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DESIGN EVOLUTION AND VERIFICATION OF THE "GENERAL-PURPOSE HEAT SOURCE"

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ABSTRACT

The "General-Purpose Heat Source" (GPHS) is a radioisotope heat source for use in space power systems. It employs a modular design, to make it adaptable to a wide range of energy conversion systems and power levels. Each 250-watt module is completely autonomous, with its own passive safety provisions to prevent fuel release under all abort modes, including atmospheric reentry and earth impact.

Prior development tests had demonstrated good impact survival as long as the iridium fuel capsules retained their ductility. This requires high impact temperatures, typically above 900°C, and reasonably fine grain size, which in turn requires avoidance of excessive operating temperatures and reentry temperatures. These three requirements--on operating, reentry, and impact temperatures--are in mutual conflict, since thermal design changes to improve any one of these temperatures tend to worsen one or both of the others. This conflict creates a difficult design problem, which for a time threatened the success of the program.

The present paper describes how this problem was overcome by successive design revisions, supplemented by thermal analyses and confirmatory vibration and impact tests, and how this may be achieved while raising the specific power of the GPHS to 83 watts/lb, a 50% improvement over previously flown radioisotope heat sources.

INTRODUCTION

Several years ago the Department of Energy's Advanced Nuclear Systems and Projects Division initiated a program to develop a new and improved radioisotope heat source for use in space. Lead responsibility for this development went to the Los Alamos Scientific Laboratory (LASL), with a supporting role, particularly in the thermal analysis area, assigned to the Battelle-Columbus Laboratory (BCL).

The new heat source was designated as the "General-Purpose Heat Source" (GPHS), because its modular design makes it adaptable to a wide range of power levels and energy conversion systems. One of the motivations for its development is the expectation that, once it has been qualified for use in space, it can be employed in future missions without extensive requalification.

The GPHS is to see its first use in a new generation of radioisotope thermoelectric generators (RTGs) which will provide electrical power in two upcoming space exploration missions. The first is for the orbiter spacecraft of the NASA/JPL Galileo (Jupiter probe) mission, slated for an early-1984 launch, to study Jupiter and its moons; and the second is the joint NASA/ESA International Solar Polar Mission, which calls for an early-1985 launch of two spacecraft, developed under the respective direction of NASA/JPL and ESA, for scientific exploration outside the earth's ecliptic plane, including the region above the sun's poles.

The primary task of any program to develop a new radioisotope heat source is its safety qualification. Each heat source module must contain its own passive safety provisions to immobilize the fuel under all credible abort modes, including earth impact after atmospheric reentry.

The GPHS design developed by LASL, hereafter referred to as the reference or "REF" design, was presented at a DOE program review in July 1979. Their test results indicated that the design met most safety goals, inasmuch as repeated tests had demonstrated good impact survival as long as the iridium fuel capsules retained adequate ductility. However, to ensure the required ductility, the fuel capsules must impact at relatively high temperatures, and with a reasonably fine grain size. The latter can only be retained by avoiding excessive operating and reentry temperatures.

To provide adequate impact ductility, three tentative goals for the iridium capsules had been established: a normal operating temperature below 1330°C, a peak reentry temperature limit of 1800°C, and a minimum impact temperature of 900°C. The analytical results presented by BCL at the July-79 review indicated that the existing REF design fell short of all three

goals. What made the problem particularly intractable was the mutual conflict between these three goals, since thermal design changes to improve any one of these temperatures tended to worsen one or both of the others.

To help deal with this problem, DOE formed a special task force, chaired by GE and including representatives of Battelle, APL, Mound, ORNL, and Fairchild. Over the next several months, this task force made very useful contributions in reviewing and refining the materials properties models, the basic assumptions for the reentry analysis, the aerodynamic heating models, and the thermal analysis models.

Simultaneously, the author--with the support of other Fairchild personnel--conducted parallel studies of various design revisions for meeting the above goals. Those studies, which eventually led to a successful solution, are the subject of this paper. In general, the paper describes only the work done under the author's direction, and refrains from describing the work of other organizations except where essential for a proper understanding. Even on our own work, much information about analytical techniques, assumptions, models, and supporting sensitivity studies had to be omitted because of space limitations. However, the paper attempts to present enough information to explain not only what design was finally arrived at, but also how and why.

BACKGROUND

Designing a radioisotope heat source solely for normal operation would be relatively easy. The heat generation rate of the Pu^{238}O fuel can be predicted with utmost confidence, and its compatibility with suitable clad materials has been amply demonstrated. Since the clad must be able to resist long-term air oxidation at elevated temperature after earth impact, the choice was restricted to platinum and iridium alloys. The platinum alloy (Pt-3008) has the advantage of retaining its ductility at low temperatures, but was eliminated because the melting point of its carbon eutectic was deemed too low to survive the reentry heat pulse.

The principal design problem is protection of the fuel capsule against all anticipated accident conditions. In the GPHS concept, this is done by surrounding the capsule with a number of graphitic shells: an outer aeroshell, to provide ablation protection during reentry; an impact shell, to absorb energy and cushion the fuel capsule during earth impact; and a thermal insulator, to keep the capsule from overheating during hypersonic reentry and from overcooling during subsonic descent. The thermal insulation also provides protection against launch pad fires, and the impact and aeroshells also protect the capsules against shrapnel caused by explosions.

Fig. 1-GENERAL PURPOSE HEAT SOURCE MODULE

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The general arrangement of these components in the REF design is depicted in figure 1. The heat source for each ISPM and Galileo RTG will consist of a stack of eighteen such modules. Each module contains four 62.5-watt fuel pellets encapsulated in vented shells made of an iridium alloy (DOP-25). There are two fuel capsules per impact shell, and two impact shells per aeroshell, giving a total thermal power of 250 watts per module. The aeroshell and impact shell are both made of FWPF (Fine-Weave Pierced Fabric), a three-dimensional structure of carbon-bonded carbon fibers, about 90% dense. This material is very tough and has exhibited excellent thermal-shock resistance. Finally, the thermal insulator consisted of two thin shells of Pyrolytic Graphite (PG). This material has a very low thermal conductivity in the direction normal to its deposition plane.

The module design depicted in figure 1 had been analyzed for a variety of reentry trajectories and attitudes. Some salient results of critical iridium temperatures are presented in table 1 for two types of reentry (min- , and orbital decay) discussed in the next section. Compared to the tentative goals cited in the previous section, the table shows that the design yielded too high an operating temperature, too high a reentry peak temperature, and too low an impact temperature.

Table 1-GPHS CAPSULE TEMPERATURE PROBLEM

	Normal Operating Temperature	Peak Min-Y Reentry Temperature	Orbital Decay Impact Temperature
TENTATIVE GOAL	< 1330°C	< 1800°C	> 900°C
BCL Predictions:			
Face-on Reentry	1375°C	1870°C	812°C
Tumbling Reentry	1375°C	2029°C	900°C
Side-on Reentry	1375°C	2320°C	887°C

The principal design variable subject to change was the thermal resistance between the aeroshell and the fuel capsule. The difficulty was that decreasing that thermal resistance would help by reducing the iridium operating temperature, but hurt by raising the peak reentry temperature and by lowering the impact temperature. Conversely, raising that thermal resistance would benefit the peak and impact temperatures, but worsen the normal operating temperature.

To solve this problem, we proposed two parallel approaches: external redesign of the module's aerodynamic shape, to prevent side-on stability; and internal redesign of the module's thermal barriers, to meet the design goals for the face-on and tumbling reentry cases. The former effort was directed by the DOE task force; the latter approach, which was pursued by Fairchild, is described in the subsequent sections.

THERMAL DESIGN AND ANALYSIS*

As a first step in the thermal redesign, it was decided to carry out some steady-state studies, to define the available options and gain an understanding of their relative merit. To do this, the very crude thermal model depicted in figure 2 was initially employed. It consisted of three bodies and two thermal resistances: an inner resistance, between the fuel capsule and the impact shell; and an outer resistance, between the impact shell and the aeroshell.

Fig. 2-EXPLORATORY ANALYSIS MODEL

Thermal resistance between bodies can be provided either by barriers or by gaps. Barriers are layers of materials with intrinsically low thermal conductivity. Because of high-temperature and compatibility considerations, the barrier materials of principal interest for the present application are PG (pyrolytic graphite) and CBCF (a carbon-bonded carbon fiber structure of about 10% density, developed by the Oak Ridge

*It should be noted that throughout the course of this study, other members of DOE task force were continually conducting new measurements, particularly on physical properties and on aerodynamic heating factors. These new results were periodically fed into our studies. For this reason, the data and analytical results presented in the earlier parts of this paper may not be consistent with those presented later. However, all the results in any one figure or table are internally consistent, and the corresponding trends and conclusions remain valid.

National Laboratory). Both materials would be used with the principal heat flow direction normal to their deposition plane.

Table 2 compares the thermal resistivity of various barriers and gaps, in vacuum and air, for the three operational phases at their representative temperatures. The values are expressed as the temperature drop in °C to produce a heat flux of 1 watt/cm². The table examines a single temperature representative of each phase of the module's history: 1200°C to represent the normal operation of the heat source in the RTG; 1800°C to represent the hypersonic reentry heating phase; and 800°C to represent subsonic cooling during atmospheric descent before impact.

Table 2-THERMAL RESISTIVITY OF BARRIERS AND GAPS °C/W/cm²

	Normal Operation 1200°C		Hypersonic Reentry 1800°C	Subsonic Descent 800°C	
	Vac	He	Vac	Vac	Air
BARRIER:					
PG (DOP-25)	4.6	4.6	9.0		3.6
CBCF (LOD-7)	40	8.0	70		40
GAP:					
	Vac	He	Vac	Vac	Air
		0.01" 0.001"			0.01" 0.001"
Ir/Ir	85	5.6 10.6	30.4	219	28 50
Ir/PG	66	5.5 10.2	20.4	163	27 47
Ir/30	52	5.5 9.8	18.1	136	26 44
PG/PG	46	5.4 9.7	10.3	107	25 41
PG/30	34	5.1 8.9	8.0	80	23 36
30/30	20	4.6 7.5	5.8	54	20 29

As can be seen, CBCF is a much better insulator than PG. This, together with its much lower density (0.2 vs 2 gm/cc), makes it an attractive candidate.

Table 2 also shows that gaps are not very desirable for this application. They offer the highest thermal resistance during normal operation, precisely when we want low resistance to minimize the iridium temperature and consequent grain growth. But during reentry, when we wish to minimize peak heating and atmospheric cooling, these gaps provide relatively little thermal resistance. Their resistance is low during high-temperature reentry, because of the fourth-power effect; and it is low during the later stages of subsonic descent, because of air conduction.

Another design decision is whether the thermal barrier should be located inside or outside the impact shell. During normal operation, the heat flux is higher on the inside, and therefore a given thermal barrier will do less harm on the outside. During reentry there is more heat flow through the outer than the inner resistance, so that placing the thermal barrier outside the impact shell should be more effective in insulating the fuel capsule from reentry heating and subsonic cooling. Thus, both qualitative arguments suggest that there is an advantage in moving the thermal barrier to the outside of the impact shell.

To verify the above considerations and explore a number of design parameters and other concepts, a total of twenty design variations were selected for further study. These are depicted symbolically in figure 3.

Fig. 3-DESIGNS FOR EXPLORATORY STUDIES

The top line represents the REF design, with two 30-mil PG shells located between the fuel capsule and the impact shell. In design 1 one of the PG shells has been moved to the outside of the impact shell, and in design 2 both have. In design 3 the two 30-mil shells have been merged into a single 60-mil shell, and in design 3A they have both been increased to 60 mils. In designs 4, 4A, and 5 the outer PG shells are replaced by a single CBCF shell of 40, 60, and 80 mils, respectively.

Designs 7 through 17 represent more exotic options, involving outer metal shells with fusible plugs which melt during reentry. They assume that during normal operation the insulators and gaps are filled with helium, and therefore in a high-conductance mode. During reentry the outer metal shell would vent its contained helium, and the insulators and gaps would switch to their low-conductance mode. Some of these designs did indeed give very good results, but the analytical studies showed that the simpler designs described earlier were good enough, and that these exotic designs would not be needed. This was fortunate, because they would have required much more developmental testing, and would have resulted in a considerably heavier heat source.

To carry out the thermal analysis for each design, the basic model depicted in figure 2 was represented by a simple one-dimensional string with not more than 15 node points. Careful studies showed that, when consistent assumptions were employed, this 1D model gave almost the same results for tumbling reentry as Fairchild's 65-node 2D model, or as BCL's 1400-node 3D model. Thus, the 1D tumbling analyses provided a very efficient method for comparing various design concepts and parameters, and for assessing the sensitivity of the results to various assumptions.

Fig. 4-TRAJECTORY FOR $\gamma = -5.3^\circ$

Figure 4 shows a typical min- γ reentry trajectory used for evaluating the various designs, and figure 5 shows the resultant heat pulse. The trajectory shown assumes that the heat source module is released from the RTG at an altitude of 400 kft and a velocity of 36 kft/sec. It is referred to as a min- γ reentry, because it is the shallowest possible non-skip reentry angle ($\gamma = -5.3^\circ$). Analysis showed that this angle gives the highest peak reentry temperature. A slightly smaller reentry angle ($\gamma = -5.037^\circ$) gives a single-skip trajectory, in which the module bounces back to an altitude of 400 kft before returning to earth. While this trajectory gives a somewhat lower peak temperature, its much longer flight time results in an appreciably lower impact temperature. Since it generally gives the lowest safety margins, this trajectory was used for later design evaluations.

Fig. 5-HEAT PULSE FOR $\gamma = -5.3^\circ$

The third reentry trajectory used in our study is the much more probable case of orbital decay, which is based on a release altitude of 270 kft and initial velocity of 25 kft/sec. While this trajectory gives a somewhat lower peak temperature, its much longer flight time results in an appreciably lower impact temperature. Since it generally gives the lowest safety margins, this trajectory was used for the final design evaluations.

The computed temperature histories of the various module components during min- reentry are shown in figure 6a for the REF design, in figure 6b for design 2 (both PG shells moved to the outside of the impact shell), and in figure 6c for design 5 (where the PG shells have been replaced by a single CBCF shell). All three figures show only the portion of the flight before significant air introduction into the module. All show the phase lag between the aeroshell temperature peak and the clad temperature peak.

Fig. 6-REENTRY TEMPERATURE PEAKS

Figure 6a confirms that the gaps produce a worse temperature drop than the PG layers during normal operation, but are relatively ineffective in isolating the fuel capsule during reentry. Comparison of figures 6a and 6b confirms that the simple expedient of moving the PG shells to the outside of the impact shell benefits both the normal operating temperature and the reentry peak temperature of the iridium clad. And figure 6c shows the effect of the high resistivity of the CBCF insulator.

The computed operating and peak temperatures for these and other designs are displayed graphically in figure 7. The objective of the thermal design study was to move from the point representing the REF design toward the lower left quadrant. Point 1 shows the benefit of moving one of the two PG shells to the outside of the impact shell, and point 2 shows the advantage of moving both. The line through points 2 and 3A shows the effect of varying the thickness of the PG shells, and the line through points 4 and 5 shows the effect of varying the thickness of the single CBCF shell. Points 9A through 13 represent some of the more exotic designs discussed earlier. As can be seen, these designs do give significantly better results, but the simpler designs are good enough, and these more complex and heavier designs are not needed.

Fig. 7-DESIGN COMPARISON OF OPERATING AND PEAK TEMPERATURES FOR MIN- γ TUMBLING REENTRY

Based on the above results, Fairchild recommended that the REF design be changed by moving the thermal insulation, whether PG or CBCF, from the inside to the outside of the impact shells. After a 3D analysis by BCL had confirmed the predicted improvements in operating and peak temperatures, the recommended design change was adopted by DOE.

Thus far, our attention has been confirmed to the operating and peak temperatures of the iridium capsules. The third temperature of concern is the impact temperature, following subsonic descent through the atmosphere.

The REF design had called for gap widths of 3 to 4 mils. With such gap widths, all of the designs discussed above exhibited unacceptably low impact temperatures; i.e., below the ductile-to-brittle transition temperature of the iridium alloy. However, the Fairchild study revealed that the iridium impact temperature is a very sensitive function of the gap widths in the heat source module, as illustrated below.

Fig. 8

This sensitivity comes about because the dominant cooling mechanism of the fuel capsule is not radiation but air conduction across the gaps. At high altitudes, where the mean free path is large compared to the gap width, air conduction is insignificant. But at lower altitudes, once the mean free path drops below about one tenth of the gap width, the air inside the module acts as a continuum, and provides the dominant cooling mechanism of the capsule.

Figure 8 shows the iridium temperature history for tumbling, min- γ reentry of module design 3A with various gap widths. The analysis assumed an abrupt transition from no-air to full-air conduction at 290 seconds, when the module reaches an altitude of 100,000 feet. The resultant impact temperatures are listed in table 3.

Table 3-EFFECT OF GAP WIDTHS ON Iridium IMPACT TEMPERATURE

AFTER MIN-GAMMA REENTRY AIR INTRODUCED AT 290 SEC.	
GAP, mils	Ir - IMPACT TEMP
5	981 °C
10	1102 °C
15	1163 °C
20	1198 °C

As can be seen, increasing the gap widths from 5 mils to 10 mils leads to a large improvement in the impact temperature. Considerable additional improvement is obtained by going from 10 mils to 15 mils, but beyond that there is diminishing benefit.

Besides the thermal effects discussed above, there are mechanical design considerations which affect the choice of gap width. To retain the thermal benefits of large gaps, stand-offs are required to keep the gaps from closing up because of g-loads during reentry. These consist of integral rings projecting from the shells to be separated. The rings must be narrow, to minimize their thermal conductance. Larger gaps require higher stand-off rings, which raises doubts about their ability to survive launch vibration and about their effect on the iridium capsule during impact. For these reasons, a compromise gap width of about 10 mils was selected.

Based on the above results, Fairchild recommended that the REF design be changed by increasing all gap widths to at least 10 mils. After a 3D thermal analysis by BCL had confirmed the predicted improvement in impact temperature, the recommended design change was adopted by DOE.

Thus far, we have been judging computed impact temperatures against a tentatively chosen lower limit of 900°C. At this point, a more sophisticated yardstick was introduced, to take account of the fact that the ductility of the iridium alloy at impact depends not only on its temperature but also on its grain structure, which is a function of its previous history. In general, designs which produce higher operating temperatures and/or reentry temperatures will have coarser iridium grains at impact, and will require higher impact temperatures to provide the ductility needed for impact survival.

To quantify the above effects, A. Schaffhauser et al of ORNL had carried out extended tests to determine the effect of time and temperature on grain growth of the iridium alloy, and on the effect of grain size and test temperature on its mechanical properties under dynamic load conditions. The ORNL grain growth results are summarized below.

Table 4-EFFECT OF OPERATING AND PEAK REENTRY TEMPERATURES ON GRAIN SIZE OF DOP-26 Ir-0.3% W

Operation Temperature (6 months)	GRAIN SIZE (Number of Grains per Wall Thickness)					
	Maximum Reentry Temperature (2 min), °C					
	1500	1600	1700	1800	1900	2000
1275	18	17	14	12	9	6
1300	15	15	14	12	9	6
1330	12	12	12	12	8	6
1375	9	9	9	9	8	6
1400	8	8	8	8	8	6

Table 4 shows the predicted number of iridium grains per capsule wall thickness, for various combinations of operating and peak temperatures. The capsule is assumed to have dwelt six months at its normal operating temperature in space, followed by two minutes at the peak reentry temperature. The assumption of six-month operation in space is based on an assumed ignition failure of the IUS booster, in which case decay of the low-altitude orbit would cause spacecraft reentry in less than six months. The two-minutes-at-peak-temperature assumption embraces typical durations for the reentry heat pulse. This is conservative, since the capsule spends much of that two-minute period significantly below the peak reentry temperature, as illustrated earlier in figure 8.

Based on the grain growth data discussed above and on additional test results relating iridium ductility to grain size and impact temperature, ORNL recommended the minimum allowable impact temperature limits shown in figure 9. For each combination of operating and peak temperatures, the figure presents two alternative values for the minimum impact temperature to ensure the integrity of the capsule after impact: the lower-valued solid curves represent the minimum temperature for surviving 10% uniform strain; and the higher and more conservative dashed curves represent the minimum temperature for surviving 20% local strain. As can be seen, the two criteria yield temperature limits which differ by about 100°C. Because of their greater conservatism and sensitivity in discriminating between designs, the higher values were used in the subsequent studies for evaluating and comparing various designs. However, in recent impact tests the fuel capsules have exhibited strains of less than 10%, which suggests that the higher temperature limits may in fact be too conservative, and that the lower values may be more realistic design goals.

Fig. 9-MINIMUM ALLOWABLE IMPACT TEMPERATURE

As can be seen, at low reentry peak temperatures, below 1650°C, the minimum allowable impact temperature of iridium is essentially independent of the peak temperature, and only a function of the normal operating temperature. Conversely, for high peak temperatures, above 1900°C, the minimum allowable impact temperature is virtually independent of the six-month operating temperature, and only a function of the peak temperature.

To apply the ORNL results to our design study, the data from table 4 and figure 9 were cross-plotted in the form shown in figures 10 and 11. On a field of operating and peak reentry temperatures, figure 10 shows the iridium grain size at impact, and figure 11 shows the minimum allowable impact temperatures, based on ORNL's values for 20% local strain. Shown on the same figures are points representing various designs, both those identified earlier and others differing in assumed gaps, contacts, physical properties, or other parameters. In both figures, the upper cluster of points represent the min-γ reentry, and the lower cluster represents the orbit decay cases. As can be seen, for either type of reentry, many of the designs investigated in the Fairchild study result in significantly finer grain structures and lower allowable impact temperatures than the original reference design.

Fig. 10-IRIDIUM GRAIN SIZE AT IMPACT

Fig. 11-MINIMUM ALLOWABLE IMPACT TEMPERATURE, °C
BASED ON SCHAFFHAUSER'S 20% LOCAL-STRAIN LIMIT

Table 5 illustrates the salient results for a number of the studied designs. For each design, it gives the normal operating temperature, and--for both min-γ and orbital decay reentries--the peak temperature, the resultant grain size, the minimum allowable impact temperature, the predicted impact temperature (with 10-mil gaps), and the corresponding impact temperature margin. As can be seen, for each design the min-γ reentry gives a higher minimum allowable impact temperature, but it also gives a higher predicted impact temperature. Therefore, it is not possible to draw *a priori* conclusions as to which reentry mode is more severe. The boxed values in the table show that for some of the designs the min-γ reentries are controlling, while for others orbital decay yields the lower margin. As can be seen, a number of the designs show a significant improvement in impact margin over the REF design with 10-mil gaps (and an even greater improvement over the original REF design with 4-mil gaps).

Table 5-IMPACT TEMPERATURE MARGINS, TUMBLING REENTRY

CLAD TEMPERATURE		REENTRY	MIN-GAMMA					ORBITAL DECAY				
			NORMAL °C	PEAK °C	GRAINS PER 0.030"	IMPACT °C	MARGIN	PEAK °C	GRAINS PER 0.030"	IMPACT °C	MARGIN	
MIN-GAMMA	ALPHA-BELL	REF	1343	967	7	1060	1095	1625	11	990	1061	-71
1		1	1332	1915	8.4	1030	1114	1588	12	975	1066	-61
2		2	1319	1862	10	1020	1116	1564	13	960	1064	-56
3		3	1321	1841	11	990	1122	152	16	925	976	-51
4		4	1288	1904	9	1020	1020	1588	16	925	976	-51
5		5	1375	1775	12	975	1168	1535	13	965	1102	-37
6		6	1338	1901	9	1020	1155	1574	14	960	1083	-38
7		7	134	1840	11	990	1235	1555	12	910	1141	-171
8		8	141	1796	11	990	1270	1546	11	918	1137	-202
2.1		2.1	1294	1884	10	1020	1061	1516	19	890	994	-128
2.2		2.2	1275	1862	10	1020	999	1521	20	850	935	-168
4.1		4.1	266	1929	8	1040	1127	1528	19	890	1089	-150
4.2		4.2	1276	190	7	1065	1062	1563	19	904	1064	-46
4.3		4.3	1273	181	10	1020	1020	1516	20	860	929	-89
AL.3		AL.3	1278	1796	13	990	1115	1475	21	850	1023	-173
5.1		5.1	1399	184	12	960	1269	1592	14	950	1115	-118
5.2		5.2	1253	1878	11	960	1174	1483	20	878	1069	-196
5.3		5.3	137	1796	15	990	1192	1481	20	860	1086	-212

MINIMUM ALLOWABLE CLAD TEMPERATURE FOR GIVEN ORBITAL AND PEAK TEMPERATURES. (SCHARF) USER DATA FOR 200, 0.030, 10.0, 10

Table 5 is of interest in showing relative differences between various designs, but does not reflect the latest predictions of impact temperature margin. Since that table was completed, there have been numerous changes in the design, the thermal model, the physical properties, and the aerodynamic heating model.

The successive design changes, which were introduced for mechanical reasons, are described in the next section. The thermal model has undergone numerous refinements, particularly with respect to gaps, contacts, and the effects of the time-dependent changes in g-loads and air pressure during atmospheric descent on contact and CBCF conductances. In general, these changes have made the analysis more conservative, and lowered the predicted impact temperature margins.

Fig. 12-PURDUE RESULTS FOR EFFECTS OF TEMPERATURE AND GAS ON CBCF CONDUCTIVITY

The latest Fairchild thermal analyses are based on values of CBCF thermal conductivity measured by Prof. R.E. Taylor of Purdue, and on the impact shell-to-aeroshell conductance derived from the results of thermal tests described at the end of the next section. Figure 12 shows the conductivity values for CBCF in vacuum and atmospheric nitrogen, measured at various temperatures to 2000°C. For lower air pressures, the ramp model depicted in figure 13 is used to interpolate between the vacuum and atmospheric values. The three points in that figure represent some very recent measurements by BCL for CBCF in 0.01, 0.1, and 1 atmosphere of nitrogen, at 850°C. They indicate that the ramp model used in our analysis is too conservative.

Fig. 13-EFFECT OF AIR PRESSURE ON CBCF CONDUCTIVITY

Numerous studies and experiments conducted by the DOE task force participants since last summer have resulted in greatly improved data on the temperature-dependent physical properties of the pertinent materials, and have produced major changes in the predicted heating rates and heat distribution factors. The principal effect of the latter changes has been a large reduction in the heat input during reentry. This has both good and bad effects. The good effect is a substantial reduction in the peak reentry temperature, to the point where this parameter is no longer of great concern. But the bad effect is a substantial reduction in the predicted impact temperature. This caused much greater concern about the impact temperature margins, particularly for the low-probability class of long-flight atmospheric-skip reentry trajectories.

The present GPHS design (LMRE) is described at the end of the next section, and the thermal analysis results for the single-axis tumbling reentry for that design are summarized in table 6. Critical temperatures for reentry trajectories are presented.

Table 6-CRITICAL REENTRY TEMPERATURES

REENTRY ANGLE	TEMPERATURE °C		
	NORMAL	PEAK	IMPACT
$\gamma = 0^\circ$	1331	1426	999
$\gamma = -5.3^\circ$	1331	1584	1048
$\gamma = -5.1^\circ$	1331	1574	1000
$\gamma = -5.037^\circ$	1331	1533	942

The orbital-decay case (initial velocity = 25 kft/sec, $\gamma = 0^\circ$) is by far the most probable type of reentry abort. It would result from a failure of the IUS upper stage to fire, and also from any type of IUS misfire which does not cause a prompt reentry. It yields a very benign peak temperature, only 95°C above the normal operating temperature, but also a relatively low impact temperature, which is undesirable.

The prompt-reentry class of abort trajectories have a much lower probability of occurrence. They occur only if the IUS has fired to produce the proper earth-escape velocity of 36 kft/sec, but in the wrong direction. Steep angles of reentry are not of major concern here. They cause high but acceptable thermal stresses in the aeroshell, but relatively benign reentry peaks and impact temperatures in the clad. Of principal concern with regard to impact temperature margin are the shallow-angle reentries, particularly skip reentries in which the heat source module is released from the spacecraft during the initial descent, and then has a long period to cool off while it skips back to an altitude of 400 kft before final descent.

The shallowest non-skip reentry is $\gamma = -5.3^\circ$. As seen in table 6, this gives the highest peak temperature, but a relatively benign impact temperature. The initial angles of -5.1° and -5.037° represent skip reentries, with $\gamma = -5.037^\circ$ resulting in a skip to 400 kft, producing a relatively modest peak clad temperature but a rather low impact temperature. Since this type of reentry only occurs over a very narrow range of reentry angles, it clearly has a very low probability of occurrence. Shallower angles, at this reentry velocity, result in earth escape.

To convert the results presented in table 6 into impact temperature margins, the minimum allowable impact temperature for each case must be determined. It was recognized, only late in these studies, that the minimum impact temperature limits shown in figure 11 should only be applied to the orbit-decay case, but not to the other three cases, since these result in prompt reentries. If the IUS upper stage fired in the wrong direction, highly eccentric elliptical orbits with long lifetimes could result. But these would gradually be circularized by air friction during successive perigees, and eventually end in orbital decay. The type of trajectory represented by the $\gamma = -5.037^\circ$ case in table 6 could only occur in a prompt reentry. As will be shown, this has an important effect on the minimum allowable impact temperature for this type of case.

Figure 14 is a modified ORNL drawing, summarizing their experimental results on DOP-26 iridium grain growth at elevated temperatures.

Fig. 14-GRAIN GROWTH DATA ON DOP-26 IRIIDIUM IN VACUUM

The figure 14 curve for 1330°C, the normal clad operating temperature of the LMRE GPHS design, has been heaved and extrapolated. For the case of orbit-decay reentry we assume 5000 hours of normal operation before reentry, which is seen to result in a grain size of 12 grains per 0.025" wall. But for the prompt-reentry cases, we need only assume 100 hours of full-temperature operation before reentry. This includes ground testing before launch. Longer times will be spent at lower temperatures (e.g., with a xenon cover gas), but as shown in figure 14, even a 90°C temperature reduction to 1240°C reduces the grain growth rate to relatively negligible rates. Thus, for the 100 hours of full-temperature operation assumed for the prompt reentry case, figure 14 predicts a grain size of 22 grains per 0.025" wall thickness, which is much finer than the 12 grains predicted for the orbit-decay case.

Fig. 15-EFFECT OF REENTRY HEAT PULSE ON DOP-26 GRAIN SIZE AFTER 6 MO. AT VARIOUS OPERATING TEMPERATURES

Similar conclusions are obtained from figure 15, from a later ORNL report. For the LMRE peak reentry temperature of 1533°C for $\gamma = -5.037^\circ$, the figure predicts 12 grains per wall with six months of prior operation at 1330°C, and 22 grains without.

The effect of clad grain size on minimum allowable impact temperature is depicted in figure 16. This was constructed by cross-plotting ORNL predictions and recommendations, both for the 10% uniform-strain criterion and for the more restrictive 20% local-strain goal. The latter curve, which is much more conservative, prescribes a minimum impact temperature limit of 975°C for 12 grains per wall thickness (orbit decay) and only 840°C for 22 grains per wall (prompt reentry).

Fig. 16-CROSS-PLOT OF ORNL RECOMMENDATIONS FOR EFFECT OF GRAIN SIZE ON MINIMUM ALLOWABLE IMPACT TEMPERATURE

Thus, the impact limit for the prompt reentry cases is much less restrictive, resulting in the very positive impact temperature margins shown in table 7. Even the highly improbable worst-case of $\gamma = -5.037^\circ$ shows a positive margin of over 100°C.

Table 7-PROMPT REENTRY TEMPERATURE MARGINS, °C

REENTRY ANGLE	-5.3°	-5.1°	5.037
NORMAL OPERATING TEMP.	1331	1331	1331
PEAK REENTRY TEMPERATURE	1584	1574	1533
GRAINS PER 0.03" WALL	22	22	22
MIN. ALLOWABLE IMPACT TEMP.	840	840	840
IMPACT TEMPERATURE	1048	1000	942
TEMPERATURE MARGIN	208	160	102

Thus, the only remaining concern about impact temperature margin pertains to the orbit-decay case, with its coarser grain size and higher minimum allowable impact temperature. The reentry solution for that case is presented in figure 16A.

Fig. 16a-ORBIT-DECAY SOLUTION FOR GPHS DESIGN LMRE

For the predicted 1331°C operating temperature and 1426°C peak reentry temperature, figure 9 shows a minimum allowable impact temperature of 875°C based on the 10% strain criterion, and of 975°C based on the more conservative 20% strain goal. (In recent impact tests at LASL, the LMRE GPHS design exhibited strains of less than 10%.) Thus, as shown in table 8, the orbit decay case yields an impact temperature margin of 24°C versus the conservative goal, and 124°C versus the criterion. Neither of these values takes credit for the analytically predicted 50% reduction in air conduction caused by internal gaps after launch vibration. With such credit taken, the above margins increase by 29°C.

Table 8-ORBIT-DECAY IMPACT TEMPERATURE MARGIN, °C

NORMAL OPERATING TEMPERATURE	1331
PEAK REENTRY TEMPERATURE	1426
GRAINS PER 0.03" WALL	12
MINIMUM ALLOWABLE IMPACT TEMP.	
20% LOCAL-STRAIN GOAL	975
10% UNIFORM-STRAIN CRITERION	875
AXIAL GAPS (TOTAL)	0
AIR CONDUCTANCE	100%
IMPACT TEMPERATURE	999
IMPACT TEMPERATURE MARGIN:	
VERSUS GOAL	24
VERSUS CRITERION	124

The above results are for the single-axis tumbling reentry case, which is not as severe as the side-on stable and face-on stable cases. But GE reports that they have succeeded in eliminating the side-on case aerodynamically, by selectively beveling the edges of the aeroshell. And BCL's latest analyses indicate that the temperature margin for the face-on orbit-decay case is slightly higher than that for the single-axis tumbling case. Thus, the impact temperature margin for orbit decay, while not as high as that for prompt reentry, appears to be adequate.

MECHANICAL DESIGN AND TESTS

Based on the thermal studies described above, and following DOE adoption of the two design changes recommended by Fairchild, two basic designs were selected for more detailed investigation. Both designs used PuO_2 fuel pellets, iridium alloy capsules, and FWPF impact shells and aeroshells separated by thermal barriers. In design 2, shown in figure 17, the thermal barrier consisted of two 30-mil PG shells, and in design 5 it consisted of a single 80-mil layer of CBCF-3. Figure 17 does not show the stand-off rings which maintain the gaps between the various shells during reentry.

Fig 17-DESIGN #2

After careful studies taking account of manufacturing tolerances and thermal expansion effects, detailed drawings were prepared for both designs, and random vibration tests were carried out, to check the launch survivability of the two configurations. Preliminary one-directional tests at GE showed that the PG insulators survived all the tests, but that the CBCF insulators failed catastrophically.

The latter results were confirmed in more detailed vibration tests at Fairchild. These tests used a module in which the geometry and weight of the impact shell and fuel capsule were duplicated. The impact shells were made of magnesium, and the fuel capsules of lead-filled steel. In this and in all subsequent tests, large axial gaps (50 mils) and radial gaps (18 mils) separated these components, so that the simulated fuel capsules acted as internal hammers on the impact shells. The test results made it clear that CBCF could not survive as load-bearing members under these conditions.

Although the PG design appeared to satisfy both the thermal and the vibrational requirements, Fairchild was requested to continue investigation of the CBCF option because of its potential weight saving. The first design fix investigated was to use PG stand-offs in conjunction with CBCF insulators, as illustrated in figure 18. As shown, the CBCF surrounding each impact shell consisted of three parts: a cylindrical sleeve, and two end caps. At the center of each CBCF end cap was a PG button which acted as an axial stand-off between the impact shell and aeroshell. At each joint between the CBCF sleeves and end caps was a PG ring, which served to separate the impact shells from each other and from the aeroshell. With this arrangement, the CBCF parts need not bear any load except their own weight.

Fig 18-DESIGN WITH CBCF INSULATORS AND PG STAND-OFFS

The above design was fabricated and vibration-tested at Fairchild. The JPL-specified random vibration spectrum for the 3-minute qualification is shown in figure 19.

Fig 19-RANDOM VIBRATION TEST OF GPHS

The design successfully passed vibration tests in all three directions, at the flight level, the qualification level, and even at twice the qualification level. The CBCF and PG parts showed negligible dimensional and weight changes.

When two impact tests of the above design were carried out at LASL, both resulted in breach of the leading fuel capsule. Thus, it appeared that the CBCF design without PG stand-offs would survive impact but not launch, while the design with the PG stand-offs would survive launch but not impact. It was therefore concluded that, for a successful design, the CBCF insulator must be unloaded, but without the use of hard, projecting stand-offs which could adversely affect the impact survival of the fuel capsule. Such a design is shown below in figure 20.

Fig 20-GPHS DESIGN LMRD

There are no PG stand-offs in this design. The 45-degree beveled edges of the cylindrical impact shells mate with conical seats in the aeroshell, and the FWPF components act as their own stand-offs. The CBCF sleeves and disks bear no load except their own weight. The figure shows a conical Grafoil compliance pad between the impact shell and aeroshell, but this is optional and was later found to be unnecessary.

One feature of this and subsequent designs is their relative ease of assembly, particularly with respect to hot-cell operations. As illustrated in figure 21, the CBCF sleeve and one of the disks can be pre-assembled in the aeroshell, and bonded to it to minimize the number of loose parts during subsequent operations. Suitable high-temperature carbon cements which can be adequately outgassed are available for this purpose. Similarly, the other CBCF disk would be bonded to the aeroshell closure plug. Thus, final assembly of the fueled module by manipulators in the hot cell is simplified, since there are only three subassemblies: the insulated aeroshell, the fueled impact shell, and the insulated closure plug.

Fig 21-MODULE ASSEMBLY PROCEDURE

The revised design shown in figure 20 was vibration-tested at Fairchild. The tests used the same module as in previous tests, with lead-filled steel fuel capsules, magnesium impact shells, and aluminum aeroshell and closure plugs. As illustrated in figure 22, sections of the impact shells and aeroshell near their contact surfaces were cut away and replaced with FWPF graphite inserts. This made it possible to check the vibration survival of the FWPF components at their critical load points, and to experimentally study the effects of various interface configurations and assembly clearances.

Fig. 22-VIBRATION TEST ARRANGEMENT

Since the FWPF components are the load-bearing contacts in this design, and the CBCF insulators are structurally passive, the initial vibration tests could be run without the CBCF parts. It was even possible to run tests with a large hole in the aeroshell closure plug, and with an accelerometer mounted directly on the impact shell. With this arrangement, a sine sweep was used to search for resonances and to measure the load amplifications experienced by the impact shell. There were no sharp resonances, but rather a broad frequency range (10-100 Hz) in which amplification factors up to 3 were observed.

All the final tests, on this and subsequent designs, were made with a full complement of CBCF components. A large number of tests were run, to determine the effect of the vibration level, the conical contact width (110, 85, and 50 mils), and the axial gap at the conical interface between the impact shell and the aeroshell. This gap could be accurately measured by using the threaded closure plug as an effective depth gage. The plug was first snugged up to a fixed torque, and then backed out to whatever angle would give the desired initial clearance. The method was highly reproducible, and could be used after the test to measure any dimensional changes in the FWPF components.

The initial tests were made with no initial gaps between impact shell and aeroshell, since this minimizes hammering at the critical interfaces and is least likely to result in damage to the FWPF components. These tests were quite successful, with no significant CBCF degradation, dimensional change, or weight loss at the prescribed qualification level. However, various concerns were raised about the practicality of a snug design, in view of unavoidable dimensional tolerances, the difficulty of manipulator operations in a hot cell, and the need for a locking device on the closure plug to prevent its rotation during launch. For these reasons, it was decided to change to a design with deliberate initial gaps.

The internal gaps in the heat source module change during its history, because of differences in thermal growth of its components during various flight and reentry phases. In the present design, the thermal growth mismatch of the impact shell and aeroshell is quite small, because they are made of the same material, and because the FWPF graphite has an extremely small thermal expansion coefficient.

Typical variation of the gap between impact shell and aeroshell is depicted in figure 23. Here it is assumed that the as-fabricated components have an axial clearance of 1.4 mils at room temperature. The figure shows the subsequent clearances during fueling (1.6 mils), during launch in a xenon-filled RTG (1.0 mil), during normal operation in space (1.2 mils), and during min-γ and orbital decay reentries. During the reentry heat pulse, the aeroshell heats up faster than the impact shell, causing the gap to increase to 4.8 mils. After the heat pulse the aeroshell cools off faster than the impact shell, causing the gap to decrease. For

a short time, a small interference fit of 0.2 mil is developed. Note that even if we had chosen a snug design with zero gap at assembly, the maximum interference fit during reentry would have been less than 2 mils, which is acceptable.

Fig. 23-EFFECT OF THERMAL GROWTH ON GAPS

To avoid uncertainties about thermal contact conductance under interference-fit conditions, it was decided to make the launch gap large enough to prevent interference during all subsequent operations. Figure 23 indicates that a launch gap of only 1.3 mils would be sufficient to avoid subsequent interference. However, because of the previously mentioned concerns about the practicality of designing to such small gaps, we were requested to repeat the vibration tests with an initial gap of 4 mils.

In the course of carrying out these tests on a module with an 85-mil contact seat width, microscopic inspection of the burnish marks in the contact area revealed that most of the conical seat area had not really participated in carrying the vibration load. Only a narrow region, where the conical seat meets the top face of the impact shell, showed severe burnish marks. In this region, the initially sharp edge had been rounded off to a radius of about 85 mils, and it was this rounded edge which appeared to have carried most of the load.

In view of the above observations, the impact shell design was changed to that shown in figure 24. Note that the formerly beveled edges have been changed to rounded edges of 85-mil radius. Since sharp edges tend to get rounded during vibration, making them rounded to begin with should help to minimize dimensional changes during launch. An additional benefit of this design modification was the anticipated reduction of the thermal contact conductance between the impact shell and the conical seat of the aeroshell.

Fig. 24-DESIGN LMRE

To evaluate the above design, we machined a new set of graphite parts and subjected the module to a complete series of tri-axial dynamic tests. The flight-level and qual-level random vibration tests were carried out at Fairchild, and the transient tests were conducted at GE. The axial gap between impact shell and aeroshell, initially 4 mils as shown in figure 24, increased to 4.5 mils during the flight-level tests, and to 7 mils during the qual-level tests. The rounded edges of the impact shell showed

no discernible flattening after the flight-level tests, and conical flats about 5 mils wide after the qual-level tests. The dimensional and weight changes of the CBCF sleeve and disks, summarized in table 9, are seen to be small and well within acceptable limits. Thus, the design meets the launch vibration requirements.

Table 9-CHANGES IN CBCF PARTS DURING RANDOM AND TRANSIENT VIBRATION TESTS

Dimensions in inches, Weights in gm			
	BEFORE	AFTER	CHANGE
SLEEVE:			
Length	2.953	2.945	-0.3%
O.D.	1.717	1.716	-0.1%
Thickness	0.080	0.080	0.0%
Weight	4.148	4.095	-1.3%
TOP DISK:			
O.D.	1.373	1.369	-0.3%
Thickness	0.074	0.074	0.0%
Weight	0.3451	0.3564	+3.3%
BOTTOM DISK:			
O.D.	1.369	1.355	-1.0%
Thickness	0.073	0.074	+1.4%
Weight	0.3524	0.3390	-3.8%

As indicated earlier, the thermal contact conductance between the impact shell and aeroshell is an important parameter, since it influences the heating and cooling of the fuel capsule during reentry. However, even if the contact conductivity between two FWPF surfaces were well known, it could not be used to predict the contact conductance for the design shown in figure 24, because the contact area between the rounded impact shell edge and the conical aeroshell seat is undefined. Moreover, there are uncertainties about machining tolerances and achievable concentricity and parallelism for this configuration, and about the effect of prior launch vibration on surface texture and contact conductivity.

For these reasons, we decided to measure the steady-state contact conductance between FWPF parts having the actual contact geometry and surface texture of the module components after launch vibration. This could be done by utilizing the FWPF insert rings previously used in the module vibration tests (see figure 22).

Figure 25 shows our test set-up for these contact conductance tests. The previously vibrated FWPF rings are at the center of the figure. The aeroshell insert was in contact with an electrically heated metal sleeve, and the impact shell insert was in contact with a conduction-cooled cold sleeve of known heat conductivity. There were a number of thermocouples in the two graphite rings, to measure the interfacial temperature drop; and a number of thermocouples in the cold sleeve, from which the interfacial heat flow rate could be inferred. The entire assembly was insulated by thick CBCF blocks.

Fig. 25-CONTACT CONDUCTANCE TEST

The Fairchild tests were carried out over a range of nitrogen pressures and contact loads, at low temperatures (to 200°C). These measurements were extended to high temperatures (to 1000°C) by C. Alexander of BCL, using the same specimens and basic experimental arrangements as those used at Fairchild. After appropriate corrections for temperature, the two sets of

measurements were in reasonably good agreement. Alexander's results, at 800°C, are depicted in figure 26 for a range of nitrogen pressures from 0 to 1 atmosphere. As can be seen, to a good approximation the measured conductance is a linear function of the cube root of the nitrogen pressure.

Fig. 26-EFFECT OF GAS PRESSURE ON GIS-TO-AS CONDUCTANCE, ALEXANDER'S RESULTS AT 800°C, MINUS RADIATION

Based on the above results, and on others not presented here, the following correlation was derived for the impact shell-to-aeroshell conductance, as a function of instantaneous temperature T , pressure P , and g-load G . The factor of $1/6$ in the equation reflects the assumption that only 60° of the 360° circumference is in physical contact when the g-load is normal to the impact shell axis.

$$K_{C,T} \left(\frac{W}{°C} \right) = \frac{1}{6} \left[0.16 + 0.036 G \right] + 0.38 \left[\frac{K_{AIR}(T)}{K_{AIR}(800°C)} \right] \sqrt[3]{P(ATM)}$$

The above contact conductance correlation was used in the Fairchild thermal analysis, to derive the reentry results presented in the preceding section.

After the successful completion of the vibration tests of the CBCF-insulated module, two such modules were impact-tested at LASL. The tests used actual flight hardware materials, including FWPF and CBCF graphites, live PuO_2 fuel, and Iridium capsules which had been pretreated to induce the predicted grain size at impact. The specimens were impacted at a velocity of 58 m/sec (which is 10% above the predicted impact velocity), an impact temperature of 954°C, and at two different impact angles: on-axis, and 45 degrees off-axis. Previous tests had shown that those angles cause the most severe impact damage. In both tests the fuel capsule was unbreached, and suffered only modest deformation. Thus, the elimination of the PG stand-offs appears to have prevented the previously encountered impact test failures.

Following the successful completion of the impact tests, the Fairchild-generated GPHS design depicted in figure 24 was adopted by DOE for use in the ISPM RTGs. An isometric view of the module sliced through its midplane is shown in figure 27.

Fig. 27-GPHS DESIGN LMRE

One final design iteration investigated has the potential of achieving a significant additional weight reduction of the GPHS module. In previous designs, as illustrated in figure 17, it was assumed that the aeroshell cusps between the two insulated impact shells have been removed, but not the cusps at the four edges of the aeroshell. These represent a significant weight fraction of the module. If they are removed, the remaining aeroshell is essentially a hollow graphite box, with a threaded fueling port at each end face. This is illustrated by the cross-sectional view in figure 28, which also depicts one other weight reduction concept, truncation of the CBCF sleeves near their line of mutual tangency. The CBCF insulator in this region serves no useful thermal role, since the two impact shells are essentially at the same temperature.

Fig. 28-DESIGN LMRG

The two weight reduction concepts illustrated in figure 28 are made possible by the fact that in the Fairchild CBCF design the fueled impact shells are supported directly by the conical aeroshell seats, and not by the insulators as in the earlier PG designs. On the contrary, the independently mounted impact shells act as internal supports for the insulator sleeves. Since the CBCF insulators need not bear any load except their own weight, which is extremely low, the external line contacts shown in figure 28 should provide adequate launch support for them.

The weights and specific powers of the principal designs discussed in this paper are compared in table 10. The design labeled LMRC is the latest version of the PG-insulated option, and the one labeled LMRE represents the present GPHS design, selected for Galileo and ISPM. The designs designated LMRF and LMRG are the later weight-saving revisions. In both, the aeroshell corner cusps have been removed, leaving a rectangular instead of an oval cavity cross-section. In addition, design LMRG includes the truncated CBCF sleeves, as illustrated in figure 28.

Table 10-GPHS SPECIFIC POWER COMPARISON

DESIGN INSULATOR AEROSHELL CAVITY CBCF SLEEVES	LMRC PG oval full	LMRE CBCF oval full	LMRF CBCF rectangle full	LMRG CBCF rectangle shaved
WEIGHT (LB)				
FUEL	1.314	1.314	1.314	1.314
IRIDIUM	0.516	0.516	0.516	0.516
IMPACT SHELL	0.415	0.433	0.433	0.433
INSULATOR	0.192	0.026	0.026	0.024
AEROSHELL	0.953	0.885	0.758	0.717
MODULE	3.390	3.174	3.047	3.004
18 MODULES	61.0	57.1	54.8	54.1
SPECIFIC POWER (W/LB) 250 WATTS BOM	73.7	78.8	82.0	83.2
INCREASE PER CHANGE		5.1	3.2	1.2
TOTAL INCREASE	0	5.1	8.3	9.5

As shown in table 10, all the designs have the same fuel and clad weights. All assume the same impact shell wall thickness. The impact shell weight for the PG design is slightly lower than for the CBCF designs, because the former has more rounded corners. The insulator weights represent two 30-mil shells for the PG case, and a single 80-mil thickness for the CBCF designs. As expected, the CBCF insulator is significantly lighter.

In addition to the insulator weight difference, there are significant differences in the aeroshell weights. All aeroshells are assumed to have the same minimum wall thickness, which is dictated by the expected ablation during reentry. (Actually, the lighter CBCF designs should experience somewhat less ablation, but no credit was taken for this.) As shown in table 10, the

aeroshell weight is significantly lower in design LMRE than in design LMRC. It is lower still in design LMRF, due to the removal of the corner cusps; and lower yet in LMRG, due to the shrinkage of the aeroshell when the CBCF sleeves are truncated and the impact shells moved together.

Table 10 gives the module weights for the four designs, and also the weight of the 18-module stack used in each RTG. Finally, for a beginning-of-mission thermal power of 250 watts per module, it compares the specific powers of the four designs, and lists the increase for each design change. As can be seen, the biggest improvement occurs in going from the PG to the CBCF design. But the specific power improvement due to removal of the aeroshell corner cusps is also quite significant, and even that due to shaving the CBCF sleeves is not negligible.

The heat source constitutes about half the weight of the RTG; and the fuel, iridium, and impact shells contribute 66 to 75% of the heat source weight. Only the insulator and aeroshell weights differ significantly from design to design. The weight of these components is 35% lower in the lightest CBCF design than in the PG design.

The above weight comparison showed the desirability of qualifying the last two design revisions, particularly the removal of the aeroshell corner cusps. One concern about that design was the feasibility of fabricating the hollow-box aeroshell depicted in figure 28. The aeroshell cavity must be hogged out of a monolithic graphite block, and all the internal surfaces must be machined through a relatively small circular port in each of the two end faces.

To demonstrate the feasibility of this, a full-scale graphite aeroshell was fabricated at Fairchild. Most of the interior material was removed by a cylindrical end mill with a ground-down shaft. The remaining, inaccessible material at the edges and corners of the aeroshell cavity was removed by means of a small ball mill, driven through a flexible shaft with a right-angle end piece, similar to a dental drill. A numerically controlled milling machine was used for moving the graphite block. No difficulty was encountered, and this fabrication method appears quite feasible.

The feasibility of milling CBCF sleeves to produce the truncated shapes shown in figure 28 has also been demonstrated at Fairchild.

To qualify designs LMRF and LMRG for flight, they would have to pass the same vibration and impact tests as LMRE, in addition to the continuing design verification tests planned for the near future. The author feels quite confident that at least the LMRF design would pass those tests, but at this time it is not certain whether this could be demonstrated in time for the Galileo and solar-polar missions.

SUMMARY

The fuel capsule in the GPHS REF design in July 1979 was predicted to have too high an operating temperature, too high a reentry temperature, and too low an impact temperature. A GE-led task force formed by DOE produced improved data on physical properties of the materials of interest, and improved thermal and aerodynamic heating models. Simultaneous design studies at Fairchild revealed that:

- The capsule's operating temperature and peak reentry temperature can be significantly reduced by moving the pyrographite insulators from the inside to the outside of the impact shell.
- Its impact temperature can be substantially raised by increasing the gaps between module components from 3-4 mils to 10-15 mils.
- The module weight can be appreciably reduced by using CBCF instead of PG insulators.
- When appropriately designed, the CBCF-insulated module can meet the thermal, vibration, and impact survival goals.

Most of the design changes recommended by Fairchild (those in Design LMRE) have been adopted for use in the ISPM and Galileo RTGs; the remainder may still be adopted, if they can be qualified in time. The lightest CBCF design has a specific power of 83.2 watts/lb, which is 50% higher than that of the MHW heat source used in previous space missions.

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