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MASTER

Heat Source Component Development Program

QUARTERLY REPORT FOR JULY-SEPTEMBER, 1977

Report Date: October, 1977

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HEAT SOURCE COMPONENT DEVELOPMENT PROGRAM

Quarterly Report for July-September, 1977

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Prepared for the Division of Nuclear Research and Applications,
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*J.C. 11/18/77
by Chicago Patent Group*

FOREWORD

This is the fourth 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. Previous quarterly reports are identified as BMI-X-672 October 1976, BMI-X-676 January 1977, BMI-X-679 April 1977, and BMI-X-683 July 1977.

The activities covered in this new series of reports are conducted under Contract No. W-7405-eng-92, Task 94. The specific efforts in progress during the period covered by this report were in support of the General Purpose Heat Source (GPHS) Development Program that is being led by the Los Alamos Scientific Laboratory (LASL). These specific efforts were concerned with: Improved Impact Members, Analysis of GPHS Conceptual Designs from viewpoint of reentry ablation and thermal stress and studies of the Surface Chemistry of Graphite materials.

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HEAT SOURCE COMPONENT DEVELOPMENT PROGRAM

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

Introduction

The impact-member work is directed to gaining needed knowledge and understanding of the response of materials and designs to heat-source impact conditions, and applying the technology gained to the selection or development and demonstration of preferred impact-member materials and designs that will insure fuel containment in the event of earth impact.

Summary

During the past quarter, major effort has been directed to analyzing the impact-member problem in the light of knowledge and understanding gained in studies to date. These studies have concentrated on the requirements for and performance of materials in impact-member service. As a consequence of the analysis, new goals have been formulated for the impact-member task, and these have been set forth along with the rationale for them in a draft topical report submitted to ERDA/DNRA. The analysis is presented below.

Experimental work during the quarter was conducted on three problems:

- (1) Determining energy consumption by carbon composite impact shells through microscopic examination of debris.
- (2) Determining the tensile constraint provided by MHW impact shells.
- (3) Determining surface strain of copper fuel mock-ups of GPHS fuel capsules.

Findings in these experiments also are reported below.

Work This PeriodGoals of RTG Impact Member Research

(W. H. Duckworth)

Introduction

Since January, 1975, Battelle has been engaged in research for ERDA/DNRA on impact members of radioisotope thermal generators (RTG's) for space use. Emphasis in the research to date has been on developing a materials technology. This effort has now reached a stage where consideration can be given to engineering measures for assessing and upgrading the performance of impact members in RTG's of current interest. New research goals have been set accordingly. The purpose here is to present these goals, give the rationale for them, and indicate the research approaches for meeting them.

Statement of Problem

In RTG's for use in space, the PuO_2 fuel is contained in metal cladding. Surrounding the metal-clad fuel is a shell of carbon-base material whose function is to prevent rupture of the cladding in the event of impact with a rigid target. Since the cladding can stretch appreciably without rupturing, it is not intended that the impact shell dissipate all of the available energy. The fuel can deform and consume energy to the extent that it does not experience a surface strain sufficient to rupture the cladding.

The effectiveness of an impact member depends on the energy it dissipates on a weight or thickness basis in the most severe impact anticipated for the fuel assembly. Impact materials, therefore, can be rated according to their energy-absorption capacity under appropriate loading conditions, but with an important stipulation--the impact material must have properties that cause it to absorb energy in

preference to the PuO_2 fuel during deceleration of the impacting mass. Any energy absorbed by the impact member subsequent to rupture of the metal cladding or after the fuel's energy-absorption capacity is exhausted is, of course, worthless. In nonprecise terminology, this means that the impact material must be "softer" than the fuel, as will be explained.

Initially, the impact loading will generate rapidly increasing stresses in both the impact member and the fuel; i.e., kinetic energy is converted to stored strain energy in both components. Following this, the elastic limit of either the impact material or PuO_2 will be reached, causing it to yield by either fracturing or plastic flow*. The basic nature of the process changes at this point to one in which energy is consumed, and if the impact member is the component which suffers damage, it has started to perform its function. Ideally this will be the case, but whether it will occur depends on the rates of stress build-up, on stress distributions, and on the stress levels at which yielding occurs in both the impact member and the fuel. These factors are difficult to assess. Even more difficult to assess is the effect of progressive damage on stresses experienced by each component, and this effect will largely determine whether the ideal result is obtained--that of impact member continuing to consume energy in preference to the fuel after sustaining initial damage.

Tensile constraint of fuel could possibly be an important function of an impact member. Conditions under which it assumes importance are: (1) cladding rupture from local strain due to lateral expansion rather than denting of the fuel, and (2) impact energy greater than absorption capacity of impact shell, as in MHW, or (3) yielding of PuO_2 at any time prior to exhaustion of impact member's energy consumption capacity, i. e., use of impact member that is too "hard". In general, one would expect the constraint afforded to be readily calculatable from knowledge of tangential tensile modulus and limited by tensile strength of the impact member. However, our investigations of golf-ball wound material indicate that the constraint afforded by this material

* Investigation revealed that there is little prospect of developing an impact member that would not undergo damage; i.e., dissipation of the impact energy through elastic recoil is not a promising approach.

continuously changes with increasing expansion. The material does not act as an elastic continuum; the inner surface expands extensively without dimensional change of the outer surface. Other 2-D c/c composites would be expected to exhibit this same behavior to some degree. Further investigation is needed to establish effectiveness of the materials with respect to tensile constraint of fuel.

Research Goals

The major thrust of future Battelle research on the problem will be directed to the key question of how much energy the impact member consumes prior to extensive fuel damage, and then to improving the member's performance by appropriate material adjustments. In case the impact member consumes energy in preference to the fuel, as desired, the adjustments to be made will be aimed at increasing the material's energy absorption capacity, and in case the fuel consumes energy first, a "softer" impact material will be sought. It is proposed that this effort be devoted to both MHW and GPHS impact members.

Review of Past Research

In the Battelle research to date, emphasis has been on developing knowledge of the properties and behavior of carbon-based materials needed to permit sound engineering practices in selecting, testing, developing, and optimizing them for impact-member use.

Carbon-base materials do not exhibit plastic flow at impact-member temperatures, limiting the practical mechanisms for energy dissipation to that consumed in the formation of new surface by fracturing; i.e., by work of fracture. In fracturing, a carbon-base impact member might either shatter or crush, and the member's performance depends very much on which of these two fracture behaviors obtains. Much higher energy-absorption capacities are found to be associated with crushing than with shattering.

In studies of various types of carbon-base materials, the Battelle research has indicated that rather simple fracture-mechanics

tests in which a single crack is propagated in the specimen can reveal the tendency of a material to crush. High ratios of the total energy consumed in breaking a standardized specimen, U_f , to that required for crack initiation, U_i , are indicative of a strong crushing tendency. Bulk graphite were found to exhibit very small U_f/U_i ratios while 2-D carbon-carbon composites with fibers aligned normal to the propagating fracture crack had large ratios. Among the carbon-base materials investigated the 2-D golf-ball wound composite used in GE's MHW RTG exhibited by far the highest ratio.

The 2-D carbon-carbon composites also were found superior on a thickness basis to other types of carbon-base materials with respect to energy-absorption capacity. In these tests, the loading configuration approximated that in impact-member service, and the 2-D composites were compressively loaded with the fibers aligned normal to the loading direction. Capacities of 3-D composites varied widely, with the best equivalent to the worst 2-D composite. Among the carbon-base materials investigated, Carborundum's Carbitex 700, a 2-D cloth lay-up composite, exhibited the greatest energy-absorption capacity. As an indication of "hardness", the maximum load sustained by specimens in this test ranged among the 2-D composites from 3350 to 9500 lb, with the MHW golf-ball wound composite sustaining the lowest load.

Considering all three essential qualities -- crushing tendency, energy-absorption capacity, and "hardness"--the golf-ball wound composite was judged the best carbon-base material for impact-member service. Detailed studies therefore were made to characterize and understand its performance as an impact member. A first question was whether it could consume a significant portion of the impact energy in the most severe impact anticipated for the MHW. This energy is about 15,000 in.-lb (1 pound fuel assembly impacting at 285 ft/sec). Tests of actual MHW impact shells indicated that they had the capacity to absorb some 7500 in.-lb of energy, or about half that required to completely decelerate an MHW fuel assembly at the maximum expected impact velocity. This potential for energy dissipation is considered significant if it is realized. However, as discussed above, whether it is realized or not

depends on whether the PuO_2 fuel is "hard" or "soft" relative to the impact-member material. The importance of this factor was clearly shown in tests of MHW impact shells with soft (plastic) fuel simulants in which the shell dissipated only a few hundred in.-lb of energy, and failed from hoop tension due to lateral fuel expansion rather than from crushing as in the case of a "hard" fuel simulant (steel).

Studies of the load-deformation behavior of golf-ball wound shells during crushing and of debris from crushed shells have provided important insight on the mechanism of energy consumption and of factors controlling "hardness". The standard shell made from Thornel 50 graphite yarn yields under about 30,000 lb load after it has compressed to about half its original thickness. The load drops gradually with continued thinning until a hole is opened at the crushed site. With complete crushing (when the energy-absorption capacity is exhausted) some of the crushed debris were fibers broken into short lengths of the order of the fiber diameter. Prior to this, the debris features longer lengths of broken fiber, suggesting that energy consumption is associated with breaking of fiber into progressively shorter lengths.

The onset of yielding apparently occurs when fibers begin breaking and subsequent yielding behavior is controlled by progressive fiber breakage. Tensile stresses to cause fiber breakage and the consumption of energy through the formation of new surfaces could be developed during the compressive loading from bending of one fiber over another, or they could be the result of Hertzian contact stresses in the fiber surfaces. In either case, the load required to initiate yielding would depend significantly on the tensile strength of the fiber used in the composite. This dependency was confirmed in tests of special shells made with various graphite fibers. Yielding tended to occur at higher loads with stronger fibers. Thus, the "hardness" of an impact shell can be adjusted by changing to a stronger or weaker fiber as needed. Although it is a predominant factor, fiber strength alone however does not control shell hardness, no doubt because the level of tensile stress in an individual fiber subjected to given load depends on its cross section and on details of the fiber array in the composite.

By changing the fiber from which impact shells were made, the load at which yielding occurred was varied by more than a factor of two. As might be expected, energy absorption capacity generally increased with the yield load, and capacities also varied by about a factor of two depending on the fiber used to make the shell.

Yielding load as well as energy-absorption capacity was found to increase with shell thickness. Temperatures to 2500 F had little effect on energy absorption or hardness of the impact shells, making room-temperature data applicable in interpreting performance at service temperatures.

Study of the effects of loading rate is incomplete. Initial results indicate that the "hardness" of an impact shell increases with loading rate as expected, but there appeared to be no basic difference in performance attributable to loading rate. With respect to "hardness", the yielding load of an MHW shell increased from the static value of 30,000 lb to 38,000 lb when loaded at 150 ft/sec. Interestingly, the shell caused complete deceleration from 150 ft/sec without exhausting its energy-absorption capacity and there was no apparent damage of the hard fuel simulant (steel) or the target. The energy absorbed in crushing the shell was practically equivalent to the available kinetic energy. In contrast, tests under the same conditions of an ATJ bulk graphite shell resulted in damage to both the steel fuel simulant and target.

This dramatic difference in results with the two materials demonstrates the importance of using an impact material that is crushable, i.e., has sufficient lateral tensile strength to retain the material between fuel and target so that it may be crushed. The carbon-carbon shell result indicates that fuel deformation can be completely prevented for a hard fuel simulant when these conditions are met and the impact energy is within the energy-absorption capacity of the particular impact shell.

Future Work

Two general approaches will be taken to answering the key question of how much energy the impact member dissipates prior to extensive fuel damage. In one approach, impact tests of actual MHW (or GPHS) fuel assemblies will be made at velocities near those to provide kinetic energies equivalent to the energy-absorption capacity of the impact shell. If little or no damage is experienced by the fuel, it would indicate that the shell material is performing its intended function. On the other hand if damage to the fuel is extensive, it would indicate that the shell is ineffective.

In the other approach, debris from impact shells of MHW (or GPHS) actual fuel assemblies after tests at maximum anticipated impact velocities will be examined to determine the extent and nature of fiber breakage, and the possible energy absorbed by the shell. This approach requires a special study of debris from shells damaged in controlled laboratory tests in which known amounts of energy have been consumed by the shell in producing the damage.

Work on these approaches can proceed immediately with standard MHW shells, but in the case of GPHS fuel assemblies the work must await fabrication of special 2-D carbon-carbon composite shells and characterization of their energy-absorption capacities for different impact orientations.

Prior to tests of actual MHW or GPHS fuel assemblies to determine whether energy consumption by the impact shell precedes that by the fuel, the two approaches will be checked in laboratory tests at Battelle using the special instrumented impact apparatus and fuel simulants having various levels of "hardness", including annealed copper.

Once it is determined whether or not significant energy consumption by the impact shell precedes extensive damage of the fuel, work would concentrate on improving the impact material. If it is found that the impact material is too "hard", approaches discussed earlier will be used to soften it. On the other hand, if it is found that energy consumption by the impact material precedes that by the fuel, effort

would be directed to increasing the material's energy-absorption capacity, again following approaches discussed previously.

Suggested GPHS Impact Member Program

The program outlined below is based on the impact member technology described above:

- Task 1. Procure impact shell specimens of various 2-D c/c composites that can be readily fabricated.
 - (a) Variables - fiber array, yarn type.
 - (b) Number of materials - 5 to 10.
 - (c) Vendors - HITCO, Carborundum.
- Task 2. Determine energy absorption capacity and yield load for each shell.
 - (a) Investigate end-on, side-on, corner-on orientations.
 - (b) Check effects of temperature and high loading rate.
 - (c) Characterize debris from all tested shells.
- Task 3. (IASL) Determine "hardness" relative to PuO_2 by impact tests in which impact energy approximates energy absorption capacity of shell.
 - (a) Test initially the "hardest" and "softest" shell materials.
 - (b) Investigate end-on, side-on, corner-on orientations.
 - (c) Characterize debris for comparison with Task 2(c).
- Task 4. Determine course of future effort on basis of Task 3 results.
 - (a) If hardest shell is softer than PuO_2 - optimize energy absorption capacity of shell material on basis of findings in Task 2 and Battelle background.

- (b) If hardest shell is harder than PuO_2 and softest shell is softer than PuO_2 - determine hardness relative to PuO_2 of shells of intermediate hardness, then proceed as in Task 4(a), above.
- (c) If softest shell is harder than PuO_2 - develop special shell material on basis of Battelle background and proceed as in Task 3 on new softer materials.

Task 5. Select and characterize preferred shell materials.

- (a) Heat-transfer properties.
- (b) Impurities.
- (c) Specify control measures for fiber and processing steps.
- (d) Develop quality inspection measures.

Task 6. (LASL) Conduct performance tests of GPHS fuel assemblies at maximum expected impact velocity.

- (a) End-on, side-on, corner-on orientations.
- (b) Characterize debris - estimate energy consumed by impact shell.
- (c) Finalize selection.

Task 7. Prepare procurement specification for GPHS impact member.

Suggested MHW Impact Shell Program

The intent here is to point out possibilities for improving the performance of MHW impact shells through application of the knowledge gained at Battelle on the impact-member problem.

Our experimental data indicate the standard shell (GIS) has the capacity to consume about half the energy in a 285 ft/sec impact with a rigid target. Accordingly, considerable energy is available to deform the metal-clad fuel even if ideal performance of the GIS is realized. Ideal performance requires that the impact loading cause the

GIS to be fully damaged prior to significant yielding of the PuO_2 fuel (by either fracture or plastic flow); i.e., the GIS must be "softer" than the fuel.

To determine whether the GIS falls short of providing ideal performance requires special experiments in which PuO_2 -fueled assemblies are impacted at service temperature. These experiments have been conceived, but not executed.

If the experiments show that the GIS is too hard, and therefore relatively ineffective, background from the research on factors controlling hardness of golf-ball wound carbon-carbon composites can be applied to developing a softer version. On the other hand, if the experiments show that the GIS is adequately soft and performing as well as can be expected the background can be applied to developing an improved version, featuring a higher energy-absorption capacity without increasing the thickness. The effect of this material improvement would be to decrease the energy available for deforming the metal-clad fuel to below the present level of ~ 50 percent of the impact energy.

Energy Consumption by MHW Impact Shells
(G. K. Bansal and W. H. Duckworth)

The questions of effectiveness of the MHW impact shell (GIS) in preventing rupture of the iridium cladding around the PuO_2 fuel and whether the GIS's capacity for absorbing energy during an impact is fully realized remain to be answered. When the fuel undergoes a permanent deformation, it is difficult to determine from simple pass/fail tests the relative magnitudes of the energies absorbed in the fracturing of the impact member material and in deforming the fuel.

Microscopic examinations of debris of tested graphite impact shells provide a tool for estimating relative performance of the impact member material in any test conditions. This approach requires a special study of debris from shells damaged in controlled laboratory tests in which known amounts of energy have been consumed by the shells in producing the damage. In the present investigation, debris from three different tests were examined using a scanning electron microscope (SEM). Test variables are tabulated below:

<u>Test Number</u>	<u>Mandrel Material</u>	<u>Impact Velocity</u>	<u>Energy Absorbed, in-lb</u>
1*	Steel	0.05 in/min	8,500
2**	Steel	149.6 ft/sec	4,300
3**	Copper	288.4 ft/sec	?

-
- * Battelle's Informal Progress Report for July-August, 1975, on Task 67, Impact Member (issued Sept. 15, 1975), pp. 5-16.
 ** Battelle's Quarterly Report on "Heat Source Component Development Program", for April-June, 1977 (issued July, 1977), BMI-X-683, p 14.

In the above three tests, shells made of Thornel 50 fiber were tested at room temperature. Debris from each test was screened through a 325-mesh sieve, and the material which passed through the sieve, was examined using SEM. Representative micrographs, one from each test, are shown in Figures 1(a), (b), and (c).

Examinations of several micrographs such as those shown in Figure 1 revealed that the fiber lengths ranged from 5 to 30 μm in Test #1, from 15 to 80 μm in Test #2, and from 40 to 200 μm and larger in Test #3. Thornel fibers in shells before testing are continuous. It is, therefore, reasonable to assume that the material consumes energy by fiber breakage, and shorter fiber lengths are indicative of higher energy absorption per unit volume of the crushed material. In other words, the shorter the fibers are after a test, the larger the proportion of the energy-absorption capacity of the material is realized.

Based on the above observations, it can be said that in the test with the copper mandrel, the impact member's capacity for absorbing energy was least realized and in the static test with the steel mandrel it was realized the most. In Test #3, the copper mandrel deformed significantly. It is, therefore, not possible to assign the amount of energy absorbed by the impact member material. However, in Tests #1 and #2 with the steel mandrels, there was no permanent deformation of either mandrel, and the total energies absorbed in the tests are, therefore, indicative of the amounts absorbed by the impact shell material. The larger energy absorption in Test #1 is in agreement with shorter fiber lengths observed in Figure 1(a) compared to those observed in Figure 1(b). In Test #2 only half the energy of that in Test #1 was consumed in fiber breakage, but this represented almost all of the available energy (i.e., the projectile was completely decelerated).

It is recommended that the potential of this approach be further explored by examining debris from impact shells of MHW (or GPHS) actual fuel assemblies after tests at maximum anticipated impact velocities to determine the extent and nature of fiber breakage and from this, effectiveness of the shells in protecting fuel.

(a) Static Test With a Steel Mandrel

(b) Dynamic Test With a Steel Mandrel

(c) Dynamic Test With a Copper Mandrel

FIGURE 1. SCANNING ELECTRON MICROGRAPHS (1000X) OF DEBRIS PRODUCED IN GRAPHITE IMPACT SHELLS TESTED UNDER THREE DIFFERENT TEST CONDITIONS

Tensile Constraint Afforded by MHW Impact Shells
(J. H. Peterson, G. K. Bansal, and W. H. Duckworth)

In an attempt to evaluate and interpret the tensile constraint imposed by MHW impact shells to the deforming fuel, several shells made of different kinds of fibers^{*} were tested as described in the last quarterly report^{**}. An easily deformable elastomer was used for the mandrel in all tests. Movements of both the external and internal wall of the GIS were measured using extensometers, and the amount of reduction in GIS thickness, i.e., thinning of the GIS, determined from these measurements. Results from two such tests are shown in Figures 2 and 3.

No significant differences attributable to fiber type were observed in tests on different shells. The pressure-displacement curves were similar in all the tests, and the outer wall of the GIS moved only slightly prior to ultimate fracturing of the shell at pressures ranging from 13,000 to 15,000 psi. The amount of thinning which is also indicative of deformation in the elastomer ranged from 0.06 to 0.10 inch, the smallest being in the composite GIS made of two different kinds of fibers (25 percent of Thornel 75 WYL 160 on outside and 75 percent of Graphite Yarn WYB 85 on inside). The maximum amount of thinning was observed in the GIS made of 100 percent Thornel 75 WYL 160 fibers which have the highest Young's modulus and the highest tensile strength of all the fibers investigated. Thus, the least amount of reduction in the thickness of the composite GIS in the presence of a negligible deformation of the outer surface indicates the maximum restraint provided by this type of GIS. It is likely that the interface between two kinds of fibers in the composite GIS plays a role in providing constraint to the fuel deformation. Further experimentation is needed to provide a firm explanation of the above observations.

* Fiber properties are listed on page 7 of the Quarterly Report for January-March, 1977 (issued April, 1977), BMI-X-679.

** Test set-up is shown on page 4 of the Quarterly Report for April-June, 1977 (issued July, 1977), BMI-X-683.

FIGURE 2. INTERNAL PRESSURE-DISPLACEMENT CURVES FOR A GOLF-WOUND IMPACT MEMBER

Material is Thornel 75 WYL160. See page 4 of Quarterly Report for October-December, 1976, for fiber properties.

FIGURE 3. INTERNAL PRESSURE-DISPLACEMENT CURVES FOR A COMPOSITE GOLF-WOUND IMPACT MEMBER

Material is Thornel 75 WYL160 (25% outside) and Graphite Yarn WYB85 (75% inside). See page 4 of Quarterly Report for October-December, 1976, for fiber properties.

To further evaluate the effectiveness of the GIS material in providing constraint to a deforming fuel, a test similar to that described on page 4 of the last quarterly report^{*} was conducted. The test measured the amount of extrusion of the elastomer through an 1-inch diameter opening in a nylon shell. The elastomer extruded or deformed 0.10 inch at an internal pressure of 375 psi, while the deformation in the elastomer with the GIS material surrounding it ranged from 0.06 to 0.10 inch at maximum pressures of 13,000 to 15,000 psi, as described above. Thus, it appears that the GIS material can provide considerable restraint to fuel deformation.

^{*} Battelle's Quarterly Report on "Heat Source Component Development Program", for April-June, 1977 (issued July, 1977), BMI-X-683.

Surface Strain in GPHS Fuel (simulated) Capsule
(B. D. Trott)

Instrumented High Loading Rate Test
for Impact-Member Materials

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. Tests are conducted at room temperature to provide relative rankings of the materials. The data obtained from this test are correlated with the "static" force-deformation tests being conducted at both room and elevated temperatures.

The tests are not simply ultra-high strain rate tests, but rather impact simulation tests 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, fast response instrumentation is required, and unwanted stress wave reflections must be avoided. During the final phase of the impact, relative motions cease and lower strain rates occur. In the past, 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 in the fall of 1976. Since that time the equipment has been designed, constructed, and several tests conducted as reported in the previous quarterly progress reports of the Heat Source Component Development Program^{*}. During the current period additional data reduction of tests conducted previously was completed, two additional shots to show reproducibility were performed, and a series of tests conducted in support of the new General Purpose Heat Source (GPHS) Program. These results are reported here.

^{*} See Foreword.

During the previous reporting period two tests, (shots 29 and 30) were conducted using annealed copper mandrels, a 1.0 lb projectile at near 285 ft/sec and dimensionally identical impact members. One was a standard carbon-carbon composite, golf-ball wound impact member (GIS). The other was machined from ATJ bulk graphite. The load-time curves for these tests are shown in Figure 4. Other data pertinent to these tests was reported in the previous quarterly report (April-June, 1977)*. The data of Figure 4 were taken directly from the oscilloscope photos. The dotted position of the curve for the GIS impact is estimated, as it was off-scale on the oscilloscope. The time base for the curves was shifted to make the times of first load correspond to facilitate comparison of the curves. The initial peak load in the curve for shot 29 (ATJ) is interpreted as the peak load supported by the graphite before its shattering relieved the load. The second peak is associated with final projectile arrest against the deforming copper mandrel. Figures 5 and 6 show the flash X-rays taken of shots 29 and 30, respectively. The approximate times corresponding to the flash X-rays are shown on Figure 4. These times have been shifted by appropriate amounts to account for the stress wave transit times through the impact member and load transducer, based on assumed constant stress wave velocities in both specimen and load transducer bar and deformations measured from the flash X-rays. The remaining specimen thicknesses as measured from the flash X-rays are shown in parenthesis adjacent to the flash X-ray time notations on Figure 4.

The area under the two curves shown in Figure 4 was calculated using Simpson's rule at 20 μ sec intervals. The results of this impulse or momentum change measurement are compared with the initial projectile impulse in Table 1. The residual projectile velocity following impact was negligible in these shots. The difference between the incoming projectile momentum and the impulse transmitted through the load transducer is in the last column. It is as yet unclear what accuracy to expect from this measurement, however the difference between the measured momentum transmitted to the load transducer for the ATJ and GIS targets is in the right direction to account for the momentum carried away by the flying

* Battelle's Quarterly Report on "Heat Source Component Development Program", for April-June, 1977 (issued July, 1977), BMI-X-683.

FIGURE 4. COMPARISON OF LOAD-TIME CURVES FOR ATJ AND CARBON/CARBON COMPOSITE (MHW GIS) TARGETS ON ANNEALED COPPER MANDRELS AT NEAR 285 FT/SEC

(a) Flash Radiograph #1
59 μ sec After Impact

(b) Flash Radiograph #2
134 μ sec After Impact

FIGURE 5. FLASH RADIOGRAPHS OF SHOT 29, ATJ TARGET ON ANNEALED COPPER MANDREL IMPACTED BY 1.0 LB PROJECTILE AT 281.7 FT/SEC

(a) Flash Radiograph #0
65 μ sec After Impact

(b) Flash Radiograph #2
140 μ sec After Impact

FIGURE 6. FLASH RADIOGRAPHS OF SHOT 3, CARBON-CARBON COMPOSITE (MHW GIS)
TARGET ON ANNEALED COPPER MANDREL IMPACTED BY 1.0 LB
PROJECTILE AT 288.4 FT/SEC

TABLE 1. COMPARISON OF IMPULSES FROM
SHOTS 29 AND 30

Shot No.	Target Material	Impact Velocity, ft/sec	Momentum lb/sec		
			Calc. from proj. MV	Measured from P (t)	% Difference based on MV
29	ATJ	281.7	8.75	8.09	-7.5
30	GIS	288.4	8.95	10.22	+14.1

particles of shattered ATJ graphite from shot 29.

The two additional tests, shots 41 and 43 were performed to show reproducibility using MHW GIS targets on hard steel mandrels impacted with 1.0 lb projectiles at 147.8 ± 0.1 ft/sec. The load-time curves from these tests together with the curve for shot 22, reported previously, are shown in Figure 7. The new tests are shown with the points read from the oscillograph records to prepare the curves. The two curves labeled "water soaked" were obtained from the original lot of MHW GIS members obtained as rejects from the MHW program. The GIS curve for the labeled new lot was obtained recently. The degree of reproducibility obtained is evident from these curves. Further reduction of the new data to produce load-deformation data is currently in progress.

A new series of tests was initiated and completed during this reporting period in support of the GPHS program. A total of six tests were performed to evaluate the effects of graphite shell thickness on surface strains produced in annealed copper mandrels used in place of fuel or fuel simulant. To attempt to learn the difference in severity between an end-on impact an edge or corner impact attitude, two different mandrel geometries were used as shown in Figure 8. Each was fitted on a steel lower portion equipped with threads to attach to the force transducer bar. A completely filled epoxy joint was made between the copper and steel parts. The targets were made up of ATJ graphite in thicknesses of 0.200 and 0.400 in. Pertinent data associated

FIGURE 7. LOAD-TIME CURVES FOR CARBON/CARBON COMPOSITE (MHW GIS) TARGETS ON HARDENED STEEL MANDRELS IMPACTED WITH 1.0 LB PROJECTILES AT 147.08 ± 0.1 FT/SEC

FIGURE 8. MANDREL AND TARGET CONFIGURATIONS USED IN GPHS TESTS
(Both configurations have circular symmetry)

with the tests is summarized in Table 2. One of the six tests aborted due to a prefire during pressurization of the accumulator. In these tests a single 0.003 in. thickness of Mylar was used for each of the diaphragms. In separate tests, the prefire was traced to overpressuring the space between the diaphragms by elastic-plastic deformation of the diaphragms, and a controlled pressurization sequence developed to prevent recurrence of the pre-fire. In another of the tests the upper part of the load-time curve was lost due to a noise burst on the signal plus inadequate oscilloscope writing speed.

The heat source module for the GPHS is expected to develop 62.5 watts thermal or 5/8 of that developed by an MHW module. To maintain an approximately correct input momentum and kinetic energy at the desired impact velocity of 200 ft/sec, the steel portion of an existing projectile was ground down to produce a projectile weight of 0.706 lb. It was judged that a lighter weight projectile might not have sufficient strength to remain undeformed during impact. The same composite steel/aluminum projectile was used on all tests in this series.

The load-time curves obtained from the edge-on impacts are shown in Figure 9. In this data reproduction, the curves have been shifted on the time axis so that the load drops following projectile arrest coincide, clearly separating the early load peaks produced by the graphite impact members. For comparison, an unrestrained projectile at 200 ft/sec requires 83.3 microsec to travel 0.200 in. Thus the load peak from the 0.400-in.-thick graphite target only slightly restrained the projectile with respect to the 0.200-in-thick graphite target.

Figure 10 shows the second flash X-radiographs from shots 34 and 36. The earlier time radiographs were less interesting. Note that in both shots, the apparently nearly-intact graphite shell is being ejected outward and rearward from the nose of the mandrel.

The load-time curves obtained from the end-on impacts are shown in Figure 11. The only fully comparable case between end-on and edge-on impacts is for 0.200 in. ATJ graphite targets. The end-on case shows lower peak loads probably principally due to the length of smaller diameter copper mandrel included in the end-on design, which is deformed

TABLE 2. SUMMARY OF GPHS SUPPORT SHOTS

Shot No.	Target		Impact Velocity, ft/sec	Max Load, lb	Approx. Positive Duration, μ sec	Impulse, lb/sec		Deviation percent
	Type	Graphite Thickness, inch				Calculated from MV	Measured from P (t)	
34	Edge	0.400	201.9	30,000	464	4.43	4.62	+ 4.3
35	End-on	0.400	180.9	Prefire, No Data				
36	Edge	0.200	200.0	35,000	356	4.39	4.98	+13.4
37	End-on	0.200	201.0	27,000	273	4.41	4.40	- 0.2
38	Edge	0.0	201.4	>36,000	188	Part of P(t) curve lost		
39	End-on	0.0	200.4	42,000	180	4.39	4.62	+ 5.2

FIGURE 9. LOAD-TIME DATA FOR EDGE-TYPE SHOTS IN SUPPORT OF GPHS

(a) Shot 34, 0.400 ATJ Target
169 μ sec After Impact

(b) Shot 36, 0.200 ATJ Target
160 μ sec After Impact

FIGURE 10. FLASH RADIOGRAPHS OF EDGE TYPE IMPACTS AGAINST ATJ GRAPHITE TARGETS ON ANNEALED COPPER MANDRELS AT NEAR 200 FT/SEC

FIGURE 11. LOAD-TIME DATA FOR END-ON-TYPE SHOTS
IN SUPPORT OF GPHS

Dashed portion of curve
from noisy record.

over its length as will be shown. In the edge-on case, even though the copper mandrel should show a somewhat easier initial deformation due to its near-pro-rated nature, it rapidly develops a larger cross-section of impact backed by an even larger zone of uniform diameter. See Figure 8 for the mandrel dimensions.

Figure 12 shows the flash radiographs for the end-on impact of the 0.200-in.-thick ATJ target. Note the ejection of the graphite laterally from the impact face. Adjusted times for these flash radiographs are also shown on the load-time curve of Figure 11.

All the copper mandrels used in these tests were scribed with a series of gage marks across the top and down the sides like a ladder to allow measurement of the radial and tangential components of surface strain. The original gage lengths were near 0.100-in.-long. The resulting values for surface strains are shown in Figures 13, 14, and 15, for the edge-on tangential and radial strains and end-on radial strains, respectively. Insufficient measurements were taken to merit plotting the end-on tangential strains. The strains on the end-on mandrel with 0.400-in.-thick ATJ were relatively small, partly due to the lower impact velocity. The end-on tangential strains measured were all tensile in nature. The maximum observed on the mandrel impacted with 0.200 in.-thick ATJ was 10.34 in the center gage. The maximum observed on the mandrel impacted bare was only 6.11 percent, again in the center gage. The larger strain observed for the mandrel with graphite "protection" over that on the bare mandrel apparently occurred because a "pocket" of graphite was trapped on the impact face and driven into the copper forming a concave surface from the originally flat end.

The post shot appearance of the annealed copper mandrels used in these tests is shown in Figures 16 and 17, for the edge-on and end-on impacts, respectively. The apparent mandrel deformations increase with decreasing graphite protection as shown in the figures. The concave impact surfaces of the end-on tests with graphite are also apparent.

(a) 71 μ sec After Impact

(b) 165 μ sec After Impact

FIGURE 12. FLASH RADIOGRAPHS OF END-ON IMPACT OF SHOT 37, 0.200 IN. ATJ
TARGET ON COPPER MANDREL AT 201.0 FT/SEC

FIGURE 13. TANGENTIAL SURFACE STRAINS ON ANNEALED COPPER MANDRELS USED IN CONICAL (CORNER-ON) IMPACTS WITH ATJ GRAPHITE PROTECTION LAYERS

FIGURE 14. RADIAL SURFACE STRAINS ON ANNEALED COPPER MANDRELS USED IN CONICAL (CORNER-ON) IMPACTS WITH ATJ GRAPHITE PROTECTION LAYERS

FIGURE 15. RADIAL SURFACE STRAINS ON ANNEALED COPPER MANDRELS USED IN END-ON IMPACTS WITH ATJ GRAPHITE PROTECTION LAYERS

(a) Side View

(b) Top View

FIGURE 16. EDGE- OR CORNER-ON COPPER MANDRELS AFTER IMPACT TEST AT NEAR 200 FT/SEC

(a) Side View

(b) Top View

FIGURE 17. END-ON COPPER MANDRELS AFTER IMPACT. MANDREL WITH 0.4 INCH ATJ IMPACTED AT 180.9 FT/SEC, OTHERS NEAR 200 FT/SEC

ANALYSIS OF GPHS CONCEPTUAL DESIGNS
(I. M. Grinberg - Task Leader)

The General Purpose Heat Source (GPHS) is being developed by Los Alamos Scientific Laboratory (LASL) for the Department of Energy (DOE) Division of Nuclear Research and Application (DNRA). The first mission schedule for the GPHS is the NASA Out-of-Ecliptic flight in January, 1983.

LASL has formulated a program to design and qualify a heat source having certain attributes^{*}, and is currently generating Conceptual GPHS Designs for analysis and evaluation. Based on the results of these evaluations, trial GPHS designs will be selected, analyzed, and evaluated followed by the prototype design selection. Proof tests of the prototype design(s) will be conducted to insure GPHS technology readiness and compliance with the applicable safety specifications.

Within this task's activities efforts were directed toward:

- Conducting performance analyses and evaluations of the thermal, ablation, and thermal stress response of the GPHS module conceptual designs
- Collecting and compiling property and performance availability, fabricability, and cost data on graphite and carbon/carbon composites candidate materials
- Experimentally measuring the diffusion-controlled combustion characteristics of graphite and carbon/carbon candidate materials.

Summary

A summary of results of the analyses and evaluations of the Conceptual Designs is presented below. For the most part, the results

^{*} "General Purpose Heat Source Program Plan", prepared for ERDA, DNRA, by LASL, CMB-5-C-77-873; Rev. 2, 31 March 1977.

are the products of thermal, ablation, and derived thermal stress analyses. Material properties and performances were considered in the selection of candidates for the reentry and impact members and property parameters that were analyzed. The results that will be forthcoming from the experiments on combustion and impact energy absorption were considered in the parameters analyzed and in the selection of material candidates.

Clad Temperature and Reentry Member Recession

Analyses of in-depth module temperature response and surface recession were conducted for shallow and steep flight path angle trajectories for all conceptual designs. The most significant thermal results are the peak clad temperature reached during the shallow flight path trajectory and the clad temperature at impact for this trajectory. The peak temperature determines whether melting of the clad will occur during reentry. Current safety specifications require that the peak clad temperature remain at least 200 C below the melting point or eutectic temperature when one is formed. For iridium in contact with graphite, it has been determined by ORNL that a eutectic forms at 2300 C; therefore, 2100 C is the maximum allowable clad temperature. For Pt-3008, a eutectic forms at 1800 C*; therefore, 1600 C would be the maximum allowable clad temperature. The temperature of the clad at impact determines the ductility of the clad. It has been determined experimentally that approximately 14 percent elongation is required for survival of the clad.** For iridium using dop 26 or dop 14 alloys, it appears that 930 C is the lower temperature limit for maintaining this ductility. On the other hand, Pt 3008 maintains an elongation of 24 percent from 1000 C down to relatively low temperatures and, in fact, has a decrease in ductility at 1300 C which should be avoided.

* ORNL-TM-5882 June, 1977.

** Schaffhauser, A. C., Oak Ridge National Laboratory, Presentation at GPHS Meeting at Headquarters, September 23, 1977.

Table 3 shows the predicted peak and impact clad temperature comparison for all the conceptual designs.* For the reentry thermal analysis, one reentry member material was chosen from each class of graphitic materials; 994-2 from the bulk graphites, Pyrocarb 903 from the 2-D materials and AVCO Mod 3a from the 3-D materials. In addition, AXF-5Q was included as an isotropic, low cost, bulk graphite material. For all the results shown in Table 3, Pyrocarb 903 was used as the impact member material.

All materials except the AXF-5Q are anisotropic, and it was assumed that the low thermal conductivity direction for both the reentry and impact members was perpendicular to the broad face for all conceptual designs except CD-3. Placing the low conductivity in this longitudinal direction parallel to the reentry flight direction tends to reduce the heat transferred from the stagnation region into the clad during the heating portion of the trajectory, resulting in a lower peak clad temperature. In addition, in the cooling portion of the trajectory approaching impact, the low conductivity decreases the loss of heat from the clad to the windward stagnation region and thus maintains a higher clad temperature at impact. Especially for iridium clad, it is essential to keep the impact clad temperature above the embrittlement region. For CD-3, because of the different assembly and module breakup, a side-on spinning reentry mode was assumed and the reentry flight direction corresponded to the high conductivity direction. In Appendix B are the assumed orientations of each conceptual design heat source module in right circular or square-shaped cylindrical billets.

As discussed later in the Evaluation Approach, Reentry Thermal Analysis section, the two-dimensional axisymmetric thermal model treats the longitudinal or low thermal conductivity direction as the z direction. The two perpendicular directions with high thermal conductivity are treated as the r direction.

There is a second potential advantage in placing the high conductivity direction parallel to the broad face (low thermal conductivity direction perpendicular to this face). During steady-state operation, the primary heat flow direction from the fuel to the thermoelectric hot junctions or heat exchanger surfaces will be in this high conductivity direction. Therefore, the clad and fuel temperatures will be somewhat

* Shallow flight path trajectory, flight path angle = 5.3 degrees, velocity = 36 kfps, altitude = 400 kft, generator breakup at 271 kft.

TABLE 3. CLAD TEMPERATURE-IMPACT TEMPERATURE COMPARISON

Reentry Member	Peak Clad Temperature, C								Impact Clad Temperature, C							
	CD1*	CD2	CD3	CD4	CD5	CD6	CD7	CD8	CD1	CD2	CD3	CD4	CD5	CD6	CD7	CD8
994-2	2118	1993	1999	2141	1961	2028	2031	2064	617	692	615	454	691	752	706	606
Pyrocarb 903	2060	1965	1995	NR	1933	1998	NR	2036	626	732	626	NR	729	762	NR	627
AVCO 3D Mod 3A	2147	2024	1986	2205	1993	2073	2065	2112	588	676	584	431	671	728	683	587
Poco AXF 5Q	2086	1966	1966	2138	1936	2013	2005	2048	614	695	613	518	691	747	708	607

* Numerical designations used herein are based on discussions held between LASL and BCL personnel on September 29, 1977.

lower than if the high- and low-thermal conductivity directions were reversed. This is advantageous both for operation and as a lower initial temperature for reentry.

The effect of thermophysical property differences among real graphitic materials on clad temperatures is not large for any specific conceptual design. The clad temperatures with the two-dimensional Pyrocarb 903 material are consistently lower at the peak and higher at impact than for the other materials considered because of the low thermal conductivity in the across-lamina direction (z), when this direction was assumed to be parallel to the reentry flight direction. However, this effect is not of major significance. There are significant differences in clad temperature among the various conceptual designs. The high peak temperatures and low impact temperatures of the single fuel pellet module CD-4 make it less desirable as a reentry shape than the other designs. The lowest peak clad temperatures were experienced with CD-2 and CD-5. These designs had the continuous pyrographite insulation facing the broad faces because the fuel pellet insertion was from the side faces. These designs also had higher impact clad temperatures due to the insulating effect of the pyrographite during cooling. The highest impact clad temperatures were experienced with CD-6 however.

All the predicted peak clad temperatures exceed the eutectic melting temperature of 1800 C for the Pt-3008 clad. It does not appear that any simple design change or real material property change could decrease the peak to a level which would permit the use of Pt-3008. Many of the design material combinations would allow the use of iridium based on an allowable maximum clad temperature of 2100 C. However, no design comes close to meeting the doped iridium impact temperature minimum requirement of 930 C. Thus, no current conceptual design is thermally acceptable based on either iridium or Pt-3008 cladding temperature limits.

Computer generated clad temperature histories such as those shown in Figure 18 were generated for all of the conceptual design reentry analyses. The temperature histories shown in Figure 18 represent the maximum and minimum clad temperatures at any time during reentry. Peak clad temperatures occur at a flight Mach number of approximately 19, at which the module is assumed to be aerodynamically broadside stable or spinning. Representative maximum and minimum clad temperature histories for each of the conceptual designs are presented in Appendix E.

FIGURE 18. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN

For the broad side stable or spinning reentry mode of the heat source module, the clad temperatures at impact are expected to be lower than those shown in Table 3 (and in Tables 5 and 6) and Appendix E, primarily because of the assumption used in the reentry analysis that an adiabatic condition (i.e., no heat flow) exists at the leeward broad surface of the module. This assumption maintains higher temperatures within the module during the subsonic portion of the trajectory than if there were heat transfer across this leeward surface. However, impact clad temperatures higher than those obtained would be experienced if the heat source module tumbled about an axis parallel to one of the broad faces. In this reentry mode, each of the broad faces would be alternately windward and leeward, resulting in an overall lower cooling rate during the subsonic flight regime.

Table 4 shows the predicted maximum recessions and impact velocities for the conceptual designs. The impact velocities shown are for the designs with the graphite 994-2 reentry member. The effect of the different reentry member materials on impact velocities is quite small (≤ 3 ft/sec) and is due to material density differences. The higher impact velocity for CD-4 is a result of the lower drag coefficient for the single fuel pellet reentry shape. The lower impact velocity for CD-5 is caused by the larger broad face surface area and resulting lower ballistic coefficient. The predicted impact velocity for CD-3 with its side-on spinning reentry mode was the same as the average of all the other broadside stable designs.

The predicted maximum recession was only slightly dependent on the reentry material and the differences are probably not significant. For the two- and three-dimensional construction materials, augmentation of heating and mass loss due to surface roughness was not included in the performance analyses. These materials exhibit surface roughnesses greater than the bulk graphite materials and this factor should be considered in subsequent analyses. There are some differences among the designs with CD-4, the single pellet module, experiencing the greatest recession. The lower recession of the CD-3 reentry member is probably due to the assumed spinning reentry mode rather than any inherent design advantage. For most of the conceptual designs, the maximum recession during reentry is approximately one-half of the thickness of the reentry member.

TABLE 4. MAXIMUM RECESSION AND IMPACT VELOCITIES
FOR CONCEPTUAL DESIGNS

Reentry Member	Maximum Recession, Inch							
	CD1*	CD2	CD3	CD4	CD5	CD6	CD7	CD8
Graphite 994-2	0.11	0.10	0.08	0.12	0.09	0.10	0.11	0.10
Pyrocarb 903	0.10	0.09	0.08	NR	0.09	0.09	NR	0.09
AVCO 3D Mod 3A	0.11	0.10	0.07	0.12	0.09	0.09	0.10	0.09
Poco AXF 5Q	0.10	0.10	0.08	0.13	0.09	0.10	0.11	0.10
Impact Velocity, ft/sec								
	CD1	CD2	CD3	CD4	CD5	CD6	CD7	CD8
	181	174	179	192	167	184	182	170

* Numerical designations used herein are based on discussions held between LASL and BCL personnel on September 29, 1977.

Sensitivity Analyses for CD-1

Table 5 shows the results of additional sensitivity studies conducted for CD-1. All the results shown in Table 5 apply to the shallow angle reentry with a velocity of 36 kfps and a flight path angle of -5.3 degrees at 400 kft, except for the second entry, which is for the steep angle abort reentry (velocity of 36 kfps and flight path angle of -89.9 degrees). For the steep angle entry, the thermal and ablation response of the conceptual designs was determined to an altitude just below that corresponding to the generation of peak stresses in the reentry member. The thermal and ablation response of each conceptual design was determined from RTG breakup to impact for a 994-2 reentry member and Pyrocarb 903 impact member combination. Comparing the reentry member recession and peak clad temperature for the shallow and steep angle trajectories for CD-1, Cases 1 and 2 in Table 5, respectively, it can be seen that the recession and peak clad temperature are greater for the former reentry condition. Also, the clad temperature at impact on the steep angle trajectory is less than the lower limit temperature of approximately 930 C for maintaining the ductility of iridium alloys.

Also shown in Table 5 are the results of analyses conducted to (1) determine the effect graphite surface kinetics has on clad temperature and reentry member recession (Case 3), (2) determine the effect graphite surface chemistry (relative amounts of CO to CO₂ formed at the wall) has on clad temperature and surface recession (Case 4), (3) determine the maximum recession and extremes in clad temperature for different reentry member materials (Cases 5 and 6), and (4) determine the effect modifications to the impact member properties has on clad temperature and recession (Cases 7-11).

Kinetically-Controlled Regime. Case 3 shows the effect graphite surface kinetics has on clad temperature extremes and total surface recession. The measured surface kinetics of ATJ-S graphite can be characterized as "slow" whereas those for Poco AXF-5Q are the fastest of the bulk and carbon composite materials measured to date. The values for AXF-5Q are approximately equivalent to kinetics designated "moderate" by Scala in his original develop-

TABLE 5. SUMMARY OF RESULTS OF SENSITIVITY STUDIES FOR CONCEPTUAL DESIGN 1

Case No.	Reentry Member	Impact Member	Clad Temperature, C		Maximum Recession, In.	Special Conditions or Changes
			Peak	Impact		
1	994-2	903	2118	617	0.11	Standard Case for Design 1
2	994-2	903	1457	662	0.04	Steep Reentry Case
3	994-2	903	2191	615	0.09	ATJ-S Surface Kinetics (Slow)
4	994-2	903	2087	660	0.07	Maximum CO ₂ at Wall
5	901	903	2045	589	0.11	Pyrocarb 901 used as Reentry Member
6	823	903	2039	596	0.11	Pyrocarb 823 used as Reentry Member
7	994-2	CC-T50	2088	559	0.10	CC-T50 Impact Member High K in R Direction
8	994-2	CC-T50	2070	593	0.10	CC-T50 High Thermal K in Z Direction
9	994-2	CC-T50	2018	654	0.10	Wound CC-T50 High K in Wound Direction
10	994-2	903	2099	608	0.11	Extended Pyrographite Disc To 1.4 in. Diameter from 1.1 in.
11	994-2	903	2066	641	0.11	Reversed Conductivity on Impact Member (High K in Z Direction)

ment of graphite ablation theory*. Kinetics for AXF-5Q were used for this material and for 994-2 and Pyrocarb 903 for the majority of the reentry analyses (see Tables 3 and 4). Kinetic data available for 3D Mod 3a were used for this material. With other material properties and conditions remaining the same as for the standard Case No. 1, ATJ-S or "slow" kinetics (Case No. 3 in Table 5) results in a decrease in surface recession from 0.11 to 0.09 in, an increase in peak clad temperature from 2118 to 2191 C, and essentially no change in clad temperature at impact.

Graphite Surface Chemistry.. For all cases in Tables 3 and 4, the conservative assumption was made that the surface chemistry was such that all CO is formed at the wall. For other surface chemistry assumptions, such as 100 percent CO₂ formed at the wall or both CO and CO₂ are formed at the wall, the surface recession would be less than for the assumption of 100 percent CO formation. Case 4 in Table 5 shows the effect the assumption of 100 percent CO₂ formation has on clad temperature levels and surface recession. The peak clad temperature decreases by approximately 30 C from the standard case and the clad temperature at impact is approximately 45 C higher than for the standard case. There is a significant decrease in surface recession, from 0.11 to 0.07 in., for the assumption of 100 percent CO₂ formation at the wall. Most probably, the actual maximum recession will be between the values shown for Cases 1 and 3 in Table 5 because of the existence of both CO and CO₂ at the wall.

Figure 19 shows the stagnation point recession histories for the kinetic and surface chemistry assumptions considered in Table 5. In Figure 19, A corresponds to Case 1 in Table 5, B corresponds to Case 3, and C to Case 4. For reference purposes, transition from supersonic to subsonic flight occurs at a reentry time of 270 sec. It can be seen that most of the surface recession occurs during the hypersonic and supersonic flight regimes and very little occurs during subsonic flight.

* Scala, S. M., "The Ablation of Graphite in Dissociated Air, Part I, Theory", IAS Paper 62-154 (June, 1962); also GE Report TIS R62SD72 (September, 1972).

FIGURE 19. COMPARISON OF RECESSION IN BULK GRAPHITE WITH CHANGES IN KINETIC PARAMETERS -- CONCEPTUAL DESIGN NO. 1

Reentry Member Material. In addition to the reentry materials shown in Table 3, Pyrocarb 901 and 823 were considered for CD-1, and the clad temperature and recession results for these analyses are given in Table 5 (Cases 5 and 6, respectively). The lower across-lamina thermal conductivity of these materials in comparison with that of 994-2, resulted in a lowering of the peak clad temperature by 75 to 80 C and a smaller decrease in the clad temperature at impact. It does not appear that acceptable thermal performance can be achieved by reentry member material changes only for those materials available commercially.

Figure 20 shows the time histories of stagnation point recession for CD-1 with various reentry member materials. The recession histories shown in Figure 20 were determined assuming AXF-5Q kinetics; thus, the differences in recession are due to thermophysical property differences among the materials. For example, the higher recession experienced by Pyrocarb 901 is due to its lower density. Similar comparative recession histories for all of the conceptual designs are given in Appendix F. The differences in recession among materials for a given design are relatively small.

Impact Member Material. Table 5 also contains results of additional analyses for CD-1 in which the impact member material was changed (Cases 7-11). The Pyrocarb 903 impact material used in all the standard analyses was replaced by a carbon-carbon T-50 wound material in three different configurations (Cases 7-9). In Case 7, the C/C T-50 wound two-dimensional material was assumed to have its high thermal conductivity orientation in the r direction (standard orientation for both the reentry and impact members). This case showed the effect of property change only (lower thermal conductivity and density for the CC T-50). The peak clad temperature and clad temperature at impact were lower for this case than for the standard case. When the high thermal conductivity direction was assumed to be in the z direction in the impact member (Case 8), there was a decrease in both the peak clad temperature and the clad temperature at impact. For this case, the clad temperature at impact was higher than for Case 7. The beneficial effects obtained in

FIGURE . COMPARISON OF RECESSION AMONG ABLATOR MEMBERS

FIGURE 20. COMPARISON OF RECESSION AMONG ABLATOR MEMBERS --
CONCEPTUAL DESIGN NO. 1

Cases 7 and 8 were combined by assuming that the high conductivity direction was in the direction corresponding to windings around the clad (Case 9). For this assumption, the peak clad temperature was found to be approximately 100 C lower than the corresponding temperature for the standard case (Case 1) and the clad temperature at impact was found to be approximately 37 C higher than the corresponding temperature for the standard case. This somewhat surprising result can be explained by referring to Figure 18 which shows the clad temperature histories for the CD-1 standard case. There is a large temperature gradient in the clad which can be as high as 500 C at any given time. By placing the high conductivity direction of the impact member parallel to the clad or at least in the flight path direction, this large temperature difference in the clad can be reduced. Although the average clad temperature remains about the same, the peak of the maximum temperature is reduced and the minimum temperature at impact is increased. This effect of orientation of the impact member high thermal conductivity direction was also checked by reversing the conductivity of the 903 impact member, and a moderate beneficial effect was found as shown for Case 11 in Table 5. The extension of the pyrographite disc diameter to provide added insulating effect did not result in significant temperature change, however, see Case 10 in Table 5.

Analyses for Other Conceptual Designs

Table 6 shows the results of special analyses conducted for other conceptual designs. These limited analyses were performed to determine the effect that certain materials properties and module design features have on the clad temperature and reentry member recession. Accordingly, these analyses were conducted for the shallow angle ($\gamma = -5.3$ degrees) trajectory.

For CD-2, doubling the thickness of the pyrolytic graphite insulation resulted in a small, beneficial effect on clad temperatures. For CD-3, the direction of the impact member conductivity was reversed (high k in z direction). The clad temperature improvement realized for CD-1 when the high thermal conductivity direction of the impact member material was reversed was also obtained for this side-on reentry configuration.

TABLE 6. SUMMARY OF RESULTS FOR SPECIAL ANALYSES

Conceptual Design	Reentry Member	Impact Member	Clad Temperature, C		Maximum Recession, In.	Special Conditions or Changes
			Peak	Impact		
2	994-2	903	1993	692	0.10	Standard Case for Design 2
2	994-2	903	1945	703	0.10	Doubled Thickness of Pyrolytic Graphite
3	994-2	903	1999	615	0.08	Standard Case for Design 3
3	994-2	903	1937	646	0.08	Reversed Conductivity of Impact Member
3	903	903	1995	626	0.08	Design 3 with 903 Reentry Member
3	903	903	1829	684	0.08	Reversed Conductivity of Impact Member
3	903	903	2369	648	0.09	Decreased High Temperature Emissivity to 0.8
1	903	903	2040	613	0.10	Design 1 with 903 Reentry Member
1	903	903	2062	626	0.10	Decreased High Temperature Emissivity to 0.8
6	994-2	903	2028	752	0.10	Standard Case for Design 6
6	994-2	903	2020	778	0.13	Alternate Thermal Model
6	3-D MOD3A	903	2073	728	0.09	Design 6 with AVCO 3D Reentry Member
6	3-D MOD3A	903	2065	745	0.11	Alternate Thermal Model

For CD-3, the peak clad temperature experienced during reentry was found to be greatly affected by the surface emissivity of the reentry member. Decreasing the high temperature emissivity of the reentry member from 0.95 to 0.80 resulted in an increase of approximately 375 C in the peak clad temperature. However, as shown in Table 6, there was an increase in peak clad temperature of only 20 C for CD-1 when the reentry member emissivity was decreased from 0.95 to 0.80.

For CD-6, alternate two-dimensional thermal models were formulated to help determine the adequacy of the 2-D modeling approach. These models are discussed in detail in the Evaluation Approach, Reentry Thermal Analysis section of this report. As shown in Table 6, agreement in clad temperatures between the two limiting case models was quite good. However, there was a significant difference in the predicted recession. The higher recession predicted with the alternate model results from the graphite oxidation continuing further along the trajectory due to a hot spot on the surface of the reentry member. This result illustrates that three-dimensional thermal modeling considerations may be required in order to accurately evaluate the response of the trial designs to reentry.

In summary, the most significant results of the thermal analyses of the conceptual designs are:

- Peak clad temperature for all conceptual designs are too high to permit the use of Pt 3008
- Impact clad temperature for all designs are too low to permit the use of doped Ir clad
- Property changes within the range of real graphitic materials have relatively insignificant effects on peak clad temperature, clad temperature at impact, and reentry member recession
- Reentry member ablation protection is adequate in the current designs
- The single fuel pellet model CD-4 is the least desirable of the conceptual designs

- Surface chemistry effects can result in significant differences in recession of the reentry member, depending on the relative formation of CO and CO₂ at the surface
- Major design changes will be required for the trial design to meet all the clad temperature requirements with current materials.

Thermal Stress

Several conclusions may be drawn from this screening analysis. It was found that the maximum stresses and maximum strains occurred at or near interior corners of the reentry member along the broad, windward, face. This finding was consistent for all conceptual designs with all materials analyzed. This result is important because it indicates that in subsequent and more detailed stress analyses, a more refined mesh in these critical areas is necessary to accurately characterize the maximum stresses in a given design.

A second conclusion from this study is that for a given material, there were significant differences between conceptual designs in terms of the maximum stress and strain ratios. For example, the worst design for 3-D Mod 3A was Conceptual Design No. 3 which gave a $\epsilon_{\text{max}} / \epsilon_{\text{failure}}$ ratio nearly three times that of Conceptual Design No. 6, the best design. Similar comparisons for the other materials can be drawn from Figures 21 and 22. These results point to the importance that geometry plays in determining the relative performance of a material. Therefore, in future thermal stress analyses, at the very least, a 2-D analysis is essential.

Finally, this study revealed that both material and conceptual design must be considered together since they both affect the level of stress in a given design. That is, no material was found to be superior to all others for all the conceptual designs considered, when based on a maximum strain/strain-to-failure criterion.

FIGURE 21. RATIO OF MAXIMUM TENSILE STRESS TO ULTIMATE STRESS
VS. CONCEPTUAL DESIGN NUMBER

FIGURE 22. RATIO OF MAXIMUM TENSILE MECHANICAL STRAIN TO STRAIN-TO-FAILURE VS. CONCEPTUAL DESIGN NUMBER

Work This PeriodReentry Thermal Analysis
(G. R. Whitacre)Background and ScopeDescription of Conceptual Designs

The GPHS module conceptual designs were conceived by LASL and then supplied to BCL for thermal, ablation, and thermal stress analysis for reentry accident conditions. Seven designs were developed at LASL. Conceptual Design No. 4 was assumed to breakup during reentry and also to remain intact so that eight designs were analyzed at BCL. Detailed LASL drawings 26Y-79505, 79507, 79508, and 26Y-79494D1-1 were supplied for Conceptual Design No. 1 (CD-1) while only single assembly drawings were supplied for the other six designs. In this report, CD-1 is discussed in detail and only significant differences in the designs and in the results obtained are discussed for the other designs. (Complete sets of results for each design were supplied to LASL as the results were completed.)

The scope of the reentry evaluations is to (1) provide comparative information on the relative thermal and ablation performance of materials for a specific GPHS conceptual design and (2) provide comparative information on the relative thermal and ablation performance of various GPHS conceptual designs. Specific information provided in these analyses includes steady-state thermal profiles, temperatures of module components during reentry and at impact, thermal gradients within module components during reentry and at impact, surface recession of the reentry member, and impact velocity of the module.

The results of the thermal and ablation performance analyses will enable LASL to (1) rank materials in terms of their thermal and ablation performance, (2) select materials for more detailed evaluation in the trial designs, (3) identify and evaluate modifications to a module conceptual design in order to use technology-ready materials, (4) evaluate the sensitivity of thermal and ablation performance to material properties, and (5) rank specific conceptual designs and make selections for further detailed evaluations in the trial design phase.

In order to provide a relative ranking of the various conceptual designs, sufficient detail regarding the different design features must be included within the thermal and ablation analyses. The overall thermal performance of the various designs and the relative ranking of materials for a given design requires that multidirectional heat transfer to and within the heat source module be considered. Of particular importance is the difference in primary heat transfer paths during steady state and reentry and the influence of multidirectional heat transfer within the heat source module on clad temperature at impact. Also of significance in providing a more realistic yet cost-effective evaluation rationale, is the need to consider two-dimensional (at least) geometric constraints in determining thermal stress levels. This is discussed in a separate section of this report on Thermal Stress Analysis. Sufficiently accurate temperatures in at least two directions within the module components must be determined to properly account for the geometric effects on thermal stress levels.

Analyses of thermal response and ablation performance of GPHS module conceptual designs were performed using a two-dimensional, finite difference thermal model. The two-dimensional modeling approach provides sufficient accuracy to realistically screen materials, accounting for their anisotropy in thermal and mechanical properties, and to compare the performance of alternate conceptual designs by including important geometric features, e.g., webs, thread discontinuities voids, radii. Bulk graphite, 2-D and 3-D C/C composite materials were considered for the ablative reentry member. Alternate reentry materials were studied within the family of bulk graphite and 2-D C/C materials to determine the effect of material property variations on thermal and ablation performance. An alternate material was also considered for the impact member, primarily to determine the effect of material thermophysical property variations on thermal performance.

General Configuration of CD-1. The GPHS module Conceptual Design No. 1 is a 250 W(t) module containing four (4) 62.5 W(t) $^{238}\text{PuO}_2$ fuel pellets. Figure 23 is an assembly drawing of the first conceptual design. The module is a right square cylinder having a nominal broad face dimension of 95.9 mm (3.78 in.) and a nominal height of 51.6 mm (2.03 in.). The

corners of the module are chamfered and the edges are rounded. Each fuel pellet is inserted from a broad face of the module; two from one face and two from the opposite face, as shown in Figure 23. Specific information on the fuel, cladding, reentry member, and impact member are given below.

Specific.

Fuel. Conceptual Design No. 1 contains four 62.5 W(t) $^{238}\text{PuO}_2$ fuel pellets. The pellets are right circular cylinders, having nominal dimensions of 1.1 in. diameter x 1.08-in.-high each pellet is chamfered top and bottom and is nominally 86 percent of theoretical density.

Cladding. Each fuel pellet is individually contained in a nominally 0.025-in.-thick metal cladding consisting of a capsule cap and a capsule cup joined together by welding. A weld shield is used between the clad and fuel pellet to reduce heat losses during welding and to protect the fuel. Dop 26 iridium alloy or Pt 3008 alloy (or other suitable material) is used for the capsule. The physical properties of the iridium alloy were used in the thermal analyses. However, Pt-3008 properties are similar and the differences would have an insignificant effect on the thermal predictions. The corner radii of the clad were subsequently changed by LASL to 0.200 in. (8 x the thickness) rather than the 0.100 in. shown on the drawing and the larger radii were modeled in the thermal analyses.

Impact Member. Each GPHS clad pellet is contained within a graphite-based, primary impact member consisting of a top portion (cap) and bottom portion (cup) which are joined together using a lapped butt joint. The primary impact member is a hollow right circular, cylinder, nominally 1.58 in. diameter x 1.54 in x 0.020 in. thick. Inner and outer corner radii are 0.122 in. and 0.322 in., respectively. Selection of specific impact member materials for evaluation is to be based on requirements associated with the energy dissipated and consumed by the material during impact, considering the particular configuration of the GPHS module as it affects impact member material fabrication and property orientation, load-deformation characteristics of the fuel, and

FIGURE 23. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 1

the commercial availability of candidate materials. Also of potential interest in the selection of the impact member material is its thermal insulating effectiveness, especially for low angle reentry.

Reentry Member. The reentry member houses the clad fuel pellets in separate cavities; two clad assemblies are inserted from one of the broad side faces of the reentry member, the other pair of diagonally located clad assemblies are inserted from the second broad side face. The reentry member is to be made from a graphite based material. The specific material to be used will depend on the results of the activities concerned with the conceptual designs, trail designs, and prototype designs.

A thin (0.003-in.-thick) Grafoil compliance pad is located on both ends of the primary impact member, adjacent to it. A thin (0.041-in.-thick) pyrographite disc is placed between the compliance pad and the cavity in the reentry member to provide thermal insulation in the event of an exposure of the module to a ground launch fire or during a low-angle reentry. Threaded retainer plugs are used to maintain the clad fuel element, compliance pads, and pyrographite insulating discs in place. (A disc is also placed between the Grafoil compliance pad and the graphite threaded retainer plug.) The minimum thickness of the reentry member is 0.20 in.

General Configuration of Other Conceptual Designs. Table 7 shows the general configuration of all eight conceptual designs grouped according to the fuel pellet configuration. The basic right circular cylinder fuel pellet design with $L/D = 1.0$ was used for CD-1, 6, and 3. CD-6 is essentially 2 CD-1 modules joined along an edge to form a module with double the broad side surface area. Assembly drawings of all conceptual designs are given in Appendix A.

Figure 24 shows the assembly drawing for CD-3. Although this design uses the basic cylindrical fuel pellets, it is significantly different from the other designs that use cylindrical fuel pellets. In the reentry analysis, it was specified that the module came apart so that each stack of 4 pellets in a long, approximately cylindrical, graphite heat shield reentered individually. The four pellets are inserted from one end and separated by sliding graphite spacers. CD-3 is the only design that does not have a pyrographite disc between the reentry and impact members.

TABLE 7. GENERAL CONFIGURATION OF CONCEPTUAL DESIGNS

Fuel Configuration	Fuel L/D	Pellet W(t)	Conceptual Design No.	No. of Pellets	Total Watts (t)	Pellet Insertion
Basic Cylinder	1.0	62.5	1	4	250	Side Entry
			6	8	500	Side Entry
			3	16	1000	End Entry
Spherical	-	62.5	7	4	250	Edge Entry
Flattened Cylinder	0.75	62.5	4	1	62.5	Side Entry
			8	4	250	Side Entry
Triad Pellets	3.0	83.3	2	3	250	Edge Entry
Small Cylinder	1.0	27.8	5	9	250	Edge Entry

FIGURE 24. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 3

CD-7 is similar to CD-1 except the fuel pellets are spherically shaped and would be inserted from the edge surface. In CD-4, the fuel pellet is flattened to an L/D ratio of 0.75, and it is assumed that the module separates and the graphite heat shield around a single fuel pellet forms the reentry body. For CD-8, it was assumed that separation did not occur.

Figure 25 shows the assembly drawing of CD-2. This design contains the triad concept in which three individual fuel pellets are enclosed in a long cylindrical clad which is inserted from the edge face. For this case, the pyrographite insulation is continuous between the impact members and the reentry member facing the broad sides. CD-5 is similar to CD-2 except that the nine fuel pellets are individually clad.

Reentry Environment

Reentry environments of concern were based on a Titan III C propulsion vehicle and velocity-flight path angle information was derived from analyses previously reported.^(*,**) For purposes of screening materials and comparing the performance of the conceptual designs, the specific reentry conditions are not important. Reentry conditions which provide a trajectory of significant duration to approximate maximum surface recession and peak heat input for the ablation and thermal evaluations are adequate. Also, reentry conditions which provide a trajectory in which the convective heating rates are sufficiently high to generate severe stress levels in the ablative heat shield/reentry member are adequate. For this study, the evaluation of ablation and thermal response, that is, peak clad temperature, clad temperature at impact, and maximum recession of the reentry member, was based on using a shallow angle reentry, with a velocity of 36 kfps and a flight path angle of -5.3 degrees at 400 kft. This is the minimum angle for a non-skip reentry and results of previous analyses have shown this to be the critical trajectory for recession and clad temperature limits. Breakup of the RTG and exposure of the GPHS modules was assumed to occur at an altitude of approximately 271 kft, with a corresponding velocity of 36 kfps. This breakup condition is approximately the same as used by GE for MHW LES 8/9;^(**) no breakup analyses were available for LCHPG or GPHS

For example, (*) Bennett, G. L., et al, "Evaluating Aeroshell Materials for the MJS/Multi-Hundred Watt Heat Source", ANSP-M-14, ERDA/DNRA 3060-014, July, 1976. (**) "Multi-Hundred Watt Radioisotope Thermoelectric Generator Program, Final Safety Analysis Report, Volume II, Accident Model Document for LES 8/9" General Electric, March, 1975.

FIGURE 25. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 2

systems. The evaluation of thermal stress was based on steep angle reentry, with a velocity of 36 kfps and a flight path angle of -89.9 degrees at 400 kft. For this reentry condition, breakup of the RTG and exposure of the individual GPHS modules was assumed to occur at 200 kft. This altitude is sufficiently high to provide the peak aerodynamic heating rates experienced on this trajectory.

Impact velocity of the modules was determined from the trajectory analysis and module drag characteristics. Terminal velocity was achieved for each of the conceptual designs on both the shallow and steep angle trajectories.

Figure 26 shows the stagnation point convective heating rate history experienced on the windward broad face of the CD-1 GPHS module during the low reentry angle trajectory. For this reentry condition, radiative heating due to gas cap radiation is negligible. For this shallow angle reentry, the time from RTG breakup to impact is approximately 464 sec. The stagnation point convective heating rate history for the high reentry angle trajectory is shown in Figure 27. At the time of peak convective heating, the radiation heating rate is approximately 2 percent of the convective value. For this steep angle reentry, the time from RTG breakup to impact is approximately 212 sec. The peak total heating rate for this steep angle trajectory is approximately eight times greater than the peak heating rate for the shallow reentry angle reentry.

The reentry analysis code automatically accounts for differences in heating due to changes in effective nose radius, wall temperature, and ballistic coefficient. The code was developed by BCL for Sandia Corporation^{*,**} for use in evaluating both transpiration-cooled nose tips and RTG reentry. For conceptual designs CD-2, 5, 7, and 8 the stagnation point heating rate history is approximately the same as that shown in Figures 26 and 27 for CD-1. However, for CD-6 the peak heat flux is approximately 15

* Kortier, W. E., et al, Final Summary Report on "A Random Intact Reentry and Earth Burial Feasibility Study for Radioisotope Thermoelectric Generators", from Battelle to Sandia Corp. ASB-48-2481 Sandia Document SC-DC-65-1761, January 31, 1966.

** Grinberg, I. M., et al, Topical Report on "Development of an Analytic Model of the Response of Self-Contained, Transpiration Cooled Nose Tips to Reentry Environments," ASB-48-4845, May 10, 1968.

FIGURE 26. STAGNATION POINT HEATING RATE HISTORY FOR CONCEPTUAL DESIGN NO. 1, SHALLOW FLIGHT PATH ANGLE

FIGURE 27. CONVECTIVE HEATING RATE HISTORY FOR CONCEPTUAL DESIGN NO. 1 - STEEP FLIGHT PATH

percent lower than for CD-1 because of the increase in effective nose radius of the module. (Convective heat flux is inversely proportional to the square root of nose radius.) For CD-3 and 4 the peak stagnation heat flux is approximately 36 percent higher than for CD-1 because of the smaller effective radius of the reentry module.

Figure 28 shows the assumed heat rate and pressure distribution across the windward broad face and side face of the GPHS module. These distributions are applicable for both a broad side stable mode or a mode in which the module is spinning about an axis perpendicular to the broad face along the flight direction. This mode of reentry was assumed for all conceptual designs except CD-3.

The distributions shown in Figure 28 based on information reported by Coughlin & Lambert* and were used for both hypersonic and subsonic flow. The rapid fall-off in pressure at the corner was distributed over a finite distance to account for rounding of the corner. The leeward broad face was assumed to be adiabatic, that is, zero heat transfer rate. This assumption is conservative in terms of thermal stress considerations, i.e., predicted temperature gradients are greater than would be actually realized, has negligible effect on predicted surface recession, and is conservative in terms of clad temperature at impact, i.e., adiabatic back face assumption results in higher module temperatures during the long duration, cool-down portion of the trajectory. For the case of a tumbling (not spinning) heat source module, heating to all of the faces exposed to stagnation conditions, although averaged, must be accounted for.

Conceptual Design 3 breaks apart in reentry to become four long cylindrically-shaped reentry bodies. The heating rate and pressure distributions on this shape are different from distributions used for the other conceptual designs reentering broad side stable or spinning. It has been established that long cylinders such as the MHW heat source assembly reenter side-on. It was also assumed for this analysis that the cylinder

* Coughlin, T., and Lambert W., "Distribution of Heat and Pressure on the SNAP 19 Fuel Capsule in Hypersonic Flow", Martin Marietta Corporation Report MND-3607-93, March, 1967.

FIGURE 28. HEAT FLUX AND SURFACE PRESSURE DISTRIBUTION ON WINDWARD SIDE - CONCEPTUAL DESIGN NO. 1

was spinning about its longitudinal axis. This is a realistic assumption and allows the analysis to consider end effects while remaining two dimensional. (The worst case analysis of the side-on stable mode would require a three-dimensional analysis or neglecting of end effects.) For the side-on spinning case, integrated heating rate and pressure factors are used. For the heating rate, Klett^{*} determined that a factor of 0.269 for the side of a spinning cylinder and a factor 0.147 for the end surface should be multiplied by the stagnation point value in order to obtain the integrated heating rates. For the pressure factor, an integration of pressure around the cylinder resulted in a factor of 0.287 times the stagnation pressure. For the ends of the cylinder, an average side face pressure factor of 0.035 was obtained from the Figure 28 distribution.

* Klett, R. D., "Drag Coefficients and Heating Ratios for Right Circular Cylinders in Free-Molecular and Continuum Flow from Mach 10 to 30", Sandia Report SC-RR-64-2141.

Evaluation Approach

Thermal Models

Two-dimensional axisymmetric thermal analysis models were used to determine the thermal and ablation performance of the eight conceptual designs during reentry. Although most of the designs shown in Appendix A have certain three-dimensional features, it was decided that a full three-dimensional treatment was not warranted for this material screening and design comparison study. However, it was also recognized that the two-dimensional models had to be carefully formulated to meet a number of criteria. The models had to contain sufficient detail to permit valid comparison of the influence of the various geometric design features on module performance. Also, the models had to account for anisotropy in materials properties, especially with regard to thermophysical characteristics. Thus, the models had to be compatible with the orientation of the heat source modules as they would be fabricated from graphite and carbon composite billets, as shown in Appendix B. The models should provide a good representation of the module ballistic coefficient* so that correct trajectory and terminal velocity can be determined. Also, the models should provide a good approximation of the total heat input to determine the correct temperature levels. Finally, the models had to be directly applicable or translatable to the thermal stress models used.

Based on the above criteria, the following general procedure was used to develop the models for the eight conceptual designs. The broad face frontal area was calculated for each design including the reduction due to rounding of the corners. Using this surface area, an equivalent axisymmetric model diameter was determined. A cross section through the fuel pellet centerline was taken as the cross section to be used in the model with all dimensions preserved in the longitudinal or z direction. In this manner, the axisymmetric model diameter represents a distance which is numerically

* Ballistic coefficient, $\beta = \frac{W}{C_D A}$, where W = mass of module, C_D = drag

coefficient, A = frontal area. The terminal velocity is approximately proportional to the square root of the subsonic ballistic coefficient.

between the actual across flat dimension and the across corner dimension of the heat source module. The adjustment to match the model diameter was always taken near the model centerline, and distances in the r direction matched the actual cross section of the module at all other locations.

Thermal Model for CD-1. Figure 29 shows the resultant two-dimensional, axisymmetric thermal network used for CD-1. The section in this model corresponds to the section view on the assembly drawing Figure A1 given in Appendix A, except that the web at the center of the model is thicker than the across-flats web thickness of the module. When this thermal model is translated to the thermal stress model, the web thickness is readjusted to match the module critical minimum web thickness. This two-dimensional, finite nodal network is symmetric about the axis; that is, the vertical left-hand edge is facing the hot junction of the thermoelectric generators. Gaps of 0.010 in. are provided at material interfaces; these gaps were assumed to be filled with helium, with heat transfer by radiation and helium conduction. The nodes labeled "clad" actually include the 0.025-inch-thick clad material and the 0.010-in-wide gaps on either side of the clad. The density and conductivity are calculated for this combined node which permits a larger stable time step in the explicit solution of the heat conduction equation. Contact resistance is modeled at the thread regions of the retainer plug. A 30 percent contact area was assumed based on details of possible thread designs provided by LASL for CD-1. The remainder of the threaded region was assumed to be filled with helium.

Temperature-dependent properties of all materials, where available, were used in the thermal and ablation analyses. For the reentry and impact members, lack of suitable thermophysical and mechanical properties precluded consideration of some potential materials. Multidimensional thermal conductivity of the impact and reentry members was used based on available property data.

Because the thermal model is axisymmetric, the fuel is placed in a torus rather than in four distinct pellets. This results in a larger volume of fuel in the model than actually exists in the module. Therefore, the density

Air Flow
↓

Pyrographite Disc
Metal Capsule (Clad)
Primary Impact Container
Reentry Member

FIGURE 29. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSIS OF CONCEPTUAL DESIGN NO. 1

and volumetric heat generation of the fuel were reduced in the model so that the the total fuel weight and heat generation of the module would be exactly matched. The continuous fuel in the model tends to block one possible heat flow path from the interior to the side surface. However, the ratio of the clad surface area facing outward to that facing inward for the module is essentially preserved in the model and heat flow can occur around the top and bottom of the fuel pellets.

Thermal Models for Other Conceptual Designs. A similar procedure was followed in developing models for the other seven designs. The resulting thermal networks are given in Appendix C. Since the CD-3 and CD-4 heat source modules are nearly axisymmetric, the thermal models used to determine the response of these two modules are essentially exact geometric simulations of the designs. For CD-2 and CD-5, the revolved cross section is the top view of the assembly Figures A2 and A5, shown in Appendix A, such that the model contains an inner fuel pellet and an outer fuel torus. The volume of fuel, clad, impact member, and reentry member present in these modules are provided for in the thermal models.

Figure 30 shows the assembly drawing of CD-6. This heat source module is of a rectangular design, with two rows of fuel pellets, each row containing four fuel pellets alternately inserted from the broad faces. Because this heat source module is highly rectangular in its broad face dimensions (in contrast to the other conceptual designs which have broad face dimensions such that the modules are approximately "square" in cross section), modeling the reentry thermal response of CD-6 with an axisymmetric, two-dimensional approach represents a greater approximation than for the other conceptual designs. The thermal model selected to determine the response of CD-6 is shown in Figure 31. In this model, as with the other thermal models, the rectangular, broad face area of the heat source module was matched with a circular area. The cross section through a corner fuel pellet centerline (e.g., fuel pellet No. 1 in Figure 30) and through the minimum web thickness was preserved in the model with the model centerline occurring near the center of a second fuel pellet (e.g., No. 3 in Figure 30).

FIGURE 30. ASSEMBLY DRAWING FOR CONCEPTUAL DESIGN NO. 6

FIGURE 31. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 6

Thus, the outer fuel pellet in the model (shown to the left in Figure 31) represents a corner fuel pellet of the module (No. 1) and the inner fuel pellet in the model (shown to the right in Figure 31) models the fuel pellet adjacent to the corner pellet (No. 3 in Figure 30). For the thermal stress model, the temperature distribution around this fuel pellet adjacent to the corner fuel pellet was repeated to incorporate the second web between fuel pellets Nos. 3 and 4. This configuration was then analyzed for thermal stress response as a cut through the A-A section shown in Figure 30.

Because of the uncertainty in the accuracy of this model in determining the thermal and ablation response of the heat source module, an alternate model was developed in which a diagonal cut through a corner fuel pellet (e.g., No. 1) was made. In this alternate model, the larger web region between No. 1 and No. 6 fuel pellets (see Figure 30) was modeled. In both of the models, the heat source module fuel density and volumetric heat generation were adjusted to maintain the correct fuel weight and total heat generation. As discussed in the previous Summary section, there was excellent agreement in the clad temperatures predicted by the two models. From this result, it was concluded that the clad temperature level predictions for all of the two-dimensional models are reasonably accurate, within the framework of the assumed boundary conditions, because the same modeling approach was used for the other module designs as was used for CD-6. However, a higher reentry member recession was predicted at the stagnation region over the web with this alternate model than was predicted with the "standard" thermal model of CD-6. The greater recession is due to the maintenance of higher surface temperatures at the web on the windward broad face of the reentry member, as predicted by the alternate thermal model, during that portion of the trajectory when the heat source module is being convectively cooled (surface temperature is greater than gas recovery temperature). The relatively thick web region of the heat source module, as modeled by the alternate approach, provides for significant heat conduction through the reentry member from the hotter aft portion of the module. With the (standard) thermal model initially used for CD-6, the thinner web region (e.g., between fuel pellets Nos. 1 and 2 in Figure 30) was modeled and the smaller cross-sectional web area served as a greater restriction to heat conduction to the windward region. The higher web surface temperature experienced with the alternate model resulted in ablation of the reentry member to a lower

altitude than for the "standard" thermal model in which the web temperatures were lower. This result indicates that three-dimensional geometric effects may be important in regard to surface recession for some of the conceptual designs.

Ablation profiles were obtained for the conceptual designs from the two-dimensional model. Figure 32 shows the CD-1 module profile at impact relative to the initial configuration. The outline of the regainer plug with the threaded region and the pyrographite disc are shown in Figure 32. Possible locations and an outline of the spanner wrench holes are also shown. The recession is nearly uniform on the windward face with a rounding of the corner and less recession on the side.

Aerodynamic Characteristics

A broadside stable or spinning aerodynamic mode was assumed for the entire flight regime for all of the heat source modules, except for CD-3, which was assumed to reenter in a side-on spinning mode. If the modules tumble such that the broad faces are alternately windward and leeward, the clad would experience a lower peak temperature than predicted and the clad temperature at impact would be higher than predicted for the stable broadside mode.

The backface of the heat source module was assumed to be adiabatic, i.e., zero heat transfer across the face. This assumption is conservative at high flight Mach numbers when peak thermal stress occurs because it predicts temperature gradients greater than would be actually realized. At subsonic speeds, where the heat source module cools to the environment, the adiabatic backface assumption results in higher clad temperatures than would be actually realized for a broadside stable mode with heat transfer across this face.

Table 8 shows the summary of parameters relating to the trajectory analysis. The broadface dimensions are nominal across-flats distances and the broadface area includes the reduction due to rounding of corners. The "L" dimension is the same for both the actual module and the thermal model. The L/D ratios for all models except CD-3 and CD-4 are in the range of 0.36

FIGURE 32. MODULE PROFILE AT IMPACT CONCEPTUAL DESIGN NO. 1
Graphite 994-2 Reentry Member

TABLE 8. SUMMARY OF PARAMETERS RELATING TO TRAJECTORY ANALYSIS

Conceptual Design No.	Broadface Dimensions, in.	Broadface area, in ²	Model Diameter, in.	L Dimension, in.	L/D	Subsonic C _D	Weight, lbs	$\frac{W}{A}$, Lb/ft ²	Impact Velocity, Ft/sec
1	3.8 x 3.8	13.53	4.15	2.03	0.49	0.885	3.20	34.1	181
2	4.4 x 3.4	14.45	4.29	1.72	0.40	0.885	3.15	31.4	174
3	2.0 x 2.0	3.98	2.25	7.20	3.20	0.80	3.37	29.9	179
4	2.1 x 2.1	3.98	2.25	1.87	0.83	0.73	0.87	31.5	192
5	4.3 x 4.3	18.00	4.78	1.77	0.37	0.885	3.62	29.0	167
6	7.1 x 3.7	25.41	5.69	2.03	0.36	0.885	6.19	35.0	184
7	3.9 x 3.9	14.30	4.27	2.20	0.52	0.885	3.41	34.3	182
8	4.2 x 4.2	15.92	4.62	1.87	0.40	0.885	3.47	29.9	170

to 0.52. In the Wallops Island drop tests*, drag coefficients were determined by TES for shapes similar to those of the conceptual designs. In the Wallops Island tests, the nominal broad face dimensions were 4 x 4 in. and the L dimension varied from 1.7 to 2.0 in. For the broadside ablated models with rounded corners that were tested, no significant effect of either thickness or radius on drag coefficient was found. Therefore, the average drag coefficient of 0.885 determined in these tests for broadside ablated shapes was used for the modeled conceptual designs having L/D values within the range tested at Wallops Island.

Conceptual Design 4 has an L/D value outside the range tested by TES (L/D = 0.83). Hoerner shows that the subsonic drag of end-on cylinders with sharp corners shows a sharp decrease in drag coefficient as the L/D ratio increases from 0.6 to 1.0**. Therefore, the drag coefficient for CD-4 with its L/D = 0.83 was obtained by taking the value for sharp corners found in Hoerner and reducing it by an increment due to rounded corners. This increment was determined as the difference between the TES data and the correlation shown by Hoerner at an L/D = 0.4.

CD-3 is assumed to reenter as a side-on spinning cylinder, and drag coefficient data different from those used for the broadside stable modules are applicable. Carr and Marchman*** obtained data on a cylinder similar in shape to CD-3. These data include the effect of reverse boundary layer transition to laminar flow due to heating. They determined an average subsonic drag coefficient of 0.80 which was used for CD-3.

Figure 33 shows the drag coefficients as a function of altitude and velocity for the designs which were assumed to be broadside stable. These individual coefficients were summed to obtain the total drag coefficient. At high altitude, the drag coefficient as a function of altitude accounts

* Anderson, R., "LCHPG Heat Source Drop Test at NASA Wallops on August 30-31, 1976", TES Memorandum SIG-RHA-865, 29 April 1977.

** Hoerner, Sighard F., "Fluid-Dynamics Drag", published by the author, 1965.

*** Carr, S. R. and Marchman, J. F., III, "Drag of Heated Circular Cylinders, VPI report Aero-042, February, 1976.

FIGURE 33 . DRAG COEFFICIENT AGAINST VELOCITY
DRAG COEFFICIENT AGAINST ALTITUDE

Numerical designations used herein are based on discussions held between LASL and BCL personnel on September 29, 1977. These revised designations are being used effective October 1, 1972.

for free molecular and transitional effects on heat source module drag. Drag coefficients down to a Mach number of 0.6 were based on Hoerner; the data used to obtain the subsonic drag coefficient at lower Mach numbers has already been described.

Figure 34 shows the drag coefficients used for CD-3. Because the shape is similar to the GE MHW heat source assembly, the data developed by GE^{*} was used down to Mach 0.6 and the Carr and Marchman data was used for the drag coefficient below Mach 0.6.

In formulating the thermal models, the actual broad face surface area and the fuel weight of the heat source modules were duplicated. Thus, the model W/A values were approximately equal to the actual module W/A's. Therefore, the ballistic coefficient and resulting impact velocity determined from the thermal model should be accurate, assuming the validity of the aerodynamic stability orientations. CD-4 experienced the highest impact velocity (192 fps), primarily because of its low drag coefficient. The lowest impact velocity of 167 fps was predicted for CD-5 because of its high broadside area.

Mass Loss and Heat Transfer Models

The mass loss of the reentry member graphitic material in the ablation analysis is determined using the model developed by Scala and Gilbert^{**}. There are three regimes which are controlled by different mechanisms and each is treated differently in the mass loss model. In the sublimation regime characteristic of high surface temperatures, the mass loss is a function of the carbon concentration at the wall, which is, in turn, expressed as a function of the surface temperature and pressure. In the diffusion-controlled regime characteristic of moderate surface temperatures, the mass loss is determined by the diffusion of oxygen across the boundary layer to the surface and the products of reaction at the wall across the boundary layer. For this regime, a correlation is used which was determined by Scala^{**}

* GE, "Annual Report on MHW RTG Part II", GESP-7097, GEMS-415, 1 July, 1971 - 31 December, 1972.

** Scala, S. M., and Gilbert, L. M., "Sublimation of Graphite at Hypersonic Speeds", AIAA Journal, Vol 3, No. 9, September, 1965, pp 1635-1644.

FIGURE 34. DRAG COEFFICIENT AGAINST VELOCITY
DRAG FIGURE AGAINST ALTITUDE

from a number of solutions to the boundary layer equations. Because the correlation is based on the diffusion of oxygen, the actual mass loss of carbon depends strongly on whether CO or CO₂ is formed at the wall surface. In the kinetic-controlled regime characteristic of low surface temperatures, the mass loss is determined by the temperature and the experimentally-determined activation energy and collision frequency for a particular graphitic material. The model also includes a transition regime between the kinetic and diffusion regimes as the surface temperature increases. The same mass loss correlations are applied away from the stagnation region using the local pressure and temperature.

In the sublimation regime there is a very strong coupling between the surface temperature, the carbon mass loss, and the net heat transfer to the surface. Because of this strong coupling, an implicit (or simultaneous) solution of wall temperature is required using the heat transfer blocking coefficients and hypersonic heat-transfer correlation developed by Scala. This implicit solution is used only at the wall with an explicit solution used for internal or nodal temperatures.

The heat transfer correlation developed by Scala applies only to hypersonic flow and cannot adequately describe the subsonic and transonic flow experienced by the current configuration nor can it properly account for the cooling portion of the trajectory. Therefore, an alternate convection heat-transfer model is used for the diffusion and kinetic-controlled oxidation regimes. The aerodynamic convective heating rate without ablation is calculated by a model which is described later. This heating rate is reduced by applying blocking coefficients for the gas formed (CO or CO₂) and the mass loss rate. The heat of combustion of the products formed is then added to the blocked convective heating to give the net heat-transfer rate to the surface. Gas cap radiation is also added directly to this net heat transfer, and it is assumed that gas cap radiation does not interact with the ablation process except to raise the surface temperature. An empirical correlation developed and used by Sandia Laboratories for gas cap radiation is used in the BCL code. This correlation has the form

$$\dot{q}_r = A R \left(\frac{\rho_\infty}{\rho_s} \right)^{1.78} \left(\frac{V}{10^4} \right)^B$$

where

$\dot{q}_r$ = is gas cap radiation heating rate in Btu/ft²sec

R = the nose radius in ft

$\frac{\rho_\infty}{\rho_s}$ = the ratio of free stream density to sea level density

V = the velocity in ft/sec

A and B = are empirical constants.

The value of A and B are as follows:

A = 0 B = 0 when V < 25,000

A = 6.8 B = 12.5 when 25,000 ≤ V < 30,000

A = 0.003 B = 19.5 when 30,000 ≤ V < 35,000

A = 20.4 B = 12.5 when 35,000 ≤ V

This approximation correlation has been compared with results from more complex approaches and the agreement is acceptable. For the GPHS configuration, gas cap radiation is a small and a negligible part of the total heating rate.

The aerodynamic convective heating rate to the stagnation point without ablation is calculated using the Fay and Riddell* correlation equation directly in the supersonic and subsonic flight regimes. Values of the gas property parameters used in the correlation include the effect of gas dissociation, where appropriate. The dissociation fraction is determined by an empirical correlation. The stagnation point velocity gradient, which is an important parameter in the Fay and Riddell equation, is supplied as a tabular function of Mach number. For supersonic flow, this gradient was obtained from modified Newtonian theory; for subsonic flow, potential theory was used.

For hypersonic flow, in the absence of material sublimation, a real-gas correlation formula developed by Lewis and Burgess** is used to

* Fay, J. A., and Riddell, F. R., "Theory of Stagnation Point Heat Transfer in Dissociated Air", Journal of the Aeronautical Sciences, Vol 25, No. 2, February, 1958, pp 73-85.

** Lewis, C. H., and Burgess, E.G. III, "Altitude-Velocity Table and Charts for Imperfect Air", AEDC-TDR-64-214 (AD 454078) January 1965, 135 pp.

determine the unblocked convective heating. This correlation is based on a large number of solutions for a wide range of reentry conditions using the Fay and Riddell formula and is easier and faster to use in the computer model. It implicitly contains hypersonic approximations.

The heat transfer model also calculates the heating rate in the free molecular flow regime. The switch to free molecular heating at high altitudes is made when the calculated free molecular heating rate becomes less than the calculated continuum heating rate.

Material Property Input

Following the development of the thermal model of the heat source module design and the determination of the trajectory using a point mass trajectory code, material properties were entered for all of the module components. Bulk graphite, 2-D and 3-D C/C composites were considered for the reentry member. Pyrocarb 903 was generally used as the impact container material. Anisotropic material properties were used; the property orientations have already been discussed previously in this report.

Iridium properties were used for the clad material. However, the results obtained are applicable for Pt-3008 clad because of the similarity in thermophysical properties of the material alloy systems. The fuel was assumed to be high density $^{238}\text{PuO}_2$ with a heat generation of 0.4 watt/gm.

Material properties and input needed for the thermal and ablation analyses are as follows:

- Thermal conductivity
- Specific heat
- Density
- Emissivity
- Oxidation kinetics
 - collision frequency
 - activation energy
- Chemistry at wall
- Contact conductance.

Thermophysical property values for the various materials used in the analysis are presented in a subsequent section. Where property data were

unavailable for some materials (for example, oxidation kinetics, emissivity), engineering estimates were made based on the available data for the other materials considered. In some cases, parametric analyses were performed to determine the sensitivity of module response to property uncertainties.

Steady-State Performance of Heat Source Modules

An initial estimate of the steady-state temperature distribution was made and entered into the computer program, assuming a hot junction temperature of 900 C. This hot junction temperature was used for all of the analyses. The heat conduction equations for the two-dimensional matrix were solved for steady-state conditions using an accelerated relaxation technique. Figure 35 shows the resulting temperature profile at the mid-plane for CD-1. Similar temperature profiles are shown for all designs in Appendix D. When the material for one of the heat source components was changed, the steady-state temperature distribution was recalculated. However, the previous result was used as the initial estimate and the time required for the new solution was quite small.

In Figure 35, it can be seen that during steady-state operation the primary temperature gradient is in the fuel. The temperature differences across the gaps exceed those in the graphitic materials. The dashed lines in Figure 35 are the straight line interpolations between nodal temperatures used in the model.

The steady-state temperature distribution was used as the initial condition for the transient analyses beginning at the breakup altitude. Breakup altitude for the RTG was assumed to be 271 kft for the shallow angle reentry and 200 kft for the steep angle abort reentry. These breakup altitudes were based on analyses conducted by GE for the MHW LES 8/9, in the absence of analyses performed specifically for the GPHS system.

Following breakup of the RTG at the prescribed altitude, the thermal history of the module was determined as was the surface recession at each of the surface or exterior nodes.

FIGURE 35. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 1
Graphite 994-2 Reentry Member

Transfer of Temperature Profiles
for Thermal Stress Analysis

The complete thermal profile within the module was transferred by punched cards for analysis of thermal stress levels for the steep flight path angle trajectory. The time at peak thermal stress conditions was selected by examining the temperature gradient pattern within the reentry member for several times prior to, at, and following peak aerodynamic heating, and was chosen when the external surface temperature of the reentry member at the windward corner reached its maximum value.

Thermal Stress Analysis
(L. E. Hulbert, J. Strenkowski)

Background and Scope

A study was undertaken to estimate the thermal stresses in conceptual designs of the General Purpose Heat Source (GPHS) with various reentry member materials. The analysis was performed to determine the thermal stresses in 2-dimensional cross sections of the reentry members at maximum heating rates for the high γ reentry trajectory. Since the purpose of this study was to screen the designs and materials under consideration, a general three-dimensional analysis was not warranted. On the other hand, it was believed that a one-dimensional analysis would not expose the effects of the geometrical differences in the conceptual designs on the induced reentry thermal stresses. Very little mechanical interference is expected between the reentry member and the fuel capsule impact protection material at the temperatures observed at maximum heating. This allowed the thermal stress model to be restricted to the reentry member itself even though the entire fuel capsule assembly had to be modeled for the thermal analysis.

The finite element code DOASIS was used to perform the thermal stress analysis of the 2-D reentry member models. A description and comparison of this code with other available codes has been presented in an earlier progress report.* For this screening study, the temperature field given by the thermal analysis was used as input to DOASIS. To avoid generating a separate mesh, the same grid was used for both the thermal and thermal stress analyses. By using similar meshes, the temperature field could be used directly in the stress analysis without any interpolation procedures. The resulting savings in staff and calendar time outweighed the loss of accuracy in modeling the stress concentration effects of the corners of the fuel cavities.

*"Informal progress report for January, 1976, on Task 67, Impact Members; Task 68, Reentry Materials", Contract W-7405-eng-92.

Materials

A total of eight conceptual designs and four materials were considered. The materials were 994-2 bulk graphite, POCO AXF5Q bulk graphite, Pyrocarb 903, and AVCO Mod 3a. In general, complete material property data were not available for all of these materials. When data were missing, reasonable assumptions were made to complete the property models. The type of model used for each material is summarized below. Obviously, this approach would not be as acceptable for a detailed design analysis.

994-2. The property data available for this bulk graphite material manufactured by Union Carbide were obtained from Report AFML-TR-76-117^{*}. The data were complete and fairly recent. This material was modeled as transversely isotropic in the $r - \theta$ plane which was denoted as the "with" grain direction of the material. The Z-direction (normal to the plane of isotropy) was denoted as the "across" grain direction. In addition, 994-2 was assumed to be mono-modulus (i.e., identical properties in compression and tension).

Pyrocarb 903. The available property data for Pyrocarb 903,^{**} a carbonaceous fiber-reinforced composite manufactured by Hitco, indicated that it should be modeled with different properties in tension and compression. Due to the incompleteness of the data, it was assumed that all properties were isotropic except for the coefficient of thermal expansion. This property was assumed to be transversely isotropic, where $r - \theta$ was the plane of isotropy. Poisson's ratio was not provided in the report for Pyrocarb 903. Therefore, a Poisson's ratio of 0.10 was chosen as a nominal value for all temperatures.

* H. Stuart Starrett, "Probable Values for the Mechanical and Thermal Properties of 994-2 Graphite", AFML-TR-76-117, February, 1977.

** "The Thermal and Mechanical Properties of Pyrocarb 409 and Pyrocarb 903", SORI-EAS-76-041, January, 1976.

3-D Mod 3a. The property data for Mod 3a, an orthotropic 3-D weaved material manufactured by AVCO, were given in report AFML-TR-74-211.* The data were sufficiently complete to model this material as transversely isotropic in the $r - \theta$ plane. The tensile and compressive properties reported were nearly equal so that a mono-modulus analysis could be conducted. However, Poisson's ratio was not given and a nominal value of 0.10 was assumed.

AXF5Q. Material property data for AXF5Q, a highly isotropic bulk graphite manufactured by POCO Graphite, Inc., were readily available in a previous Battelle Informal Progress.** All the properties were available to model this material as isotropic and mono-modulus.

Analysis Approach

As mentioned previously, the DOASIS code was used to perform the thermal stress analysis. Although this code is capable of analyzing thermoelastic-plastic behavior, plasticity was ignored in this study. In addition to being prohibitively expensive, there was a lack of plasticity property data available for all materials. Therefore, a linear elastic analysis was considered appropriate for this screening study. This limitation undoubtedly underestimates the performance of the Pyrocarb 903 and 3-D Mod 3-A reinforced composites, since such materials exhibit significant strength beyond their elastic limit.

In order to model the three-dimensional reentry members as two-dimensional, a simplifying assumption was necessary. A plane strain assumption was used for those modules that remained intact during reentry. This assumption was used for all conceptual designs except Nos. 3 and 8. For these designs, break-up into individual fuel capsules was assumed to have occurred so that an axisymmetric condition was employed. The axisymmetric assumption more closely approximates the reentry condition of designs Nos. 3 and 8 than did the plane strain approximation for all other conceptual designs. Therefore, the stress

* "Mechanical and Thermal Properties of Mod 3a, A Pierced Fabric Carbon-Carbon Material", AFML-TR-74-211, December, 1974.

** "Informal Progress Report for February, 1976, on Task 67, Impact Members; Task 68, Reentry Materials", Contract W-7405-eng-92.

results for designs Nos. 3 and 8 are believed to be more accurate than for the other designs.

The mesh for the reentry member of one of the conceptual designs (CD 1) is shown in Figure 36. This mesh is identical to the thermal analysis grid except for minor adjustments in the overall dimensions. Advantage was taken of the symmetry along the Z-axis so that only one fuel capsule had to be modeled. Note that this mesh, which was well-suited to the thermal analysis, was not as appropriate for the stress analysis. In particular, a more refined mesh should be used in the fuel cavity corners, where higher stresses are expected. Also, the aspect ratio for some elements (i.e., ratio of the lengths of the sides of an element) was very high. For displacements which are nearly equal in the r and z directions, an aspect ratio of unity is desirable. Higher aspect ratios result in stresses that are artificially low. Therefore, these elements could lead to overly nonconservative stresses. Fortunately for this design, the critical stresses did not occur at elements with the highest aspect ratios. Due to the screening nature of this study, however, the thermal meshes were judged to provide adequate stress results.

Thermal Stress Results

The stresses due to the peak temperature field induced during reentry were computed for reentry members for all conceptual designs. For Conceptual Design No. 1 (shown in Figure 36) with Pyrocarb 903, the temperature distribution along the leading face of the reentry member is shown in Figure 37. A plot of the deformed shape of the reentry member due to this temperature field is shown in Figure 38. The corresponding plot of the in-plane stress (σ_r) is shown in Figure 39. Note from this three-dimensional plot that tensile stresses occur along the inside edge of the leading face and compressive stresses occur along the outside edge. In particular, the maximum tensile stress occurs at the inside corner of the fuel cavity.

Several measures were used to rank all the materials analyzed. The criteria selected were based upon the maximum tensile stress and the

FIGURE 36. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 1

FIGURE 37. TEMPERATURE DISTRIBUTION ALONG LEADING FACE OF CONCEPTUAL DESIGN 1 REENTRY MEMBER WITH PYROCARB 903

FIGURE 38. REENTRY MEMBER DISPLACEMENT PLOT FOR CONCEPTUAL DESIGN 1 WITH PYROCARB 903

FIGURE 39. RADIAL STRESS (σ_r) FOR CONCEPTUAL
DESIGN 1 WITH PYROCARB 903

maximum tensile strain. In Table 9 are shown the maximum tensile and compressive stresses as well as the percentage of tensile yield and ultimate stress for Conceptual Design No. 1. Note that the maximum tensile stress always occurs at the inside corner of the fuel cavity for all materials (element 159 in Figure 36). Based on the stress/ultimate stress ratio, 3-D Mod 3a gave the lowest ratio and Pyrocarb 903 gave the highest. In Table 10, the maximum tensile strains and the corresponding strain/strain-to-failure ratios are shown for the four materials used for Conceptual Design No. 1. Although the location of the maximum tensile strain has changed from that of the maximum tensile stress, these maxima still occur near the inside fuel cavity corners. In contrast to the stress/ultimate stress results in Table 9, the strain/strain-to-failure data indicate that AXF5Q is a better material to use than 994-2 for Conceptual Design No. 1.

Results similar to these figures and tables for the other conceptual designs analyzed are presented in Appendix G. For all eight conceptual designs, the maximum tensile stress always occurs at a corner, or near an interior web, if one exists. Furthermore, Mod 3a always gives the lowest maximum stress/ultimate stress ratio, with 994-2, AXF5Q, and Pyrocarb 903 giving ascending ratios.

The results shown in Tables 9 and 10 may be cross-plotted to show the relative ranking of the conceptual designs for a given material. In Figure 40, one ranking is shown based on the ratio of maximum tensile stress to the yield stress. As shown in this figure, 3-D Mod 3a gives lower stresses than 994-2, irrespective of which conceptual design is chosen. As a second measure of relative performance, the ratio of maximum tensile stress to the ultimate stress was used (see Figure 21). This figure shows that, in general, 3-D Mod 3a gave lower ratios than all other materials analyzed. It should be noted that both the material and conceptual design cannot be considered separately. For example, Conceptual Designs Nos. 3 and 4 with 3-D Mod 3a had maximum stress/ultimate stress ratios nearly twice as large as Conceptual Designs Nos. 1 and 2. Furthermore, for a given design (such as Conceptual Design No. 3), there was no noticeable difference between 3-D Mod 3a and 994-2.

TABLE 9 . SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 1

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Max. Stress (Ksi)	Max. Stress (Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	Max. Stress (Location)
994-2	4143.2	3.61	159	102.3	56.2	5564.8	2.27	211
AXF5Q	3788.4	9.81	159	-	93.4	5603.8	7.97	212
Pyrocarb 903	2480.1	12.22	159	-	122.2	5620.0	13.69	215
3-d Mod 3a	3953.2	2.61	159	45.8*	29.5	5632.4	1.97	211

* Based on compressive yield stress

TABLE 10. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 1

Material	Temp (°F)	Max. Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4357.8	3.72	161	0.76
AXF5Q	3788.4	9.31	159	0.63
Pyrocarb 903	2732.4	2.72	169	1.21*
3-d Mod 3a	3953.2	2.96	159	0.32

*Based on $\epsilon_{\text{failure}} = \frac{\sigma_{\text{ult.}}}{E}$

FIGURE 40. RATIO OF MAXIMUM TENSILE STRESS TO YIELD STRESS
VS. CONCEPTUAL DESIGN NUMBER

When the ratio of strain/strain-to-failure is used as a ranking criterion, the advantage of using 3-D Mod 3a is not as significant as was shown in Figure 21 (see Figure 22). In addition, AXF5Q appears to be a more desirable material candidate when ranked on the basis of strain-to-failure data. This is due in part to the increased strain-to-failure data for AXF5Q as compared to 994-2 for temperatures in excess of about 3000 F (see Figure 41).

FIGURE 41. STRAIN-TO-FAILURE DATA VS. TEMPERATURE
FOR AXF5Q AND 994-2

Surface Chemistry Studies
(A. A. Boiarski)

Background and Approach

The objective of this subtask was to determine the boundary layer and wall chemistry for candidate heat shield materials at simulated reentry conditions. These data, when combined with a suitable theoretical model, will result in improved criteria for specifying the reentry member (ablator) thickness and a more accurate determination of the clad temperature at impact by removing ambiguities due to surface oxidation effects.

Program Scope

Due to time and funding limitations, the boundary layer chemistry and temperature data could not be obtained this fiscal year using the Raman scattering technique. However, the required instrumentation is now on hand and the Raman effort can proceed as originally planned during the next fiscal year if adequate funding is available.

In order to obtain some basic data which would still be beneficial to the program for the ranking of candidate reentry member materials, our attention was shifted to the microprobe, gas sampling technique. With this method, the chemistry of the stable species (i.e., CO, CO₂, O₂) near and at the wall can be determined; however, the gas temperature in the boundary layer cannot be measured. It was decided to first concentrate on the wall chemistry measurement which did not require the presence of a probe in the boundary layer itself that could possibly perturb the flow field.

Experimental Approach

A special quartz microprobe/graphite sample configuration was designed which satisfied the requirements to sample the high-temperature surface gases, to freeze the chemistry at a point near the surface,

and to prevent other gases from entering the probe. As shown in Figure 42, this design incorporated a 7-mm-diameter hemispherically tipped probe with a 100- μ m (0.1-mm) diameter hole through which the gas sample passed. A corresponding 0.15-mm-diameter x 2.0-mm-long hole was drilled in the graphite sample to allow surface gases to be sampled by the microprobe (see Figure 42). Gas pressure inside the probe body was held at a low value of approximately 10 mm-Hg by pumping on the probe with a vacuum pump. The pressure exterior to the probe was always near ambient conditions (i.e., 760 mm-Hg). This large pressure difference provided for free jet expansion of the gases as they entered the probe tip. This expansion rapidly cooled the gas sample and thereby severely retarded any further chemical reactions involving CO and CO₂ from taking place. The small mass flow rate through the probe hole also insured that the sample was extracted with a minimum perturbation of the wall boundary layer flow field. The boundary layer thickness^{*} was estimated to be 1-mm. The cooled sample gases passed out of the test region through various plumbing connections to a sampling apparatus. There, the gases were collected in 200-cc bottles for later analysis.

To minimize any in-leakage of gases from the backside of the graphite sample, a cylindrical quartz collar was used to effect a seal between the probe and the graphite sample. Several rings were machined around the small diameter portion of the graphite sample (see Figure 42). Graphfoil (graphite sheet material, 0.13-mm thick) was wrapped around the cylinder and then the quartz probe was slipped over the Graphfoil to obtain a moderately tight fit. When the sample was heated, the graphite cylinder diameter thermally expanded approximately 0.17 mm to effect the final seal. To continually check seal effectiveness, argon gas was always introduced into the cavity behind the sample. If any leak occurred, argon was detected in the gas sample bottle during the post-run gas analysis.

Photographs of the graphite sample and quartz sampling probe are shown in Figure 43. Also shown in Figure 43 is the split-cylinder alumina sample holder used to contain the graphite sample during the test.

* The boundary layer is the distance from the wall at which the gas velocity is equal to 99 percent of the freestream value.

FIGURE 42. MICROPROBE AND GRAPHITE SAMPLE DESIGN USED TO OBTAIN WALL CHEMISTRY MEASUREMENTS

FIGURE 43. PHOTOGRAPHS OF GRAPHITE SAMPLE AND QUARTZ MICROPROBE IN THE ALUMINA SAMPLE HOLDER

Quartz wool insulation material was inserted into the alumina holder cavity along with the argon bleed tube, and the two halves of the alumina holder were joined together. This assembly of components was then inserted into a hole in the 10-cm-diameter Micarta aero-shape container that housed induction coils used to heat the graphite sample. A photograph of the Micarta holder with a graphite sample in place is shown in Figure 44. Also shown in Figure 44 is the assembled sample and holder installed in the test apparatus.

Room air was blown over the heated graphite sample at a velocity of approximately 100 ft/sec (30.5 m/sec) using a ducted fan as shown in Figure 44. The shape of the Micarta aero-shell holder and the air velocity were picked to simulate a GPHS module configuration during the low-altitude (i.e., $p \approx 1$ atm), subsonic portion of reentry. Air pressure and velocity were held constant in these tests and the wall temperature was varied by varying the amount of induction heater plate current. In this manner, the graphite sample wall temperature could be adjusted and kept relatively constant over a 500-1600 C range. The surface temperature was monitored using an infrared pyrometer with a strip chart recorder as an output recording device.

Shown in Figure 45 is the gas sampling apparatus used to collect samples for later analysis. Pyrex glass was used wherever possible to reduce the chance of contamination and to eliminate the need for metal to glass seals between the sampling apparatus and the 200-cc glass bottles used to collect the sample. These bottles were evacuated before the test and were kept closed until the time of filling from a common glass chamber which was continually pumped on (i.e., flushed out) to insure that the bottled sample was representative of the gas entering the microprobe tip. A pressure gauge was used to monitor the probe pressure during the filling process (see Figure 45).

The experimental procedure was as follows. The fan was turned on and allowed to achieve maximum speed. Then, the heater was activated and the graphite sample was slowly brought up to temperature using the induction current control. After a constant temperature was attained,

FIGURE 44. PHOTOGRAPH OF GRAPHITE SAMPLE INSTALLED
IN HIGH TEMPERATURE TEST APPARATUS

FIGURE 45. PHOTOGRAPH OF GAS SAMPLING APPARATUS

approximately 10 seconds was allowed to elapse before a bottle was opened. This time period was more than triple the time required to refill the entire 190-cc volume of the gas sampling system. Then, a sampling bottle was opened and approximately 10-20 seconds was provided to fill the bottle. The bottle was then closed and the induction heater current control was either re-adjusted to obtain a new sample temperature or was turned off if the graphite sample was severely oxidized during the 20-30 seconds of test time. Sample bottle opening and closing times were marked on the strip chart recording of temperature to ascertain the degree of surface temperature nonuniformity during the sampling period.

The bottled gas samples were analyzed in the following manner. Approximately one-half the collected sample was extracted for analysis in a quadrupole mass spectrometer. This analysis procedure gave quantitative values for the CO_2 , O_2 , argon, and H_2 in the sample bottle. Since CO and N_2 are very close in mass, only the $\text{CO} + \text{N}_2$ total was ascertained in the mass spectrometer analysis. To determine the amount of CO in the sample, a Gas Chromatograph Analyzer (GCA) was used on the remaining one-half of the bottle sample. The combined mass spectrometer/GCA analysis provided for an accurate quantitative analysis of the sampled gases.

Several gas samples were discarded due to probe plugging and leakage problems, which occurred mostly during the initial checkout. However, for the later tests, the experimental procedure was quite routine.

Results

Valid gas samples were obtained for ten wall temperatures from 700 to 1350 C, including one repeat sample at a constant temperature but different sample hole length. The graphite material used for these experiments was Poco-type HD1. This is a high-purity graphite material of the same average density as Poco AXF-5Q. The oxidation threshold is reported by Poco to be 50 C less than the AXF-5Q type.^(*)

Results of the gas sample analysis for CO and CO_2 wall concentrations are shown in Figure 46 as a function of wall temperature.

*"Nominal Properties and Characteristics Comparison Charts", Poco Graphite, Inc.; Decatur, Texas, 1977.

FIGURE 46. MEASURED CO AND CO₂ WALL CONCENTRATIONS AS A FUNCTION OF GRAPHITE SURFACE TEMPERATURE -- POCO HD1

Note that as the oxidation threshold is exceeded (above approximately 800 C), CO_2 dominates the chemistry at the surface. The amount of CO_2 increases to a peak value of 12 percent at a temperature of approximately 1000 C and then decreases rapidly above this temperature. A small amount of CO was measured at low wall temperatures; as the wall temperature is increased above 1000 C, the measured CO concentration increases rapidly. Extrapolations would indicate nearly all CO would be present at and above 1500 C. In the temperature range from 1000 to 1500 C, both CO and CO_2 are present in substantial yet varying amounts.

In Figure 47, the measured oxygen concentration at the wall is shown as a function of wall temperature. The concentration decreases rapidly with increasing temperature. These data on available oxygen at the wall agree with the overall theory of graphite chemistry which assumes that available wall oxygen becomes limited by diffusion through the boundary layer at elevated temperatures.

Also shown in Figures 46 and 47 (i.e., flagged symbols) are the measurements obtained at nearly the same temperature but immediately following one another. These results show that the graphite sample hole length did not have a pronounced effect on the measured wall chemistry at this temperature level. This is encouraging because of the concern that some chemical reactions might occur within the sampling hole length before the gases reached the probe tip.

The ratio of CO to CO_2 concentration (i.e., $\lambda = \frac{[\text{CO}]_w}{[\text{CO}_2]_w}$) is an important parameter that indicates the quantitative effect of wall chemistry on ablation rate and heat input to the wall. This parameter is shown plotted in Figure 48 as a function of wall temperature for the measurements obtained in this study. Also shown in Figure 48 are two theoretical estimates of λ versus wall temperature.*** The Air Force Materials Laboratory (AFML) theory** was given for a pressure of 5.85 atm, and was extrapolated to a surface pressure of 1 atm using a linear relationship obtained from Scala's results* for 1 and 10 atm conditions. Both of these theoretical estimates assume Scala's "fast" graphite kinetics.

* Scala, S. M., IAS Paper No. 62-154, 1962.

**Bernstein, H., and Baron, J. R., AFML-TR-69-84, Part IV, Vol. II, December, 1969.

FIGURE 47. MEASURED OXYGEN CONCENTRATION AVAILABLE AT THE WALL AS A FUNCTION OF GRAPHITE SURFACE TEMPERATURE -- POCO HD1

FIGURE 48. COMPARISON OF THE MEASURED RATIO OF CO TO CO₂ WALL CONCENTRATIONS WITH THEORY

From Figure 48, it can be seen that the measurements for Poco-HD1 graphite agree most closely with the AFML theory at temperatures above 900 C. The dashed line in Figure 48 was obtained from the smoothed curves through the data in Figure 46.

Discussion of Results and Conclusions

The results obtained in the present graphite chemistry study are very encouraging. The measured variation of the ratio of CO to CO₂ concentration at the wall with temperature was in qualitative agreement with trends based on theoretical considerations. There is some scatter in the data, especially at the low oxidation rates characteristic of wall temperatures below 900 C, where the concentrations of both CO and CO₂ are small. This scatter is believed to be caused by measurement inaccuracies at the low concentration levels and possible residual CO absorbed in the quartz probe tip which was driven off during heating.

The sampling method used in these experiments can be applied to determine the surface chemistry of other candidate graphitic reentry member materials. Coupled with a nominal effort to incorporate the experimental results into the reentry ablation analyses, a more realistic performance evaluation of the candidate reentry member materials can be obtained.

For other graphitic materials having surface chemistry characteristics different than Poco-HD1, that is, different CO and CO₂ wall concentration levels at a given wall temperature, it is expected that the data shown in Figure 46 will shift with temperature (horizontally) and/or in concentration (vertically). For other graphitic materials, the resulting correlation of the concentration levels with temperature will primarily shift with temperature (horizontally) although there could be slight changes in slope of the correlation curve shown in Figure 48 for Poco-HD1.

The effect that surface chemistry has on the recession of the reentry member is presented elsewhere in this report. In Figure 19, the stagnation point recession history is shown for GPHS Conceptual Design No. 1 for the two extreme assumptions of all CO at the wall (Curve A) and all CO₂

at the wall (Curve C) for the maximum integrated heating trajectory. In the former case, the total recession is approximately 0.11 in., whereas the total recession is 0.07 in. for the assumption of all CO_2 at wall. For the assumption of 50 percent CO-50 percent CO_2 formation at the wall, the total recession is approximately 0.09 in. By knowing the surface chemistry characteristics of the reentry member material, potential weight and volume savings of the heat source module can be determined. For example, Table 11 shows the heat source module weight and volume savings that can be achieved for Conceptual Design No. 1 for various reentry member wall thicknesses using the current 0.20 in. wall thickness as a baseline. These values are based on a wall thickness 50 percent greater than for the three wall chemistry assumptions, as shown in Table 11. Substantial savings in reentry member weight and heat source module weight and volume can be potentially realized if CO_2 or CO and CO_2 are present at the wall during reentry when oxidation is occurring. The reduction in overall heat source weight and volume to be realized by decreasing the reentry member thickness can be achieved through knowledge of the surface chemistry characteristics of the material. This information also provides for increased confidence in the design and performance of the modules.

Savings in weight and volume can be realized for the entire RTG system based on the reductions in heat source module weight and volume and potential decrease in dimensions for the thermoelectrics, insulation, and other system components. Specific overall system potential weight and volume savings and increased power density (weight and volumetric) can be estimated as the overall RTG system is defined.

TABLE 11. POTENTIAL WEIGHT SAVINGS RELATED TO
REENTRY MEMBER WALL THICKNESS -
CONCEPTUAL DESIGN No. 1

GRAPHITE REENTRY MEMBER						HEAT SOURCE MODULE		
Wall Thickness, in.	Total Recession, in.	Wall Surface Chemistry Assumption	Volume, in ³	Weight, lb	Weight Savings, Percent	Weight, lb	Weight Savings, Percent	Volume Savings, Percent
0.20 ⁽¹⁾	0.11	100% CO	10.7	0.70	-	3.2	-	-
0.11 ⁽²⁾	0.07	100% CO ₂	6.1	0.40	43.	2.9	9.4	16.6
0.14 ⁽²⁾	0.09	50% CO-50% CO ₂	7.6	0.50	29.	3.0	6.4	11.3
0.17 ⁽²⁾	0.11	100% CO	9.1	0.60	15.	3.1	3.2	5.8

(1) Baseline wall thickness for CD-1.

(2) Thickness based on increasing total recession by 50 percent.

Material Performance and Data Compilation
(R. E. Hess)

The collection of information on candidate reentry and impact member materials has as its objective a compilation of mechanical and thermophysical properties, performance (ablation, thermal stress, and impact) data together with their descriptions (constituents, constructions, processing) and comments on their availability, fabricability, and expected cost.

The materials to be considered as candidates were limited to those materials that are currently considered to be:

- (1) Available in the forms and sizes that may be required
- (2) Of good and consistent quality
- (3) Characterized to a significant extent (so that the property data needed for ablation, thermal, and thermal stress analyses do not have to be measured).

Summarized below is information pertaining to the primary materials considered for the GPHS reentry and impact members and the associated thermophysical properties. Information on additional materials considered in the analysis is also summarized.

Material Candidates

The material candidates can be considered in two groups: the bulk graphites and the carbon/carbon composites. For any given material to be a suitable candidate, it was necessary to first establish its availability. It was then necessary to secure adequate data related to its thermophysical properties as input to the analysis of the conceptual designs. Within these constraints and the time available for this data assembly, the majority of the effort was devoted to obtaining information for four bulk graphites and three carbon/carbon composites.

The Bulk Graphites. Property data were obtained for the following bulk graphites:

<u>Manufacturer</u>	<u>Designation</u>
Union Carbide	994-2
"	ATJ
"	ATJ-S
Pure Oil	POCO AXF5Q.

These several materials are individually considered below.

Union Carbide 994-2^{(1)*}. This material is a custom-produced bulk graphite intended to have improved mechanical properties in comparison with those of ATJ-S. It was prepared for a specific Air Force program and is not currently in production. Preparation of it would be on a minimum order basis with an estimated nominal cost of \$2000 per billet. The billet size would be 4 inches in diameter and 9-1/2 inches long.

Union Carbide ATJ. Grade ATJ⁽²⁾ is an extremely fine-grain, essentially flaw-free, high-strength, premium quality graphite. It can be machined to very close limits and sharp detail with fine surface finish. The maximum grain size is approximately 6 mils and the ash content does not exceed 0.2 percent. Grade ATJ comes in a variety of standard sizes for the cylindrical- and rectangular-shaped billets. The nominal price is about \$3 per pound, and procurement is obtained by a routine ordering procedure.

Union Carbide ATJ-S. ATJ-S^(2,10) is an improved version of ATJ. The details related to these improvements are proprietary. The composition is basically the same as ATJ, but the size of the pieces are generally smaller and the processing is controlled more closely. These considerations lead to greater homogeneity within the billet. The nominal price is approximately \$45 per pound, and the procurement is a routine ordering procedure.

* Superscripts in parentheses indicate references listed on pages 140 and 141.

Pure Oil Company POCO AXF5Q. The POCO Graphites^(9,10) are a standard line of products in widespread use. The price range is that of a commercial graphite and the procurement is a routine ordering procedure. AXF5Q is available in bulk plate, flat plate, and solid cylinder forms as well as other configurations. In bulk plate form, sizes from 1-1/2 x 4 x 6 inches to 4 x 5 x 8 inches are available, with a price range from approximately \$20 to \$26 per pound. Flat plate sizes range from 1/8 x 4 x 6 inches to 1/2 x 4 x 6 inches, with prices ranging from \$34 to \$26 per pound. In solid cylinder form, cylinder sizes from 3/4 inch in diameter x 12 inches long to 8 inches in diameter x 12 inches long are available, with prices ranging from \$56 to \$26 per pound.

The Carbon/Carbon Composites. Property data were obtained for the following carbon/carbon composites:

<u>Manufacturer</u>	<u>Designation</u>
Hitco	Pyrocarb 903
Hitco	Pyrocarb 901
Avco	3D-Mod 3a.

The Pyrocarb⁽³⁾ materials are a series of carbonaceous fiber-reinforced composites. Pyrocarb can be manufactured in practically any configuration and size and the reinforcing fibers can be oriented to control properties in specified directions within a component. The Pyrocarb 900 Group is manufactured by transforming an organic resin matrix with carbon or graphite reinforcing fibers into a reinforced carbon or graphite matrix using a proprietary high-temperature process resulting in a high-density system.

Pyrocarb 903. Pyrocarb 903 can be manufactured in plates, billets, or hollow shapes such as cylinders, cones, and frustums. It is characterized by a high-elastic modulus and an available density range of 1.60 to 2.00 gm/cm³. Approximate cost is \$150 per pound with a nominal delivery times of 6 weeks.

Pyrocarb 901. Pyrocarb 901 can also be manufactured in plates, billets, and hollow shapes such as cylinders, cones, and frustums. It is characterized by a low-elastic modulus and an available density range of 1.45 to 1.85 gm/cm³. Approximate cost is \$150 per pound with a nominal delivery times of 6 weeks.

Avco 3D-Mod 3a⁽⁴⁾. Mod 3a is an orthotropic material made up of layers of WCA fabric in the xy-plane with bundles of Thornel-50 fibers pierced through the fabric in the longitudinal z-direction. In this Mod 3a material, the warp and fill directions of the WCA fabric are systematically alternated so that the x and y properties of the billet are equal. This alternation of direction is the modification from earlier practice that distinguishes Mod 3a. The nominal billet size for this material is 4 x 4 x 8 inches. The material was originally a customized development and no cost data are available.

Thermophysical Property Data

The physical property data needed for the analytical studies are

- (1) Thermal Conductivity
- (2) Density
- (3) Specific Heat
- (4) Emissivity
- (5) Reaction Kinetics.

Thermal Conductivity. In general, graphites and carbon/carbon composites are anisotropic, resulting in directionally-sensitive values for the thermal conductivity.

The Bulk Graphites. Figure 49 is a composite plot of the thermal conductivity in the with-grain direction for the bulk graphites considered during the conceptual design analyses. UC-994-2 forms the upper bound of the conductivity range and AXF5Q forms the lower bound.

Figure 50 is a composite plot of the thermal conductivity in the across-grain direction for these bulk graphites. The range in thermal conductivity at a given temperature is less for the across-grain direction than for the with-grain direction. Above 1000 F, the variation in thermal conductivity among these three materials in the across-grain direction is approximately 10 percent.

The Carbon/Carbon Composites⁽⁴⁻⁶⁾. Figure 51 is a composite plot of the thermal conductivity in the with-lamina direction for the carbon/carbon composites. At low temperatures, below 1000 F, there is very little difference between the Pyrocarb 903 wl and 994-2 wg bulk graphite thermal conductivities.

Figure 52 is a composite plot of the thermal conductivity in the across-lamina direction for the carbon/carbon composites. The thermal conductivity values for the three Pyrocarb materials are lower than for 3D-Mod 3a and are practically identical to each other.

Density. The density values used for the materials considered in the analyses are as follows:

Bulk Graphites

UC-994-2	1.801 gms/cm ³	6.58×10^{-2} lb/in. ³
ATJ	1.77 gms/cm ³	6.40×10^{-2} lb/in. ³
ATJ-S	1.83 gms/cm ³	6.61×10^{-2} lb/in. ³
POCO AXF5Q	1.78 gms/cm ³	6.43×10^{-2} lb/in. ³

Carbon/Carbon Composites

Pyrocarb 903	1.89 gms/cm ³	6.84×10^{-2} lb/in. ³
Pyrocarb 901	1.61 gms/cm ³	5.82×10^{-2} lb/in. ³
Avco 3D-Mod 3a	1.63 gms/cm ³	5.89×10^{-2} lb/in. ³

Specific Heat. In general, the specific heat for both bulk graphites and carbon/carbon composites has been considered an insensitive property since these materials regardless of form are, on a weight basis, essentially carbon. Figure 53 shows the temperature variation of specific

FIGURE 50. COMPARISON OF THERMAL CONDUCTIVITY -- ACROSS GRAIN -- AMONG BULK GRAPHITES

FIGURE 51. COMPARISON OF THERMAL CONDUCTIVITY -- WITH LAMINA -- AMONG CARBON/CARBON COMPOSITES

FIGURE 52. COMPARISON OF THERMAL CONDUCTIVITY -- ACROSS LAMINA -- AMONG CARBON/CARBON COMPOSITES

FIGURE 53. AVAILABLE SPECIFIC HEAT DATA FOR GRAPHITIC MATERIALS

heat for ATJ-S⁽¹⁾ and Pyrocarb 903⁽⁵⁾. This curve for ATJ-S was included in the literature for UC-994-2 as representative data. Numerical values taken from some earlier analytical work with POCO AXF5Q also agree with this curve.

Emissivity. The emissivity of the bulk graphites and the carbon/carbon composites is poorly characterized. Figure 54 shows the total hemispherical emissivity-temperature variation used for the current analysis (solid curve).⁽⁹⁾ The data upon which this curve is based were obtained for National Carbon Company graphites. Also shown in Figure 54 are plots of the normal total emittance for ATJ graphite⁽¹³⁾ and the emissivity for POCO AXFQ1⁽¹⁴⁾. Emissivity data for other materials could not be found.

Reaction Kinetics The data related to reaction kinetics were taken from Reference (7). The data provide the slope and intercepts for an Arrhenius plot assuming a one-half power dependence of the static pressure. Teledyne Energy Systems has also obtained experimental kinetic data for POCO AXFQ1, FMI PN 48381, and Graphnol.⁽⁸⁾

Supplementary Materials

A limited number of reentry analyses were performed using materials other than those previously considered. Thermal conductivity values for these materials are given below.

Thornel-50 Wound Carbon/Carbon Composite. Thornel-50 wound C/C⁽¹¹⁾ composite was considered as an alternative impact member material. Figure 55 shows the thermal conductivity in the with-lamina direction for Thornel-50 wound and Pyrocarb 903 composites. Figure 56 gives the thermal conductivity temperature dependence for these two materials in the across-lamina direction. In the with-lamina direction, the T-50 wound material has a lower thermal conductivity than does the Pyrocarb 903.

Pyrocarb 823 - Carbon/Carbon Composite. Pyrocarb 823 was considered primarily because of its low thermal conductivity.⁽³⁾ The

FIGURE 54. REPRESENTATIVE EMISSIVITY DATA FOR GRAPHITIC MATERIALS

FIGURE 55. COMPARISON OF THERMAL CONDUCTIVITY--WITH LAMINA--FOR PYROCARB 903 AND THORNEL-50 WOUND CARBON COMPOSITES

FIGURE 56. COMPARISON OF THERMAL CONDUCTIVITY--ACROSS LAMINA--FOR PYROCARB 903 AND THORNEL-50 WOUND CARBON COMPOSITES

thermal conductivity of this material is shown in Figures 51 and 52 with-lamina and against-lamina directions, respectively. This material has a density of 1.61 gm/cm^3 ($5.81 \times 10^{-2} \text{ lb/in.}^3$) which is lower than the 994-graphite or the Pyrocarb 903 material.

Pyrocarb 406 Carbon/Carbon Composite. Pyrocarb 406 is a relatively low-density (1.4 gm/cm^3), two-dimensional carbon composite.⁽³⁾ The thermal conductivity-temperature dependence of this material is shown in Figure 57. Thermal analyses were not conducted for Pyrocarb 406, either as the reentry or the impact member.

Carbitex 700 Carbon/Carbon Composite. Carbitex 700 was considered as a potential reentry member material during the initial project startup. The thermal conductivity-temperature dependence of this material is shown in Figure 58. This material has a nominal density of 1.46 gm/cm^3 . No conceptual design thermal analyses were conducted using Carbitex 700.

FIGURE 57. THERMAL CONDUCTIVITY OF PYROCARB 406 CARBON COMPOSITE

FIGURE 58. THERMAL CONDUCTIVITY OF CARBITEX 700 CARBON COMPOSITE

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APPENDIX A

ASSEMBLY DRAWINGS

APPENDIX A

Contained in this Appendix are the assembly drawings of the eight conceptual designs that were analyzed for thermal, thermal stress, and ablation response during reentry of the heat source modules.

Figure A1. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 1

FIGURE A2. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 2

FIGURE A3. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 3

FIGURE A4. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 4

FIGURE A5. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 5

FIGURE A6. ASSEMBLY DRAWING FOR CONCEPTUAL DESIGN NO. 6

FIGURE A7. ASSEMBLY DRAWING OF GPHS CONCEPTUAL DESIGN NO. 7

FIGURE A8. ASSEMBLY DRAWING CONCEPTUAL DESIGN NO. 8

APPENDIX B

PRIMARY ORIENTATION OF THE HEAT SOURCE MODULES

APPENDIX B

Contained in this Appendix are sketches showing the primary orientation of the heat source modules in the graphite or carbon-carbon composite billets. The orientations were selected such that the low thermal conductivity direction for the anisotropic materials was parallel to the primary heat flow direction during reentry of the heat source module.

FIGURE B1. ORIENTATION OF CONCEPTUAL DESIGN NO. 1 IN GRAPHITIC BILLET

FIGURE B2 . ORIENTATION OF CONCEPTUAL DESIGN NO. 2
IN GRAPHITIC BILLET

FIGURE B3. ORIENTATION OF CONCEPTUAL DESIGN NO. 3 IN GRAPHITIC BILLET

FIGURE B4. ORIENTATION OF CONCEPTUAL DESIGN NO. 4 IN GRAPHITIC BILLET

FIGURE B5. ORIENTATION OF CONCEPTUAL DESIGN NO. 5
IN THE GRAPHITIC BILLET

FIGURE B6. ORIENTATION OF CONCEPTUAL DESIGN NO. 6 IN GRAPHITIC BILLET

FIGURE B7. ORIENTATION OF CONCEPTUAL DESIGN NO. 7 IN GRAPHITIC BILLET

FIGURE B8.. ORIENTATION OF CONCEPTUAL DESIGN NO. 8 IN GRAPHITIC BILLET

APPENDIX C

NETWORKS USED TO MODEL THE THERMAL RESPONSE OF
THE HEAT SOURCE MODULE CONCEPTUAL DESIGNS

APPENDIX C

Contained in this Appendix are the networks used to model the thermal response of the heat source module conceptual designs.

Air Flow
↓

Conceptual Design No. 1

Pyrographite Disc
Metal Capsule (Clad)
Primary Impact Container
Reentry Member

FIGURE C1. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 1

FIGURE C2. THERMAL NETWORK USED FOR ABLATION AND ABLATION ANALYSIS OF CONCEPTUAL DESIGN NO. 2

FIGURE C3. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 3

FIGURE C4 . THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 4

FIGURE C5. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 5

Air Flow
↓

Conceptual Design No. 6

Pyrographite Disc
Metal Capsule (Clad)
Primary Impact Container
Reentry Member

FIGURE C6. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 6

FIGURE C7 . THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 7

FIGURE C8. THERMAL NETWORK USED FOR THERMAL AND ABLATION ANALYSES OF CONCEPTUAL DESIGN NO. 8

APPENDIX D

STEADY-STATE TEMPERATURE PROFILES

APPENDIX D

Contained in this Appendix are the steady-state temperature profiles for each of the heat source modules based on the thermal networks given in Appendix C. A hot junction temperature of 900 C was used for all of the analyses. The dashed lines shown in Figures D-1 through D-8 are the straight line interpolations between node temperatures used in the thermal analysis.

The temperature profiles given in this Appendix are for a 994-2 reentry member and Pyrocarb 903 impact member.

FIGURE D1. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 1 WITH GRAPHITE 994-2 REENTRY MEMBER

FIGURE D2. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 2
 WITH GRAPHITE 994-2 REENTRY MEMBER

FIGURE D3. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 3--GRAPHITE 994-2 REENTRY MEMBER

FIGURE D4. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 4 WITH GRAPHITE 994-2 REENTRY NUMBER

FIGURE D5. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO.5 WITH GRAPHITE - 994-2 REENTRY MEMBER

FIGURE D6. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 6 WITH GRAPHITE 994-2 REENTRY MEMBER

FIGURE D7. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN No. 7 WITH GRAPHITE 994-2 REENTRY MEMBER

FIGURE D8. TEMPERATURE PROFILE FOR CONCEPTUAL DESIGN NO. 8

Graphite-994-2 Reentry Member/Case 501/EU

APPENDIX E

TYPICAL CLAD TEMPERATURE HISTORIES

APPENDIX E

Contained in this Appendix are typical clad temperature histories for each of the conceptual designs. Shown in Figures E-1 through E-8 are the time-dependent maximum and minimum clad temperatures. These clad temperatures were determined for the shallow angle ($\gamma = -5.3$ degrees) trajectory with 994-2 as the reentry member. Clad temperature histories for other reentry member materials and the steep angle abort trajectory were calculated, but are not included in this Appendix.

Maximum and Minimum Clad Temperatures C

FIGURE E1. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 1 WITH 994-2 GRAPHITE

Maximum and Minimum Clad Temperatures

FIGURE E2. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 2 WITH 994-2 GRAPHITE

FIGURE E3. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 3 WITH 994-2 GRAPHITE

FIGURE E4. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 4 WITH 994-2 GRAPHITE

FIGURE E5. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 5 WITH 994-2 GRAPHITE

FIGURE E6. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 6 WITH 994-2 GRAPHITE

FIGURE E7. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 7 WITH 994-2 GRAPHITE

FIGURE E8. MAXIMUM AND MINIMUM CLAD TEMPERATURE HISTORIES FOR CONCEPTUAL DESIGN NO. 8 WITH 994-2 GRAPHITE

APPENDIX F

REENTRY MEMBER RECESSION HISTORIES

APPENDIX F

Contained in this Appendix are the reentry member recession histories for each of the conceptual designs and reentry member materials considered in the conceptual design analysis. The recession histories shown in Figures F-1 through F-8 were determined for the maximum integrated heating, shallow angle ($\gamma = -5.3$ degrees) trajectory. POCO AXF-5Q graphite reaction kinetics was used for AXF-5Q, 994-2, and Pyrocarb 903. MOD-3A kinetics were used for the 3-D MOD 3A carbon composite.

FIGURE F1. COMPARISON OF RECESSION AMONG ABLATOR MEMBERS - CONCEPTUAL DESIGN NO. 1

FIGURE F2. REENTRY MEMBER RECESSION
HISTORY FOR CONCEPTUAL
DESIGN NO. 2

FIGURE F3. REENTRY MEMBER RECESSION
HISTORY FOR CONCEPTUAL
DESIGN NO. 3

FIGURE F4. REENTRY MEMBER RECESSION HISTORY FOR CONCEPTUAL DESIGN No. 4

FIGURE F5. REENTRY MEMBER RECESSION HISTORY
FOR CONCEPTUAL DESIGN NO. 5

FIGURE F6. REENTRY MEMBER RECESSION HISTORY FOR
CONCEPTUAL DESIGN NO. 6

FIGURE F 7. REENTRY MEMBER RECESSION HISTORY
FOR CONCEPTUAL DESIGN NO. 7

FIGURE F8. REENTRY MEMBER RECESSION HISTORY
FOR CONCEPTUAL DESIGN NO. 8

APPENDIX G

This Appendix contains the finite element meshes, displacement plots, and summary tables for some of the conceptual designs considered in this screening study. The finite element meshes and corresponding displacement plots for Conceptual Designs Nos. 2 and 8 are not included due to their similarity to Conceptual Design Nos. 5 and 1, respectively.

TABLE G1. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 2

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Maximum Stress (Ksi)	(Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	(Location)
994-2	4030.6	3.50	92	95.6	54.2	5160.9	2.66	125
AXF5Q	3729.4	9.57	92	-	92.46	5007.9	9.85	125
Pyrocarb 903	2391.6	12.59	92	-	126.53	5022.1	14.75	129
3-d Mod 3a	3859.1	2.51	92	42.5*	28.85	5184.1	2.17	125

* Based on compressive yield stress

TABLE G2. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 2

Material	Temp. (°F)	Max Tensile Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4322.5	3.51	97	0.72
AXF5Q	3729.4	9.40	92	0.68
Pyrocarb 903	2391.6	2.75	92	1.32 [*]
3-d Mod 3a	4114.1	2.83	97	0.27

*Based on $\epsilon_{\text{failure}} = \frac{\sigma_{\text{ult.}}}{E}$

FIGURE G1. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 3

FIGURE G2. DISPLACEMENT PLOT FOR CONCEPTUAL
DESIGN 3 WITH PYROCARB 903

TABLE G3. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 3

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Maximum Stress (Ksi)	(Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	(Location)
994-2	2759.3	4.04	93 (σ_r)	92.0	59.1	4018.4	4.12	107
AXF5Q	2779.9	10.00	1 (σ_z)	-	114.94	5206.7	12.72 (σ_z)	80
Pyrocarb 903	-	-	-	-	-	-	-	-
3-d Mod 3a	2621.8	3.80	81 (σ_θ)	57.8*	55.72	4252.4	2.93 (σ_r)	107

*

Based on compressive yield stress.

TABLE G4. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 3

Material	Temp. (°F)	Max. Tensile Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	2957.1	3.07 (ϵ_z)	1	0.62
AXF5Q	5331.5	6.47 (ϵ_r)	88	-
Pyrocarb 903	-	-	-	-
3-d Mod 3a	2587.4	2.51 (ϵ_θ)	85	0.72

FIGURE G3. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 4

FIGURE G4. DISPLACEMENT PLOT FOR CONCEPTUAL DESIGN 4
WITH PYROCARB 903

TABLE G5. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 4

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Maximum Stress (Ksi)	(Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Maximum Stress (Ksi)	(Location)
994-2	3336.9	5.27(σ_{θ})	73	120.9	78.0	3456.1	7.54 (σ_{θ})	10
AXF5Q	2815.4	9.49(σ_{θ})	73	-	109.08	3850.1	14.69 (σ_{θ})	10
Pyrocarb 903	-	-	-	-	-	-	-	-
3-d Mod 3a	3166.7	4.22(σ_{θ})	73	63.2*	56.4	4199.1	5.01(σ_{θ})	10

*Based on compressive yield stress.

TABLE G6. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 4

Material	Temp. (°F)	Max. Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3} \right)$	(Location)	
994-2	3336.9	2.48 (ϵ_{θ})	73	0.50
AXFQ5	2815.4	5.23 (ϵ_{θ})	73	0.81
Pyrocarb 903	-	-	-	-
3-d Mod 3a	3166.7	2.85 (ϵ_{θ})	73	0.62

FIGURE G5. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 5

FIGURE G6. DISPLACEMENT PLOT FOR CONCEPTUAL DESIGN 5
WITH PYROCARB 903

TABLE G7. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 5

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Maximum Stress (Ksi)	(Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	(Location)
994-2	3990.0	3.33	92	89.8	51.5	5029.5	2.90	125
AXF5Q	3740.0	8.93	92	-	85.05	5013.2	9.61	123
Pyrocarb 903	2384.4	11.88	87	-	119.40	4961.1	14.73	129
3-d Mod 3a	3966.1	2.27	99	40.0*	25.51	5085.2	2.38	125

*Based on compressive yield stress.

TABLE G8. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 5

Material	Temp. (°F)	Maximum Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4249.4	3.38	97	0.69
AXF5Q	3832.4	9.09	99	0.57
Pyrocarb 903	2552.3	2.58	99	1.19*
3-d Mod 3a	4059.6	2.75	97	0.28

*Based on $\epsilon_{\text{failure}} = \frac{\sigma_{\text{ult.}}}{E}$

FIGURE G7. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 6

FIGURE G8. DISPLACEMENT PLOT FOR CONCEPTUAL DESIGN 6
WITH PYROCARB 903

TABLE G9. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 6

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Max. Stress (Ksi)	(Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	(Location)
994-2	4160.7	2.77	142	78.9	43.2	5425.5	2.54	187
AXF5Q	3672.1	8.32	142	-	80.97	5286.7	10.11	187
Pyrocarb 903	-	-	-	-	-	-	-	-
3-d Mod. 3a	4064.5	1.94	145	36.1*	21.56	5511.6	2.03	187

* Based on compressive yield stress.

TABLE G10. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 6

Material	Temp. (°F)	Maximum Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4361.8	3.14	147	0.64
AXF5Q	3672.1	8.24	142	0.64
Pyrocarb 903	-	-	-	-
3-d Mod 3a	4064.5	2.50	145	0.25

FIGURE G9. FINITE ELEMENT MESH FOR CONCEPTUAL DESIGN 7

FIGURE G10. DISPLACEMENT PLOT FOR CONCEPTUAL DESIGN 7
WITH PYROCARB 903

TABLE G11. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 7

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Max. Stress (Ksi)	Max. Stress (Location)	Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress (Ksi)	Max. Stress (Location)
994-2	3738.4	4.48	91	112.0	68.3	4744.9	4.38	118
AXF5Q	3486.0	10.54	91	-	107.55	5282.0	13.38	119
Pyrocarb 903	-	-	-	-	-	-	-	-
3-d Mod 3a	3517.3	2.91	91	43.8*	36.28	4809.2	3.46	118

* Based on compressive yield stress.

TABLE G12. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 7

Material	Temp. (°F)	Max. Tensile Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4142.2	3.61	95	0.74
AXF5Q	3696.3	9.92	99	0.75
Pyrocarb 903	-	-	-	-
3-d Mod 3a	3750.6	2.99	99	0.39

TABLE G13. SUMMARY OF THERMAL STRESS RESULTS FOR CONCEPTUAL DESIGN NO. 8

Material	Tensile Stress Results					Compressive Stress Results		
	Temp. (°F)	Max. Stress		Stress/Yield (%)	Stress/ Ultimate (%)	Temp. (°F)	Max. Stress	
		(Ksi)	(Location)				(Ksi)	(Location)
994-2	4038.4	3.60	159	98.6	55.8	5574.0	2.34	213
AXF5Q	3762	9.38	159	-	89.33	5143.1	8.77	210
Pyrocarb 903	2698.1	12.04	169	-	116.05	5606.8	13.90	222
3-d Mod 3a	3891.4	2.53	159	43.3*	28.95	5697.3	1.89	214

* Based on compressive yield stress.

TABLE G14. MECHANICAL STRAIN RESULTS FOR CONCEPTUAL DESIGN NO. 8

Material	Temp. (°F)	Maximum Strain (mechanical)		Strain/Strain-to-Failure
		$\left(\frac{\text{in.}}{\text{in.}} \times 10^{-3}\right)$	(Location)	
994-2	4253.0	3.48	161	0.71
AXF5Q	3762.7	8.94	159	0.60
Pyrocarb 903	2698.1	2.72	169	1.22*
3-d Mod 3a	3891.4	2.81	159	0.32

*Based on $\epsilon_{\text{failure}} = \frac{\sigma_{\text{ult.}}}{E}$

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