 had a wall approximately 0.1 inch thicker than standard, and Nos. 2 and 3 wound looser and tighter, respectively, than the standard. No. 7 had a duplex fiber structure with the inner wall of weaker WYB85 fiber and the outer wall of high-strength Thornel-75 WYL160 fiber.

All the specimens were initially made in the form of complete spherical shells, and then each was cut along a diameter into two halves. One half, i.e., a hemispherical shell, from each of the eight specimens has been tested to date. All except No. 7 were tested using a fitted steel mandrel inside the shell and a flat steel platen outside of the shell, as shown in Figure 1. No. 7 was tested using a plastic mandrel inside and a steel platen outside. The steel and plastic mandrels are used as simulants for "hard" and "soft" fuels, respectively.

The load-deflection curves recorded for each test are given in Figure 2. The area under the curve provides the energy-absorption data, which are reported along with fracture loads in Table 2.

* Informal Battelle letter report for March, 1976.

TABLE 1. SPECIAL CARBON COMPOSITE IMPACT-MEMBER SPECIMENS

Fiber Type	Fiber Properties		Fiber Diameter, μm	Precursor	Specimen Density, g/cc
	Longitudinal Modulus, $E, 10^6 \text{ psi}$	Tensile Strength, $\sigma_f, 10^3 \text{ psi}$			
1. Standard + 0.2-inch diameter	57	315	5-9	Rayon	1.18
2. Standard, decrease tension	57	315	5-9	Rayon	1.17
3. Standard, increase tension	57	315	5-9	Rayon	1.26
4. WYB85	6	90	8.9	Rayon	1.02
5. Thornel 75 WYL160	78	380	6	Rayon	1.27
6. Thornel 300 WYB90	33	385	3.7	PAN	1.19
7. Thornel 75 (25% WYB85 (75%))	A s A b o v e			Rayon	1.13
8. Standard (Thornel 50 WYG130)	57	315	5-9	Rayon	1.25

FIGURE 1. LOADING APPARATUS FOR ENERGY-ABSORPTION TESTS OF SPECIAL COMPOSITES

FIGURE 2. LOAD-DEFLECTION CURVES OBTAINED IN MULTIPLE FRACTURE TESTS ON SPECIAL CARBON COMPOSITES

TABLE 2. FRACTURE LOADS AND AMOUNTS OF ENERGY ABSORBED IN TESTS OF SPECIAL CARBON COMPOSITE SPECIMENS

Specimen Number	Fracture Load, lb	Energy Absorbed, in.-lb
1	31,300	9,750
2	25,800	6,200
3	28,400	6,700
4	16,300	4,500
5	34,200	6,600
6	25,200	5,700
7	8,520	2,000
8	27,600	7,000

The following general conclusions can be drawn from the above tests where the steel mandrel was used:

- Longitudinal moduli of the fibers do not significantly affect the initial portion of the load-deflection curves, i.e., initial energy absorption. It may be that the small differences observed in this regime are due to differences in transverse moduli which are difficult to assess.
- Fiber strength appears to have a significant effect on crushing strength of the composite; maximum loads ranging from ~ 16 to 34×10^3 pounds were obtained (a value of 8×10^3 pounds for No. 7 was obtained when a plastic mandrel was used), with increased fiber strengths giving greater maximum loads. The energy absorbed, however, did not necessarily increase with increased fiber strength, as evidenced by Nos. 5 and 8.
- For fibers of constant strength, composite crushing strength increases with increased fiber modulus (compare Nos. 5 and 6). However, the lower strength of No. 6 could have resulted because of smaller fiber diameter or different fiber type, i.e., PAN vs. rayon.

- The rate of load decay after the maximum load appears to be related to the fiber strength; the higher the strength the faster the load decay.
- Tighter winding, resulting in a higher density composite, gave both higher crushing strength and energy absorption (compare Nos. 2 and 3).
- Increased wall thickness appears to cause a significant increase in both the fracture load and the energy absorbed. However, the shape of the load-deflection curve is relatively unaffected by differences in wall thickness (compare Nos. 1 and 8).
- In all tests with the steel mandrel, progressive crushing occurred, and the post-mortem appearances of all specimens were the same as reported earlier*.

Specimen No. 7 was tested using the plastic mandrel. The purpose of this test was to determine whether energy absorption by crushing can be achieved with a soft fuel through the use of a layered composite made by successive winding with fibers having different strengths. Crushing did not occur in a similar test on a standard MHW impact member**. In the present test, also, very limited crushing was observed. This occurred in the region designated "A" on the load-deflection curve for Specimen 7 in Figure 2. The specimen fractured from hoop tension, as shown in Figure 3.

It is apparent from the above tests that if the fuel is hard and unyielding an impact member made with a relatively strong, high-modulus fiber should be preferred. However, very high strengths of fibers should be avoided because the rate of load relaxation after the maximum load is reached and the amount of energy absorption are adversely affected. At present, it is not possible to quantitatively determine effects of variables other than wall thickness on energy absorption. Calculations indicate that the energy absorbed varies linearly with the amount of crushed material in the test if all other

* Figures 6(a) and (b), Informal Battelle letter report for July-August, 1975.

** Informal Battelle letter report for December, 1975.

9199

FIGURE 3. SPECIMEN 7 AFTER TESTING USING A PLASTIC MANDREL

variables are kept the same. For example, the crushed volumes of Nos. 1 and 8 were found to weigh 25.5 and 19.0 grams, respectively, and the bulk densities of Nos. 1 and 8 were 1.18 and 1.26 g/cm³, respectively. This gives crushed volumes of 1.32 and 0.92 in.³. The ratio of the two volumes is, therefore, 1.43. This is in good agreement with the ratio of 1.39 observed between the energy absorbed in the two specimens (Table 2).

Effect of Elevated Temperature on Energy
Absorption by Impact-Member Materials
 (J. H. Peterson)

During the past quarter the elevated-temperature test apparatus was assembled and checked out, and the testing of impact-member materials at temperatures up to 2500 F was initiated. Previously, energy absorption in these materials had been measured only at room temperature.

Tests were conducted on MHW impact shells made from "golf-ball" wound Thornel 50 yarn and on shells of the same size and shape made from ATJ bulk graphite. As expected, initial test results indicated no appreciable effect of temperature on energy absorption or fracture mode in these materials.

The basic test configuration is the same as that used earlier for the room-temperature tests of MHW impact shells. The test utilizes an internal mandrel with a spherical radius matching that of the impact member. A flat platen applies the load to the external surface at a uniform cross-head movement of 0.05 in./min. In the room-temperature tests, either a hardened steel mandrel or a deformable polyethylene mandrel has been used in conjunction with a steel platen.

For the elevated-temperature tests, KT^(R)-grade silicon carbide tooling procured from the Carborundum Company was utilized. A ring at the maximum diameter of the shell specimen provided lateral restraint as previously shown in Figure 1. The specimen did not have the bottom support. A description and sketch of the test apparatus were given in the July-September, 1976, quarterly report (BMI-X-672).

A summary of the data obtained is given in Table 3, and Figure 4 shows the load-deflection curves for each of the specimens tested. The ATJ graphite specimen tested at 2500 F failed catastrophically at a maximum load of 4800 pounds, and the results essentially duplicated those from a similar

FIGURE 4. ELEVATED- AND ROOM-TEMPERATURE LOAD-DEFLECTION CURVES FOR IMPACT-MEMBER MATERIALS

TABLE 3. RESULTS OF ELEVATED-TEMPERATURE TESTS OF IMPACT SHELLS

Specimen	Test Temperature, F	Total Energy Absorbed, in.-lb	Maximum Load, lb
ATJ graphite	2500	110	4,800
8B (a)	1500	4800	25,400
9B (a)	2400	5100	27,000
9A (a)	Room	5200	28,100

(a) "Golf-ball" wound MHW impact shell; Thornel 50 fiber; one-half sphere.

test conducted at room temperature. None of the elevated-temperature tests of MHW impact shells gave significantly different results from room-temperature tests. The maximum load and total energy absorbed were about the same at each test temperature. The total energy absorbed in these tests is somewhat less than in some earlier tests because no bottom support was provided for the hemispherical specimen. The absence of bottom support also altered the load-deflection curve.

To substantiate tests conclusions, tests were performed with and without bottom support on two half-spherical sections of the same material at room temperature. The specimens were "golf-ball" wound Thornel 50 fiber with a wall thickness 0.10 inch greater than the standard MHW impact shell. Although the greater thickness results in higher maximum loads and greater energy absorption than the standard shell, the expected relative results from the two tests were obtained, as indicated by the load-deflection curves shown in Figure 5. Up to the maximum load very little difference between the two tests is noted, but from this point on, the half shell with the bottom support exhibits a greater energy absorption. Both of these tests were performed using a ring at the maximum diameter to provide lateral restraint and a hardened steel internal mandrel.

The data obtained to date from the elevated-temperature tests indicate that there is no significant effect of temperature on energy-absorption characteristics. Additional tests are being conducted to substantiate these conclusions, and then a topical report will be prepared on the effects of elevated temperature.

FIGURE 5. COMPARISON OF ENERGY ABSORBED WITH AND WITHOUT BOTTOM SUPPORT OF IMPACT-MEMBER MATERIAL

Instrumented High Loading Rate Test
for Impact-Member Materials
(B. D. Trott)

The objective of this test is to develop force-deformation data on present and future impact materials for use in assessing rate effects to ~300 fps on energy-absorption mechanisms. It is planned that the test will be conducted at room temperature to provide relative rankings of the materials. The data obtained from this test will be correlated with the "static" force-deformation tests being conducted at both room and elevated temperatures described separately.

The planned test is not simply an ultra-high strain rate test, but rather an impact simulation test which will duplicate the strain rates occurring at simulated ground impact of a reentering heat source. As such, impacts in the range of expected reentry impact velocities will initiate the generation of force-deformation data. During this initial phase of the impact, strain rates are very high and fast response instrumentation is required, and unwanted stress wave reflections must be avoided. During the final phase of the impacting body, relative motions cease and lower strain rates occur. To date, all high strain rate tests of impacting materials have been performed on a pass-fail basis only due to the unavailability of such a sophisticated apparatus in this country. The ability to perform such tests should be of great significance to future NRA activities.

Work on this new test was initiated during the previous reporting period. During that period, the test design was established and ~95 percent of the parts were fabricated. During the present reporting period, the equipment was set up and a number of calibrating shots were fired but no data shots were obtained due to a failure of the capacitance motion transducer. A new motion transducer is being developed which should be operational during the next reporting period.

In the following sections the analysis for the launcher design, the launcher performance, the experimental study to determine reasons for failure of the capacitance motion transducer, and a description of the principle of operation of the new optical motion transducer are given.

Launcher Design. As described in the previous quarterly report*, the projectile launcher selected was a short-barreled, compressed-gas-operated gun equipped with vents milled into the barrel near the muzzle. These vents allow the escape of the accelerating gas from behind the projectile so that the last inches of projectile travel prior to impact with the test material will occur at essentially constant velocity. The required vents were found to be of sufficient length from simple considerations that the projectile acceleration during venting of the gases had to be considered in establishing the required vent size, and, hence, the overall launcher design. The following analysis is divided into two parts--acceleration of the projectile before venting begins, and acceleration of the projectile during venting. A nomenclature glossary is included for reference as Appendix A to this report.

For the initial acceleration, the initial conditions assumed were as shown schematically in Figure 6(a). The assumptions made in the analysis were

- (1) No friction between projectile and barrel
- (2) No heat exchange between launch gas and barrel or projectile
- (3) No initially unpressurized volume between projectile and accumulator
- (4) Diaphragm between projectile and accumulator breaks open completely at time, $t = 0$
- (5) No choking of flow from accumulator to barrel and projectile base
- (6) Accumulator gas is an ideal gas.

The expression governing the adiabatic expansion of an ideal gas is

$$PV^\gamma = k \quad . \quad (1)$$

The volume of the gas at any time is given by

$$V = V_{AC} + A_B X \quad . \quad (2)$$

* "Heat Source Component Development Program, Quarterly Report for July-October, 1976", BMI-X-672, October, 1976.

a. Projectile Stationary

b. Vent Configuration

FIGURE 6. LAUNCHER SCHEMATIC GEOMETRIES

The differential equation of motion of the projectile is given by

$$\frac{d^2 X}{dt^2} = \frac{A_B}{M_P} (P - P_A) \quad . \quad (3)$$

From Equations 1 and 2, the equation of motion of the projectile may be written as

$$\frac{d^2 X}{dt^2} = \frac{A_B}{M_P} \left(\frac{P_{AC} V_{AC}^\gamma}{(V_{AC} + A_B X)^\gamma} \right) \quad . \quad (4)$$

Equation (4) does not appear to be directly integrable. Hence, a finite difference approach was adopted to obtain an approximate solution. The pressure was assumed to remain constant at its initial value during a time increment Δt , during which the initial projectile motion is given by

$$a_i = M_P (P_{AC} - P_A) / A_B \quad (5)$$

$$U_i = a_i \Delta t \quad (6)$$

$$X_i = a_i \Delta t^2 / 2 \quad (7)$$

$$P_i = C_1 / (V_{AC} + A_B X_i)^\gamma \quad (8)$$

$$T_i = \Delta t$$

Using these initial values, the subsequent projectile motion was calculated from

$$a_i = M_P (P_{i-1} - P_A) / A_B \quad (9)$$

$$U_i = U_{i-1} + a_i \Delta t \quad (10)$$

$$X_i = X_{i-1} + (U_{i-1} + a_i \Delta t / 2) \Delta t \quad (11)$$

$$T_i = T_{i-1} + \Delta t \quad (12)$$

$$P_i = C_1 / (V_{AC} + A_B X_i)^\gamma \quad (13)$$

The above equations were programmed for computer evaluation over a large number of small time steps. In various versions of the program, the calculation was arranged to terminate after either a preselected velocity or projectile travel was reached for parametric evaluation of the effects of projectile

mass, accumulator volume, accumulator pressure, and barrel length. In the final version of the program, venting was allowed to begin after a prescribed distance of travel without venting X_{NV} .

Because a fixed time increment was used, the following equations were necessary to adjust the calculated values to those corresponding to the projectile position X_{NV} . The last set of values calculated for time step i_0 were such that $X_{i_0} > X_{NV} > X_{i_0-1}$. The following equations were the FORTRAN expressions used. As such, their interpretation is that the current values of variables on the right of the equals sign are used to calculate a new value for the variable on the left of the equals sign.

$$D = (X_{NV} - X_{i_0-1}) / (X_{i_0} - X_{i_0-1}) \quad (14)$$

$$T_{i_0} = T_{i_0-1} + \Delta t D \quad (15)$$

$$U_{i_0} = U_{i_0-1} + (U_{i_0} - U_{i_0-1}) D \quad (16)$$

$$P_{i_0} = P_{i_0-1} + (P_{i_0} - P_{i_0-1}) D \quad (17)$$

$$X_{i_0} = X_{NV} \quad (18)$$

The first step employed after initiation of venting was a fractional time step $(1-D)\Delta t$. During this time step, no venting was allowed and expressions 9 through 13 above were used except substituting $(1-D)\Delta t$ for Δt . At the end of this first venting time step, a vent area was calculated for use in subsequent calculations.

The vent configuration used in the final version of the program was as shown in Figure 6(b). Two sets of four each identical vents were used, corresponding to the exact configuration finally machined in the gun barrel. Expressions were derived for the vent area opened by the passage of the projectile through the vents, utilizing the geometry shown, so that once a projectile position was calculated the appropriate vent area could be calculated using these expressions. These expressions are not reproduced here due to their lengthy nature and lack of theoretical interest.

Thus, at the end of the first fractional time step with venting, a venting area α_1 was calculated for use in subsequent calculations of venting.

The analysis of projectile motion with simultaneous venting began with the ideal gas law:

$$P = mR\phi/VM \quad . \quad (19)$$

Differentiation of Equation (19) with respect to time yielded:

$$\frac{dP}{dt} = \frac{R}{VM} \left[\phi \frac{\delta m}{\delta t} + m \left(\frac{\delta \phi}{\delta t} - \frac{\phi}{V} \frac{\delta V}{\delta t} \right) \right] \quad . \quad (20)$$

To evaluate the time rate of change of pressure, expressions were thus needed for the mass, temperature, and volume, as well as their rates of change. The rate of mass loss was obtained from ⁽¹⁾

$$\frac{\delta m}{\delta t} = - \frac{C_D \alpha P f(\gamma)}{(R\phi/M)^{1/2}} \quad . \quad (21)$$

The temperature was obtained from the consideration that all expansion in the barrel is adiabatic. Thus,

$$Pv^\gamma = k \quad . \quad (22)$$

From the ideal gas law, the gas specific volume was given by

$$v = R\phi/MP \quad , \quad (23)$$

which substituted in Equation (22) yielded

$$Pv^\gamma = (R/M)^\gamma \phi^\gamma P^{1-\gamma} = k \quad , \quad (24)$$

which was put in more useful form by utilizing the initial accumulator conditions:

$$\phi^\gamma P^{1-\gamma} = \phi_{AC}^\gamma P_{AC}^{1-\gamma} \quad (25)$$

which, solved for the temperature, become

$$\phi = \phi_{AC} (P_{AC}/P)^{(1-\gamma)/\gamma} \quad . \quad (26)$$

Equation (26) was differentiated with respect to time, yielding

$$\frac{\delta \phi}{\delta t} = \phi_{AC} P_{AC}^{[(1-\gamma)/\gamma]} \left(\frac{\gamma-1}{\gamma} \right) P^{-1/\gamma} \frac{dP}{dt} \quad . \quad (27)$$

Equations (21), (26), and (27) were substituted into Equation (20) yielding, after some manipulation, the following expression:

$$\frac{dP}{dt} = \frac{-\frac{G}{V} \left[B\alpha P^{[(3\gamma-1)/2\gamma]} + \frac{mP^{[(\gamma-1)/\gamma]}}{V} \frac{dV}{dt} \right]}{1 - Gm(\gamma-1) P^{-1/\gamma}/\gamma V} \quad (28)$$

where

$$G = R\phi_{AC} P_{AC}^{[(1-\gamma)/\gamma]}/M \quad (29)$$

$$B = C_D f(\gamma) R^{-1/2} M^{1/2} \phi_{AC}^{-1/2} P_{AC}^{[(\gamma-1)/2\gamma]} \quad (30)$$

Examination of Equation (28) showed that all the remaining variables could readily be calculated by finite difference equations, and Equation (28) itself could easily be converted to finite difference form. Thus, values for the variables were calculated using the following derived finite difference equations for each new time step, i .

$$P_i = P_{i-1} - \frac{\frac{G}{V_{i-1}} \left[-B\alpha_{i-1} P_{i-1}^{[(3\gamma-1)/2\gamma]} + \frac{A_{B,i-1}^m P_{i-1}^{[(\gamma-1)/\gamma]}}{V_{i-1}} \right] \Delta t}{1 - (Gm_{i-1}) (\gamma-1) P_{i-1}^{-1/\gamma}/\gamma V_{i-1}} \quad (31)$$

$$X_i = X_{i-1} + U_{i-1} \Delta t + A_B [(P_{i-1} + P_i)/2 - P_A] \Delta t^2 / 2M_P \quad (32)$$

α_i was calculated from one of several equations using the coordinate X_i to describe the geometrical vent area open at time step i .

$$\Delta\alpha_i = \alpha_i - \alpha_{i-1} \quad (33)$$

$$U_i = U_{i-1} + A_B [(P_{i-1} + P_i)/2 - P_A] \Delta t / M_P \quad (34)$$

$$V_i = V_{AC} + A_B X_i \quad (35)$$

$$\phi_i = \phi_{AC} (P_{AC}/P_i)^{[(1-\gamma)/\gamma]} \quad (36)$$

$$m_i = m_{i-1} - C_2 (\alpha_{i-1} + \Delta\alpha_i/2) (P_i + P_{i-1}) \Delta t / 2\phi_i^{1/2} \quad (37)$$

$$T_i = T_{i-1} + \Delta t \quad (38)$$

The above equations were programmed in FORTRAN for computer evaluation along with the previous equations. In the final version of the program, called GUNVENT, the calculations were terminated after the projectile traveled at least four inches beyond the end of the vents. Calculated values of the time of flight, projectile travel, projectile velocity, pressure behind the projectile, and gas temperature behind the projectile were printed out. Typically, twenty values before venting began and fifteen values after venting began were printed. Using a 0.1-msec time step, this corresponded to every fourth time step before venting and every second time step after venting began. The results were not highly sensitive to the size of time step employed. Each computer run of the program required ~0.25 sec per case examined. During the design study, a total of perhaps 50 combinations of input parameters were examined to settle on the final launcher design and evaluate some of its capabilities as given in the previous progress report.

Launcher Performance. The detailed design of the launcher includes two rupture diaphragms of Mylar sheet plastic between the accumulator volume of pressurized gas and the initial projectile position. These diaphragms are separated by approximately one inch and each is individually O-ring sealed. In operation, the volume between the diaphragm is pressurized to one-half the final pressure desired in the accumulator before the accumulator is finally pressurized. Thus, the pressure drop across each diaphragm is one-half the accumulator pressure. The thickness of the Mylar diaphragms is chosen such that an individual diaphragm will rupture at a pressure greater than one-half the desired accumulator pressure and less than the total pressure.

After the desired accumulator pressure is attained and stabilized, the gun is fired by venting the pressure between the two diaphragms. The two diaphragms are then rapidly successively ruptured and the accumulator pressure is applied to the rear of the projectile.

For the 2.50-in. accumulator and barrel inside diameters used, Table 4 shows the observed rupture pressures for several thicknesses of Mylar diaphragms. Except where two pressure values appear, these are single test results. The pressure was increased slowly by bleeding in gas (pure dry nitrogen) from a high pressure bottle and observing the rupture pressure on a large Bourdon pressure gage with 2 psi divisions. In two cases shown in Table 4, two sheets of Mylar were loaded together to form a single diaphragm.

TABLE 4. MYLAR DIAPHRAGM RUPTURE PRESSURES

Number of Sheets	Mylar Thickness, in.	Rupture Pressure, psi
1	0.003	72
1	0.005	119, 120
1	0.007	153
1	0.005	204
1	0.003	
2	0.005	265

Thus, over the range of accumulator pressures used, several diaphragm thicknesses or combinations were used.

Projectile velocities were measured using three 0.031-in.-diameter brass pins extending into the launcher barrel ~ 0.05 in., located 0.3 in. apart in the last 1.1 inches of the barrel. Distances between the trigger pins were measured with aid of a fixture on a depth micrometer. The trigger pins were attached to an established* pulse-forming circuit to trigger two electronic time interval meters. The two velocities so measured normally agreed within 0.3 percent.

The observed velocity performance of the gun is shown in Figure 7, together with the predicted performance curve. The predicted performance curve exceeds the observed performance for several reasons, the principal ones of which are probably that no account was taken in the calculations of the following.

- (1) The unpressurized (or low pressure) volume between the first diaphragm and the inside of the cup-shaped projectile. This amounts to ~ 10.5 in.³ which is 17.5 percent of the initial accumulator volume of 60 in.³.
- (2) Projectile-barrel friction.

* Ingram, G. E., and Graham, R. A., Fifth Symposium (International) on Detonation, Pasadena, California, August, 1970, ACR-184, Office of Naval Research, Department of the Navy, Arlington, Virginia, p 369-386.

FIGURE 7. LAUNCHER PERFORMANCE

- (3) Failure of the Mylar diaphragms to rupture completely open. Not infrequently, only perhaps one-half the bore area is opened on diaphragm rupture.

Despite the discrepancy between calculated and observed performance, the launcher has met the velocity objectives quite easily and could reach appreciably higher velocities as the initial accumulator pressure could be raised well above the values currently required. As observed by the small scatter of the experimental velocity points on Figure 7, it appears that velocity dispersion will not be a problem with this launcher design.

Capacitance Motion Transducer. Considerable effort was expended on the debugging of the capacitance motion transducer described in the previous progress report. Problems associated with achieving sufficient response time, transient recovery time, stability, and signal-to-noise ratio were found and solved by various circuit modifications. In addition, problems occurred with operational amplifier burnout and resupply. When these problems were all solved satisfactorily such that very good performance of the circuit was indicated as judged by its response to input waveforms from a signal generator which duplicated those expected in practice, some of the recorded signals from test firings showed large, initially mysterious transient signals which obscured the desired record. Other shots showed simply excessive noise levels.

The source of these large signals was eventually traced to an effect known as the triboelectric effect. This is the well-known generation of static charges on the surfaces of dielectric materials by mechanical rubbing. Confirming electrometer measurements showed residual surface potentials on the order of 1000 volts were generated by passage of the projectile on the interior surface of the nylon dielectric insert used in the last four inches of the launcher to form the variable capacitor between the projectile and the launcher. These residual surface charges, generated in a nonreproducible pattern, were so large that they caused charge redistributions in the measuring circuit at least an order of magnitude bigger than the calculated signal level for motion measurement.

The only reliable solution to this problem appeared to be milling back the nylon so no projectile rubbing could occur, together with modifications to allow the insertion of metal slides to guide the projectile to impact. These slides would have to be electrically isolated from the launcher barrel and

electrically grounded. The potential unreliability and associated costs with these modifications lead us to abandon the capacitance approach in favor of an optical approach described in the next section.

Optical Motion Transducer. The backup approach to the capacitance motion transducer design considered initially was an optical system. Active investigation of this system was begun late in the reporting period when it was determined that modifications to make the capacitance motion transducer operable were found to be difficult and costly.

The optical transducer design is shown schematically in Figure 8. This should be regarded as preliminary pending confirming breadboard studies to establish the required response which are currently in progress.

Energy Absorption by Bulk Graphite
(G. K. Bansal and W. H. Duckworth)

Of interest to the problem of developing the preferred impact material are the data recently reported by LASL from impact tests of copper balls contained in Carbocell, golf-ball wound Thornel 50 composite, and bulk graphite shells, all in the MHW impact shell dimensions. The LASL data revealed less deformation in the copper ball encased in bulk graphite than in either of the two carbon-carbon composites.

To examine whether bulk graphite provides similar protection for an unyielding (hard) and a yielding (soft) fuel simulant in static tests, two shells of ATJ graphite were machined from solid blocks to standard MHW impact shell dimensions. The shells were tested at room temperature in an Instron machine at a cross-head speed of 0.05 inch per minute using an apparatus similar to that shown earlier in Figure 1. One shell was tested with a steel mandrel and the other with a plastic mandrel. The load-deflection curves obtained in the two tests are shown in Figure 9. The general fracture appearances of both shells after the tests were as shown in Figure 10 for the case of the steel mandrel.

As hypothesized in our last quarterly report^{*}, in both tests a characteristic conical fracture initiated at the contact point. Also, the

* BMI-X-672, "Heat Source Component Development Program--Quarterly Report for July-September, 1976", October, 1976.

FIGURE 8. OPTICAL MOTION TRANSDUCER SCHEMATIC

FIGURE 9. MULTIPLE-FRACTURE TESTS ON GRAPHITE SHELLS

9016

FIGURE 10. ATJ GRAPHITE SHELL AFTER TESTING WITH STEEL MANDREL

post-mortem examination of the plastic mandrel (Figure 11) revealed deep striations, in the form of dents, emanating from the contact point which itself was severely indented by the graphite.

The energy absorption in each test was quite small and no significant crushing of the graphite was observed in either test.

On the basis of these results, it appears that the indicated good performance of the bulk graphite as an energy absorber in the LASL tests was associated with strain hardening characteristics of the copper in the dynamic test, coupled with formation of a graphite "plug" at the impact face due to brittle (catastrophic) fracture early in the impact event, with the plug subsequently entrapped and crushed. Whether $^{238}\text{PuO}_2$ fuel exhibits similar strain hardening needs to be determined, suggesting that service-type impact tests of bulk graphite shells encasing iridium-clad fuel should be conducted by LASL. Without a significant strain hardening effect in the fuel (which seems unlikely for $^{238}\text{PuO}_2$), the behavior of bulk graphite as an energy-absorbing member is predicted to be unsatisfactory. On the other hand, if the fuel strain hardens like copper, rupture of iridium cladding from local indentation of the plug is indicated as a possibility when using bulk graphite.

9337

FIGURE 11. PLASTIC MANDREL USED IN TEST OF ATJ GRAPHITE SHELL

IMPROVED REENTRY MATERIALS
(I. M. Grinberg - Task Leader)

Introduction

The objective of this program is to expand and improve on the understanding of the reentry behavior of carbon-carbon composites for isotope heat sources and then to select or develop an improved material for this application. If possible, this material will serve the function of impact protection as well, so that the overall goal of a bifunctional material development can be achieved.

Task activities are being conducted such that the recommended heat shield or bifunctional material can be applied to the General Purpose Heat Source (GPHS) which is being designed by LASL.

Shown below are the major efforts being conducted as part of this task along with a brief description of the nature of each activity.

- Evaluate Bifunctional Component Configurations. The objectives of this activity are to determine the response of bifunctional component configurations for two-dimensional thermal, ablative, and thermal stress performance and to identify critical factors associated with the use of graphite and carbon/carbon materials for heat source heat shield/bifunctional members.
- Surface Chemistry Studies. The overall objective of this effort is to develop an improved criterion for specifying the heat shield/bifunctional member (ablator) thickness. The immediate objective of the work currently in progress is to determine the boundary layer surface chemistry on candidate heat shield members at simulated reentry conditions.
- Develop Carbon/Carbon Composite Failure Criteria. The objective of this effort is to formulate an approach to predict the thermal stress resistance of C/C composites for isotopic heat source heat shield/bifunctional members and define suitable failure criteria for these materials.

Additional task activities to be implemented are

- Select ablation materials for heat source heat shield
- Verify ablation response models
- Select bifunctional materials concept

- Fabricate/procure bifunctional material
- Verify bifunctional material
- Interface with GPHS systems designer.

The time schedule for the conduct of these activities leading to the development of the heat shield/bifunctional member of the GPHS is being reviewed to ensure technology readiness of this heat source component by January, 1978.

Summary

The efforts in this task are directed toward several related studies which are described in subsequent sections. A major effort was devoted to redirecting programmatic effort toward the GPHS, especially toward smaller fuel form sizes. Modifications of computer logic to permit improved scanning of containment member temperatures and selected interface temperatures were undertaken. Additional configuration analyses were performed. Evaluation of various codes for thermal stress analyses capabilities was completed. Substantial progress was also made on assembly of the experimental apparatus to investigate the surface oxidation chemistry under conditions representative of heat source reentry.

Work This Period

Evaluate Bifunctional Component Configurations (I. M. Grinberg, et al.)

The effort to Evaluate Bifunctional Component Configurations is organized into four subtask activities as follows:

- Development of thermal/ablation models for use in configuration and thermal stress studies
- Configuration sensitivity studies to identify preferred materials/heat source shapes
- Two-dimensional thermal/ablation analyses for input to thermal stress studies
- Two-dimensional thermal stress studies.

Work on these subtask activities has been completed or is in progress as is reported below.

Thermal and Ablation Response Models (G. R. Whitacre). The development of the thermal and ablation response models for isotope heat source entry has been essentially completed. The various two-dimensional options available are described in the previous quarterly report^{*} and are shown in Figure 12.

During this quarter, discussions were held with LASL, Johns Hopkins University-Applied Physics Laboratory, ORNL, Teledyne, and ERDA personnel concentrating mainly on the General Purpose Heat Source (GPHS) requirements, concepts, and scheduling. It appears that the available 2-D options of the thermal and ablation response model will be adequate for initial work on GPHS configurations. However, some three-dimensional thermal analyses may be required on the final configuration to determine corner effects.

Because of problems associated with the fabrication and maintenance of integrity of large fuel forms (due to internal thermal stresses) and the requirement for modularity, the future work on GPHS will be on smaller size fuel forms. A typical size might be 25 watts_(t), although several of these encapsulated fuel forms could be included in one reentry module. A thermal stress proof test of the actual reentry module will probably be required, especially if failure criteria for carbon/carbon cannot be established. Thus, the size of the GPHS C/C heat shield will also be limited by the size of available test facilities. The current two-dimensional thermal stress study is being completed using the 150-watt baseline configuration. This allows direct comparison with previous material sensitivity, screening, and one-dimensional thermal stress studies. However, future work will shift to the consideration of smaller size heat sources.

The temperature of the containment member, the iridium, iridium alloy, or Pt 3008 shell, is significant--perhaps critically significant--to the proper functioning of this member. During this quarter, preparation has begun of computer logic to scan the containment member temperature during reentry response analyses and record the maximum and minimum temperatures at each print interval. At the conclusion of the run, a dual-plot containment member temperature, both maximum and minimum, as a function of time will be made. These efforts should be completed during the next quarter.

The ablation response model will also be improved when data and correlations of mass loss and heat addition become available from the surface

* BMI-X-672, "Heat Source Component Development Program--Quarterly Report for July-September, 1976", October, 1976.

FIGURE 12. THERMAL AND ABLATION RESPONSE MODEL OPTIONS

chemistry experiments. With this refinement, it should be possible to reduce the requirement that the ablator thickness be twice the predicted maximum recession.

Configuration Sensitivity Studies (A. A. Boiarski). Configuration sensitivity studies were conducted during the previous quarter to determine potential advantages and/or materials problems resulting from use of spherical, cylindrical, and disk heat source shapes. In general, it was found that the nonspherical heat source configurations would experience lower impact velocities than the spherically shaped heat sources due to their higher subsonic drag characteristics. Results of these studies were presented in the last quarterly report. Also, it was found that weight savings could be realized in using nonspherical heat source configurations; however, metal containment temperatures at impact* were lower than anticipated iridium and iridium alloy embrittlement temperatures for these cylindrical and disk-shaped heat source shapes. Discussions regarding the metal containment member temperature at impact were held with TES since it is expected that the LCHPG heat source configuration would impact at low velocities.

TES uses a Pt 3008 platinum alloy in the LCHPG design to avoid metal containment embrittlement problems encountered with low impact velocity shapes. However, the Pt 3008 alloy melts at a lower temperature than iridium so TES includes a pyrolytic graphite insulation member around each of their 25 W_t heat sources that make up the 100 W_t reentry capsule. Hence, the use of an insulating sleeve will probably be required for low impact velocity shapes for either containment member material. This additional material will certainly add to the total heat source weight and thus negate some of the advantages of these nonspherical shapes.

During the current reporting period, additional configuration sensitivity analyses have not been conducted. Further analysis efforts in this activity will be initiated following generation of results of the two-dimensional thermal stress analyses in progress and the development of preliminary GPHS designs by LASL expected July 1, 1977. As appropriate, analyses will be conducted to evaluate the weight penalty that must be paid to ensure containment material integrity for the nonspherical-shaped heat source configurations.

* For $\gamma = 5.3^\circ$, $V_I = 36,000$ ft/sec maximum recession trajectory.

In the bifunctional materials area, some efforts will be expended to further evaluate the sensitivity to materials property variations of the C/C material concept which utilizes a golf-ball-wound, graded-density structure to obtain a spherical bifunctional ablator/impact member. Two-dimensional heating effects will also be examined for this bifunctional concept. Finally, nonspherical shapes may be considered that utilize this graded bifunctional design.

Two-Dimensional Thermal/Ablation Analyses (G. R. Whitacre and R. E. Hess). During this quarter, the detailed, two-dimensional, thermal/ablation response of a cylindrically shaped heat source configuration utilizing a POCO-AXF-5Q heat shield was determined in support of the thermal stress studies. Initial reentry conditions were selected that would result in the development of peak thermal stress conditions in the heat shield. These conditions, $\gamma = -89.9^\circ$, $V_I = 36,000$ ft/sec, have been used in previous analyses by Battelle to determine the thermal stress resistance of bulk graphites and C/C composites using one-dimensional analysis techniques. They will also be used in the current effort on determining the two-dimensional thermal stress response of spherical, cylindrical, and disk-shaped heat source configurations.

Figure 13 shows the baseline cylindrical configuration which was used for the thermal/ablation analysis and the thermal stress analysis. This configuration was analyzed in the side-on-stable mode with a two-dimensional convective heating distribution. Sizing of the heat shield member thickness was based on successful reentry of the heat source of a $\gamma = -5.3^\circ$, $V_I = 36,000$ ft/sec trajectory. The rationale for selecting the RIM thickness was discussed in the previous quarterly report. Transient temperature and shape profile results from these analyses were used in the thermal stress predictions.

The matrix of heat source configurations to be studied in these two-dimensional thermal/ablation and stress analyses is shown in Table 5. Different configurations were selected to identify the effect of geometry on heat shield thermal stress response and two or more materials are being considered to provide insight into the significance of material properties on thermal stress response. Selection of specific materials and conduct of the analysis are planned for the next reporting period.

FIGURE 13. BASELINE CYLINDRICAL HEAT SOURCE DESIGN

TABLE 5. MATRIX OF TWO-DIMENSIONAL THERMAL STRESS ANALYSIS

Heat Source Configuration*	Reentry Mode	Number of Materials to be Analyzed
Sphere (r, Ψ)	Stable	4
Long cylinder (r, θ)	Side-on stable	2
Short cylinder (r,z)	End on	2
Disk (r,z)	End on	2

* Refer to Figure 12 for identification of symbols.

During this quarter, additional work was performed to improve the transfer of temperature data to the thermal stress code. In the initial efforts, the two-dimensional temperature profiles were defined only by the nodal temperatures, and these temperatures were transferred to a permanent file for subsequent usage. As the work progressed, it was apparent that selected interface temperatures were also needed for adequate interpolations to the thermal stress program. Logic was added to the two-dimensional thermal analysis program to also write these interface temperatures on the permanent file.

A program was written to take data for selected reference times from the permanent file and give a two-dimensional temperature plot. During this quarter, this program was improved by adding logic to give greater flexibility in the selection of viewing angles.

An interpolation program was prepared which provides a punched-card output to be used directly as input data by the thermal stress program. Two interpolation subroutines have been utilized in this program; they are discussed in the following subsection of this report.

Thermal Stress Studies (L. E. Hulbert and J. Strenkowski). Given below is the progress of work conducted during this quarter related to thermal stress analyses of heat source heat shield members. This documentation is organized into two subsections, one devoted to reporting the progress made in conducting the "production" analyses of the heat source heat shield member

configuration thermal stress response (see Table 5 for configurations), and the second devoted to reporting the results of a comparative study performed to evaluate available thermal stress codes and select one for the detailed analyses.

Thermal Stress Analysis of Heat Source Configurations. Previous thermal stress analyses conducted on this program were performed for the purpose of comparing the relative thermal stress resistance of bulk graphites and C/C composites.

For the purpose of comparing these materials under uniform environmental conditions, a heat shield of each material was modeled for the same maximum thermal stress trajectory. Each heat shield was assumed to be spherical with all having the same inner radius. The thickness of each heat shield was determined so that each had the same factor of safety in terms of anticipated recession as experienced for the maximum ablation reentry trajectory. By conservatively assuming that stagnation point temperatures existed uniformly over the entire heat shield surface, both the thermal and thermal stress models became one dimensional. For a preliminary comparison of materials, this approach was adequate. However, these studies shed no light on the interaction of multi-dimensional stress distributions resulting from realistic spherical and nonspherical geometries and the thermal stress resistance of the materials. This interaction is particularly significant in the orthotropic materials.

The purposes of the study currently being conducted are to (1) further evaluate the thermal stress performance of several bulk graphites and C/C composites in both spherical and nonspherical geometries without artificially imposing one-dimensionality, (2) study the interaction of geometry and material properties with respect to thermal stress performance, and (3) determine the validity or degree of conservation of the one-dimensional simplifying assumptions by comparing the results of two-dimensional analyses with the previously reported results of one-dimensional analyses.

The thermal stress analyses are to be conducted using the two-dimensional finite-element code DOASIS. As is mentioned in the following subsection, this code has been selected for its pre- and postprocessors, its efficiency with respect to computer cost, and its capability to handle spherical, cylindrical, or rectangular orthotropic material properties. For this screening

and sensitivity study, the code will be used in the elastic mode, but the capability exists for elastic-plastic analyses.

To use the DOASIS code, the temperature field must be known for each element in the finite-element mesh. These temperatures are found from the thermal analyses, and the time at which the thermal stresses are a maximum (i.e., the critical time) had to be found. Originally it was planned to determine the critical time by visually examining temperature profiles at various times. However, after reviewing the output from several thermal response analyses, it became apparent that this approach did not expose the critical time for the more complicated two-dimensional models. Instead, it was decided to conduct a complete thermal stress analysis for various times. The computational cost would not be prohibitive since only an elastic analysis was planned for these sensitivity studies. In addition, it is known from previous one-dimensional analyses that the maximum stresses and outer surface temperatures usually occur at about the same time during reentry. By computing the stresses immediately before and after the maximum temperature is experienced, the critical time could be determined with just a few runs.

Using the above technique to find the maximum stresses, rapid construction of accurate thermal stress models was needed. This included mesh generation (taking into account the ablation response), and an interpolation scheme to convert the thermal analysis temperatures into element temperatures of the finite-element mesh. Work in this quarter was directed at studying an existing bivariate linear interpolation routine which had been interfaced to the thermal analysis code. This routine was designed to provide temperatures at the finite-element mesh points as needed. To evaluate this scheme, a sample thermal stress analysis was conducted for a POCO AXF-5Q cylindrical heat shield as shown in Figure 13 using the peak thermal stress trajectory initial conditions. The DOASIS-generated finite-element mesh for this case is shown in Figure 14. Unfortunately, the bilinear interpolation scheme led to erroneous interpolated temperatures for this heat shield.

As an alternative to the above interpolation routine, a DOASIS temperature interpolation preprocessor (TEMINT) was investigated. This program is based on a new bilinear scheme which employs formulas of a quadrilateral isoparametric finite element. The advantages of using this preprocessor is that it may be coupled easily to the thermal stress routine, as well as the plotting postprocessor. Figure 15 is a DOASIS-generated contour plot of this interpolated

FIGURE 14. SAMPLE DOASIS FINITE-ELEMENT GRID FOR THE EVALUATION OF THERMAL GRADIENTS IN A SIDE-ON STABLE CYLINDRICAL HEAT SHIELD

FIGURE 15. SAMPLE ISOTHERM PLOT FOR A POCO AXF-5Q CYLINDRICAL HEAT SHIELD DURING A SIDE-ON STABLE REENTRY

temperature distribution. Note that the temperature field may be quickly verified by visually examining this contour plot.

Currently, the procedure to provide data input to the TEMINT routine is being streamlined. When this phase is completed during the first week of the next quarter, the thermal model will be coupled to the thermal stress analysis. Thereafter, the stress analysis of the side-on cylindrical heat shield and the other cases outlined in Table 5 will be completed.

Thermal Stress Response Comparative Study. A comparative study was undertaken to determine the accuracy and efficiency of using the finite-element code DOASIS to perform thermal stress analyses of two-dimensional heat shield members. This code is the result of several modifications which have been incorporated into the original Wilson finite-element code by Weiler Research, Inc.* Basically, the DOASIS code is a two-dimensional program, capable of analyzing generalized plane stress, generalized plane strain, and axisymmetric problems. Several different materials may be treated, all of which may be isotropic, transversely isotropic, or orthotropic. Thermoelastic or thermoelastic-plastic behavior may be modeled, the latter being represented by a deformation theory. In addition, the DOASIS program possesses useful peripheral design capabilities such as a mesh generator, a temperature interpolation preprocessor, and a plotter postprocessor.

In order to evaluate the DOASIS code, a thermal stress analysis was conducted of the LCHPG heat source heat shield in broadside attitude in a super-orbital low-altitude abort trajectory. This problem was chosen because (1) it typified the thermal analyses which were planned, and (2) another comparative stress analysis using this model had been conducted previously**. In that study, the results of two other structural analysis codes, SAAS3 and ANSYS, were compared on the basis of thermal stresses and computational cost. Therefore, by using the LCHPG heat shield in broadside attitude, a comparison between three finite-element codes could be made to determine relative cost and accuracy.

* "DOASIS--A Computer Code for the Deformation Plastic, Orthotropic, Axisymmetric (and Plane) Solution of Inelastic Solids" -- Tech. Rep. AFML-TR-75-37, prepared by Weiler Research, Inc., July, 1975.

** Anderson R., "Comparison of Thermal Stress Results of LCHPG H/S's Heat Shield Between SAAS3 and ANSYS", Teledyne Energy Systems Memorandum LCHPG-RHA-453, May 3, 1976.

An exact reproduction of the TES models (including temperatures and material properties) was used for the DOASIS analysis as described in the following.

The finite-element mesh of the heat shield shown in Figure 16 was generated by MESHGN, a preprocessor of the DOASIS code. The elements along the boundary of this mesh are shown in Figure 16. Both the shield geometry and loading are axisymmetric so that only one-half of the disk needs to be modeled. For this model, 260 quadrilateral elements and 324 nodes were used. The temperature-dependent properties for POCO AXF-Q1 are summarized in Table 6, where the bilinear plastic properties are also given.

The disk was subjected to nonuniform heating, which was a maximum at the leading surface. The temperature field was input via temperatures at all the finite-element nodes. Some of the corresponding element temperatures used are shown in Column 2 of Table 7. Due to these high temperatures, a thermo-elastic-plastic analysis was performed. Note that the effect of ablation was ignored in this model.

Comparative Results. A partial summary of the stress analysis results was tabulated in Table 7. The stresses calculated using the SAAS3 and ANSYS codes were originally given in a previous report^{*}. As a measure of the discrepancy between the stresses of the three codes, a percent difference was calculated using DOASIS as a basis. This percentage is shown in Table 7 to the right of the slash in Columns 4, 5, 7, 8, 10, and 11, where the corresponding value to the left of the slash is the stress calculated using either the SAAS3 or ANSYS code. Note that for small values of the stresses, the percent difference has been omitted since these would reflect the smallness of the stresses, rather than provide a true measure of the discrepancy. As can be seen from Table 7, the DOASIS code usually predicts lower stresses than the SAAS3 program. Note that the maximum discrepancy (~11.2%) occurs for the maximum principal stress. In contrast, DOASIS usually predicted higher stresses than the ANSYS code for maximum principal and hoop stresses. These discrepancies between ANSYS and DOASIS are also substantially more, being as large as 41 and 38.8 percent for σ_θ and $\sigma_{\max}$, respectively. Some of these trends are displayed in Figure 17 which shows hoop stress as a function of axial distance at a radial distance of 0.963 inch. Finally, as a measure of the overall agreement between codes, the

* Anderson, R., Teledyne Energy Systems Memorandum LCHPG-RHA-453, May 3, 1976.

FIGURE 16. FINITE-ELEMENT MESH FOR LCHPG HEAT SHIELD ANALYSIS

TABLE 6. MATERIAL PROPERTIES FOR POCO AXF-Q1**
($\rho = 0.0658 \text{ lb/in.}^3$)

Code	Temperature, F	Modulus of Electricity, 10^6 psi	Poisson's Ratio	Coefficient of Thermal Expansion, 10^6 in./in.-F	Yield Stress, psi	E_P/E_E
A/D	0	1.84	0.152	4.8	6040	0.544
S/A/D	70	1.84	0.152	4.8	6040	0.544
"	1000	1.86	0.173	4.5	6980*	0.544*
"	2000	1.88	0.197	4.6	8020*	0.496*
"	2500	1.90	0.208	4.73	8430	0.453
"	3000	1.65	0.220	4.88	8960*	0.382*
"	3500	1.42	0.232	5.04	9290	0.294
"	4000	1.05	0.244	5.20	8600*	0.200*
"	4500	0.68	0.256	5.38	7020	0.120
"	5000	0.51	0.268	5.55	5150*	0.094*
"	6000	0.29	0.291	5.90	2000*	0.062*
"	7000	0.19	0.315	6.25	2000	0.062
"	8000	0.17	0.338	6.60	2000	0.062
A/D	9000	0.17	0.362	6.95	2000	0.062

* Values used in ANSYS only due to code's limitations.

Code - A $\equiv$ ANSYS, S/A/D $\equiv$ SAAS3, ANSYS, and DOASIS.

E_P/E_E - Ratio of plastic to elastic modulus.

** Anderson, R., Teledyne Energy Systems Memorandum LCHPG-RHA-453, May 3, 1976.

TABLE 7. PARTIAL SUMMARY OF COMPARATIVE RESULTS

Element	Temperature, F	σ_{θ}			$\sigma_{\max}$			$\sigma_{\min}$		
		DOASIS (psi)	SAAS3 (psi/%)	ANSYS (psi/%)	DOASIS (psi)	SAAS3 (psi/%)	ANSYS (psi/%)	DOASIS (psi)	SAAS3 (psi/%)	ANSYS (psi/%)
61	1775	-210	-217/-3.3	-212/-1.0	752	738/1.9	728/3.2	-42	-55/	-39/
65	1785	-495	-492/0.6	-487/1.6	17	17/0.	16/5.9	-686	-674/1.7	-666/2.9
106	1777	525	513/2.3	450/14.3	3065	3053/0.4	2960/3.4	4	1/	0/
110	1785	650	-648/0.3	-674/-3.7	26	24/	24/	-2811	-2796/0.5	-2719/3.3
156	1853	5013	5020/-0.1	4773/4.8	3845	3864/-0.5	3883/-1.0	1361	1396/-2.6	1161/14.7
160	1795	2792	2775/0.6	2035/27.1	30	31/-3.3	-11/	-5532	-5505/0.5	-5434/1.8
161	2180	3121	3210/-2.9	3269/-4.7	3121	3210/-2.9	3232/-3.6	-26	-44/	28/
181	2132	4315	4391/-1.8	4255/1.4	4316	4392/-1.8	4229/2.0	-14	-26/	72/
201	2313	2771	2812/-1.5	2543/8.2	2775	2814/-1.4	2531/8.8	30	23/	35/
221	3341	-8442	-8414/0.3	-8136/3.6	49	44/	971/	-8443	-8414/0.3	-8124/3.8
241	5314	-4718	-4911/-4.1	-5189/10.0	22	7/	-200/	-4718	-4911/-4.1	-5144/-9.0
171	1977	4749	4832/-1.7	4607/3.0	4255	4328/-1.7	3994/6.1	1	-1/	0/
191	2012	5226	5325/-1.9	5046/3.4	4893	4998/-2.1	4686/4.2	2	3/	-1/
211	2321	2426	2493/-2.8	2175/10.3	2252	2341/-4.0	2059/8.6	6	6/0.	5/
231	3456	-9357	-9357/0.	-8435/9.9	1	4/	1023/	-9369	-9338/0.3	-8365/10.7
251	5481	-4166	-4386/-5.3	-4591/-10.2	0	-1/	-226/	-4158	-4374/-5.2	-4588/-10.3
180	1826	3319	3328/-0.3	2208/33.5	200	230/	58/	-5642	-5601/0.7	-5691/-0.9
200	1920	3722	3765/-1.2	2859/36.6	1085	1207/-11.2	469/	-5209	-5154/1.1	-5670/-8.9
220	2329	1621	1709/-5.4	953/41.2	2877	2992/-4.0	1762/38.8	-4132	-4087/1.1	-5126/-24.1
240	3546	-7898	-7890/0.1	-7512/4.9	357	230/	2140/	-3244	-3413/-5.2	-3425/-5.6
260	5608	-3796	-3964/-4.4	-3830/-0.9	151	161/-6.6	246/	-2184	-2248/2.9	-1544/29.3
A.D. * + +			2.2%	11.2%		3.0%	7.5%		1.75%	9.6%

$$* \text{ A.D. } = \sum_{i=1}^n \frac{(Z)_i}{n}$$

FIGURE 17. HOOP STRESS AS A FUNCTION OF AXIAL DISTANCE
AT $r = 0.936$ in.

averaged difference was also computed (A.D. shown in Table 7). Clearly, agreement is best between stresses calculated by the DOASIS and SAAS3 codes.

To obtain the relative efficiency of the DOASIS code, the computed stresses were plotted as a function of the number of iterations until convergence was achieved. Typical convergence is shown in Figure 18 for the maximum principal stress of element 175. As can be seen, convergence is attained after five iterations for both the SAAS3 and DOASIS codes; however, DOASIS computes a more accurate starting value than either SAAS3 or ANSYS. As shown in this figure, the DOASIS results approach an asymptotic value in a sense opposite to the other two codes. This behavior was found to be typical for other stresses at various locations. Finally, it can be seen that the DOASIS results are bounded by the other two codes (for iterations ≥ 3) and are in closer agreement with the SAAS3-computed stresses than ANSYS.

The computational cost corresponding to the three finite-element codes is shown in Table 8. It can be seen that the cost of using the ANSYS code was about 4-1/2 times that of the DOASIS code. A valid comparison between DOASIS and SAAS3 is not possible since different machines were used and the cost basis for the IBM 360 was not given. However, these codes required the same number of iterations so that their costs appear comparable.

TABLE 8. COMPARISON OF COMPUTER TIMES/COST

	Code		
	SAAS3	ANSYS	DOASIS
Machine	IBM 360	CDC 6400	CDC 6400
CP	Not available	1000 sec	180 sec
IO	Not available	79 sec	49 sec
CM	Not available	61580 kws	114 sec
SS	Not available	1590 sec	343 sec
Iterations required	5	7	5
Cost, \$	23	238	52

FIGURE 18. MAXIMUM PRINCIPAL STRESS AS A FUNCTION OF ITERATIONS SHOWING TYPICAL CONVERGENCE

Summary and Conclusions. Based upon the previous results, it is concluded that the DOASIS code is relatively efficient and accurate for performing the two-dimensional thermal analyses of LCHPG heat shield. In addition, this code possesses useful pre- and postprocessors. For example, the plotter postprocessor may be used to construct contour, cut-section, and displacement plots. An example of the displacement plot option for the LCHPG heat shield is shown in Figure 19. With this feature, massive amounts of thermal stress results may be visually examined to quickly determine if the output is reasonable. Finally, the DOASIS code may be readily coupled to the BCL STAGING system. Use of this interactive graphics system allows construction of three-dimensional plots in which the variable of interest may be displayed as a third dimension. A sample of this output is shown in Figure 20, where the radial stress computed at the node points has been plotted across the leading edge of the heat shield. This feature can be used to quickly verify stress output, as well as input data such as temperature profiles.

Surface Chemistry Studies (A. A. Boiarski)

Introduction. The behavior of graphite in reentry environments has been extensively studied, both theoretically and experimentally, for many years. Correlations have been obtained describing the mass loss rate and the heat-transfer rate including mass loss and combustion effects for a variety of trajectories. For example, the mass loss rate relative to the diffusion mass loss rate is shown as a function of wall temperature in Figure 21 for thermochemical equilibrium conditions. As indicated in Figure 21 by the shaded region, there are several regimes that are still now well understood. These questionable regions include the rate-controlled regime and the transition between the diffusion-controlled and rate-controlled regimes of mass loss as a function of wall temperature. The trajectory scenario where the heat source encounters these regions is a combination of low velocity and low altitude where the surface temperature is also low. In this case, the mass loss is high and is dependent upon the surface temperature which controls the reaction rates. Also, the exothermic combustion of carbon in the boundary layer increases heating to the surface above the usual aerodynamic heating or reduces the aerodynamic cooling effects. The amount of heating is not well-known at this time

Exaggerated displacement plot for the LCHPG H/S heat shield.
Displacements exaggerated by a factor = 9.9

FIGURE 19. EXAGGERATED DISPLACEMENTS FOR LCHPG HEAT SHIELD

FIGURE 20. CARPET PLOT OF RADIAL STRESS EMPLOYING THE BCL STAGING SYSTEM

FIGURE 21. GRAPHITE ABLATION

because the amount of CO and CO₂ in the boundary layer is not well-known and the amount of exothermic heat release is highly dependent upon knowing the surface layer chemistry.

The low velocity-low altitude portion of the trajectory is of special concern in the design of heat shields for isotopic power sources since the heat source spends considerable time in the diffusion-, transition-, and rate-controlled mass loss regimes associated with these trajectory conditions. The uncertainty associated with surface oxidation chemistry has led to relatively conservative and somewhat inconsistent assumptions in the specification of RTG reentry member thicknesses. This results in obvious weight penalties. Hence, an attempt is being made to better define the graphite surface oxidation utilizing a new spectroscopic technique called Laser Raman Scattering which directly measures wall chemistry.

Experimental Setup. Results of the feasibility study reported in the previous quarterly report indicated that a narrow pulse width, violet-colored laser source was required to avoid gray body radiation from the hot graphite surface. Further considerations have also resulted in a conclusion that the entire Raman spectrum should be obtained from a single laser firing to avoid ambiguity as to the location of the measurement point with reference to the receding graphite surface. These and other considerations have led to the design of the experimental setup shown in Figure 22.

A high-powered (i.e., 100 MW/pulse) Ruby laser will be used as the exciting source. The pulse width for this laser of 10-15 nanoseconds is ideal; however, this laser operates at 6943 Å which does not satisfy the requirement for a violet laser source. To overcome this difficulty, a frequency doubling (i.e., wavelength halving) crystal will be employed to convert some of the 6943 Å radiation to 3472 Å light. Typically, 10 percent efficiencies are obtainable with modern crystal doublers. To separate the desired violet laser radiation from the dual-colored beam exiting the doubler, a quartz dispersing prism will be utilized. Hence, 10 MW of the desired 3472 Å radiation will be directed toward the measurement point. The order of magnitude loss in laser power due to doubling will be nearly made up for by the fact that eight times more doubled (i.e., 3472 Å) laser photons will be Raman scattered by the gas molecules than undoubled ones.

FIGURE 22. BLOCK DIAGRAM OF EXPERIMENTAL SETUP TO DETERMINE C/C CHEMISTRY

A lens, L1, will be used to focus the 1-cm-diameter beam at a point on the centerline, and just off the surface of an inductively heated graphite or C/C sample. Raman-scattered radiation from a 1-cm length of the laser beam will be collected by lens L2 and focused onto the entrance slit of a 3/4-meter spectrograph. At the exit plane of this instrument, the photographic plate will be replaced with the sensing element of an Optical Multichannel Analyzer (OMA). The OMA can record an entire Raman spectrum simultaneously as required. This device can also be electronically gated to observe Raman radiation when the laser fires while rejecting the hot graphite surface radiation at other times. The spectrum can be digitized and stored for future analysis.

The experimental setup will also contain a pyrometer for monitoring the surface temperature at the time when the Raman measurements are made. Further, a fan will blow cold air over the hot sample at velocities from 100-300 ft/sec to simulate low-velocity entry conditions where surface oxidation is the dominant mass loss mechanism. A thermal imaging camera (not shown in Figure 22) will also be used to check wall temperature uniformity on the sample surface.

Current Progress. The graphite sample holder design was completed and general features are shown in Figure 23. A cylinder of alumina which is split lengthwise will be used to clamp down and hold on to the replaceable sample. Induction coils will then be wrapped around the alumina cylinder near the sample to provide efficient coupling. Finally, a 4-in.-diameter aerodynamic-shaped holder made of Micarta will be used to hold the alumina cylinder. "Cerama-Bond" cement will be employed to fill in gaps around the induction tubes to provide smooth transitions from one material to another. The purpose of the Micarta holder is to provide sufficient frontal area to obtain a thicker aerodynamic boundary layer on the graphite sample. The small angle (exaggerated in Figure 23) shown on the face of the Micarta aeroshape will allow for focusing of the laser beam as close to the graphite surface as possible. Note also in Figure 23 that the shape of the sample itself has been altered from the previous reported design to provide for uniform sample heating, without the requirement for using two induction heaters.

The BCL Ruby laser was refurbished with a new dye cell Q-switch and nominal output performance of 1 joule in 10 nanoseconds was obtained for the prescribed flash lamp voltage setting. The BCL doubling crystal had received

FIGURE 23. SAMPLE HOLDER FOR GRAPHITE CHEMISTRY EXPERIMENT

water damage from excessive exposure to the atmosphere. It was repolished and anti-reflection coated. A test at 100-MW doubler input power produced 7 MW of $3742 \text{ \AA}$ radiation after dispersion from the quartz prism and passage through a red cutoff filter. This output implies that the crystal doubling efficiency is quite close to the 10 percent optimum value.

A quartz cross-shaped cell was manufactured then filled with a known sample of N_2 , CO_2 , and CO and sealed. This cell will be used for calibration and alignment purposes.

The OMA was scheduled to be shipped to Battelle on December 27 and should be operable soon. An adapter plate to mount the OMA at the spectrometer exit focal plane has already been machined from an aluminum block.

Thermal Stress Failure of C/C Composites (L. E. Hulbert)

Effective utilization of carbon/carbon composites in providing reentry thermal protection for nuclear power sources requires an understanding of the possible failure behavior of these composites when they are subjected to thermal stresses. This requirement will become even more important in the design of the bifunctional reentry impact member (RIM) because of the material design tradeoffs necessary to provide both types of protection.

In spite of the importance of the development of adequate failure criteria for C/C composites to the design of RIM's, it became apparent that ERDA did not have sufficient resources to carry out such a development program alone. Thus, as discussed in the previous quarterly report, contacts were initiated with the Department of Defense to explore the possibility of inter-agency cooperation in the development of C/C composite failure criteria. Activities in the fourth quarter of 1976 were primarily directed toward further exploration of this interaction.

Two significant activities were carried out in this quarter. The first involved participation by Arnold Litman (ERDA/NRA) and Gene Hulbert (BCL) in an industry/government workshop on "The Mechanics Modeling of C/C Composites" on November 15-16, 1976. The second involved a presentation of the ERDA RIM development program to Jerome Persh, who is Staff Specialist for Materials and Structures in the Office of the Director, Defense Research and Engineering.

The workshop was organized by the Air Force Materials Laboratory and included 61 government (primarily DOD) and industry representatives directly

interested in modeling material response characteristics of C/C composites. It appeared that this workshop allowed an up-to-date assessment of the present state of micromechanical modeling of C/C composite material behavior.

Two essential conclusions could be made as a result of this workshop.

- (1) Currently, material variability of three-dimensional C/C composites, as fabricated, is so great that a failure model would not be meaningful if available now. Thus, the greatest immediate need is to develop more reproducible material.
- (2) Material processing development should be accompanied by development of micromechanical modeling development (including a failure model). This would permit using the modeling results to guide (and be guided by) the material developments. Further, this would allow the use of the proven models in design of materials for chosen applications in a time way as materials are improved. Attempting the empirical development of uniform C/C composites and then a subsequent development of mathematical models would be more costly in both time and money.

Participants in the presentation and subsequent discussion of the ERDA materials development program with Jerry Persh included Arnold Litman of ERDA, Bill Pardue and Gene Hulbert of BCL, and Ellis Foster who was on leave from BCL at IDA. The presentation covered the background of the reentry protection problems for RTG's, the objectives in trying to develop C/C composite reentry protection heat shields, and the areas in which it appeared that DOD and ERDA had common problems and objectives. As a result of the succeeding discussion, it was agreed there were areas of mutual interest and that further meetings between ERDA, DOD, and (possibly) NASA representatives were desirable to explore the possibility of interagency cooperation in the development of C/C composites.

In view of the complexity associated with developing suitable failure criteria for C/C composites, efforts should also be devoted to identifying those activities which can be conducted to demonstrate the adequacy of specific C/C

composites and design approaches for the GPHS heat source RIM. The unavailability of suitable failure criteria for the service performance of these materials, along with the problems associated with producing and maintaining large integral fuel forms ($\geq 100 W_t$), strongly supports the development of a modularized GPHS. In this way, ground proof tests of the RIM module could be conducted using available test facilities to demonstrate the thermal stress resistance of specific C/C composites as well as evaluate improved methods of end closure.

THERMODYNAMIC STUDIES
(C. A. Alexander and J. S. Ogden)

Introduction

The purpose of this task is to supply supportive information on the thermodynamic behavior of components within actual or potential heat source designs by basic studies of mechanistic nature.

Summary

Indications have been obtained that previously unobserved gaseous molecules exist in the iridium- $^{238}\text{PuO}_2$ system that might lead to unexpected long-term deleterious effects.

Work This Period

The use of iridium containment for $^{238}\text{PuO}_2$ fuel has been established practice for several recent RTG missions; a total understanding of possible degradation mechanisms is not yet available, especially those associated with vapor transport.

Additional experiments were conducted to establish minimum oxygen and sodium pressures resulting from the $^{238}\text{PuO}_2$ impurities which are necessary for the formation of the complex vapor molecules which lead to transport of the PICS material. A special iridium cell was fabricated from an ORNL iridium rod rather than the tungsten used previously since prior experiments had shown tungsten as a vaporizing species. Figure 24 shows the cell and the support rods. Figure 25 shows the shields which surround the heating element and the cell. This assembly is inserted into the vacuum system of the mass spectrometer. Figure 26 shows the electronic panels of the mass spectrometer.

Experiments this period were conducted using a mixture of U_3O_8 and Na_2SiO_3 in the iridium cell to obtain various pressures of sodium and oxygen representative of those found within a PICS. Enough sodium was added to make its concentration about 100 ppm. This extra amount of sodium, as compared with the normal level of about 10 ppm in $^{238}\text{PuO}_2$, was added to allow extra time for the investigation of the sodium tungstates and sodium iridates prior to the

FIGURE 24. THERMODYNAMIC VAPORIZATION CELL

FIGURE 25. THERMODYNAMIC VAPORIZATION CELL WITH SHIELDS IN PLACE

FIGURE 26. ELECTRONIC PANELS FOR THERMODYNAMIC MASS SPECTROMETER

vaporization from the cell. Previous experiments have established the existence of gaseous species from other production fuel impurities such as PbO, CaO, CuO, Ni and Fe, so these were omitted from this experiment. These results have been previously reported in BMI-X-672.

The intensity of the sodium vapor emanating from the iridium cell decreases with time at a given temperature. This indicates that the sodium is alloying with the iridium itself or the tungsten impurity of the iridium. This may be a part of the mechanism necessary for the formation of the complex molecules such as Na_2WO_4 or Na_2IrO_4 .

At 700 C the sodium intensity decreases from 50 Volt Divisions on the mass spectrometer readout to 1 Volt Division in a period of 30 minutes. At 910 C the sodium intensity decreases from 150 Volt Divisions to 15 Volt Divisions in 30 minutes.

The major difference between these experiments and previous experiments is the existence of the Na_2WO_4 and the WO_3 at temperatures as low as 1050 C.

Table 9 lists the sodium and oxygen intensities observed during these experiments. This table shows that the oxygen intensity remains relatively constant while the sodium intensity fluctuates first by alloying and then by depletion through vaporization.

TABLE 9. SODIUM AND OXYGEN INTENSITIES

Temperature, C	I_{Na}	I_{O_2}
700	5E4	1.8E5
910	1.8E5	1.9E5
1050	4.0E4	2.2E5
1140	1.3E6	2.9E5
1225	1.7E6	2.7E5
1310	2.5E6	3.9E5
1395	4.3E6	4.6E5
1650	4.9E4	9.8E4

Table 10 shows the relationship of the sodium intensity and the Na_2WO_4 and WO_3 . The Na_2WO_4 intensity increases and decreases with the sodium intensity as one would predict. The decrease of the WO_3 and W_2O_6 probably indicates a depletion of the tungsten, since there is no concomitant change in the oxygen concentration in the cell during the course of these experiments.

TABLE 10. INTENSITIES OF SODIUM, OXYGEN AND TUNGSTEN SPECIES

Temperature, C	I_{Na}	$I_{\text{Na}_2\text{WO}_4}$	I_{WO_3}	$I_{\text{W}_2\text{O}_6}$
700	5E4	--	--	--
910	1.8E5	--	--	--
1050	4.0E4	--	--	--
1140	1.3E6	4.1E5	3.8E4	--
1225	1.7E6	6.6E5	1.9E5	5.8E5
1310	2.5E6	8.5E5	5.5E5	6.1E5
1395	4.3E6	6.3E5	7.4E5	5.2E5
1650	4.9E4	2.0E4	2.0E5	5.9E3

Table 11 shows the relationship of the sodium intensity and the Na_2IrO_4 and IrO_3 intensities. The Na_2IrO_4 pressure is dependent upon the sodium pressure but the IrO_3 pressure is not; this indicates that the IrO_3 is real and not a fragment caused by the ionization of Na_2IrO_4 .

TABLE 11. INTENSITIES OF SODIUM, IRIDIUM AND OXYGEN SPECIES

Temperature, C	I_{Na}	$I_{\text{Na}_2\text{Ir}_4}$	I_{IrO_3}
700	5E4	--	--
910	1.8E5	--	--
1050	4.0E4	--	--
1140	1.3E6	2.3E2	--
1225	1.7E6	7.6E2	--
1310	2.5E6	1.0E3	--
1395	4.3E6	1.4E3	5.1E2
1650	4.9E4	--	9.8E2

Table 12 summarizes the data obtained from these experiments using the iridium cell.

TABLE 12. SUMMARY OF DATA

Temperature, C	I _{Na}	I _{O₂}	I _{Na₂WO₄}	I _{WO₃}	I _{W₂O₆}	I _{Na₂IrO₄}	I _{IrO₃}
700	5E4	1.8E5	--	--	--	--	--
910	1.8E5	1.9E5	--	--	--	--	--
1050	4E4	2.2E5	--	--	--	--	--
1140	1.3E6	2.9E5	4.1E5	3.8E4	--	2.3E2	--
1225	1.7E6	2.7E5	6.6E5	1.9E5	5.8E5	7.6E2	--
1310	2.5E6	3.9E5	8.5E5	5.5E5	6.1E5	1.0E3	--
1395	4.3E6	4.6E5	6.3E5	7.4E5	5.2E5	1.4E3	5.1E2
1650	4.9E6	9.8E4	2.0E4	2.0E5	5.9E3	--	9.8E2

Experiments to date have indicated that there does exist a potential for iridium transport which is greater than that transport caused solely by IrO₃(g). The data also indicate strongly that there is absorption of gaseous sodium by the iridium. To what extent this sodium in the iridium affects the impact properties of the iridium is not known, but the effect may be large. Interpretation of the data indicates a rapid reaction occurred until the surface was equilibrated with the sodium gas. Most likely this surface reaction is followed by a bulk diffusion or a grain boundary diffusion mechanism through the iridium. Ion microprobe experiments are planned to locate and define the reaction path of the sodium in iridium.

Another observation from the mass spectrometric runs was that tungsten, even in a solid matrix of iridium, is very reactive and that this tungsten would be selectively leached out by the combined presence of sodium and oxygen. Again, after the surface is depleted, this reaction must proceed by a grain-boundary or bulk diffusion mechanism. Possibly this loss of tungsten is less deleterious to the properties of the iridium than is the increase of sodium levels due to absorption because of the likely lower diffusion of tungsten.

A diffusion coefficient of tungsten as high as 10^{-11} cm²/sec at the PICS operating temperature could, however, lead to almost complete tungsten loss over a five-year mission, with serious implications.

Experiments have indicated that impurities in the plutonium in the 10 to 100 parts per million by weight can lead to considerable transport and to potential contamination reaction. It would appear that any efforts to lower these impurities in the fuel would be beneficial. By ceramic standards the fuel is of good purity, but the stringent requirements on the system tend to indicate that near ultra purity is required.

HELIUM VENT DEVELOPMENT
(C. A. Alexander and M. P. Rausch)

Introduction

The objective of this program is to develop helium vent systems capable of long-term unobstructed helium passage while containing particulates and restricting the egress and ingress of other gases through the primary fuel containment member. The approach is to use a parametric study of the meaningful variables determined by analytical examination of previous efforts, theoretical vent modeling, and basic vapor transport relations that have been developed at BCL in earlier studies.

Summary

Studies of selective vents have been initiated and are described.

Work This Period

In the past period a small, ultra-clean hot press has been completed for the exclusive purpose of fabricating vent assemblies. This hot press was required to ensure the quality of contaminant-free solid-state bond required between the noble metal components and to adequately control the atmosphere and pressure application. The hot press is used in conjunction with a Lepel 455KH RF generator of 20-kw capability. The Lepel unit is fitted with a L&N CAT control for precise programing and control of the power and temperature profile during the heating cycle. The hot press used in the past was quite adequate for the high-temperature work with iridium but its response characteristics were not sufficiently controlable for use with the Pt-30Rh-8W material.

In December the remainder of the iridium and the platinum base material required for fabrication of vents was received from ORNL. Material characterization has been completed. Results are shown in Table 13.

Efforts have been directed toward the development of a selective vent. The filter materials previously chosen for the selective vent will meet the following requirements: melting point greater than 1500 C, viscous at

TABLE 13. CHEMICAL ANALYSES OF PLATINUM AND IRIIDIUM VENT MATERIALS

Element	Content, w/o			
	Iridium		Platinum	
	5 mil	10 mil	5 mil	10 mil
Rh	--	--	30.1	30.2
W	0.04	0.06	3.0	8.0
Ir	--	--	0.40	0.40
Fe	0.0004	0.0004	0.005	0.020
Ni	ND	ND	0.002	0.01
Al	ND	ND	0.002	0.002
Mo	ND	ND	0.006	0.004
Cu	<0.0005	<0.0005	0.006	0.008
Ti	ND	ND	ND	0.0006
Co	ND	ND	<0.001	0.001
Si	0.0008	0.0006	0.0005	0.0006
Cr	ND	ND	0.003	0.010
No other elements at detection limit.				

reentry temperatures, low vapor pressure and transparent to helium at temperatures greater than 800 C. Candidate filter materials, all of which have melting points in excess of 1500 C include: anorthite ($\text{CaO} \cdot \text{Al}_2\text{O}_3 \cdot 2\text{SiO}_2$), leucite ($\text{K}_2\text{O} \cdot \text{Al}_2\text{O}_3 \cdot 4\text{SiO}_2$), forsterite ($2\text{MgO} \cdot \text{SiO}_2$), olivine [$2(\text{Mg}, \text{Fe})\text{O} \cdot \text{SiO}_2$], spinel ($\text{MgO} \cdot \text{Al}_2\text{O}_3$), phosphate glass ($2\text{Ca}_3\text{PO}_4 + \text{SiO}_2$), and nephelite ($\text{Na}_2\text{O} \cdot \text{Al}_2\text{O}_3 \cdot 2\text{SiO}_2$). A 100-g batch of nephelite was made by combining 32.33 w/o Na_2CO_3 , 21.10 w/o Al_2O_3 , and 36.59 w/o SiO_2 . Approximately 30 grams of the mixture was loaded into a platinum crucible and fired in air for 30 minutes at 1650 C. During firing, the glass appeared to be completely melted at 1580 C. (The theoretical melting point of nephelite is 1526 C.) At 1650 C the glass appeared to be a homogeneous melt and highly viscous. The glass was quenched to room temperature, then grounded and sieved to -400 mesh.

The glass frit was applied to the filter ring area by slurry impregnation, and the vent was fired in vacuum for 15 minutes at 1600 C. Three applications of the glass frit were necessary to obtain a uniform filter. A graphite die was machined to accommodate the vent; the two vent pieces were aligned and loaded into the die for hot pressing. Hot pressing was carried out at 1500 C for 15 minutes with 8000 psi of pressure on the vent. The apparatus was cooled slowly to room temperature and the vent removed for testing.

The vent was tested at room temperature using a pressurized fixture. No indication of either He or Ar release was observed. The vent was then welded into the iridium vent evaluation crucible. A slightly different welding procedure than had previously been used was employed here to minimize warpage problems during the welding operation. Room-temperature testing after welding indicated a release rate through the vent for both He and Ar greater than the permissible 10^{-8} std cc/sec rate. Further testing showed the weld to be porous; and as a consequence of welding, the vent had warped slightly. A hairline crack across half of the top vent surface (assumed to be the result of thermal stresses incurred during welding) added to the rapid gas release rate. Metallography is currently being performed to determine the effects of the weld on the glass-metal bond.

Figure 27 shows the glass-metal bond of an iridium/nephelite vent. Indications of weld damage were also apparent with this vent, e.g., slight warpage.

To ensure a sound weld and an intact vent after welding, a slightly modified vent is being fabricated. It is hoped that in removing the vent from the localized heat of the weld zone the desirable properties observed prior to welding will be maintained. This problem of warpage is more severe in the testing stage than in the actual fuel container because of the necessity for welding the light-weight material to the robust vent crucible. This crucible is being reground in an effort to better control the welding operation.

Simultaneously with this effort, development of the platinum selective vent is continuing. No difficulty has been experienced in welding the platinum vent to its vent crucible, so likely the frit parameters can be obtained most rapidly with the platinum vent. The most critical range for helium passage would seem to be 600 to 800 C. In this range the fuel likely will not hold all its helium and so the frit must pass the generation rate at this span of temperature.

FIGURE 27. CROSS SECTION OF METAL/NEPHELITE INTERFACE

Nephelite, while used for the activities described above, has been discontinued as a possible frit due to the potential for undesirable reactions between iridium and the sodium contained in nephelite (see section on thermodynamics).

A batch of anorthite ($\text{CaO} \cdot \text{Al}_2\text{O}_3 \cdot 2\text{SiO}_2$) has been synthesized and fired in air for 30 minutes at 1600 C and weight loss experiments have been performed at 1400 C to determine this material's suitability for use in selective vents. The weight loss is a factor of six lower than for nephelite and indicates that there are no molecular complexes volatilizing. The first set of compatibility experiments in the mass spectrometer was very encouraging with this material in contact both with iridium and with Pt 3008. There were no vapor complexes observed at all. At the higher temperatures, Ca^+ was finally observed but this is a vapor pressure down about 10^{-11} atm at 1500 C in contact with iridium. At 1250 C with Pt 3008 there were no observable species at 3×10^{-12} atm level. This is just above background for the mass spectrometer.

The final checkout of the hot press is presently under way and upon its completion a series of selective vents will be fabricated and their evaluation begun.

Two nonselective vents were successfully welded into PICS by Mound Laboratory and delivered to LASL for fuel encapsulation prior to long-term compatibility testing. In addition, the test matrix to be applied to the final vent selection process was compiled on a preliminary basis.

APPENDIX

NOMENCLATURE GLOSSARY FOR GAS GUN DESIGN

APPENDIX

NOMENCLATURE GLOSSARY FOR GAS GUN DESIGN

A_B	Cross sectional area of barrel
a_i	Projectile acceleration during time step i
B	Constant defined by Equation (30)
C_1	Constant = $P_{AC} V_{AC}^\gamma$
C_2	Constant equal to $C_D f(\gamma) / (R/M)^{1/2}$
C_D	Venting coefficient, conservatively chosen equal to 0.6 in this analysis
D	Fraction of a time step
$f(\gamma)$	Function of γ given* to be $= [2\gamma / (\gamma + 1)]^{1/2} [2 / (\gamma + 1)]^{[1 / (\gamma - 1)]}$ $= 0.6847 \text{ (for } \gamma = 1.4)$
G	Constant defined by Equation (29)
k	Arbitrary constant
m	Mass of gas
M	Molecular weight of gas
M_P	Mass of projectile
P	Pressure, absolute
P_A	Atmospheric pressure
P_i	Pressure at the end of time step i
R	Gas constant
Δt	Time step
T_i	Acceleration time at the end of time step i
U_i	Projectile velocity during time step i
v	Specific gas volume
V	Total gas volume
V_{AC}	Volume of accumulator
X	Projectile position (see Figure 6a)
X_i	Projectile position at the end of time step i
X_{NV}	Non-vented barrel length

* Kinney, G. F., and Sewell, R.G.S., "Venting of Explosions", NWC Technical Memorandum 2448, Naval Weapons Center, China Lake, California, July, 1974.

α	Venting area
α_i	Venting area at the end of time step i
γ	Ratio of specific heats of gas = 1.4
ϕ	Temperature of gas, absolute
ϕ_{AC}	Initial accumulator gas temperature

DISTRIBUTION LIST

ERDA - Division of Nuclear Research
and Applications, Headquarters

A. P. Litman (9)

ERDA - Chicago Operations Office

Patent Office

ERDA - Technical Information Center,
Office of Information Services
Oak Ridge (5)

Air Force Materials Laboratory,
Wright-Patterson Air Force Base

D. L. Schmidt
C. Pratt

Fairchild-Hiller Industries

A. Schock

General Electric Company, Philadelphia

E. W. Williams

Jet Propulsion Laboratory

V. Truscello

Kirtland Air Force Base

Directorate of Nuclear Safety

Los Alamos Scientific Laboratory

R. D. Baker
S. Bronisz
S. Hecker

Monsanto Research Corporation,
Mound Laboratory

E. W. Johnson/D. L. Coffey

Oak Ridge National Laboratory

A. C. Schaffhauser (2)

BCL Internal Distribution

C. A. Alexander (2)
M. P. Rausch
J. S. Ogden
I. M. Grinberg (2)
A. A. Boiarski
R. B. Stonesifer
L. E. Hulbert
G. R. Whitacre
W. H. Duckworth (2)
G. K. Bansal
J. H. Peterson
B. D. Trott
E. L. Foster
W. M. Pardue (2)
J. E. Davis/ERDA Files

Teledyne Energy Systems, Inc.

W. Osmeyer

Johns Hopkins University

J. Hagen