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Heat Source Component Development Program

REPORT FOR PERIOD JULY 1978-DECEMBER 1978

Report Date: January, 1979

Battelle

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HEAT SOURCE COMPONENT DEVELOPMENT PROGRAM

Report for July–December, 1978

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

Report Date: January, 1979

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FOREWORD

This is the seventh of a series of reports describing the results of several analytical and experimental programs being conducted at Battelle-Columbus Laboratories to develop components for advanced radio-isotope 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 presentations of results can be achieved. In addition, a series of summary management monthly reports was initiated in July of 1976, to permit DOE assessment of contractual progress. Previous reports are identified as BMI-X-672 October 1976, BMI-X-676 January 1977, BMI-X-679 April 1977, BMI-X-683 July 1977, BMI-X-685 October 1977, BMI-X-691 March 1978, and BMI-X-696 July 1978.

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: analyses of trial designs with emphasis on detailed predictions of the response of Trial Design 4.1 including the sensitivity of the response to design and environmental uncertainties, and on comparison of performances of Trial Designs 4.1 and 1.3.1.

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ANALYSIS OF GPHS TRIAL 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 application for the GPHS is the Solar Polar mission scheduled for 1983.

LASL has formulated a project leading to the design and demonstration of technical feasibility of a heat source having certain attributes.* This project, which originally consisted of five phases, is scheduled for module qualification by December, 1979. The current phase, Trial Designs, began in October, 1977, and was scheduled to run through December, 1978. In this project phase, LASL has selected trial designs for reentry analyses, and is conducting and has plans for reentry testing, launch abort testing, and materials testing.

Battelle's support of LASL during the current reporting period has been to determine the operational and reentry response of selected heat source trial designs, and their thermal response to a space shuttle solid propellant fire environment. Thermal, ablation, and thermal stress analyses were conducted using two-dimensional modeling techniques previously employed for the analysis of the earlier trial design versions**, and modified in part to improve the modeling accuracy***. Further modifications were made to improve the modeling accuracy as described herein. Thermal, ablation, and thermal stress analyses were then conducted for the trial design selected by LASL/DOE for more detailed studies using three-dimensional modeling techniques.

Summary

Summary results of the thermal analyses of the trial designs are given in Tables 1-4. Details regarding the analysis approach used and assumptions made are given later in this report. Results

* "Project Overview General-Purpose Heat Source Project," Snow, E. C. Project Manager, Los Alamos Scientific Laboratory, Prepared for DOE/ASMP, June 26-27, 1978.

** Battelle's Report on "Heat Source Component Development Program," for October, 1977-February, 1978, (issued March, 1978), BMI-X-691.

*** Battelle's Report on "Heat Source Component Development Program,:" for March-June, 1978, (issued July, 1978), BMI-X-696.

of reentry thermal and ablation analyses performed for Trial Design 4.1 (TD 4.1) using three-dimensional (3-d) modeling techniques are summarized in Table 1. TD 4.1 is the heat source design presented by Fairchild Industries (FI) at the GPHS Project Review, June 26-27, 1978. The results of reentry analyses performed for TD 4.1 using 2-d modeling techniques are also summarized in Table 1 to facilitate an evaluation of the accuracy of the 2-d techniques.

Shown in Table 2 are results of analyses performed for TD 4.1 and TD 1.3.1 using 2-d modeling techniques. TD 1.3.1 is a modification of TD 1.3 previously developed by LASL. Results given in Table 2 are based on modified analysis techniques incorporated to improve the surface energy and mass balance modeling accuracy, as described later in this report, and on modeling techniques previously used and documented.*

The thermal responses of TD 4.1 and TD 1.3.1 to solid propellant fire environment (launch pad fire) are summarized in Table 3 for the peak metal clad temperature. Two-dimensional modeling techniques were used to determine the thermal response of the module designs.

In Table 4, the results are given for reentry analyses conducted to determine the sensitivity of peak metal clad temperature and impact metal clad temperature to uncertainties in (1) contact conductance at material interfaces, (2) the altitude of release of the individual heat source modules and (3) aerodynamic heating rate. Two-dimensional modeling techniques, were used to obtain the results shown in Table 4.

Thermal Response of TD 4.1 During Reentry - 3-d Analysis

Analyses of TD 4.1 were conducted to determine the thermal response of the heat source module to the aerodynamic heating that is experienced during reentry on a low flight path angle trajectory. This type of trajectory was used because of its relatively high probability of occurrence in comparison with trajectories resulting from other initial reentry conditions. Also, metal clad temperatures experienced during reentry for this trajectory are the highest and the metal clad at temperatures impact are the lowest in

* Techniques used previously are described in Battelle Reports, BMI-X-691 (March, 1978) and BMI-X-696 (July, 1978).

comparison with temperatures experienced on other trajectories. The response of TD 4.1 was determined using two- and three-dimensional modeling techniques with most of the analyses using 2-d techniques. Three-dimensional analyses of the module response were conducted for TD 4.1 to (1) verify the viability of the design in terms of peak clad temperature, clad temperature at impact, peak stress levels in the reentry member, and reentry member recession and (2) determine if the accuracy of the two-dimensional model techniques is sufficiently good to justify their use in further design and screening analyses rather than the more costly 3-d analysis techniques.

Results of the 3-d analyses conducted for TD 4.1 are summarized in Table 1.

Clad Operating Temperature. Upper limits of the metal clad operating temperatures for long time periods have been reported to be 1100-1120 C for Pt-3008 and 1300 C for Iridium alloys.* From Table 1, it can be seen that the maximum metal clad operating temperature for TD 4.1 is higher than the reported upper limit for Pt-3008. The operating clad temperature shown in Table 1 was calculated for a hot shoe temperature of 900 C, a metal clad emissivity of 0.4, helium gas inboard of the metal clad, and vacuum gaps outboard of the metal clad. Increasing the effective clad emissivity would result in a decrease in the clad operating temperature. Module operating temperature profiles for TD 4.1 and TD 1.3.1 are given in Appendix A.

Peak Clad Temperature. The peak clad temperature occurs during reentry of the heat source module on a shallow flight path angle trajectory. A minimum angle, non-skip trajectory was selected based on the results of previous analyses which showed this to be the critical trajectory for reentry member recession and clad temperature limits.** Limited analyses were conducted to determine the effect of the altitude of generator breakup on peak clad temperature; results of these analyses are discussed later in this report.

Upper limits of the peak clad temperature have been reported to be 2100 C for Iridium alloys (200 C below Ir-C eutectic temperature) and 1650 C for Pt-3008 (200 C below Pt-C eutectic temperature). * From

* For example, GPHS project review, DOE, Germantown, MD, June 26-27, 1978.

** Flight path angle = -5.3 degrees, velocity = 36 kfps, altitude = 400 kft, module separation assumed to be at 330 kft for peak clad temperature.

TABLE 1. SUMMARY OF REENTRY RESPONSE OF TD 4.1 USING
3-D MODEL/COMPARISON WITH 2-D RESULTS

Model Used	Altitude of Module Sep- aration, Kft	Contact Conductance, Btu/hr-ft ² -F	Clad Temperature, C			Recession, In.		Impact Velocity, Ft/Sec
			Operating	Peak	Impact	Stagnation Point	Corner	
3-d	330	250	1157	1569	968	0.060	0.08	190
2-d	330	250	1176	1610	885	0.057	0.08	185

Table 1, it can be seen that the peak clad temperature experienced during reentry, based on the 3-d analysis technique, is 1569 C. It appears that TD 4.1 satisfies the upper temperature limit for Pt-3008 and Iridium alloys. A decrease in the peak clad temperature experienced during reentry could be achieved by using a third pyrographite thermal barrier in the module design. Other, less effective, design modifications could also be implemented to reduce the peak clad temperature during reentry. Detailed clad temperature histories are given in Appendix B.

Clad Impact Temperature. During reentry of the heat source module, following maximum aerodynamic heating, the clad temperature decreases with time from its peak value. The reentry member surface temperature exceeds the air recovery temperature and aerodynamic cooling occurs. The extent of this cooling and decrease in clad temperature is greatest for the shallow flight-path angle trajectory. Of concern is the clad temperature at module impact in comparison with the temperature below which the clad becomes embrittled, i.e., fuel deformation cannot be accommodated without clad failure. For the impact velocities characteristic of the GPHS modules, assuming separation of individual modules, lower temperature limits of 400 C for Pt-3008 and 900 C for Ir alloys have been reported.*

From Table 1, it can be seen that the clad temperature at impact (970 C) for TD 4.1 is above the lower embrittlement temperature of Pt-3008 and Ir alloys. An increase in the metal clad temperature at impact can be realized if a third thermal barrier (pyrographite) is used in the design.

Reentry Member Recession. Recession of the fine-weave, pierced-fabric (FWPF) carbon-carbon reentry member is shown in Table 1 for the stagnation point and corner regions of the heat source module. Results of previous analyses have shown that maximum ablation is experienced on the shallow flight path angle trajectory.* Results of recent arc-heated ablation tests of FWPF C/C were not factored into the analyses of the ablation response of the reentry member.** In these tests, there was evidence of surface-roughness-induced boundary-layer transition, leading to greater mass loss rates for the FWPF material than for POCO AXF-5Q bulk graphite. Detailed reentry member recession histories are given in Appendix C.

*For example, GPHS Project Review, DOE Germantown, MD, June 26-27, 1978

**Conn, D. W., "Preliminary Results of IMHW-HSA Ablation Tests," APL/JHU Memo ANSP-141/EM-4844, December 7, 1978.

Impact Velocity. The impact velocity of TD 4.1 is shown in Table 1 to be 190 ft/sec.

Stress Response of TD 4.1 - 3-d Analysis

The thermal stress response of the TD 4.1 reentry member was determined for the prompt reentry trajectory. A finite element computer program, SAVFEM, was used for the analysis. Analyses were conducted for two models of the reentry member.

The first of these models consisted of a quarter section of the entire reentry member with a solid end cap. In the second model, the effects of the threaded plug were simulated by setting up a cylindrical surface of double nodes. In this model, nodes were allowed to separate in the regions for which tensile stresses were calculated in the first model. Stiff springs connected the nodes in the regions of compressive stress to represent the contact of the threads normal to the cylindrical surface.

An evaluation of the results of these analysis showed that in both cases the maximum stresses were caused by the thermal gradients between the reentry surface and the fuel cavity at the thinnest section of the reentry member. Maximum tensile stresses were approximately 22-24 ksi on the inner cavity surface of the reentry member at its thinnest point under the reentry surface. The maximum compressive stresses were about 6-8 ksi on the reentry surface over the minimum section. Both stresses are in the direction normal to the fuel cavity axis. Reported tensile strength for the FWPF composite is 24-29 ksi at 2000 F which is about the computed temperature on the cavity surface. Measured compressive strength at 6000 F is about 7 ksi. However, at this temperature the FWPF exhibits yield at less than 3 ksi and will "flow" plastically for 10 mils/in. or more. This flow should result in stress relaxation and lower both the tension and compressive stresses.

The stresses computed in these three-dimensional analyses were very nearly constant along the length of the fuel cavity which was virtually in a state of plane strain. As will be discussed later, the analysis of the 2-d plane strain model gave a tensile stress of 18 ksi and maximum compressive

stresses of 7 ksi. Since the 3-d models were quite coarse, these stresses computed with for the 2-d model may be more realistic than the 3-d analyses above.

A comparison of the stresses in the two 3-d models showed that the principal difference between the threaded and solid ends was that the maximum bending stresses described in the previous paragraph extended further into the plug end. Since these stresses are in the tangential direction, parallel to the threads, it is believed that their effect may be less severe than if they were normal to the thread direction. More refined analyses are needed in the thread area to compute the stresses in sufficient detail to contribute to the thread closure design.

It is concluded that on the basis of the described analyses, further and more refined studies are needed of TD 4.1 if it is the chosen GPHS design. Certain design modifications may be possible to reduce the thermal stresses. These would include rounding the external corners or enlarging the fuel cavity to reduce the reentry member wall thickness.

Thermal Response of TD 4.1 During Reentry - Comparison of 2-d and 3-d Results

A comparison of the thermal performance of TD 4.1 using 2-d and 3-d analysis techniques is given in Table 1.

Clad Operating Temperature. From Table 1, it can be seen that there is excellent agreement between the metal clad operating temperatures predicted for TD 4.1 using the 2-d and 3-d analysis techniques, 1176 and 1157 C, respectively. Relatively good agreement is expected for this steady-state operating condition because the fuel inventory and mass of heat source components were accurately modeled in the 2-d (and 3-d) approach. Greatest inaccuracies would be expected for those temperatures calculated based on the transient response of heat source components and for temperature gradients within a component, especially the reentry member. For TD 4.1, there is no effect of the contact conductance on the clad operating temperature because of side-face insertion of the fuel pellets and the relative sizes of the module components which limit intimate contact between adjacent materials.

Peak Clad Temperature. The peak clad temperature calculated for TD 4.1 during reentry with 3-d analysis is approximately 40 C lower than the value obtained using the 2-d technique, 1569 C compared with 1610 C, see Table 1. A lower value of the peak clad temperature is obtained using the 3-d modeling approach because of the heat dissipation effects realized by modeling heat flow in the third dimension. However, the agreement between the two peak clad temperatures is considered to be excellent.

The peak clad temperature experienced during reentry can be lowered by reducing the contact conductance at material interfaces. Results of analyses performed to determine the effect of contact conductance on metal clad temperature are discussed later in this Summary.

Clad Impact Temperature. The greatest difference between the 2-d and 3-d analysis results for TD 4.1 is in the value obtained for the clad temperature at impact. The value calculated for the clad impact temperature based on the 3-d analysis is 968 C, whereas the clad impact temperature based on the 2-d analysis is 885 C. This result is encouraging in that a higher value for the clad temperature at impact is obtained using the more accurate 3-d modeling technique. The difference in the two values is due to the refinements inherent in the 3-d approach resulting from more accurate representations of geometric features and multi-directional material properties.

Reentry Member Recession. The recession of the reentry member was calculated for the stagnation point region and the corner region using the 2-d and 3-d modeling techniques. From Table 1, it can be seen that the values predicted are practically identical using both techniques, with the corner recession being higher than the stagnation point value.

Impact Velocity. The impact velocity of the TD 4.1 heat source module is predicted to be approximately 190 ft/sec based on the 3-d analysis and approximately 185 ft/sec based on the 2-d analysis. This difference in impact velocities is due to a more accurate modeling of the weights of the individual components in the 3-d analysis. In both cases, drag coefficients characteristic of modules having rounded corners (due to ablation) were assumed.

Two-Dimensional Analysis of TD 4.1 Thermal Stresses

Analysis of the two-dimensional thermal stresses followed the same approach reported in earlier progress reports. A plane-strain finite element model was generated of the crosssection through the midplane of the reentry member normal to the direction of the fuel cavity axis. Temperatures obtained from the reentry thermal analysis were interpolated to the finite element grid points. Then program DOASIS was used to carry out the analysis.

The analysis of TD 4.1 with material properties for the FWPF C/C composite yielded a maximum stress of 18 ksi at the inside surface of the fuel cavity on the reentry side. For comparison, an analysis was made of this model using the same temperature distribution but substituting 994-2 properties. This analysis gave a maximum stress of 6 ksi. An investigation showed that the differences were principally the result of the much higher stiffness of the FWPF composite in comparison with the 994-2 graphite.

Comparison of Reentry Performance of TD 4.1 and TD 1.3.1

The reentry responses of TD 4.1 and TD 1.3.1 were determined using 2-d modeling techniques in order to compare the performance of the designs and to aid in the selection of a trial design for further testing, analysis, and development. Results of the analyses are summarized in Table 2 for the revised and original surface chemistry/aeroheating models.

Clad Operating Temperature. The clad temperature during steady-state generator operation (900 C hot shoe temperature) is higher for TD 4.1 than for TD 1.3.1. The higher temperature is due to the additional gaps existing in TD 4.1 than in TD 1.3.1 in the heat-flow direction during operation. These additional gaps are due to the side-face insertion of the fuel.

TABLE 2. COMPARISON OF TD 4.1 AND TD 1.3.1
REENTRY RESPONSE - 2-D ANALYSIS

Design	Altitude of Module Separation, Kft	Contact Conductance, Btu/hr-ft ² -F	Clad Temperature, C			Stagnation Point Recession, In.	Impact Velocity, Ft/Sec
			Operating	Peak	Impact		
<u>Revised Surface Chemistry/Aeroheating</u> ⁽¹⁾							
TD 4.1	330	250	1176	1610	885	0.057	185
TD 1.3.1	330	250	1122	1650	826	0.054	180
<u>Original Surface Chemistry/Aeroheating</u> ⁽²⁾							
TD 4.1	271	250	1176	1547	--- ⁽³⁾	0.08	185
TD 1.3.1	271	250	1122	1551	810	0.076	180

(1) Assumptions as given in the current report.

(2) Assumptions as given in Battelle report, BMI-X-696 (July, 1978).

(3) Analyses terminated at altitude of 103 kft. Recession is extrapolated based on results of previous analyses.

Peak and Impact Clad Temperature. During reentry, the additional gaps, which are assumed to close on the windward face of the heat source module (at the contact resistance value given in Table 2), result in a lower peak clad temperature for TD 4.1 than for TD 1.3.1. The gaps also serve to reduce the heat loss from the heat source module during subsonic flight. Thus, the clad temperature at impact for TD 4.1 is higher than for TD 1.3.1.

Reentry Member Recession. There is little difference between the reentry member recession experienced for TD 4.1 and TD 1.3.1.

Impact Velocity. Impact velocities of the two trial designs differ only very slightly. The impact velocity of TD 4.1 is slightly higher than that of TD 1.3.1 due to the slightly higher weight/frontal area of TD 4.1. Rounded corners due to ablation were assumed for both designs.

Comparison of Results Obtained with Original Surface Chemistry/Aeroheating. Comparative results obtained for TD 4.1 and TD 1.3.1 using the original surface chemistry/aeroheating models^{*} are also given in Table 2. There is little difference between the calculated peak clad temperatures during reentry for the two designs. Peak metal clad temperatures calculated using the revised surface chemistry are approximately 60-100 C higher than the values calculated based on the original surface chemistry/aeroheating assumptions. Part of this difference, approximately 25 C, is due to the higher altitude of heat source module separation used for the analysis conducted with the revised surface chemistry/aeroheating assumptions. Higher net heating rates and reentry member surface temperatures downstream of the stagnation point result in the higher peak metal clad temperatures obtained with the revised surface chemistry/aeroheating models.

The metal clad impact temperature of TD 1.3.1 is relatively independent of the surface chemistry/aeroheating assumptions. At subsonic flight conditions, the surface chemical reactions result in little additional mass loss and surface recession and the heat generation by the fuel becomes important relative to the aeroheating.

^{*} See Battelle Report, (July, 1978), BMI-X-696.

Recessions of the reentry member calculated for TD 4.1 and TD 1.3.1 are essentially identical to each other, 0.08 in. The stagnation point recession calculated using the revised surface chemistry/aeroheating assumptions is lower than that calculated with the original assumptions. Stagnation point surface temperatures and mass loss rates are lower based on the former assumptions. The corner recession calculated by both techniques is approximately 0.08 in.

Response of Trial Designs to Launch Pad Fire Environment

Analyses were conducted to determine the thermal response of TD 4.1 and TD 1.3.1 in a solid propellant fire environment resulting from a launch abort. The analysis utilized a two-dimensional thermal model to determine the temperature response of the heat source module components assuming all faces of the module, except one, were exposed to the fire environment. The heat source module was assumed to be lying on the earth with a broadface in contact with the ground. It was further assumed that this module surface in contact with the ground was an adiabatic surface, i.e., no heat transfer from the module to the ground. The analyses were conducted for a Solid Rocket Motor (SRM) fire model in which the fuel burns as separate fragments.* This model utilizes the external tank fireball environment for approximately the first 7 seconds followed by a solid propellant fire environment that decreases in severity with time.

Results of the thermal response analyses are summarized in Table 3, where the operating and peak metal clad temperatures are shown for TD 4.1 and TD 1.3.1 for two values of the contact conductance between material components.

* Edgecombe, D. S., et al, Draft, Volume II of Final Technical Report on "Evaluation of the Space Disposal of Defense Nuclear Waste-Phase II", to NASA-MSFC from Battelle Columbus Laboratories (December 8, 1978), Final copy to be dated January 31, 1979.

TABLE 3. SUMMARY OF RESPONSE OF TD 4.1 AND TD 1.3.1
IN SOLID PROPELLANT FIRE - 2-D ANALYSES

Design	Contact Conductance, $\text{Btu/hr-ft}^2\text{-F}$	Clad Temperature, C	
		Operating	Peak
TD 4.1	250	1046	1698
TD 4.1	500	1046	1708
TD 1.3.1	250	1032	1709
TD 1.3.1	500	1030	1775

The clad temperature prior to exposure of the heat source module is based on a hot-shoe temperature of 900 C and maintenance of helium between the fuel and the metal clad and air in the other gaps between materials, including the region between the heat source module and the hot shoe. It can be seen from Table 3 that the peak clad temperature experienced during exposure to the assumed fire environment exceeds 1650 C (200 C below the Pt-C eutectic temperature) for TD 4.1 and TD 1.3.1. The time during which the peak clad temperature exceeds 1650 C is approximately 150 sec for TD 4.1 and 165-190 sec for TD 1.3.1.

Although analyses were not conducted to determine the heat source module thermal response to a SRM fire model resulting from a single split motor case failure, this environment is more severe than the separate fragments model considered* and would result in much higher peak clad temperatures, probably at least 200-400 C higher than obtained from the separate fragments model. Clad temperature histories for the analyses summarized in Table 3 are given in Appendix D.

Sensitivity Analyses of TD 4.1

Analyses were conducted to determine the reentry response of heat source components to various uncertainties associated with reentry. The uncertainties considered are (1) the extent of contact between materials during reentry, (2) the altitude of release and separation of individual heat source modules, and (3) the aeroheating rates during supersonic and subsonic flight. All of these analyses were conducted using 2-d thermal modeling techniques. Results of these analyses are summarized in Table 4 and are discussed briefly below.

Effect of Contact Conductance. A limited number of analyses was conducted to determine the effect contact conductance between materials has on the peak metal clad temperature and metal clad temperature at impact. Analyses conducted with the revised and original surface chemistry/aeroheating assumptions are summarized in Table 4 and graphically illustrated

* Ibid.

TABLE 4. SUMMARY OF SENSITIVITY ANALYSES OF TD 4.1 USING 2-D MODELING

Altitude of Module Release, Kft	Contact Conductance, Btu/hr-ft ² -F	Heating Factor		Clad Temperature, C			Maximum, Recession, In.
		Supersonic	Subsonic	Operating	Peak	Impact	
<u>Revised Surface Chemistry/Aeroheating</u> ⁽¹⁾							
<u>Contact Conductance</u>							
330	150	1.0	1.0	1176	1583	909	0.08
330	250	1.0	1.0	1176	1610	885	0.08
330	500	1.0	1.0	1176	1641	857	0.08
<u>Original Surface Chemistry/Aeroheating</u> ⁽²⁾							
<u>Altitude of Module Separation</u>							
400	1000	1.0	1.0	1176	1615	834	0.09
400 ⁽³⁾	1000	1.0	1.0	1176	1616	842	0.09
330	1000	1.0	1.0	1176	1622	835	0.09
271	1000	1.0	1.0	1176	1598	-- ⁽⁴⁾	0.08
271	250	1.0	1.0	1176	1547	-- ⁽⁴⁾	0.08
<u>Aeroheating Rates</u>							
330	250	1.0	1.0	1176	1569	882	0.09
330	250	1.4	1.0	1176	1631	901	0.09
330	250	1.2	1.0	1176	1605	-- ⁽⁴⁾	0.09
330	250	1.0	1.4	1176	1569	840	0.09
330	250	1.4	1.4	1176	1631	858	0.09
330	250	1.0	0.6	1176	1569	929	0.09

(1) Assumptions as given in the current report.

(2) Assumptions as given in Battelle Report BMI-X-696 (July, 1978).

(3) Analysis performed assuming no movement of the ablated surfaces during reentry.

(4) Analysis terminated at an altitude of 103 kft.

in Figure 1. Although the number of sensitivity analyses conducted was quite limited, the trends shown in Table 4 and in Figure 1 are evident, i.e., increases in the contact conductance between materials results in increases in the peak clad temperature experienced during reentry.

The smallest value of contact conductance shown in Table 4 (150 Btu/hr-ft²-F) represents a realistic lower limit "effective contact conductance" between materials. For poorer contact, heat transfer between materials by radiation would yield an effective conductance of approximately 150 Btu/hr-ft²-F at the heat source module temperatures experienced during operation and reentry. From Table 4 and Figure 1, it can be seen that an increase in contact conductance of approximately 65 percent, from 150 to 250 Btu/hr-ft²-F, results in an increase in peak clad temperature of 27 C, approximately 2 percent, from 1583 to 1610 C. An increase in the conductance of approximately 235 percent, from 150 to 500 Btu/hr-ft²-F, results in an increase in peak clad temperature of 58 C, approximately 4 percent, from 1583 to 1641 C. Further increases in the contact conductance will cause smaller increases in the peak clad temperature.

Also shown in Figure 1 is the effect contact conductance has on the metal clad temperature at impact. An increase in the contact conductance from the "minimum" value of 150 to 500 Btu/ft²-hr-F results in a decrease in the minimum clad impact temperature of 52 C. Thus, the decrease in metal clad temperature at impact due to increasing the contact conductance is approximately the same (in absolute terms) as is the increase in peak clad temperature resulting from the contact conductance increase.

The reentry member recession is essentially unaffected by the variation in contact conductance assumed between materials during reentry of the heat source module, see Table 4.

Low contact conductance is beneficial in that reduced peak metal clad temperatures during reentry and increased metal-clad temperatures at impact can be achieved, with a minimum practical limit of 150 Btu/ft²-hr-F at heat source module temperature levels.

Effect of Altitude of Heat Source Module Separation. A limited number of analyses was conducted to determine the effect altitude of release of the heat source module has on the module thermal response. The majority

FIGURE 1. EFFECT OF CONTACT CONDUCTANCE ON PEAK AND IMPACT METAL CLAD TEMPERATURE (REENTRY) - TD 4.1

of the thermal analyses conducted to date was based on release of the modules at an altitude of 271 kft* and there was concern that higher peak metal-clad temperatures might be experienced for heat source module release at higher altitudes. Also, for low angle reentry of the discrete heat source modules, it was of interest to determine if the trial design was "mission independent" for the assumed reentry altitudes of the module.

The analyses were conducted for TD 4.1 prior to revising the surface chemistry/aeroheating models. Thus, emphasis should be placed on changes in the peak and impact metal clad temperature and reentry member recession resulting from different release altitudes rather than on the magnitude of the values. Results of these analyses for release altitudes of 400, 330, and 271 kft and a contact conductance of $1000 \text{ Btu/ft}^2\text{-hr-F}$ are listed in Table 4 and graphically illustrated in Figure 2. This relatively high value of contact conductance was used to more closely couple the thermal response of the metal clad to changes in the external heating/cooling environment resulting from the different release altitudes. From Figure 2, it can be seen that the effect of altitude of heat source module (discrete) release on the peak and impact metal clad temperature is insignificant. A maximum in the peak metal clad temperature occurs for module release at an altitude of 330-350 kft. From Table 4, it can be seen that effect of module release on maximum recession of the reentry member is small for the release altitudes considered. Subsequent analyses (2-d and 3-d) were performed using a module separation altitude of 330 kft.

Effect of Aeroheating Rates. Analyses were conducted to determine the effect variations in the aeroheating rate has on the peak metal clad temperature and the metal clad temperature at impact. Uncertainties in the aeroheating rate arise, for example, because of an inadequate theoretical and experimental basis to accurately predict the surface roughness developed by the reentry member during reentry and the resulting effect on laminar heat transfer or on promotion of boundary layer transition and turbulent heating rates. Also, there are uncertainties associated with accurately predicting the effects "cavity" geometries, e.g., thread closure, spherical interlock, and "checkerboard"-type configurations being considered

* Same as MHW system.

FIGURE 2. EFFECT OF HEAT SOURCE MODULE RELEASE ALTITUDE ON PEAK AND IMPACT METAL CLAD TEMPERATURE-TD 4.1

in the GPHS trial designs, have on local heating rates and pressure levels.

Generally, the local heat and mass transfer rates are affected within and downstream of the cavities. Recently, design criteria were developed for GPHS modules for cavity-type surface discontinuities.* However, because of time limitations, the results of this recent study could not be incorporated into the 2-d and 3-d thermal analyses in progress. Instead, changes in the thermal environment resulting from module cavities were modeled by imposing heating rates that differed from the unperturbed values over all of the reentry member surfaces exposed to the aerodynamic flow. Separate factors were used in the supersonic (including hypersonic) and subsonic flight regimes. Results of these analyses are summarized in Table 4 and graphically illustrated in Figure 3 for a module release and separation altitude of 330 kft and a contact conductance of $250 \text{ Btu/hr-ft}^2\text{-F}$ between materials.

From Figure 3 and Table 4, it can be seen that a uniform increase in the supersonic aeroheating rate of 40 percent results in an increase of 62 C or approximately 4 percent in the peak metal clad temperature from the baseline value (1569 C). The resulting increase in metal clad temperature at impact is 19 C or approximately 2 percent greater than the baseline value (882 C).

Changes in the subsonic aeroheating rates do not affect the peak metal clad temperature experienced because the maximum clad temperature occurs in the supersonic flight regime. Increasing the subsonic aeroheating rates by 40 percent from the baseline values results in a decrease in the metal clad temperature at impact of 42 C or approximately 5 percent of the baseline value (882 C). A decrease in clad temperature occurs because the reentry member surface temperature (or gas enthalpy at the wall) is greater than the aerodynamic recovery temperature (or recovery enthalpy) for a significant time period during subsonic flight and the convective aerodynamic heat transfer is from the reentry member to the aeroenvironment. The heat source module is also rejecting heat to the aeroenvironment by radiation from the reentry member. Thus, by increasing the heating factor in the subsonic flight regime, (increasing the heating rate to the module), in effect, the heat transfer from the module to the environment is also increased. A decrease

* Conn, D. W., "Aerothermodynamic Environment Induced by GPHS Closures and Spherical Interlock Recesses," APL/JHU Memo ANSP-138/EM-4839, October 19, 1978.

FIGURE 3. EFFECT OF AEROHEATING FACTOR ON METAL CLAD TEMPERATURE - TD 4.1

of 40 percent in the subsonic aeroheating rates from the nominal values results in an increase of 47 C or approximately 5 percent in the metal clad impact temperature from the baseline value (882 C).

Work This PeriodReentry Thermal Analysis
(G. R. Whitacre)

During this reporting period, analyses were conducted to determine the response of trial designs selected by LASL and DOE to reentry and launch abort, solid propellant fire environments. Presented in this section of the report are (1) descriptions of the trial designs analyzed and (2) a discussion of the approach used to evaluate the trial designs, with emphasis on those aspects that differ from those used in the previous analysis of the trial designs.

Description of Trial Designs

Two trial designs were analyzed during the current reporting period, TD 4.1, which was formulated by Fairchild Industries, and TD 1.3.1, which was conceived by LASL.

Trial Design 4.1. Trial Design (TD) 4.1 (based on October 12, 1978 blueprint) contains four cylindrically-shaped fuel pellets, each of which are 62.5 W(t), for a total fuel inventory of 250 W(t) per heat source module. Two cavities are provided in the reentry member (also known as heat shield or aeroshell) into which two fuel pellets each are inserted. Entry of the 1.10 in. diameter fuel pellets and the associated components is through side faces of the reentry member. Overall outside dimensions of the sharp-cornered heat source module are 3.724 x 3.965 x 2.159 in., with entry of the fuel pellets through the two faces that are 2.159 x 3.965 in.

Each fuel pellet is contained within a metal containment member (metal clad) 0.025 in. thick. The purposes of the metal containment shell are to prevent release of the $^{238}\text{PuO}_2$ fuel in accordance with safety requirements and to absorb a portion of the kinetic energy at impact. A pyrographite thermal insulation member, 0.040 in. thick, is placed completely

around each fuel-containing clad member. A carbon-or graphite-based impact member, 0.20 in. thick, is placed around each fuel/metal clad/pyrographite assembly. Each set of two fuel pellet assemblies, as described above, is then completely contained in a pyrographite thermal insulation member which is 0.040 in. thick. The fuel/clad/pyrographite/impact member/pyrographite assemblies are positioned in the carbon-carbon reentry member (heat shield/aeroshell) which has a nominal wall thickness of 0.20 in. Threaded closures are used to retain the assemblies within the reentry member.

Trial Design 1.3.1. An assembly drawing of TD 1.3.1 (TD 1.3.1 is identical to TD 1.3 in geometric features, materials and size) is shown in Figure 4. TD 1.3.1 contains four right circular, cylindrically-shaped fuel pellets, and has an inventory of 250 W(t) per heat source module. As shown in Figure 4, TD 1.3.1 has rounded corners. Overall dimensions of the module are 3.97 x 3.97 x 2.11 in. with entry of the fuel pellets and associated components through the two 3.97 x 3.97 in. faces (2 pellets each face). Each fuel pellet is contained in a metal clad, pyrographite thermal barrier, carbon-carbon primary impact member, and second pyrographite thermal barrier. Thicknesses of all component members are nominally identical to the corresponding component member thicknesses of TD 4.1.

Evaluation Approach

The overall objective of the trial design thermal analyses is to provide information on the operating temperature and temperature histories of heat source module components during atmospheric entry. This information, along with the results of other project activities, is being used by LASL and DOE to select a trial design for further development. These analyses, along with the various tests that are being conducted are being directed to enable LASL to evaluate the viability of the trial designs relative to the applicable safety documents, evaluate the effectiveness of design changes in moderating the extremes in clad temperature resulting

from reentry or exposure to a solid propellant fire environment, and to perform trade-off analyses to select the optimum design in terms of materials, geometry, power density, cost, safety, and quality assurance.

The approach used and assumptions made in evaluating the trial designs are given below. The thermal models used for the trial designs are given first, both 2-d and 3-d, followed by the material properties used for the thermal analyses, the reentry environments, including modifications made to the modeling techniques to improve the accuracy of the analyses, and the thermal environment used in determining the response of TD 4.1 and TD 1.3.1 to a ground launch abort fire. Emphasis is placed on those aspects of the work that differ from those reported previously.*

Thermal Models. Operational temperatures and reentry thermal and ablation responses of TD 4.1 were determined using three-dimensional and two-dimensional finite difference models. In addition, the thermal response of TD 4.1 to a fire environment resulting from a launch abort was determined using a 2-d model. The operational temperature response, reentry thermal and ablation response, and thermal response to a fire environment were determined for TD 1.3.1 using a two-dimensional finite difference model.**

The two-dimensional analysis approaches used provide for sufficient accuracy to compare the effects of alternate trial design materials and geometry features on reentry performance as well as provide a realistic indicator of the operational and reentry response of the heat source modules, consistent with the assumptions made regarding aerodynamic, aeroheating, and boundary conditions. In this regard, the accuracy of the analysis is dependent, in part, on how closely the geometry and design of the module and its components are approximated two-dimensionally and on the importance of local characteristics, such as plug closures or module-to-module attachment cavities.

The two-dimensional models were carefully formulated to meet certain criteria. The models contain sufficient detail to permit valid comparison of the influence of the design features on module performance.

*

Battelle Reports, BMI-X-691 (March, 1978) and BMI-X-696 (July, 1978).

**

Two-dimensional model used for TD 1.3.1 was previously reported in Battelle Report BMI-X-696 (July, 1978).

Also, the models account for anisotropy in materials properties, especially with regard to thermophysical characteristics. They provide a good representation of the module ballistic coefficient so that correct trajectory and terminal velocity can be determined. Also, the models provide a good approximation of the total heat input to determine the correct component temperature levels.

The three-dimensional model provides improved modeling accuracy of heat source module geometric and materials property/orientation characteristics in comparison with the two-dimensional techniques. End effects and heat transfer in the third dimension (dimension in which heat flow is neglected in the 2-d techniques) are accounted for with 3-d approach.

Modeling inaccuracies still exist using 3-d thermal modeling techniques. For example, the three-dimensional network is limited to a "reasonable" number of nodes due to computing machine storage limitations and increasing computer processing or "running time" as the number of nodes increase. Thus, compromises in modeling certain design features and approximations must be made, especially for complex geometries such as the GPHS modules.

TD 4.1. Three-dimensional Thermal Model. Two views of the 3-d model used to determine the thermal response of TD 4.1 are shown in Figure 5. Figure 5a shows the face of the heat source module through which the fuel pellet assemblies enter (face perpendicular to the z axis) and is representative of a plane through the center of two of the fuel pellets. Model dimensions of this face are 3.96 x 2.16 in. and module dimensions are 3.965 x 2.159 in. Figure 5b shows one-half of the module face that is 3.724 x 2.159 in.; dimensions of this face used in the 3-d thermal model are 3.72 x 2.16 in. The section shown in Figure 5b is a plane through the center of one of the fuel pellet assemblies at the diameter of the fuel pellet. Symmetry planes are shown in both views of Figure 5. A hot shoe temperature of 900 C with radiation heat transfer from the reentry member to the hot shoe is assumed in the analysis.

Node sizes and locations of node boundaries vary from those shown in Figure 5 at different locations within the heat source module, e.g., at material boundaries or geometric changes. The weights of module

a. Face of Fuel Pellet Entry (Half-Face)

b. Side View of Thermal Model (Half-Face)

Thermal Barrier (Pyrographite)
 Metal Clad
 Primary Impact Member
 Reentry Member

FIGURE 5. THREE-DIMENSIONAL THERMAL MODEL OF TD 4.1

components as used in the 3-d model are given in Table 5. A network of 8 (r) x 13 (z) x 14 (θ) nodes was used for the 3-d analysis of TD 4.1.

Gaps between materials and material contact interfaces are provided based on the information contained in the assembly drawing provided by FI. Consideration was given to preloading of components during assembly of the module in selecting regions of contact and gaps. In the 3-d thermal model, contact is provided between the inner pyrographite thermal barrier and the impact member and between the outer pyrographite thermal barrier

TABLE 5. TD 4.1 COMPONENT INVENTORY USED IN 3-D MODEL

Component	Weight, g (lb)	Density, g/cm ³
Fuel	631 (1.392)	9.91
Reentry member	541 (1.192)	1.96
Metal clad (Ir)	232 (0.512)	(1)
Impact member	214 (0.472)	1.70
Pyrographite insulators (2)	<u>91 (0.200)</u>	2.21
Total Weight	1709 (3.768)	

(1) Density as used in model includes volume of gaps.

and the reentry member. These regions of material contact, at which a finite contact conductance is used, are designated in Figure 5b by the segments AB. Contact between materials in these regions is symmetric about the r-axis shown in Figure 5b. The value of the contact resistance used in these regions, 250 Btu/hr-ft²-F, implies relatively poor contact between materials. This value was selected based on results of the 2-d thermal analyses of TD 4.1 and TD 1.3.1 to yield a relatively low peak metal clad temperature during reentry and a relatively high metal clad temperature at impact in comparison with results obtained with improved levels of material contact. As indicated previously in the Summary section, the extent of contact between materials affects the peak clad temperature experienced during reentry and the clad temperature at impact. The degree of preloading, material construction, surface roughness, component

dimensions, and relative material thermal expansion characteristics will determine the contact conductance at the component interfaces.

Gaps are provided in the 3-d model at all other material interfaces shown in Figure 5b. These gaps are also symmetric with respect to the r-axis. It is assumed that the converter does not maintain a helium cover gas during generator operation. Thus, during operation of the generator, those gaps that exist outboard of the metal clad are assumed to be vacuum interfaces, with the fuel-metal clad gap helium-filled. During reentry, the windward (forward) gaps, indicated by the dashed line segments in Figure 5a, are assumed to close due to the resulting deceleration forces. Adjacent materials within this 90 degree segment of contact in the z-direction plane are assumed to be in contact to the same degree as those regions in contact indicated by the AB segments in Figure 5b. Remaining gaps are assumed to be air-filled during reentry using the procedure previously described.* Helium is maintained in the fuel-metal clad interface throughout reentry.

TD 4.1. Two-Dimensional Thermal Model. Figure 6 shows the 2-d model used to determine the thermal response of TD 4.1. The view shown in Figure 6 is a plane through two of the fuel pellets (half-face shown) parallel to the face through which the pellets are inserted into the heat source module. The r-axis is an axis of symmetry and the vertical left-hand edge is facing the hot shoes of the generators. A hot shoe temperature of 900 C with radiation heat transfer from the reentry member to the hot shoe is assumed. This 2-d model uses a 16 x 19 nodal network.

Gaps are assumed to exist at all material interfaces shown in Figure 6 during normal generator operation. Use of gaps between materials is based on the dimensional information provided in the assembly drawing of TD 4.1. It is assumed that during reentry, windward gaps will close due to deceleration forces resulting in contact between materials at certain interfaces, as shown by the dashed line segments in Figure 6.

Assumptions made regarding the presence of gas within the gaps are identical to the treatment employed for the 3-d thermal analysis.

* Battelle Report, BMI-X-691, March, 1978, pp 5-6.

FIGURE 6. TWO-DIMENSIONAL THERMAL MODEL OF TD 4.1

TD 1.3.1. Two-Dimensional Thermal Model. The 2-d model used to determine the thermal response of TD 1.3.1 is shown in Figure 7. This thermal model is identical to that used previously for TD 1.3.1* and contains a 20 x 23 nodal network.

Based on the dimensions of individual components, as provided in detail drawings by LASL, contact between adjacent materials was assumed at those interfaces indicated by the dashed line segments shown in Figure 7. Gaps are assumed to exist at all other material interface regions. Reentry deceleration forces are assumed to cause the windward (perpendicular to z-axis) gaps to close and to enable the fuel and metal clad and the metal clad and inner thermal barrier to contact each other.

The z-axis shown in Figure 7 is an axis of symmetry and the vertical left-hand edge is facing the hot shoes of the converter. A hot shoe temperature of 900 C with radiation heat transfer from the reentry member to the hot shoe are used in the analysis.

Assumptions made regarding the presence of gas in the gaps are identical to those used in determining the thermal response of TD 4.1.

Material Properties. Thermophysical properties for AVCO FWPF carbon-carbon composite were used for the reentry member. For the impact member, properties of Pyrocarb 903 were used in place of Pyrocarb 848, the material initially selected by LASL for the primary impact member. Table 6 shows the thermophysical properties used for all of the heat source module components using the format generated by the computer analysis model. In Table 6, Material 1 is the FWPF c/c reentry member, Material 2 is the Pyrocarb 903 primary impact member, Material 3 is the pyrographite thermal insulators, Material 4 is the iridium clad plus gaps on either side of the metal clad, and Material 5 is the $^{238}\text{PuO}_2$ fuel. The Z, R, and Theta directions indicated in the thermal conductivity table are consistent with the coordinate system previously given in Figure 5.

Reentry Environment. Environments for the low angle and prompt reentry conditions were based on a Titan III C propulsion vehicle and velocity-flight path information as previously reported.** Initial reentry conditions

* Battelle Report BMI-X-696 (July, 1978).

** Battelle Reports BMI-X-685 (October, 1977), BMI-X-691 (March, 1978) and BMI-X-696 (July, 1978).

FIGURE 7. TWO-DIMENSIONAL THERMAL MODEL OF TD 1.3.1

TABLE 6. MATERIAL THERMOPHYSICAL PROPERTIES USED
IN ANALYSIS OF TRIAL DESIGNS

THERMAL CONDUCTIVITY IN Z,P AND THETA DIRECTIONS -BTU/SEC.IN.R. VS. TEMPERATURE -R.								
MATERIAL 1 -19 ENTRIES	960.000	.1707E-02	.1350E-02	.1707E-02	1210.000	.1495E-02	.1186E-02	.1495E-02

TABLE 6. (Continued)

SPECIFIC HEAT - BTU/LB.-R. VS. TEMPERATURE -R.										
MATERIAL 1 -13 ENTRIES	360.000	.10000	610.000	.21000	960.000	.31000	1110.000	.34000	1460.000	.39000
	1360.000	.43000	2460.000	.46500	2960.000	.48500	3460.000	.50500	3960.000	.51500
	4360.000	.52600	7460.000	.52600	8300.000	.52600				
MATERIAL 2 -10 ENTRIES	460.000	.15000	1060.000	.32500	1110.000	.35000	1210.000	.38000	1460.000	.42700
	1710.000	.45600	1960.000	.47500	2210.000	.48800	5460.000	.49200	8000.000	.49200
	MATERIAL 3 - 6 ENTRIES	505.000	.23200	1360.000	.39000	2260.000	.47500	4060.000	.49000	5410.000
MATERIAL 4 - 6 ENTRIES	10460.000	.53000								
	460.000	.03060	1460.000	.03470	2460.000	.03880	3460.000	.04290	4460.000	.04700
	4910.000	.04700								
MATERIAL 5 -17 ENTRIES	0.000	.06200	560.000	.06200	1032.000	.07750	1212.000	.07940	1392.000	.08050
	1572.000	.08110	1752.000	.08170	1932.000	.08210	2112.000	.08250	2292.000	.08270
	2472.000	.08300	2652.000	.08320	2960.000	.08350	3460.000	.08380	3960.000	.08400
	4460.000	.08400	4810.000	.08400						

EMISSIONITY VS. TEMPERATURE -R.										
MATERIAL 1 - 7 ENTRIES	0.000	.80000	2460.000	.80000	2960.000	.85000	3460.000	.90000	3960.000	.92500
	4460.000	.95000	10460.000	.95000						
MATERIAL 2 - 2 ENTRIES	0.000	.80000	8000.000	.80000						
MATERIAL 3 - 8 ENTRIES	505.000	.50000	1900.000	.50000	2260.000	.50000	2620.000	.45000	3160.000	.45000
	3610.000	.55000	4060.000	.95000	10460.000	.95000				
MATERIAL 4 - 2 ENTRIES	0.000	.40000	8000.000	.40000						
MATERIAL 5 - 2 ENTRIES	492.000	.59300	4810.000	.98960						

DENSITY -LBS./CU. IN.									
MATERIAL 1		.070800							
MATERIAL 2		.061400							
MATERIAL 3		.079500							

MATERIAL 4		.584900							
MATERIAL 5		.357900							

of velocity of 36 kfps and a flight path angle of -5.3 degrees at an altitude of 400 kft were used to determine the peak metal clad temperature during reentry, the minimum clad temperature at impact, and the maximum recession of the reentry member. The evaluation of reentry member thermal stress was based on a prompt reentry with a velocity of 36 kfps and a flight path angle of -89.9 degrees at an altitude of 400 kft.

Module Separation. In previous analyses reported by Battelle^{*}, it was assumed that for low-angle reentry the heat source modules would be released (and would separate) at an altitude of 271 kft. This altitude is approximately the same as was determined for breakup of MHW LES 8/9. However, during the current reporting period, analyses were conducted to determine the effect of altitude of module release and separation on the peak metal clad temperature experienced during reentry and on the metal clad temperature at impact. Results of these analyses were shown previously in Figure 2 of this report. It was found that a maximum in the peak clad temperature for TD 4.1 for reentry of the individual heat source modules from an altitude of 330 kft. This altitude was used in subsequent 2-d and 3-d analyses of TD 4.1.

For prompt reentry of the generator, release and separation of the heat source modules was assumed to occur at an altitude of 200 kft as in the previous analyses.^{**} This altitude is sufficiently high to expose the modules to the peak aerodynamic heating rates experienced on the prompt trajectory. It should be noted that the peak thermal stress developed in the reentry member is dependent on the converter break-up altitudes. It is possible that higher stress levels could be experienced for break-up and module separation at other altitudes.

Aerodynamic Characteristics. Aerodynamic characteristics used for the trial design heat source modules are identical to those reported previously.^{***} For the hypersonic and supersonic flight regimes, it was assumed that the heat source module maintains a broadface stable reentry mode. Results of stability tests conducted at NASA-Ames for LASL suggests that this orientation is a potential mode of stability. However, the test results also indicate that the side-on orientation may be a stable

^{*} Battelle reports, BMI-X-685 (October, 1977), BMI-X-691 (March, 1978), and BMI-X-696 (July, 1978).

^{**} Op cit.

^{***} Battelle Report, BMI-X-685, October, 1977, pp 81-87.

orientation or that, most likely, the heat source module will tumble in a random manner due to loads experienced during breakup of the generator or due to aerodynamic forces.

As previously reported,^{*} autorotation of the modules is assumed during subsonic flight, based on the results of drop tests conducted by TES at Wallops Island.

Aeroheating and Mass Loss Models. As part of the continuing effort to update and improve the thermal and ablation models, several modifications were made prior to the three-dimensional analysis of Trial Design 4.1. The effect of these changes was investigated by comparative runs using the two-dimensional models of Trial Designs 4.1 and 1.3.1. Five changes or additions were made as follows:

- Hypersonic stagnation point convective heating correlation revised
- Graphite sublimation model revised
- Graphite oxidation downstream in diffusion controlled regime modified
- Form of transitional mass loss expression changed
- Mechanical mass loss model added.

Based on a detailed study of experimental shock-tube data at John Hopkins University's Applied Physics Laboratory (APL)^{**} it was recommended that the empirical correlation of Detra, Kemp, and Riddell^{***} be used for continuum flow predictions of the laminar, hypersonic convective stagnation-point heating rate of spherical bodies in the velocity regime of 6000 to 58,000 ft/sec. This correlation was therefore substituted for the correlation of Lewis and Burges^{****,*****}

* Battelle Report, BMI-X-696, (July, 1978).

** Perini, L. L., "Compilation and Correlation of Experimental Hypersonic, Stagnation Point Convective Heating Rates", Report ANSP-M-4, Applied Physics Laboratory, July, 1972.

*** Detra, R. W., Kemp, N. H., and Riddell, F. R., "Addendum to Heat Transfer to Satellite Vehicles Re-Entering the Atmosphere", Jet Propulsion, Vol. 27, No. 12, pp 1256-1257, Dec., 1957.

**** Lewis, C. H., and Burgess, E. G. III, "Charts of Sphere Stagnation Heat-Transfer Rate in Air and Nitrogen at High Temperatures",

AEDC-TDR-63-139 (AD 409870), July, 1963, 17 pp.

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

which had been used for the same velocity regime in all previous analyses. The conversion to a flat-faced reentry body and the heating away from the stagnation point was handled by heating factors normalized to the stagnation point rate which remained the same and have been discussed in detail in previous reports^{*,**}. The change to the Detra, Kemp, and Riddell correlation resulted in an increase in the convective heating rates by approximately 20 percent above the levels previously calculated.

In previous analyses, graphite sublimation was handled by the correlation equations developed by Scala and Gilbert^{***}. Recently, discrepancies have been found by APL^{****} between results obtained using the Scala and Gilbert equations and the techniques employed at APL. These techniques make use of current thermochemical data and correlations of heat and mass transfer coefficients^{*****} which were not available when Scala developed his correlation. However, the complete solution employed at APL is too detailed to be added directly to the reentry analyses. Fortunately, APL has developed correlation equations which can be readily used; these were programmed to replace the Scala equations previously used. In addition to the advantages already cited, the APL correlation extends the mass loss range into the super-orbital velocity regimes and is also appropriate for downstream locations for both laminar and turbulent boundary layers.

The results with the APL correlation equations are different from the results using the Scala correlation equation. At the stagnation point, the surface temperatures and mass-loss rate in the sublimation regime are less using the APL correlation. The stagnation point temperatures are decreased as much as 280 C and the surface mass-loss rate is lower by up to 35 percent. However, downstream from the stagnation

* Battelle Report BMI-X-696, (July, 1978), pp 24-25.

** Battelle Report BMI-X-691, (March, 1978), pp 7-13.

*** Scala, S. M., and Gilbert, L. M., "Sublimation of Graphite at Hypersonic Speeds", AIAA Journal, 3 (9), 1635-1644 (September, 1965).

**** Perini, L. L., "Heat and Mass Transfer Correlation Equations for Subliming Graphite in High Speed Flow", Report AnSP-M-11, Applied Physics Laboratory, August, 1974.

***** Putz, K. E., Bartlett, E. P., "Heat-Transfer and Ablation Rate Correlations for Reentry Heat Shield and Nosetip Applications", Journal of Spacecraft, Vol. 10, No. 1, January, 1973, pp 15-22.

point the results are reversed with the APL correlation resulting in much higher temperatures, particularly at the corner region, with temperatures up to 600 C higher. The mass loss rate is also increased by as much as a factor of three at the corner. Because of the brief time spent by the heat source module in the sublimation regime, particularly in the low-angle, maximum recession trajectory, the increase in corner recession resulting from use of the revised mass-loss correlation does not have a large integrated effect on the total recession experienced by the reentry member. Likewise, the changes in surface temperature have only a minor effect on the total heat balance and the resultant clad temperature. However, the changes in surface temperature may have a significant effect on the peak thermal stresses.

The technique for calculating the graphite oxidation rate in the diffusion-controlled regime was revised to be more applicable to non-spherical shapes. The revised technique is also compatible with the APL correlation equations in the sublimation regime. In previous analyses, the following Scala correlation for mass loss rate in the diffusion-controlled regime* was used.

$$\dot{m}_D = 0.00635 \left(\frac{P_W}{R_N} \right)^{1/2}$$

where

$\dot{m}_D$ = diffusion mass-loss rate in lb/sec ft²

P_W = pressure at the wall in atmospheres

R_N = nose radius in ft.

In the APL approach, the generalized mass-loss rate at the wall is

$$\dot{m}_W = B' \phi (\rho_e U_e C_{h_o})$$

*Scala, S. M., "The Ablation of Graphite in Dissociated Air, Part I Theory", General Electric Company, Missile and Space Division, Report TIS R62 SD72, September, 1962.

where

- B' = mass-transfer parameter
- ϕ = blowing correlation parameter
- ρ_e = density at edge of boundary layer
- U_e = velocity at edge of boundary layer
- C_{h_o} = no blowing local heat-transfer coefficient.

In the diffusion-controlled regime, with CO as the product, $B' = 0.174$ and $\phi = 0.88$ so that the expression becomes

$$\dot{m}_D = 0.153 (\rho_e U_e C_{h_o})$$

This expression was used to replace the Scala diffusion mass-loss expression. When CO_2 becomes the product below the specified temperature of 1100 C, the same procedure is used as was previously employed, i.e., the constant in the mass-loss equation is reduced by a factor of two* and appropriate changes are made in the heat balance and blocking coefficients.

The results of the change in the diffusion-controlled oxidation expression are similar to the results of the sublimation correlation change. The mass-loss rate calculated at the stagnation point is about 35 percent lower than that calculated previously whereas the rate at the heat source module corner is approximately 35 percent higher than calculated previously. The sidewall recession is approximately twice the value calculated previously. Because of the changes in mass-loss rate distribution in both the diffusion and sublimation regimes, the maximum recession is predicted to occur at the module corner rather than at stagnation point. However, the magnitude of the maximum recession is almost the same as was calculated previously.

In the transition region, between the kinetic and diffusion controlled regimes, a revised mass-loss expression for 1/2 order Arrhenius kinetics has been derived by APL** as follows

$$\dot{m}/\dot{m}_D = (1/2 + [1/4 + (\dot{m}_D/\dot{m}_R)^2]^{1/2})^{-1},$$

* Perini, L. L., "Graphite Oxidation Modeling in Subsonic Flow",

** Applied Physics Laboratory Memorandum ANSP-111, August 9, 1977.

Conn, D. W., Letter to Gale Whitacre, ANSP-L-368, November 6, 1978.

where

$$\begin{aligned}\dot{m} &= \text{transitional mass-loss rate} \\ \dot{m}_D &= \text{diffusion mass-loss rate} \\ \dot{m}_R &= \text{kinetic reaction mass-loss rate.}\end{aligned}$$

This equation represents an extension of the combined (kinetic and diffusion) resistances technique demonstrated by Frank-Kamenetskii* for first-order kinetics. This expression is slightly more rigorous than the half-order expression developed by Scala, which was previously used,

$$\dot{m}/\dot{m}_D = [1 + (\dot{m}_D/\dot{m}_R)^2]^{-1/2},$$

and was substituted for the Scala expression. Mass loss in the transition regime with the revised expression is slightly less than previously calculated (maximum of 13 percent difference). AVCO Mod 3A kinetic data were used for the FWPF material used in the 2-d and 3-d analyses in the absence of data for the latter material.

When the APL sublimation model and correlation is used, the results agree with the data of Lundell and Dickey** when a mechanical mass loss model is included. Therefore, the approximate mass loss model developed by APL*** from the Lundell and Dickey data was also added when the change was made to the APL correlation equations. This curve fit model is

$$\dot{m}_e = 0 \quad \text{for } T_W < 3873 \text{ R},$$

$$\frac{\dot{m}_e - \dot{m}_W}{\dot{m}_W} = \frac{565.2}{7520 - T_W} - 0.155$$

for $3873 \leq T_W \leq 7228 \text{ R}$, and

$$\frac{\dot{m}_e - \dot{m}_W}{\dot{m}_W} = 1.78$$

* Frank-Kamenetskii, D. A., Diffusion and Heat Transfer in Chemical Kinetics, 2nd Edition, Plenum Press, 1969.

** Lundell, J. H., and Dickey, R. R., "Ablation of ATJ Graphite at High

*** Temperatures, AIAA Journal, Vol. 11, No. 2, February, 1973, pp 216-222.

Conn, D. W., Letter to Gale Whitacre, ANSP-L-368, November 6, 1978.

for $T_W \geq 7228 \text{ R}$

where

$\dot{m}_e$ = mechanical mass-loss rate

$\dot{m}_W$ = local thermochemical mass-loss rate

T_W = local wall temperature.

Results obtained for the low angle (maximum recession) trajectory indicates that this mechanism makes an insignificant contribution to the total mass loss because of the relatively low reentry member surface temperatures. For the steep angle reentry, mechanical mass should be included particularly if it is augmented by surface roughness or discontinuity effects.

In summary, five changes and additions have been made to the thermal and ablation analysis technique. The combined effect of the changes on predicted containment member peak and impact temperature has been small. The magnitude of the predicted maximum recession has also remained about the same. However, it is now predicted that the maximum recession will occur at the corner of the heat source module rather than at the stagnation point.

Solid Propellant Environment. Analyses were conducted to determine the thermal response of heat source module components for exposure of the module to a fire environment resulting from a space shuttle launch pad abort. The heat flux environment is shown in Figure 8 by the solid line^{**}. The severity of this environment, which results from an assumed separate fragments model, can be compared to the environment contained in the current applicable Overall Safety Manual,^{***} (see Figure 8). Also shown in Figure 8 is the space shuttle heat flux environment that would result from a single-split motor model. This model was not used in the current analyses.

* Lundell, J. H., and Dickey, R. R., "Ablation of ATJ Graphite at High Temperatures, AIAA Journal, Vol. 11, No. 2, February, 1973, pp 216-222.

** Edgecombe, D. S., et al, Draft, Volume II of Final Technical Report on "Evaluation of the Space Disposal of Defense Nuclear Waste - Phase II; to NASA-MSFC from Battelle Columbus Laboratories (December 8, 1978).

*** Overall Safety Manual, Prepared for the U.S. A.E.C. by NUS Corporation (June, 1974) Volume 1, Summary, Appendix A.

FIGURE 8. FIRE ENVIRONMENT

Thermal Stress Analysis of Trial Designs
(L. E. Hulbert)

The prior screening and evaluation studies of the conceptual designs and the trial designs were carried out on the basis of a two-dimensional analysis of the thermal stresses. In this reporting period, procedures were developed for the analysis of the three dimensional thermal-stress distributions in the reentry member. These procedures were applied to the analysis of the stresses in TD 4.1. Consideration is currently being given to the utilization of Fine Weave Pierced Fabric (FWPF) carbon/carbon composites for the reentry member of this design. This section will describe the thermal-stress analyses of TD 4.1 FWPF composite reentry member.

The analysis procedure required the following steps:

- (1) Acquisition of the most representative material property data for FWPF c/c composite
- (2) Development and analysis of a 2-d finite-element model of TD 4.1
- (3) Development of a 3-d interpolation program to calculate temperatures at finite-element nodal-point positions from temperature computed by the reentry thermal computer code at finite difference mesh point positions
- (4) Development of a 3-d finite element mathematical model of TD 4.1
- (5) Selection and application of an appropriate three-dimensional finite element analysis program to the analysis of the thermal stresses.

Acquisition of Material Properties

Carbon-carbon composite technology has been going through a transition period because of the unavailability of the traditional carbon fiber precursors. In recognition of this fact, LASL has selected a pan

fiber FWPF carbon/carbon composite for the reentry member. This material has been under investigation by the Department of Defense for use in reentry systems.

The decision to use this composite required the acquisition of its thermophysical properties over the temperature range (0-6500 F) that the reentry member would experience in high-angle reentry. Certain material property data for this material have been generated by Southern Research Institute (SoRI) for the Air Force Material Laboratory. With Air Force approval, the available data were obtained in a raw form from SoRI. This data included tensile moduli to 2000 F, compressive moduli to 5500 F shear moduli to 5000 F, thermal expansion coefficients to 6000 F and thermal conductivity coefficients to 4500 F. The lack of high temperature tensile data necessitated an extrapolation of the available data to the higher temperature. Initially, this extrapolation was made by extending the 2000 F moduli parallel to the data reported for AVCO MOD 3a composite. Based on subsequent discussions between Battelle and SoRI staff, a new set of curves of the thermoelastic material properties of FWPF composite was provided.

Data on the compressive and tensile yield strengths at various temperatures as well as the secondary moduli at these temperatures are still in preparation. Thus, the decision was made to conduct only elastic analyses at this time. This was consistent with all of the previous analyses that were made.

Temperature Interpolation, FD to FE Meshes

In preparation for carrying out the 3-d thermal stress analysis, an evaluation was made of the temperature interpolation program used earlier in the 2-d analysis of the thermal stresses in the trial designs. It was decided that this 2-d interpolation program was not suitable for extension to three-dimensions. Therefore, a new 2-d interpolation approach was developed. This was based on finding the finite difference node points nearest to the finite element point in the x and y directions. Then, the temperature at the finite element node was obtained from these finite difference nodes using a bilinear interpolation formula. An efficient sorting scheme was devised, based on the structure of the data generated

by the 2-d finite difference thermal analysis program. It was found that this program gave somewhat more realistic temperatures at the reentry surface than the interpolation scheme used earlier. The temperatures generated by this interpolation program were used for the 2-d thermal stress analysis of trial design TD 4.1.

Extension of this 2-d temperature interpolation program to accomplish the corresponding 3-d interpolation proved to be more difficult than originally anticipated. A variety of problems were encountered, caused primarily, by the relative coarseness of the finite difference and finite element meshes that were required in the 3-d models. The initial approach to the 3-d interpolation problem consisted of determining the finite difference points nearest the given finite element node in each of the r , θ and z -directions. (The thermal analysis program was based on cylindrical coordinates related to the fuel cavity). A sorting scheme was developed to find these points efficiently. This sorting scheme had to be programmed to account for many special problems, including the transition from 360 degrees to zero degrees in the circumferential direction, and certain regions where there was only one finite difference point in the radial direction. After accounting for all of the various special geometrical cases, it was found that the computed temperatures on both the outer and inner surfaces of the reentry member were unrealistically low. A careful study of this phenomenon showed that it was the result of extrapolating temperature data computed at points interior to the reentry member to predict temperatures at either the inner or outer surface. Since the temperatures near the outer surface showed a steep gradient and the temperatures at the fuel cavity showed a shallow gradient, sampling the temperatures at two points interior to the reentry member underestimated the outer surface gradient and overestimated the inner surface gradient, this led to underestimating both surface temperatures as noted above.

An appropriate remedy for this problem involved using the total set of finite difference temperature data points only to compute the temperatures at interior points of the reentry member. On the inner and outer surfaces of the reentry member, two-dimensional interpolation schemes were used to compute the finite element temperatures using only finite difference temperatures that were computed for each surface. This approach yielded a realistic set of temperatures at the finite element mesh points.

Two-Dimensional Stress Analysis

Figure 9 shows the finite element model used to determine the peak stress in the TD 4.1 reentry member. The crosssection shown in Figure 9 is taken through the capsule midsection. The stress analysis used interpolated temperatures obtained from the thermal analysis. As in analyses previously conducted, only elastic behavior was assumed.

Generation of the 3-d Finite Element Model

Development of 3-d finite element models of structures as complex as the reentry member is a difficult and time-consuming task. A number of computer programs have been developed to accomplish this task. One of the most useful of these mesh generators was developed by William Cook of the Los Alamos Scientific Laboratories. With Dr. Cook's generous assistance, two geometrical models were successfully generated for one-quarter sections of the TD 4.1' reentry member. These models differed only in that for one model a double set of nodes is allowed along the cylindrical surface at the midplane of the threads to permit separation of the model where the threads are under "tension".

Selection and Application of a 3-d Finite Element Computer Program

Selection of an appropriate finite element computer program to carry out the analysis of the reentry member was difficult because of the requirements to model the material behavior. The FWPF composite is a

FIGURE 9. FINITE ELEMENT MESH FOR TD 4.1

transversely isotropic material with the properties in the pierced fiber direction differing from those in the weave plane. These properties vary significantly over the temperature range which the reentry member experiences.

A search of available analysis programs showed two programs with the required capacity to represent the material behavior of the FWPF composite. These were NASTRAN Level 17 available on a time-shared network and the program SAVFEM which has been made available to Battelle by a local consultant and used in three-dimensional stress analysis on other research programs. Although it had originally been planned to use NASTRAN Level 17, a decision was made to use program SAVFEM because NASTRAN was available only on the CYBERNET time sharing system which was inconvenient to use. Moreover, with prior experience in using SAVFEM and the easy availability of the program originator for assistance, SAVFEM was more cost-effective to use than NASTRAN Level 17.

APPENDIX A

MODULE OPERATING TEMPERATURE PROFILES

APPENDIX A

MODULE OPERATING TEMPERATURE PROFILES

Contained in this Appendix are the operational temperature profiles for TD 4.1 and TD 1.3.1. These temperature profiles were calculated using the 2-d thermal analysis techniques and network described in the body of the report. A hot shoe temperature of 900 C was used for both designs and it was assumed that the clad emittance was 0.4. The temperature profile for TD 1.3.1 was previously given as Figure A-5 in the July, 1978, BMI-X-696 report.

FIGURE A-1. OPERATING TEMPERATURE PROFILE FOR TD 4.1

FIGURE A-2. OPERATING TEMPERATURE PROFILE
FOR TD 1.3.1

APPENDIX B

CLAD TEMPERATURE HISTORIES

APPENDIX B

CLAD TEMPERATURE HISTORIES

Contained in this Appendix are the maximum and minimum clad temperature histories for TD 4.1 and TD 1.3.1. The temperature histories were calculated for the shallow flight path trajectory ($\gamma = -5.3$ degrees). The matrix of clad temperature histories given in Figures B-1 - B-14 is given in Table B-1 so that the effect of release altitude of the heat source modules, contact conductance, and heat transfer factor can be associated with the temperature history.

TABLE B-1 MATRIX OF CLAD TEMPERATURE HISTORY FIGURES IN APPENDIX B

Figure No.	Trial Design	Type of Model Used	Altitude of Release, KFT	Contact Resistance, Btu/hr-ft ² -F	Heat Transfer Factor	
					Supersonic	Subsonic
<u>Revised Surface Chemistry</u>						
B-1	4.1	3-d	330	250	1.0	1.0
B-2	4.1	2-d	330	250	1.0	1.0
B-3	4.1	2-d	330	150	1.0	1.0
B-4	4.1	2-d	330	500	1.0	1.0
B-5	1.3.1	2-d	330	250	1.0	1.0
<u>Original Surface Chemistry</u>						
B-6	4.1	2-d	400	1,000	1.0	1.0
B-7	4.1	2-d	330	1,000	1.0	1.0
B-8	4.1	2-d	271 ⁽¹⁾	1,000	1.0	1.0
B-9	4.1	2-d	330	250	1.0	1.0
B-10	4.1	2-d	330	250		
B-11	4.1	2-d	330 ⁽¹⁾	250	1.2	1.0
B-12	4.1	2-d	330	250	1.0	0.6
B-13	4.1	2-d	330	250	1.0	1.4
B-14	4.1	2-d	330	250	1.4	1.4

⁽¹⁾ Analysis terminated at heat source module altitude of 103 KFT.

FIGURE B-1. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 3-D
ANALYSIS - 250 CONTACT RESISTANCE

FIGURE B-2. CLAD TEMPERATURE HISTORIES FOR TD 4.1
-2D ANALYSIS - 250 CONTACT RESISTANCE

FIGURE B-3. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2D ANALYSIS - 150 CONTACT RESISTANCE

FIGURE B-4. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2D
ANALYSIS - 500 CONTACT RESISTANCE

FIGURE B-5. CLAD TEMPERATURE HISTORIES FOR TD 1.3.1 - 2D
ANALYSIS - 250 CONTACT RESISTANCE

FIGURE B-6. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 1000 CONTACT RESISTANCE
400 KFT RELEASE

FIGURE B-7. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 1000 CONTACT RESISTANCE -
330 KFT RELEASE

FIGURE B-8. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS -1000 CONTACT RESISTANCE -
271 KFT RELEASE

FIGURE B-9. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 250 CONTACT RESISTANCE -
330 KFT RELEASE

FIGURE B-10. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 1.4 SUPERSONIC HEATING FACTOR

FIGURE B-11. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 1.2 SUPERSONIC HEATING FACTOR

FIGURE B-12. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D ANALYSIS - 0.6 SUBSONIC HEATING FACTOR

FIGURE B-13. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D ANALYSIS - 1.4 SUBSONIC HEATING FACTOR

FIGURE B-14. CLAD TEMPERATURE HISTORIES FOR TD 4.1 - 2-D
ANALYSIS - 1.4 SUPERSONIC AND SUBSONIC HEATING
FACTORS

APPENDIX C

REENTRY MEMBER RECESSION HISTORIES

APPENDIX C

REENTRY MEMBER RECESSION HISTORIES

Contained in this Appendix are the stagnation point reentry member recession histories for TD 4.1 and TD 1.3.1. The recession histories were calculated for the shallow flight path trajectory ($\gamma = -5.3$ degrees). Assumptions regarding altitude of module release and separation, the extent of contact between materials, and aeroheating boundary conditions are summarized in Table C-1.

TABLE C-1. MATRIX OF REENTRY MEMBER RECESSION HISTORY FIGURES IN APPENDIX C

Figure No.	Trial Design	Type of Model Used	Altitude of Release, KFT	Contact Resistance, Btu/hr-ft ² -F	Heat Transfer Factor	
					Supersonic	Subsonic
<u>Revised Surface Chemistry</u>						
C-1	4.1	3-d	330	250	1.0	1.0
C-2	4.1	2-d	330	250	1.0	1.0
C-3	4.1	2-d	330	150	1.0	1.0
C-4	4.1	2-d	330	500	1.0	1.0
C-5	1.3.1	2-d	330	250	1.0	1.0
<u>Original Surface Chemistry</u>						
C-6	4.1	2-d	400	1,000	1.0	1.0
C-7	4.1	2-d	330	1,000	1.0	1.0
C-8	4.1	2-d	271 ⁽¹⁾	1,000	1.0	1.0
C-9	4.1	2-d	330	250	1.0	1.0
C-10	4.1	2-d	330	250	1.4	1.0
C-11	4.1	2-d	330 ⁽¹⁾	250	1.2	1.0

⁽¹⁾ Analyses terminated at heat source module altitude of 103 KFT.

FIGURE C-1. REENTRY MEMBER RECESSION HISTORY FOR
TD 4.1 - 3 - D ANALYSIS

FIGURE C-2. REENTRY MEMBER RECESSION HISTORY FOR
TD 4.1 - 2-D ANALYSIS - 250 CONTACT
RESISTANCE

FIGURE C-3. REENTRY MEMBER RECESSION HISTORY FOR
TD 4.1 - 2-D ANALYSIS - 150 CONTACT
RESISTANCE

FIGURE C-4. REENTRY MEMBER RECESSION HISTORY FOR
TD 4.1 - 2-D ANALYSIS - 500 CONTACT
RESISTANCE

FIGURE C-5. REENTRY MEMBER RECESSION HISTORY FOR
TD 1.3.1 - 2-D ANALYSIS - 250 CONTACT
RESISTANCE

FIGURE C-6. REENTRY MEMBER RECESSION HISTORY FOR TD 4.1 -
2-D ANALYSIS - 1000 CONTACT -
400 KFT RELEASE

FIGURE C-7. REENTRY MEMBER RECESSION HISTORY FOR TD 4.1 - 2-D
ANALYSIS - 1000 CONTACT RESISTANCE -
300 KFT RELEASE

FIGURE C-8. REENTRY MEMBER RECESSION HISTORY FOR TD 4.1 -
2-D ANALYSIS - 1000 CONTACT RESISTANCE -
271 KFT RELEASE

FIGURE C-9. REENTRY MEMBER RECESSION HISTORY FOR TD 4.1 -
2-D ANALYSIS - 250 CONTACT RESISTANCE -
330 KFT RELEASE

FIGURE C-10. REENTRY MEMBER RECESSION HISTORY FOR TD 4.1 -
2-D ANALYSIS - 1.4 SUPERSONIC HEATING
FACTOR

FIGURE C-11. REENTRY MEMBER RECESSION HISTORY FOR
TD 4.1 - 2-D ANALYSIS - 1.2 SUPERSONIC
HEATING FACTOR

APPENDIX D

CLAD TEMPERATURE HISTORIES
IN LAUNCH PAD FIRE

APPENDIX D

CLAD TEMPERATURE HISTORIES IN LAUNCH PAD FIRE

Contained in this Appendix are the minimum and maximum clad temperature histories for TD 4.1 and TD 1.3.1 during exposure of the heat source module to a space shuttle launch pad fire environment. The fire environment was given in the report in Figure 8. For all of the analysis results shown in this Appendix, it was assumed that there was no heat transfer to or from the one heat source module surface (broad face) resting on the earth. This assumption is conservative in that heat transfer from the module to the earth will result in lower maximum metal clad temperatures than shown herein, other assumptions being identical.

FIGURE D-1. CLAD TEMPERATURE HISTORIES FOR TD 4.1 IN
FIRE ENVIRONMENT - 2-D ANALYSIS - 250
CONTACT RESISTANCE

FIGURE D-2. CLAD TEMPERATURE HISTORIES FOR TD 4.1 IN FIRE ENVIRONMENT - 2-D ANALYSIS - 500 CONTACT RESISTANCE

FIGURE D-3. CLAD TEMPERATURE HISTORIES FOR TD 1.3.1
IN FIRE ENVIRONMENT - 2-D ANALYSIS -
250 CONTACT RESISTANCE

FIGURE D-4. CLAD TEMPERATURE HISTORIES FOR TD 1.3.1
IN FIRE ENVIRONMENT - 2-D ANALYSIS -
500 CONTACT RESISTANCE

DOE - Department of Energy
Advanced Systems and
Materials Production

Robert B. Morrow (9)

DOE - Chicago Operations Office

Patent Office

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

Air Force Materials Laboratory,
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D. L. Schmidt
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Fairchild Space and Electronics Company

A. Schock

General Electric Company, Philadelphia

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R. D. Baker	K. Staudhammer
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