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**MULTI-HUNDRED WATT
RADIOISOTOPE THERMOELECTRIC
GENERATOR PROGRAM**

Part II

Annual Report
July 1970 - June 1971

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DATE 10/27/72

NUCLEAR SYSTEMS PROGRAMS

SPACE DIVISION

ISOTOPE POWER SYSTEMS OPERATION

P. O. Box 8661 • Philadelphia, Penna. 19101

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**MULTI-HUNDRED WATT
RADIOISOTOPE THERMOELECTRIC
GENERATOR PROGRAM**

Part II

Contract No. AT29-2-2831

Annual Report

July 1970 - June 1971

prepared for:

**US Atomic Energy Commission
Albuquerque Operations Office**

by:

**NUCLEAR SYSTEMS PROGRAMS
SPACE DIVISION**

ISOTOPE POWER SYSTEMS OPERATION

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SECTION 4

HEAT SOURCE DESIGN AND ANALYSIS

4.1 INTRODUCTION

During the MHW Base Program period, the heat source effort was directed toward extending the designs and analyses, initiated during Phase I. These analyses and tests are necessary to develop, fabricate and qualify a Helipak heat source capable of meeting the composite design requirements placed upon it by nuclear safety and the RTG power and integration requirements. This effort included: the preparation of conceptual heat source designs using SS, PMC, and PPO fuel forms; analyses of these various designs to determine their operational temperatures, reentry temperatures, impact velocity, and post-impact condition and performance; design and conduct of tests in the areas of impact, non-fuel material compatibility, oxidation, and emissivity; support of tests conducted by Mound Laboratories, the AEC fueling agency and LASL, the fuel development laboratory; design drawings and specifications.

In addition to the Helipak design evaluation, three other types of heat source concepts were evaluated. These included:

1. GNOME - A GE developed concept using SSC pucks with a noble metal fuel liner and a graphite strength member.
2. Individually protected SSC pucks integrated into various heat source configurations.
3. Transit/Pioneer/Isotope Brayton type heat source for MHW application, using both the current complement of materials, as well as various material substitutions to improve performance.

4.1.1 HEAT SOURCE DESIGN REQUIREMENTS

The design requirements for the MHW heat source were derived primarily from operational and safety considerations. These requirements are defined in Reference 4-1 and the safety aspects are further discussed in Section 2. The following MHW-RTG program guidelines were used in the development of the heat source.

4.1.1.1 Physical Constraints

Physical constraints are as follows:

1. Fuel Form: Fuel Spheres of $^{238}\text{PuO}_2$ in the form of SS, PMC, and PPO.
2. Helipak type design.
3. SiGe thermoelectric system with a sink temperature of 1000°C and 1100°C .

4.1.1.2 Key Nuclear Safety Constraints

Immobilization of the fuel is required under the following conditions:

1. During normal mission phases, including storage, test, prelaunch, launch, and operational mission.
2. During and for a period of at least one month after launch pad aborts.
3. During suborbital, orbital and superorbital reentries.
4. At impact; at a velocity 10 percent greater than, sea level, terminal velocity, following a reentry.
5. After impact; for a period of at least one year.

The Helipak concept was selected for detailed evaluation during the Base Program due to the superior impact and safety aspects associated with the individually protected (impact/post-impact) spheres.

4.1.2 FUEL FORM DEVELOPMENT, SELECTION AND CRITERIA

The selection of the fuel form for the MHW application has occupied a considerable portion of the effort by General Electric, Mound Lab - Monsanto Research Corp., and Los Alamos Scientific Laboratory - University of California during the Base Program. This section deals with the status of fuel forms, the selection criteria for MHW, the fuel characterization program, the fuel form development program, and associated problem areas.

4.1.2.1 The Criteria for Fuel Form Selection

The selection of a fuel form for the MHW-RTG program is constrained by the need to satisfy requirements for operational performance and to protect the general populace from accidents which might endanger their health or safety, either directly or indirectly.

4.1.2.2 Operational Requirements

The selection of a fuel form depends on at least the following factors:

1. Thermal Capability	3. Chemical Stability
2. Mechanical Capability	4. Fabrication Technology Status

4.1.2.2.1 Thermal Capability

Thermal capability implies a melting temperature high enough to meet the RTG operational temperature requirements of approximately 1400 C at the fuel liner interface based on a temperature of 1000⁰ C at the hot junction of the thermopile. The melting temperatures must be high enough for the material to remain solid, with reasonable thermal protection under a reentry environment. In addition, the vapor pressure must be low enough to exclude significant redistribution of the fuel. Thermal capability also implies high power density to minimize capsule size. In addition, the thermal expansion coefficient and conductivity of the fuel must be defined over the temperature spectrum encountered during operational and abort modes to enable determination of temperatures and dimensional changes.

4.1.2.2.2 Mechanical Stability

Mechanical stability implies that the material resists changes in dimensions during all phases of the mission and fabrication cycle such that the encapsulating structure and the re-entry protection can satisfactorily be designed around it.

4.1.2.2.3 Chemical Stability

The chemical stability of the fuel form is principally concerned with the compatibility of the fuel with the encapsulating materials with particular emphasis on changes which might alter initial compatibility, whether these arise from radioactive processes, exposure to air, water, hard vacuum or pyrolysis. In addition, the fuel must be resistant to changes that might be induced by the normal environment, i. e., exposure to air, fresh and salt water, and to various kinds of soils.

4.1.2.2.4 Fabrication Technology Status

Fabrication Technology status implies that the methods for fabrication of the fuel in a desired physical form have developed to a point where they are well understood and can be reduced to practice for production.

4.1.2.3 Safety Requirements

The fuel form should in addition to meeting operational requirements, be capable of surviving sequential abort conditions without contamination of the environment through exposure of the fuel. In addition to the obvious requirements for surviving the handling operations through encapsulation, within the converter it must sustain launch environments and the exposure to the space environment over periods of up to twelve years.

In the event of an abort, the fuel is to be provided with suitable protection against the high temperatures occurring during reentry and with an impact shell to protect against forces induced on it by impacting against unyielding surfaces, at terminal velocity and with a "containment" shell which along with inherent fuel capability provides for fuel immobilization/containment. In addition, with the protection provided it must be able to sustain the effects of blast, fragmentation, fireball, and impact associated with a launch vehicle failure. Regardless of the type abort, the fuel must be immobilized after impact (i. e., it must not release to the environment any quantity of radioactive material in excess of acceptable levels). Since the hazard from a radioactive source is two-fold (i. e., direct radiation and inhalation or ingestion of free radioactive material) the fuel form must also have the minimum emission of ionizing radiation.

4.1.2.4 Status of Fuel Forms

At the initiation of the Base Program four fuel forms were receiving active consideration for application in the MHW-RTG program. These fuel forms were: (1) the solid solution (SS) a solid ceramic of plutonia and thoria in various percentages; (2) a solid solution cermet (SSC), which was a solid solution of plutonia plus 10% thoria (PuO_2 -10% ThO_2) with a nominal 14 v/o of molybdenum as a metal matrix, (3) a plutonia molybdenum cermet (PMC), consisting of a ceramic phase, plutonium dioxide, and a metal matrix phase, nominally 18 v/o molybdenum (this corresponds closely to the cermet developed by Battelle Memorial Institute, Columbus Laboratories under their fuel development contract) and (4) pure plutonium dioxide solid ceramic compacts (PPO), which consists of precisely that material, namely a solid ceramic body of definite physical geometry and the nominal composition PuO_2 . A summary of the expected properties of these materials is shown in Table 4.1.2-1.

4.1.2.4.1 Solid Solution Fuel Form (SS)

Although the decision to proceed with a solid solution cermet fuel form was made in the fall of 1969, work on the solid solution fuel form continued at LASL, primarily in the area of compatibility studies. Consideration of this fuel continued at a relatively low level through June 1970. The assumed advantages of the SS fuel form are high temperature stability and reduced sensitivity to changes in the O/Pu ratio. The disadvantages of this fuel form are reduced power density, corresponding directly to the percentage of diluent, reduced thermal conductivity, resulting from the introduction of a foreign atom into the plutonium dioxide lattice, and a number of questions regarding the thermal/mechanical properties of this material.

When the percentage of diluent required to provide the advantages discussed above were estimated to be in the 30 to 40% range, consideration of this fuel form was reduced to low level.

4.1.2.4.2 Solid Solution Cermet (SSC)

The solid solution cermet, consisting of the plutonia-10% thoria ceramic phase in a matrix of 14 v/o plutonium molybdenum metal, was the primary candidate material at the beginning

Table 4.1.2-1. Summary of Material Composition

Material/Composition	Theoretical Crystalline Density (g-cm ⁻³)	Effective Density (g-cm ⁻³)	Specific Thermal Power (w-g ⁻¹)	Effective Power Density (w-cm ⁻³)	Neutron Production (α, n) (n-sec ⁻¹ -g ⁻¹)	Thermal Conductivity (cal-sec ⁻¹ -cm ⁻¹ -°K ⁻¹)	Melting Temperature ($\pm 50^\circ\text{C}$)	
1. Ceramic Solid Solution 90 m/o PuO ₂ + 10 m/o ThO ₂ (SS)	11.3	10.15 (90% TD)	0.36	3.64	1.2×10^4 < 6×10^3 (1)	≤ 0.006	2400 (2400) (3300)	4352 (4352) (5970)
2. Solid Solution Cermet 90 PuO ₂ + 10 ThO ₂ in 14 v/o Mo	11.1	10.44 (93% TD)	0.306	3.2	3×10^4 (2)	≤ 0.020	2400 (2400) (2610)	4352 (4352) (4730)
3. Plutonia Molybdenum Cermet Pure PuO ₂ + 18 v/o Mo (PMC)	11.2	10.5 (93% TD)	0.337	3.5	3×10^4 (2)	≤ 0.038	2400 (2610)	4352 (4730)
4. Pure Plutonium Dioxide PuO ₂ (PPO)	11.46	9.7 - 10.3 (85-90% TD)	0.400 ± 0.004	3.8 - 4.1	1.2×10^4 < 6×10^3 (1)	≤ 0.0065	2400	4352
NOTES (1) With depletion of ¹⁷ O and ¹⁸ O (2) Made with MoF ₆								

of this reporting period (Ref. 4-1). A considerable amount of work had been done on other programs both in the fabrication of cermet fuel discs and the production of a large quantity of cermet simulant, using pure thorium as the ceramic phase. The usefulness of the cermet structure rests primarily on the improved thermal conductivity of the resulting material and the effect that this has on reducing the center line operational temperatures. In addition, it was postulated that the metallic phase would act as an adhesive so that during impact, the quantity of fines produced would be less than that of a pure ceramic. Among the negative considerations, were the fabrication difficulties in making this material in unflawed discs of the prescribed dimensions. In addition, the problem of oxidation of the molybdenum metal matrix had not been fully explored. Tests on non-fuel cermet materials conducted by GE indicated that catastrophic oxidation of a fuel compact with the nominal 14 v/o of moly could occur at temperatures as low as 600°C, (1112°F).

The addition of approximately 10 w/o of thorium in the ceramic phase does not appear to contribute a significant improvement in the melting point of the resulting solid solution. (References LA70a and LA70b)

4.1.2.4.3 Plutonium Molybdenum Cermet (PMC)

The difficulties encountered in the fabrication of the SSC fuel form were recognized by the laboratories involved. A decision was made by the AEC to drop SSC in favor of a slightly different approach for a fuel in the disc shape specifically aimed at eliminating fabrication problems for the TRANSIT and PIONEER application. This modification consists of a metal phase of a nominal 18 v/o with no Mo overcoat on the individual disc shape and a substrate of pure plutonium oxide particles. Production of this fuel was initiated around the middle of 1970, and was designated as one of the principal forms for the MHW-RTG program (Reference LA70c). The problems affecting the PMC are the same as for the SSC, discussed above. There is a slight improvement in the effective power density because the overall percentage of diluent is reduced from ~ 25% to ~ 20%, however, the principal problem with this material is its susceptibility to oxidation when exposed to air at temperatures above 400°C in a post-impact situation. The advantage of this material is the improvement in thermal conductivity. In addition, because of the process used for applying the Mo coating to individual ceramic

pieces, it has not yet been possible to reduce the neutron production of this fuel below $\sim 3 \times 10^4$ neutrons per second per gram of included ^{238}Pu . The coating process involves the thermal decomposition of molybdenum hexafluoride on the surface of the individual ceramic particles. Some residual fluorine appears to remain even in the most careful production process. This results in an excessive production of neutrons by the (α, n) (alpha-neutron) interaction on fluorine. The current specification for the PMC disc material permits a maximum neutron flux of 3×10^4 neutron per second per gram of plutonium (Reference ML70a). In oxide material in which the less common isotopes oxygen-17 and oxygen-18 have been reduced by a factor of 10 or more, it is inferred that all of the neutron production above the level produced by spontaneous fission results from the presence of residual fluorine in the finished compact. A process using molybdenum pentachloride is under development and promises to eliminate the production of neutrons from this source.

The cost of forming individual compacts is high, since the dies used for hot pressing of these materials must be sacrificed in the formation of one or two discs. This results from the requirement for high pressures, in the neighborhood of 10,000 psi, at temperatures in excess of 1200°C , in order to produce compaction of the high fired particles with their Mo coating.

4.1.2.4.4 Pure Plutonium Dioxide (PPO)

The fourth candidate material for the MHW fuel form is pure plutonium dioxide compacted, without the addition of diluents, into the geometrical shape of a sphere. The pure plutonium dioxide results in a high power density fuel. If the plutonium dioxide is already in a fully oxidized condition further oxidation cannot take place in air even at very high temperatures; consequently, it is not subject to the oxidation problem of the cermet, whose metal matrix is oxidizable molybdenum (Reference BR70a).

Like all ceramics, PPO is subject to brittle fracture and to fracture by thermal shock. Its thermal conductivity is relatively poor, being approximately 1/3 that found in the cermet

formulations. This is an additional disadvantage since it creates a higher differential temperature across the fuel body.

Pure plutonium dioxide compacts are fabricated directly from "as received" production grade plutonium dioxide powder without need for intermediate chemical processing. This is ball-milled, if required, to achieve the desired particle size. In processing, at LASL, rejected material or broken material has been recycled using a dry process. This is in contrast to the cermet fuel at Mound Laboratory where rejected ceramic materials are dissolved, reprecipitated, calcined, and fired for use. The actual cost of production, for the same geometrical size and shape, of a finished fuel body will involve fewer operations than for either of the two cermet materials previously discussed. A substantial amount of data is available for pure plutonium dioxide, since it has been used either by itself or a constituent in mixed oxide reactor fuels over a period of at least 15 years. Despite the existence of data, on PPO, the application of this material in the MHW program involves temperatures in excess of temperatures used in many of the previous data investigations. This is exemplified by the lack of information regarding thermal expansion coefficient above 1000°C and the lack of thermal conductivity data above 1300°C.

4.1.2.5 Fuel Evaluation

The four potential fuel forms discussed previously as candidates for the MHW-RTG program fuel form have been narrowed down to two, the plutonia molybdenum cermet (PMC) and pure plutonium dioxide (PPO). On the basis of the technical data available neither fuel form was considered to have a decisive advantage. Consequently, it was necessary to defer final selection until additional data are available. A program was defined and initiated which will provide the data required. The program covers fabricability, operational and safety performance. The applicational constraints were further defined so that the specific requirements for the fuel forms could be more clearly identified. In the MHW Phase I Final Report (Ref. 4-1), a detailed discussion of the two fuel forms then under consideration was presented. These were the solid solution cermet fuel (SSC) and the pure plutonium oxide (PPO), as well as a solid ceramic consisting of a solid solution of plutonia and thoria. The characteristics of these materials are shown in Table 4.1.2-1, and are briefly discussed in the succeeding section.

4.1.2.6 Fuel Form Development

The fuel form development effort falls into two general categories: 1) The definition of fuel properties where data are lacking and 2) the development of fabrication techniques to produce a fuel in the physical form and size necessary to meet MHW systems requirements.

In the MHW Phase I Final Report (Reference 4-1) fuel form behavior and potential problem areas were discussed for both the SSC and ceramic fuel forms. It is from this base that the work during the past year has expanded and for continuity, the problem area discussed are repeated herein.

4.1.2.6.1 Fuel Characterization

Characterization of the fuel forms was required in the following general areas:

1. Oxidation
2. Dimensional change in response to changes in the O/Pu ratio
3. Mobility/stability of the plutonia material under conditions of operational temperature and high vacuum or reduced helium pressure
4. Dynamic performance/mechanical stability under vibration and impact conditions
5. Radiation damage, which includes helium release, decay product in-growth, and the effects of self-radiation on the fuel form.
6. Response to re-entry environments and fire-ball conditions when exposed without protection
7. Radiation properties, including the neutron and gamma fluxes and the neutron and gamma spectra
8. Basic fuel properties: thermal, mechanical, and physical
9. Compatibility with candidate encapsulating materials and with other components of the heat source

A program for the characterization of both PMC and PPO fuels where data were lacking was undertaken. An outline of the program is shown in Table 4.1.2-2. The status of these programs are presented below.

Table 4.1.2-2. Fuels Characterization Program

Test	Fuel	Test Conditions	Data
1. Oxidation (E)	a. PMC b. (PMC shards) c. SCS d. PMC pellet in Ir e. Sphere, aged	1000°C 850°C 850°C	1. Rates 2. Fines 3. Residue characteristics 4. Water solubility 5. Pu in gas
2. Effect of O/Pu Change (E)	a. PPO Hi b. PPO Lo		1. Dimension and weight changes 2. Microstructure changes
3. Mobility/Stability a. in Helium (E)	Spheres in Ir PPO PMC	1370°C	1. Fuel redistribution 2. Changes in properties
4. Dynamic/Impact a. Vibration (E) b. Impact: Comminution	PPO Hi, PPO Lo, PMC Aged, Unaged	1000°C 1000°C At 100, 300, ft/sec	Fines, break-up Fines
5. Radiation Damage Swelling, Cracking Helium Inventory (Item 4-CMB1782) (E, S)	PPO Hi PPO Lo PMC	To 1800°C	1. Helium release 2. Dimension and weight changes 3. Microstructure changes
6. Ablation/Vaporization Re-entry (S)	LASL investigating		
7. Radiation (S)	Selected fuel sphere	Ambient	Radiation levels
8. Fuel Properties a. Density (E) b. Compressive & Tensile Strength & Modulus (E) c. Conductivity (E) d. Specific Heat (E) e. Melt Point (E) f. Thermal Expansion (E) g. Emittance (E) h. Temperature Ramping i. Solubility j. Vapor Pressure	PPO, PMC Selected fuel PPO Calculated Selected fuel PPO, PMC Selected fuel Selected fuel sphere PPO Selected fuel	Ambient 1500°C 1800°C 2200°C Up to 2000°C -- -- Up to 2000°C Up to 2200°C Re-entry temp ramp -- 2800°C	1. Strength 2. Modulus 3. Elongation Conductivity Vs temp Specific heat Vs temp Melt point Thermal expansion vs temp Surface emittance vs. temp 1. Physical characteristics 2. Fines Solubility rates LASL will calculate

4.1.2.6.1.1 Behavior of Fuel Forms in Air

PMC - LASL conducted a number of tests on PMC fuel material, using fractured pieces of production fuel (References Ba71a and Ba71e). Tests were conducted at 1100°C and at 625°C. All of the data on these tests is not yet available. Insofar as the information has been reported, it confirms previous tests on both simulant material and on fuel, indicating extensive interaction between the fuel and the molybdenum oxide. In both cases at, 1100°C and 625°C, activity was found on the filters down-stream of the test specimens. Whether this activity was carried as particulates or as a part of a compound with the molybdenum oxides is not as yet known, but sufficient material for detection has been reported. The size of the particles in the oxidized fuel itself formed as a result of the interaction between the molybdenum oxide and the plutonium dioxide has not been reported to date.

PPO - Although it is anticipated that no serious reaction will occur when pure plutonium oxide is exposed to the air even at high temperature, it is necessary to verify the behavior of this fuel form, especially if it is used in a substoichiometric condition. If the O/Pu ratio is reduced below ~ 1.98, changes in the size of the lattice can occur. An additional effect, reoxidation of the material, could occur spontaneously as long as the temperature exceeds 100°C, possibly forming fines or resulting in microscopic changes in a fuel compact. Tests to determine this behavior are under way.

4.1.2.6.1.2 Effects of Changes in the O-Pu Ratio. It has been reported (Reference Ai68c) that a volumetric change on the order of approximately 2% in linear expansion is experienced for each 0.1 decrease of the O/Pu ratio below 2.00. Preliminary calculations carried out independently by both General Electric and LASL (References Kn71a and Mu71) indicate that the loss of oxygen under operational conditions will be very slow. It is necessary however, to define the extent of the change which might be experienced should the fuel become substoichiometric during prolonged exposure to the space environment. Test specimens to demonstrate these effects were fabricated and placed on test, but the tests have not yet been completed. This test, although particularly important for the PPO solid ceramic compact may also be of concern where PMC is involved, since the loss of oxygen from the individual coated particles could permit swelling of these particles and of the fuel compact to occur.

4.1.2.6.1.3 Mobility/Stability. The vapor pressure of plutonium dioxide has been the subject of extensive investigation by researchers in a number of different locations. Ackermann and Battles at Argonne National Laboratory (References Ac66a, Ba69b, Mu60a, and Ai68a), Mulford and Leary at Los Alamos (Reference Mu60a), and Aiken (Reference Ai68a), at GE-Vallecitos have all concerned themselves with the vapor pressure developed by plutonium dioxide at various temperatures under a variety of conditions. Pardue et al, (Reference Pa64a), of Battelle have examined the behavior in neutral and reducing gases. As a result, a large body of experimental data exists permitting calculation of the behavior of plutonium oxide at various temperatures. Given certain assumptions regarding the initial O/Pu ratio, the temperature /time relationship and orifice size, the amount of oxygen and plutonium bearing species released can be determined. As noted above, studies were conducted, by both General Electric Company (Reference Kn71a)¹ and LASL (Reference MU71a) indicating that the changes in oxygen to plutonium ratio, at MHW operational temperatures, or slightly above, will occur at an extremely slow rate if the vent hole in the post-impact shell surrounding the fuel sphere is small enough (References Mu71a, Kn71a). It is estimated that the time for the oxygen to plutonium ratio to change from 1.98 to 1.96 at 1500^oC with a hole diameter of 0.002 inch, will be approximately 2.3×10^4 years. A similar calculation by Mulford (LASL) for the escape of plutonium bearing species, assuming an oxygen to plutonium ratio near the point of congruency for an operational temperature nominally 1500^oC, indicates extremely small losses, such that saturation of the graphite impact shell in a cone shaped area adjacent to the vent holes will not occur in less than about 1.9×10^4 years (Reference Mu71a). The quantity of plutonium involved amounts to about 5 microcuries per sphere. If a helium pressure of approximately 0.1 atmosphere, ~76 torr, is maintained within the heat source assembly, these times are increased and the rate of loss is further reduced.

Two mechanisms for the release of plutonium bearing species through vent holes, under operational conditions have been identified which are subject to analytical treatment. These are: 1) the loss of plutonium bearing species in the vapor phase and 2) the loss of plutonium bearing material by the alpha knock-on/vaporization phenomenon. Analytical results indicate extremely small emission of plutonium bearing material. However, a third possibility for

fuel release through the vent hole exists. This is the release of fine particulates generated by the break-up of the sphere or by mechanical abrasion from its surface from either vibration during normal handling or launch, by impact, or by thermal shock.

It is not possible at this time to assess analytically either the quantity of fines material thus produced, (by fines material is meant here those particulates which are 3 micrometers or less in average diameter), or to determine whether any of these events will in fact occur. A test is planned to demonstrate the effectiveness of the small vent hole, with its barrier, to prevent the release of plutonium bearing material.

4.1.2.6.1.4 Dynamic Stability

Vibration. Although the problem of fuel comminution as a result of vibration in handling and launch is recognized, there are currently no available facilities for experimentally demonstrating fuel behavior under these conditions.

Impact Tests. A series of impact tests using small pellets of both aged and unaged PMC and PPO fuels were carried out during the past year by LASL (LASL Monthly Reports), using the Sandia impact apparatus. These tests were limited to velocities of 325 feet per second and temperatures of approximately 450°C ($\sim 850^{\circ}\text{F}$). The results of these tests indicate that there is some difference in the quantity of fines production between PMC and PPO fuel materials, but that the PMC material behaves differently when aged than when it is freshly made. There is a substantial increase in the fines production reported for the aged PMC material. However, even the aged PMC material produces about half of the percentage of fines in the region below 10 microns as is produced in similar conditions with PPO. There is however, no apparent difference between fresh and aged PPO. The data are summarized in Table 4.1.2-3. It is not appropriate to make final determinations at this time because the aging period is not representative of the expected real situation and the impact temperatures are lower than those anticipated at impact for the MHW system. It has been analytically determined that the impact temperature for MHW will be between 1300 and 1400°C (2370 to 2550°F). The behavior of either fuel at these temperatures must be determined.

Table 4.1.2-3. Fines Production in Fresh and Aged* PMC and PPO Fuel

Sample	Weight Fraction < 10 μ m
PMC -- Fresh	0.01000
-- Aged	0.02704
PPO -- Fresh	0.06842
-- Aged	0.05416
*75 days at 1370 ^o C	
Impact velocity: 325 fps. Impact temperature: 454 ^o C (850 ^o F).	
Sample size: ~ 1/2 in. diam x 1/4 in. thick	

The new impact apparatus designed by LASL will be available for conducting impact tests in latter part of 1971. This apparatus will permit the testing of pieces up to and including a full sized FSA at the expected terminal velocity of ~ 350 ft per second with the sphere at a temperature in excess of 1370^oC, (2500^oF).

Section 4.2.1.3.2.4 of this report presents a detailed description of the impact tests conducted at a temperature of 1370^oC, using thoria simulant for the PPO fuel spheres. No impacts of full size fuel spheres were made.

4.1.2.6.1.5 Radiation Damage Studies. Radiation damage studies will include helium release/retention, decay products ingrowth, and the effects of neutron-alpha bombardment on structures. LASL has prepared a number of specimens of both PMC and PPO to determine these effects both with aging at the operational temperatures and at nominal room temperature plus an excursion to operational temperature which simulates behavior as a result of possible storage at lower temperature. The results of these tests as far as the effects of helium release/retention are concerned, will become available in August 1971. The test specimens were removed for examination at the end of June but data have not yet been reported on the results. The release of helium at the nominal operational temperature is of direct practical importance in the design of the FSA. The size of the vent hole must be such

that it will release all of the helium at any time during the life of the FSA. If the helium release rate from a full sized fuel sphere closely approximates the generation rate at all times, then no inventory or pressure will build up. If, however, the release rate from the compact is less than the generation rate, a gradual inventory accumulation will occur which could be released quickly during a re-entry heat pulse. It would be anticipated that all of the helium inventory would be released within a very short time, i. e., the order of 60 - 90 seconds, in which case, depending upon the quantity involved and the size of the vent hole, damage to the post-impact shell or the impact shell may occur.

The examination of these test specimens should indicate whether the helium release occurs as a simple diffusion process or whether it is accompanied by bubble formation and microscopic changes in the fuel compact. There have been some indications from work reported by D. W. Douglas Laboratories (Reference An70a) that swelling of PMC and retention of appreciable percentages of the generated helium have occurred in test specimens exposed for a 1000 hours at temperatures as high as 1800°C. In addition, specimens exposed at 2000°C, although releasing all their helium, exhibited catastrophic swelling in the process. For these reasons the results of the helium retention test on the structure of both PPO and PMC are important to the MHW design.

It is not presently known what types of data, if any, can be obtained on the effects of the neutron and alpha particles on the structure of either PMC or PPO. In the pure ceramic, previous work on UO₂ systems has indicated that at temperatures of 1400°C annealing of damage takes place. However, there is sufficient uncertainty regarding long-term behavior to require verification of this effect for PPO. For cermet materials, the embrittlement of refractory metals by alpha particle bombardment and by neutron irradiation has been demonstrated by a number of researchers (Reference Wo68a and Bi66a); whether this effect can be neutralized by the annealing process expected to occur for the ceramic is not presently known.

4.1.2.6.1.6 Response to Thermal Ramps. The response of the fuel spheres to thermal ramps, anticipated in the course of re-entry or accidental exposure in a launch pad explosion/fire, required definition. Since it is not anticipated that a bare FSA will ever be exposed to the re-entry heat pulse, the temperature distribution in the heat source has been analytically determined for certain worse case conditions. To determine the behavior of the sphere under these conditions, a series of temperature ramping tests are planned.

While it is evident that isotropic temperature ramping can be applied to a fuel sphere by inserting it into a furnace, this does not simulate the real environment. A temperature ramp test which will provide the asymmetrical heating necessary to simulate the anticipated thermal input to the sphere during a re-entry pulse is required. It is expected that test apparatus for performing the temperature ramping will be available and the testing completed by the end of 1971.

4.1.2.6.1.7 Radiation Properties. The measurement of radiation from both the PPO and the PMC fuels is carried out routinely, as fabricated. Although difficulties have not been anticipated with the PPO fuel with respect to a high level of light element impurities, which are reflected in the production of a high neutron count from the alpha-n reaction, the behavior of PMC may be of particular concern. The neutron production rate is currently specified to be not more than 3×10^4 neutrons per second per gram of included plutonium-238. This is a high value, particularly since the material involved has had substantially all of the oxygen-17 and oxygen-18 removed, prior to being fabricated into the cermet form. The resulting neutron production therefore can be attributed to the presence of residual fluorine in the PMC fuel. For PPO, it should be possible to hold the neutron production level to less than 5×10^3 neutrons per second per gram of included ^{238}Pu . The result of measurements taken during fuel fabrication runs will be closely monitored to assure the feasibility of meeting limits specified by users with production fuel.

4.1.2.6.1.8 Basic Fuel Properties

Density

The density of both PPO and PMC material is measured on individual pieces as a part of the fuel fabrication development which is discussed in some detail later in this report. The final fuel form selected will have the density specified and measured, as a routine matter.

Although an increase in the density of the fuel material produces a corresponding increase in the power density, the maximum density obtainable with current technology is not necessarily optimum for this application. There are two reasons that optimization is not possible for MHW; the first is that in the case of PPO at least, there appears to be a greater susceptibility to fracture through thermal stress as higher densities are achieved; of more direct significance to the operational considerations, however, is the supposition that at densities in excess of about 93% of theoretical, the structures no longer contain connected porosity (i. e., since helium is constantly evolved as the alpha particles are neutralized, the potential escape paths are effectively closed by this lack of porosity). It has been found with other ceramic materials, that some amount of connected porosity is desirable to permit the free passage of fission gases, in the case of reactor fuels, and helium, in the case of plutonium-238 oxide fuel. For this reason, it is anticipated that for PPO, the density will be maintained below 93% TD as recommended by LASL.

In the case of PMC where the individual ceramic particles are encased in an initially impermeable metal coating, the question of providing connected porosity within a compact made

from these particles appears to be unanswered. If the helium cannot escape satisfactorily from the individual particle through the molybdenum coating, the addition of more porosity in the compact does not appear to offer a solution. Further investigation into these effects is required for design application.

Thermal Conductivity

1. PMC - The thermal conductivity of PMC has been measured, for certain formulations, to around 1500°C. Measurements at higher temperatures cannot be effected at LASL until additional equipment is installed. It is anticipated that the thermal conductivity measurements between 1500°C and 2200°C will be made during the next program phase.
2. PPO - The thermal conductivity of pure plutonium dioxide has been reported by Battelle Memorial Institute, Columbus Laboratories, Reference La68a, to approximately 1300°C. Measurements above this temperature will be made when LASL installs the additional high temperature equipment required.

Thermal Expansion Coefficient

1. PMC - The thermal expansion coefficient of PMC has been measured up to 1500°C (Reference Ba70b and Ba70c). The sonic modulus apparatus required to make determinations at higher temperatures will be available during the latter half of fiscal year 1972, when this measurement will be made.
2. PPO - The thermal expansion coefficient of plutonium dioxide both in its exact stoichiometry and in the substoichiometric condition, from 1.98 to ≈ 1.61 , in the O/Pu ratio, have been reported for ^{239}Pu up to 1000°C (Reference Br60a). This level is insufficient for the MHW application; it is important that the thermal expansion coefficient behavior be obtained at least through the MHW operational temperature range. It is anticipated that LASL will perform dilatometric measurements on PPO of exact stoichiometry and of some substoichiometric formulations. Because of the difficulty involved in making specimens for this test, it has been proposed that $^{239}\text{PuO}_2$ be used for these measurements.

Melting Point

The melting point of plutonium dioxide is nominally 2400°C (4352°F) when the dioxide is in an oxidizing environment. It is anticipated that the melting point of actual fuel materials may be somewhat less than the theoretical value. The melting point of molybdenum is $\approx 2610^{\circ}\text{C}$ (4732°F) and the melting point of iridium is $\approx 2450^{\circ}\text{C}$, (4442°F). Consequently, the other components in the FSA have melting points which are nominally in excess of that for the fuel. The inclusion of various impurities can materially lower the melting point of the fuel. Of special significance are such elements as iron, silicon, chromium, and in the case of re-cycled material, any additional materials that may have been picked up by contact with the production equipment. For example, for the Mound Laboratory all-tantalum dissolution line, it has been found that as much as 1000 parts per million of tantalum appeared in the final product (Reference Ba71e). The melting point of tantalum oxide is considerably lower than that of PuO_2 being around 1950°C (3542°F), and forms a complex oxide $\text{Pu}_x\text{Ta}_y\text{O}_z$ with PuO_2 ; this oxide has a low melting temperature. The melting point constitutes a good practical measure for the purity of the fuel material. It appears desirable that the melting point not be permitted to go below 2300°C , (4172°F) in any formulation used for the MHW system. This lower limit takes into account the tolerances on measurement capability. Melting point will be determined on specimen pieces as a part of the fuel form fabrication development program discussed below.

Specific Heat

The specific heat of PuO_2 was reported at a 1970 Plutonium Fuel Conference (Reference Ba71h) and is available from the literature.

Mechanical Strength

1. PMC - The mechanical strength of PMC has been reported on the basis of a few experimental data points. More detailed information, under controlled conditions, is needed to permit stress analysis of many of the anticipated operational and abort conditions.

2. PPO - The mechanical strength of PPO has not been reported in the literature with a significant level of confidence. Data at temperatures approaching the operational temperatures of the MHW system are not available. A series of experiments are planned in which specimens of PPO and specimens of the current simulant material, ThO₂, will be exposed to the operational and impact temperature, nominally 1400°C and subjected to compressive strength tests. In addition, the so-called four-point bend test measurement will be performed on both materials to establish the equivalent of modulus of rupture for these materials. The equipment for these tests will become available at LASL during late 1971.

4.1.2.6.1.9 Compatibility of Fuel with Candidate Encapsulating Materials. Reaction between the fuel material and the candidate encapsulating material must be examined beyond solid-solid contact. Because of the permeability of various materials to gases at high temperatures and because some systems require actual physical venting of the helium generated by the decay process, it is necessary to conduct compatibility experiments to insure that the materials are exposed to representative environmental conditions, or under worse case conditions.

A matrix of 27 compatibility tests was set up involving short term tests of 1000 hours of exposure and long term tests of 4000 hours or more of exposure at temperature. The candidate materials included iridium, Pt30Rh, and the Oak Ridge National Laboratory alloy, platinum-rhodium-tungsten. To prevent contamination of the furnaces in which the test specimens would be exposed, a protective outer container was used around the compatibility test specimens. Figures 4.1.2-1, 2, 3 and 4 show the stack-up of materials in the compatibility tests, including the outer protective can. Most of the tests specimens, including both PPO and PMC fuels, are on test at LASL. An additional group of 10 test specimens which contained various candidate barrier materials are on test at Donald W. Douglas Laboratories, in Richland, Washington. A summary of the compatibility tests is shown in Table 4.1.2-4 and status of each crucible is shown in Table 4.1.2-5.

Figure 4.1.2-1. Schematic of Iridium/Plutonium Dioxide Fuel (PPO and PMC) Test Capsules (Pt-30Rh and Pt-Rh-W Substituted for Ir in a Few Tests)

Figure 4.1.2-2. Schematic of Iridium/Plutonium Dioxide Fuel Test with Graphite Shell and Ta-10W Protective Capsule (Pt-30Rh Substituted for Ir in Some Tests)

Figure 4.1.2-3. Schematic of Iridium with Barriers in Contact with Fuel

Figure 4.1.2-4. Schematic of Iridium Capsule with Barriers in Contact with Fuel with Graphite Shell and Ta-10W Protective Capsules (only one shown)

Table 4.1.2-4. Fuel Compatibility Test Matrix

Test Conditions	(°C)	Crucible Material	PPO Fuel Form		PMC Std O ₂ Content
			Low O ₂ Content	High O ₂ Content	
LASL: Short Term Testing (1000 hr)	1370	Ir	1, 3 ^u , 20*	2	13, 15 ^u , 24*
	1500	Ir	4	8	16
			7 (Mo-MgO)	10 (Mo-MgO)	
	1500	Pt30Rh	21		
Long Term Testing and/or Re-entry Pulse	1370	Ir	5, 19*	14	17, 23*
	1500	Ir	6	25	18
			11 (Mo-MgO)	12 (Mo-MgO)	
	1500	Pt30Rh	26, 27* 37 (0.01 Ir Foil)		
	1500	PtRhW	9		
DWDL: Short Term Testing (1000 hr) at 1500°C		Ir	28 (Mo-HfO ₂) 30 (Mo-HfO ₂)* 32 (Mo-Ir & Mo-Ir inter-metallic foil)	34 (Mo-Ir & Mo-Ir inter-metallic foil)	
Long Term Testing at 1500°C and/or Short Term Testing at 1500°C Plus a Re-entry Pulse		Ir	29 (Mo-HfO ₂) 31 (Mo-HfO ₂)* 33 (Mo-Ir & Mo-Ir inter-metallic foil)	35 (Mo-Ir & Mo-Ir inter-metallic foil)	
		Pt30Rh	36 (Mo-HfO ₂)		
		PtRhW	22 (Mo-HfO ₂)		

u Unvented

* Contains carbon

Table 4.1.2-5. Fuel Compatibility Test Status

Crucible	Not on Test	On Test	Completed Test, Not Yet Examined
LASL	9, 19, 20, 21, 24, 24, 26, 27, 37	1, 3, 5, 6, 11, 12, 17, 18, 25	2, 4, 7, 8, 10, 13, 14, 15, 16
DWDL		22, 28, 29, 30, 31, 32, 33, 34, 35, 36	

To obtain preliminary indications on the compatibility of iridium with plutonia, a mixture of 4 a/o iridium powder and plutonia powder was pressed into a compact and exposed at 1800⁰C for 24 hours, to determine any gross reactions. Subsequent metallographic examination indicated no interaction between the plutonia and the iridium particles. This data substantiates the data obtained on the compatibility between iridium and plutonia at Argonne National Laboratory, which used iridium as a liner for the Knudsen cell measurements conducted by the Chemical Engineering Division (Reference Ba69b). In the experiments, plutonia was heated to temperatures in excess of 2000⁰C for $\approx$ 24 hours repeatedly, without observable interaction between the plutonia and the iridium.

4.1.2.6.1.10 Thermodynamic Stability Analysis. The complex of materials proposed for use in the MHW heat source assembly requires an evaluation of the thermodynamic stability of the system at the operating conditions. To make this evaluation, a theoretical review of the thermodynamic relations existing among the candidate heat source materials was undertaken. This evaluation will provide some indication of both the potential reactions, as well as the absence of any reactions, which would be verified by the compatibility testing conducted with fuel by LASL and DWDL, and at GE-IPSO using only the non-radioactive materials.

Operational Conditions. In the operational mode of the MHW heat source, the temperature of the fuel is estimated to be between 1350⁰C and 1500⁰C, in a helium atmosphere, of around 75 torr (0.1 atm), by bleeding the decay He to the space vacuum through a non-selective vent.

Materials Considered. Three cases involving slightly different arrangements of materials have been considered. The fuel is assumed to be pure plutonium oxide (PPO) in the form of a sphere approximately 1.5 inches in diameter, which is in direct, solid-solid contact with a metal shell, with holes through it, to permit the release of He and vapor phase constituents from the plutonia. This shell is in turn, surrounded by a graphite structural member which provides impact protection to the fuel sphere after reentry.

The material layups considered, proceeding outward from the plutonia fuel, are outlined below.

Case 1

- a. PuO_2 sphere 80 - 85% T.D. 1.5" dia.
- b. Ir shell - 0.020 inch thick
- c. Graphite - impact shell - 0.25 inch thick
- d. Graphite - Re-entry shell - 0.5 inch thick
- e. Ir or Ta outer clad - 0.020 inch thick

Case 2

- a. PuO_2 as above (a)
- b. Mo barrier 0.005 inch thick
- c. HfO_2 barrier 0.003 inch thick
- d. Ir shell as above (b)
- e. Graphite as above (c)
- f. Graphite as above (d)
- g. Ir or Ta as above (e)

Case 3

- a. PuO_2 as above (a)
- b. Pt-30 Rh shell 0.020 inch thick
Remainder as in Case 1

The potential compounds in the system of Case 1, Pu-O-C-Ir, are:

PuO_2
 $\text{PuO}_2 - x$
 $\text{PuO}_{1.61}$
 $\text{PuO}_{1.5}$
Pu Oxy carbides

Pu carbides

Pu Ir₃ (by analogy to Pu Rh₃)

Pu₂ IrC₂

IrC

IrO₂

It must be emphasized that these are thermodynamically possible compounds considered in isolation. When considered in the complex of materials involved in the MHW Heat Source, the competing reactions, under operational and abort conditions, limit the actual compounds which will be formed. In addition, the laws of mass action also limit the degree to which reactions can occur. For example, finely powdered PuO₂ mixed with finely powdered graphite and heated above 1200⁰ C in a pumped vacuum will yield plutonium carbides in two steps.

As noted in Reference Mu67a, a small partial pressure of CO will inhibit this reaction. Moreover, experiments at LASL (Reference LA67a) indicate that the contact between the reactants must be intimate or the reaction stops. This reaction requires solid - solid contact between the reactants.

In the context of the layouts under consideration for the MHW heat source, the concern is first with reactions at the solid-solid interface and second with reactions which may occur with vapor-solid interfaces. In the following discussion it is a general rule that compounds with the larger negative Gibbs free energy of formation, ΔG_f^0 , are more stable than those with a smaller negative ΔG_f^0 or with a positive ΔG_f^0 all other factors being equal. In the example previously given: the interaction of PuO₂ with C (graphite), the pressure of free oxygen

stops the reaction between the oxide and carbon in favor of the reaction of carbon with the free oxygen. On the other hand in pumped vacuum, where the CO reaction product pressure can be reduced to less than 10^{-3} atm at 1800°K (1527°C), the $\text{PuO}_2 + \text{C}$ reaction will proceed to the right as shown in (1) above.

Case 1 Analysis

The Interface $\text{PuO}_2 + \text{Ir}$

The reactions can be shown as:

the free energy change is

$$\Delta G^0 = 33700 - 2.4 T$$

which indicates that this reaction as it stands will not go at any reasonable temperature ($< 10,000^{\circ}\text{K}$).

The other possible reaction is:

Here (R) is any reductant such as H_2 , C, or even Ta, and (RO) is the product with oxygen. Given this reduced condition when the partial pressure of oxygen in the environment $\leq 10^{-22}$ atm, for: $\text{PuIr}_3 \Delta G_f^0 \cong -20,000 + 10 + \text{Cal-mole}^{-1}$.

This reaction is similar to the method used for determining the oxygen content of refractory oxides with molten platinum (7)

Note that the melting point of Ir is $> 2300^{\circ}\text{C}$.

It should be noted that neither of these reactions will proceed to the right unless the partial pressure of oxygen is reduced in some fashion and unless the contact between the refractory oxide and the noble refractory metal is intimate. In the case of the MHW heat source, if the partial pressure of CO exceeds 1.5×10^{-3} atm at 1800°K (1527°C), the reaction will not proceed.

The initial reduction of PuO_2 to PuO_{2-x} in the absence of free oxygen occurs as a function of temperature and proceeds to a point of congruency at which the solid PuO_{2-x} is in equilibrium with the vapor phase constituents PuO_2 , O^{-2} and O_2 , (Figure 4.1.2-4a). However, to proceed to lower O/Pu ratios, the oxygen must be removed continuously, by combination or physical removal. Reference Ea 71a indicates that in the Pu-O-C-Ir system, no further reactions will take place until the O/Pu ratio is reduced to ≤ 1.5 . The time required to reach this point is estimated at around 140 years assuming only vapor transport of carbon vapor to the PuO_{2-x} surface and free access of the carbon vapor to the plutonia surface.

The Interface Ir to Graphite

The formation of noble metal carbides occurs to a limited extent and the free energy of formation is estimated to be around

$$\Delta G_f^0 = -10,000 + 5 T \text{ cal-mole}^{-1}$$

for IrC, PtC and RhC indicating that they are marginally stable.

Experimental evidence indicates that, in fact these carbides are not formed. However, the possibility of permeation of the noble metal by carbon cannot be ruled out. Under these conditions it appears as doubtful that any interaction between Ir and C will occur. This in turn precludes interaction of C with the PuO_2 .

Interfaces of Graphite with Graphite

No chemical or structural reaction is expected to occur.

Interface of Graphite with Outer Shell

The use of the outer metal shell is dictated by the need to provide some protection of the graphite re-entry body during a skip re-entry. The material may be Ir or a Ta alloy. In either event, it appears likely that a barrier must be added to the graphite.

Case 2 Analysis

Interface of PuO_2 with Mo

Slightly substoichiometric plutonia (PuO_{2-x}) will not react with Mo. Both theoretical thermodynamic considerations and experimental evidence (Reference Pa 69c and Se 70a) demonstrate this fact.

Figure 4.1.2-4a. Rate of Change of Composition of Stoichiometric Plutonium Oxide $\text{PuO}_{2.00}$ at 2000°K. Approach to the Congruently Vaporizing Composition

Interface with Mo with HfO₂

HfO₂ is slightly more stable than PuO₂ so that contact with Mo is expected to produce no reaction.

Interface of HfO₂ with Ir

Only if the HfO₂ is reduced quantitatively will it react with Ir following the same patterns discussed above for PuO₂.

The remaining interfaces within the system of Case 2 are identical with those in Cases 1 and 3. No synergies are expected to alter these projections.

Case 3 Analysis

Case 3 using Pt-30Rh is expected to give essentially the same results as Case 1 except that the temperatures of reaction are somewhat lower, roughly in proportion to the difference in melting points.

Conclusions. Based on these evaluations, the conclusions reached are:

1. Plutonium carbide formation will not occur if solid contact between plutonium oxide and graphite does not occur either directly or through carbon transport through the metal shell.
2. Formation of the intermetallic PuIr₃ will occur only if the PuO₂ is sufficiently reduced to allow the metals to interact.

Conditions for PuO₂/Carbon Reactions. From the foregoing discussion, it is clear that the critical reaction is the reduction of PuO₂ to the point where it will form other compounds: e. g., PuIr₃, Pu_xC_y. Since carbon as graphite, is the only identifiable reductant now in the system, the conditions for this reaction were considered.

A solid-solid contact between graphite and PuO₂ is required to form a plutonium carbide. This conclusion is supported by the data of Skavdahl (Reference SK 64a) and Mulford (Reference Mu 67a) which indicate that the carbothermic reduction of PuO₂ will occur only if the reactants are in intimate contact and in a fine state of division. In the case cited by Mulford the PuO₂ was in chunks buried in carbon powder and he reached the following conclusion:

"Four of the five pieces exhibited no evidence at all of carbide formation. The oxide exhibited a microstructure characteristic of reduced PuO_2 (i. e. , PuO_{2-x}) which is believed to be the result of exposure to vacuum at high temperature and not related to the presence of carbon reaction is dependent on establishing contact between PuO_2 and carbon. Previous tests have shown that powders ($\text{PuO}_2 + \text{C}$) pressed together begin to react at 1200°C . Under present conditions (PuO_2 lumps in graphite powder) it appears that reactivity will be erratic or that the reaction will not start until the temperature is high enough to have an appreciable carbon vapor pressure ($1850 - 1950^\circ\text{C}$)." (Reference Mu67a) (Figure 4.1.2-5).

These results support the previous conclusions, that the graphite in the MHW heat source at operational temperature will not drastically affect the PuO_2 composition unless it penetrates through the Ir shell.

Thermal Reduction of PuO_2 . Numerous studies have been performed during the past decade to establish the conditions for the thermal reduction of PuO_2 under the following conditions:

- Oxidizing
- Neutral
- Reducing
- Vacuum

Katz and Seaborg (Reference Ka57a) recognized the tendency of PuO_2 to disproportionate at high temperatures indicating the greater stability of the 3+ valence even in the solid state.

Studies by Phipps, (Reference Ph50a), Mulford (Reference Mu60a), Pardue (Reference Pa64a), Ackerman (Reference Ac66a), Ohse (Reference Oh67a), Pritchard (Reference Pr66b) have done much to clarify the behavior of PuO_2 under the conditions noted above.

Figure 4.1.2-5. Pu-C Phase Diagram as Proposed by Mulford et al.

Under oxidizing conditions the compound does not lose oxygen until the melting point is reached. Under neutral gas and vacuum conditions the compound becomes oxygen deficient with the composition PuO_{2-x} where ($0 < x < 0.4$). Under reducing gas the composition will not go below $\text{O/Pu} = 1.6$, which is the $\alpha - \text{Pu}_2\text{O}_3$.

It is clear that the variability of composition of the nominal plutonium "dioxide" has given rise to a number of misunderstandings some of these are of concern only to the theoretician but others such as the very notable differences in thermal expansion have direct engineering significance. (Reference Br60a).

The phase diagrams, Figures 4.1.2-6, 4.1.2-7, and 4.1.2-8 illustrate respectively the total compositional diagram, and examination of the low temperature, high oxygen states, and the behavior at high temperatures.

Ackermann et al (Reference Ac66a) and Battles (Reference Ba69a) have defined the equilibrium compositions at various temperatures in both open and closed systems as part of their work in investigating the vapor pressures and vapor species in the Pu-O system. This work as well as that of Mulford, Ohse, Aitken, and others has emphasized the impact of the materials used in the experimental apparatus.

Figure 4.1.2-9, derived from Ackermann et al (Reference Ac66a), shows the difference in vapor pressure in systems using Ta, W, and Re Knudsen cells. Battles (Reference Ba69a) has found moreover that W, which had become accepted as the standard material for this apparatus, still produces measureable quantities of tungsten oxides whereas a Knudsen cell lined with Ir, gives no evidence of Ir, iridium oxides, or iridium-plutonium species at 2273 K (2000°C). The determination was made by mass spectrometer.

The equilibrium compositions at various temperatures in both open and closed systems using W as the material of the Knudsen cells is shown in Table 4.1.2-6. It is noteworthy that Battles found the solid composition to be 1.84 in Ir at 2200K as compared to 1.82 for the W system. This apparently indicates the greater stability of the $\text{PuO}_2 - \text{Ir}$ system.

Figure 4.1.2-6. Proposed Phase Equilibria Diagram for the System Plutonium-Oxygen

Figure 4.1.2-7. Low Temperature-High Oxygen Content Portion of Diagram Showing Experimental Points

Figure 4.1.2-8. High Temperature Phase Diagram Plutonium-Oxygen System

Figure 4.1.2-9. Temperature Dependence of Plutonium Oxide Vapor Pressure Measured with Various Refractory Metal Effusion Cells:

Series I Tungsten
 Series II Rhenium
 Series III Tantalum

Table 4.1.2-6. Congruently Vaporizing Composition of Hypostoichiometric Plutonium Dioxide - PuO_{2-x}

Temperature °K	Oxygen to Plutonium Ratio (In Tungsten or Rhenium Cells)	
	Closed	Open
1000	1.995	1.995
1200	1.99	1.98
1400	1.96	1.95
1600	1.92	1.90
1800	1.89	1.87
2000	1.86	1.85
2200	1.85	1.82*
2400	1.84	1.81

*in W or Re; in Ir the value is 1.84

This thermal reduction or disproportionation of PuO_2 to the formula PuO_{2-x} is additive to any other factors which may work to reduce PuO_2 . However, as previously noted in 5.0, mere proximity to carbon is apparently not sufficient to drive the PuO_2 to lower O/Pu ratios.

Behavior of PuIr_3 . As indicated in the discussion of the PuO_2 /Ir interface the reaction between Pu and Ir is energetically possible. Therefore if PuO_2 is reduced to the point where the system is deficient in oxygen to the extent that free Pu is available, the binary PuIr_3 will probably be formed. McHenry (Reference McH70b) has investigated a number of the compositions between the actinides and the noble refractory metals. Figures 4.1.2-10 and 11 show the phase diagram of Pu/Rh and U/Rh which is the homolog of PuIr. Figure 4.1.2-12 shows U/Ir and Figure 4.1.2-13 a hypothetical diagram of Pu/Ir.

Reference McH70h examined some of the putative properties of the compound CmIr_3 which he predicted to form with a $\Delta G_f^0 \sim 60-90$ Kcal-mole. Work at ORNL and elsewhere indicates that these ordered alloys are considerably more resistant to oxidation than might be expected from thermodynamic considerations alone. Consequently, in the unlikely event that the fuel were to react with the Ir shell, the resulting compound should be inert to oxidation at post-impact temperatures. The melting point of the PuIr_3 is predicted to be above 2100°C by analogy to the PuRh_3 . This is marginal for re-entry survival.

Figure 4.1.2-10. Plutonium-Rhodium Phase Diagram

Figure 4.1.2-11. The U-Rh System

Figure 4.1.2-12. The U-Ir System

Figure 4.1.2-13 . Hypothetical Phase Diagram Plutonium-Iridium

The effects of the formation of PuIr_3 from the iridium of the containment shell on the mechanical properties of Ir are unknown.

4.1.2.6.2 Fuel Form Fabrication Development

A spherical fuel form has been specified for the MHW Helipak concept. The fabricability of such a fuel shape is of primary importance whether the fuel structure is pure plutonium dioxide, PPO, or the plutonia molybdenum cermet PMC. In addition to the fabrication feasibility of a spherical shape, the limitations on sphere diameter must also be evaluated, since it can be shown that size and density of the fuel sphere directly affect the weight of the overall heat source. In addition to the fabrication considerations, there are concerns as to behavior of the finished fuel shape with respect to helium release, the resistance to thermal and mechanical shock under operational and anticipated abort conditions, and the stability of the microstructure when subjected to thermal cycling. Consequently, a concerted effort has been made to establish both the feasibility of forming spheres as well as the determination of certain additional aspects of their behavior. The possibility of sphere fabrication was demonstrated by BMI/CL under their fuels development contract (References Pa 68a and Pa 69a). In addition, BMI has made spherical ceramics as large as 6" in diameter for various purposes in the past. GE, in anticipation of the use of spherical fuel forms, has fabricated a large number of cermet fuel simulant spheres ranging in size from 0.5" up to 2.83" diameter as well as pure thoria spheres in a variety of sizes. The feasibility of making spherical shapes to reasonable tolerances with materials closely analogous to PuO_2 has therefore, been demonstrated. There was however a question as to whether the thermal load imposed by the activity of ^{238}Pu would permit the satisfactory formation and subsequent handling of spheres in the size range desired.

4.1.2.6.2.1 PPO Fuel Fabrication at LASL. Table 4.1.2-7 lists the spherical fuel forms and their sizes and thermal content fabricated at LASL. The nominal sphere size contained approximately 120 thermal watts, with a diameter of ≈ 1.60 ". Because of fabrication problems, it was determined that the thermal loading of the sphere would be limiting and the resulting diameter would be accepted to provide a mechanically sound material.

Two approaches to fuel sphere fabrication were undertaken. The first was a hot-pressing technique in which the plutonia powder is placed within a die having hemispherically shaped

Table 4.1.2-7. LASL Sphere Production and Inventory

A. Hot Pressed Fabrication	
Description	Status
1. ThO ₂ (83% T.D.)	<u>Good pressing.</u> Destroyed during test of "drop" apparatus.
2. SS (84 wt% PuO ₂ -16 wt% ThO ₂) (84% T.D.)	<u>Good pressing.</u> Deliberately thermally shocked--did not survive.
3. ²³⁸ PuO ₂ (SPP-1) (82% T.D.)	<u>Good pressing.</u> ~ 0.020 in. equatorial band; kept on insulating support; surface cracks did not change for 3-1/2 months; destroyed during test of "drop" apparatus.
4. ²³⁸ PuO ₂ + 1/16 in. ThO ₂ (PTO-1) (86% T.D.)	<u>Good pressing.</u> ~ 0.20 in. equatorial band; in storage; no change since birth.
5. ²³⁸ PuO ₂ + 1/16 in. ThO ₂ (PTO-2) (~ 86% T.D.)	<u>Good pressing.</u> Equatorial band gone, ~ 1/2 in. ² ThO ₂ slip casting spalled; broken up with difficulty by hammering.
6. ²³⁸ PuO ₂ (SPP-2) (~ 83% T.D.)	<u>Good pressing.</u> Equatorial band 0.025 in.; used to try O/M adjustment by sintering; broken up for chemistry.
7. ²³⁸ PuO ₂ (SPP-3) (80% T.D.)	<u>Good pressing.</u> Equatorial band reduced; 1.580 ± 0.005 in.; 122 watts.
8. ²³⁸ PuO ₂ (SPP-4)	<u>Good pressing.</u> Same conditions as SPP-3; 1.600 ± 0.002 in.; 119 watts. To MRC for soak tests.
9. ²³⁸ PuO ₂ (SPP-5)	<u>Failure.</u> Test of different bake out of SR feed; low bulk density.
10. ²³⁸ PuO ₂ (SPP-6)	<u>Failure.</u> Ellipsoid due to early termination of run.
11. ²³⁸ PuO ₂ (SPP-7)	<u>Good pressing.</u> Initial pressing elliptical due to spacer break; re-pressed to good sphere; 1.590 ± 0.005 in.; 123 watts. To MRC for soak tests.
12. ²³⁸ PuO ₂ (SPP-8)	<u>Failure.</u> Some recycled material used with different bake out cycle. Piece fell into two hemispheres upon removal from die.
13. ²³⁸ PuO ₂ (SPP-9)	<u>Failure.</u> Same feed as SPP-8. Piece fell into four roughly equal quadrispheres upon removal from die.
14. ²³⁸ PuO ₂ (SPP-10)	<u>Good pressing.</u> Fresh SR feed used. 1.584 ± 0.004 in.; 122 watts. Sphere subsequently underwent spontaneous fracture while awaiting encapsulation.
15. ²³⁸ PuO ₂ (SPP-11)	<u>Good pressing.</u> First trial of 1.1 in. die. Final dimensions: 1.103 ± 0.004 in.; 41 watts. Sphere subsequently destroyed during drop test.
16. ²³⁸ PuO ₂ (SPP-12)	<u>Good pressing.</u> 1.553 ± 0.004 in.; 122 watts; kept in inventory.
17. ²³⁸ PuO ₂ (SPP-13)	<u>Good pressing.</u> First trial of 1.2 in. die. Slight (0.020 in.) equatorial band. 1.235 ± 0.010 in.; 63 watts. Broken up for recycle.
B. Cold Press - Sinter Fabrications	
²³⁸ PuO ₂ (CPS-1)	Developmental studies for various die liners and inserts.
²³⁸ PuO ₂ (CPS-2)	<u>Good pressing.</u> "Molykote used on punch surfaces; some dimpling on final product; 1.614 ± 0.001 in.; 122 watts. To MRC for soak tests.
²³⁸ PuO ₂ (CPS-3)	<u>Good pressing.</u> 1.598 ± 0.001 in; 1 polar crack. Survived drop tests of 1 in.; 6 in., 1 ft and 2 ft. At 3 ft, specimen fractured along crack into two equal hemispheres.
²³⁸ PuO ₂ (CPS-4/5)	<u>Good pressing.</u> Hemispherical "keyed" punches used. Both hemispheres removed intact.

plungers. Pressures as high as 12000 psi are applied, at temperatures up to 1500°C, to shape and sinter the material in the spherical shape. The dies for hot-pressing are fabricated from graphite of high strength which is readily machined to shape, but which cannot be used more than once or twice.

The second approach to fuel fabrication involved the pressing of plutonia powder in the cold condition then sintering. With $^{238}\text{PuO}_2$ this designation of cold pressing is not strictly correct. The thermal content of this material is such that even if the die is massive and cooled, the temperature involved is quite high when a fuel loading of 125-watts is involved. The die is made of tool steel and the hemispherically shaped plungers are made of the same material. Pressures on the order of kilotons are applied and the material is pressed to a "green density" of $\approx 60\%$ in the cold condition. This material is then sintered in a controlled atmosphere to its final density. The temperature of sintering may range from 1400°C to 1650°C. In the case of the spheres made to date, the temperature has been $\approx 1400^\circ\text{C}$.

The two methods, when properly exploited, yield materials which appear to differ little in final characteristics. Because the number of spheres made by either method is small, this conclusion is somewhat speculative. However, the cost and time involved in making a cold press die is substantially more than that required for making a hot press die. Conversely, the hot press die may be usable only for a single pressing whereas the cold press die may be used repeatedly without damage. No final decision with regard to the pressing of PPO has been made at this time.

4.1.2.6.2.2 PMC Sphere Fabrication at Mound Laboratory. During the Base Program, Mound Laboratory has fabricated two spherical bodies by the hot pressing technique whose final machined dimensions were approximately 1.54" with a thermal loading of approximately 92 watts; a larger sphere has been fabricated with a thermal content of ≈ 125 watts, but has not been machined to final shape. Using the same techniques applied to production of PMC discs for the TRANSIT and PIONEER programs, Mound Laboratory hot pressed the PMC fuel spheres. This involved the use of graphite dies, but since the material is machined to its final shape and dimensions after pressing, the graphite is removed from the sphere surface.

4.1.2.7 Experiments in Progress - Full Sized Spheres.

As previously indicated, there is concern for the long term behavior of both the PPO and the PMC spheres. The stability of either fuel form in a relatively large size and in the spherical shape has been open to some question. A series of tests were defined to determine the performance of a fuel sphere on both a short-term basis involving an exposure of spheres at a temperature of 1370°C for a period of around 300 hours, and a series of long-term tests at 1370°C . The test period for the long-term tests has not been established but 4000 hours is desired.

Short Term Sphere Aging Tests

The short term tests are designed to expose the test spheres of the correct size and thermal content to the MHW operational temperature of 1370°C without attempting to simulate the overall conditions of the FSA. Consequently, the spheres were exposed in a container which provided protection to the furnace but did not attempt to simulate the conditions existing in the complete FSA. The short term test had contemplated the exposure of several spheres but the fabrication problems associated with PPO development as noted in Table 4.1.2-7, PPO sphere fabrication, reduced the number of PPO spheres available. The Mound fabrication effort only produced one full size sphere in the PMC material. Consequently, the short term test was run with a single PPO sphere and a single PMC sphere. These were encapsulated separately in a set of Mo50Re hemispheres which were welded; the sphere itself was contained within a moly shell surrounded by an MgO spacer (see Figure 4.1.2-14). The original intent was to include no other materials; due to an assembly error, iridium hemispheres were included in the tests, between the fuel and the MgO spacer. The iridium in the test was in direct contact with the plutonia.

The data to be obtained in this test is as follows:

1. Physical changes in dimension, appearance
2. Survivablility of the sphere
3. Gross chemical reactions observable on visual inspection.

GRIND OFF AFTER SEAL
WELDING (HOLE DIA. ~ 0.010 , MAX.)

Figure 4.1.2-14. Short Term Test Configuration

Long Term Sphere Aging Tests

The long term sphere tests were set up typically as indicated in Figure 4.1.2-15. The two PPO spheres, produced by the hot pressing technique, were placed in the unwelded iridium shells and installed inside an iridium can which had a welded lid and which had a vent hole five mils in diameter. One of these spheres has an MgO/Mo barrier between it and the iridium shell. The use of an iridium can served a two-fold purpose: at the time the test lay-up was planned and executed Mound did not have experience for the welding of the iridium hemispheres and, there was concern that the welding of the hemispheres directly in contact with the PPO might induce a fracture of the spheres by thermal shock.

After hardware for the tests described above was produced, an additional fuel test was planned using the PPO cold pressed and sintered sphere from the short term test. This sphere will be encapsulated directly in a special iridium shell with weld lips to enable welding and enclosed in a graphite shell which simulates the FSA graphite impact assembly. This test will be performed as part of the long-term test evaluation. The test configuration is shown in Figure 4.1.2-16.

The full size PMC sphere made by Mound will be encapsulated for the long term tests in the material configuration shown in Figure 4.1.2-17. This is substantially identical to that used for the 2 PPO spheres. A molybdenum shell is used directly in contact with the PMC; this in turn is separated from the iridium by an MgO spacer similar to one of the PPO spheres. It is anticipated that the long term tests will commence during the early portion of the next program phase.

A description of the test hardware is as follows.

Short Term Test - PMC

1. Fuel - PMC Sphere (1.53 Dia., 104 watts thermal) supplied by Mound Laboratory.
2. Molybdenum hemispherical shells formed from 0.002 thick material.
3. MgO hemispherical shells supplied by General Electric (1.750 ± 0.050 ID with 0.22 ± 0.050 wall thickness).

Figure 4.1.2-15, Mound Fuel Sphere Test Configuration

Figure 4.1.2-16. Schematic of Proposed Fuel Sphere Test with Close-Coupled Graphite Materials

Figure 4.1.2-17, Mound Fuel Sphere Test Configuration

4. Mo50Re "hemispherical" shells formed from 0.030 thick material (2.42 ID x 0.030t).

After welding the Mo50Re sphere, a 0.010 max. hole will be drilled through the Mo50Re shell.

Short Term Test - PPO

1. Fuel - Cold pressed and sintered PPO-Hi sphere supplied by LASL (CPS-2, 1.603 - 1.609 dia.).
2. Molybdenum hemispherical shells - same as above.
3. MgO hemispherical shells - same as above.
4. Mo50Re "hemispherical" shells - same as above.

After welding the Mo50Re sphere, Mound Laboratory will place a 0.010 max. hole through the Mo50Re shell. An attempt should be made to duplicate the hole size for both tests. Both Mo50Re spheres will then be placed in a Tantalum can. The tantalum can will utilize a frit filter, or equivalent, to maintain the helium back pressure between 0.01 and 0.4 atmospheres.

Long Term Test - PMC

1. Fuel - PMC Sphere (1.60 dia, max.) to be supplied by Mound Laboratory
2. Molybdenum hemispherical shells, formed from 0.002 thick material
3. MgO hemispherical shells (1.680 ± 0.005) OD x (0.035 ± 0.002) thick
4. Iridium hemispherical shells (1.700 ± 0.005) ID x (0.020 ± 0.002) thick, available at Mound Laboratory.
5. Iridium "single ball" can and lid, to be supplied by General Electric, per GE Drawing CT-001, Group 2.
6. Iridium spacers (3), (1.125 ± 0.005) ID., x (0.5 ± 0.005) long x (0.015 ± 0.002) thick

7. Graphite (≈ 0.25 thick)
8. Tantalum cans (2) to be supplied by Mound Laboratory.

The iridium can and the two tantalum cans will be seal welded in a clean helium atmosphere.

Long Term Test - PPO

1. Fuel - PPO Spheres SPP4 and SPP7 supplied by LASL.
2. Molybdenum hemispherical shells formed from 0.002 thick material (for sphere SPP7 only).
3. MgO hemispherical shells (1.680 ± 0.005) OD x (0.035 ± 0.002) thick (for sphere SPP7 only).
4. Iridium hemispherical shells (same as above)
5. Iridium "double ball" can and lid to be supplied by General Electric, per GE Drawing CT-001, Group 3.
6. Iridium spacers (2) (same as above)
7. Graphite (≈ 0.25 thick)
8. Tantalum cans (2)

Both the "encapsulated" PPO spheres will be tested in the same iridium can. Iridium can and tantalum can welding will be in a clean helium atmosphere.

Long Term Test - PPO with Graphite Near Fuel

1. Fuel - PPO Sphere CPS 2 (used previously for short term test).
2. Iridium hemispherical shells (See C-4).
3. Graphite hemispherical shells (≈ 1.85 ID)
4. Mo50Re "hemispherical" shells formed from 0.030 thick material

After welding the Mo50Re sphere, a 0.010 max. hole will be drilled through the Mo50Re shell. The Mo50Re sphere will then be placed in a tantalum can be capable of maintaining a helium back pressure between 0.01 and 0.4 atmospheres.

The data requirements are as follows:

Fuel Spheres

1. Cracking and crazing (macro effects)
2. Dimensional changes (swelling, etc.)
3. Weight changes
4. Microstructure*
5. Helium Content*
6. Stoichiometry*

*After long term test only

Iridium

1. Microstructural examination
 - a. Metallographic
 - b. Electron probe (if deemed necessary)

Molybdenum

1. Cracking, etc.
2. Attack by fuel, MgO, Ir or Graphite

MgO

1. Cracking, etc.
2. Attack by fuel, Mo, Ir or Graphite

In addition the sphere is to be X-rayed after encapsulation in iridium and upon completion of the test prior to being removed from the iridium.

Oxidation Tests on PMC Spheres

To determine the effect of oxygen on a PMC fueled FSA, a test is planned utilizing PMC spheres inclosed in the iridium post-impact shell. A molybdenum shell will be placed immediately adjacent to the fuel; a shell of MgO will be used between these two members. The PMC fuel sphere will be broken or cut into a minimum of eight pieces to provide a large surface area available for oxidation to simulate the condition which might exist after the end of a reasonable lifetime prior to re-entry and impact. The helium vent with its internal cover will be the reference design size. The spheres will be exposed under two conditions, one simulating still air, in which only thermal convection will tend to cool the iridium, the other, being placed in an air stream moving at about 10 miles per hour which is expected to provide some cooling of the iridium surface but a higher iridium oxidation rate. The spheres will reach their equilibrium temperatures under the conditions noted. It is anticipated that this test will begin in the next program phase.

4.1.2.8 Problem Areas

Principal problem areas remain with respect to both fuel forms and resolution is required to permit final selection of the fuel.

PMC

The overriding problem of PMC is its post impact oxidation behavior. Preliminary indications, based on LASL tests (Reference Ba70c) of fuel material and on GE tests (Reference BR70b) on fuel simulant, indicate severe break-up of the fuel material when exposed to air at temperatures above 600⁰C. In addition, some radioactivity has been shown to be carried in the molybdenum trioxide vapor, which is produced at the test temperatures. This is considered a mechanism for the potential dispersal of plutonium-238.

As noted previously, the release of decay product helium from the cermet fuel form is a matter of concern. The tests conducted by Donald W. Douglas Laboratory (Reference An70a) indicate swelling of the fuel compact and fragmentation of various portions of the molybdenum matrix enclosing the ceramic particles. The substantial amount of pull-out evident in the polished specimens suggests that the ceramic particles are fragmented and no longer cohesive after exposures, for as short a period as 1000 hrs, at 1800⁰C. The behavior for longer periods of time is equally suggestive of severe change in the character of both the ceramic and the molybdenum matrix. The tests at DWDL were performed using BMI-CL fabricated cermet. Tests at DWDL on material fabricated by LASL and Mound are now in process. The results of these tests should provide a decisive answer to questions concerning the long term behavior of the cermet fuel in regard to helium ingrowth and partial loss of oxygen from the ceramic.

PPO

The overriding problem for PPO appears to be its sensitivity to thermal shock at low temperature. In the attempt to package spheres for shipment to Mound Laboratory for the sphere tests, one of the hot pressed spheres fractured within its shipping container. A second sphere fractured into two hemispheres while sitting on an insulating block in a transfer box. The remaining spheres: two hot pressed and one cold pressed sphere survived the trip to Mound Laboratory without incident. One of the hot pressed spheres was accidentally broken into several large pieces when it was dropped during assembly. Because the number of spheres fabricated to date is small, it is difficult to ascertain whether the sensitivity to break-up is a function of the sphere size and the material properties exclusively. Defects which cannot be determined by external observation may have existed in the spheres which broke. Analysis performed by LASL indicate that the stresses imposed on the sphere under abrupt changes in temperature exceed the estimated nominal strength of the material. Since there is no base-line for the determination of the basic mechanical properties or initial forming stresses of the sphere, and of the PPO material, the validity of these analyses rests on assumptions of material properties which require verification. The compressive and tensile strength and modulus of rupture tests discussed above are designed to provide the necessary data.

In addition to the susceptibility of the PPO spheres to thermal shock by cooling, there is concern that they will also be susceptible to thermal shock occurring in a re-entry heat pulse. The temperature ramping test previously discussed will determine the behavior of the PPO under these conditions.

The initial impact tests at relatively low temperature indicate that the percentage of fines formed by the impact of the PPO is approximately twice that formed by the impact of comparable PMC compacts, at the same temperature and velocity. However it is felt that this differential (3% to 6%) is not a significant tradeoff between PMC and PPO.

The response of a solid PPO compact of dimensions approximately 1.6" diameter, with a thermal loading of ≈ 125 watts, to the ingrowth of helium, over substantial periods of time is not known at present. It is expected that the output of the long-term sphere tests will provide answers to questions concerning the dimensional stability of the sphere to helium ingrowth and its mechanical stability to survive without break-up from the effects of helium production within its crystal structure.

PROBLEMS COMMON TO PMC AND PPO

For both PPO and PMC, long term compatibility capability with the candidate encapsulating materials will be resolved by the tests currently in progress. The release of plutonium bearing species from the FSA and ultimately from the heat source will also be determined by tests. The nature of these tests have not been determined to date. The question of long term mechanical stability both with respect to dimensions and to integrity of the spherical form must be determined by life tests on the full scale spheres.

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4.2 HEAT SOURCE DESIGN DEVELOPMENT

The Helipak heat source concepts recommended in the "MHW" Phase I Final Report (Reference 4-1) were used as the initial configurations for further detailed evaluation. These heat source configurations (Figure 4.2-1 and 4.2-2, as well as updated configurations and additional concepts were evaluated over a broad spectrum of composite operational and abort environments. Detailed design tradeoff studies were then conducted to further refine the Helipak configuration into workable heat source designs. The design tradeoff studies were also used to define the range of parameters and component matrices for the various heat source developmental test programs.

4.2.1 HELIPAK HEAT SOURCE EVALUATION

The design evaluation of the Helipak concept, which provides reentry and impact protection to the fuel was separated into two distinct parts for convenience in evaluation; 1) the fuel sphere assembly (FSA), which includes the fuel, the post-impact containment shell (PICS), the impact shell and compliancy members and 2) the components that comprise the remainder of the Helipak, whose primary functions are to provide basic thermal and structural integrity to the heat source consisting of the outer can, aeroshell, insulation, crushup, compliance and assembly integration hardware.

The MHW Heat Source development covered three basic areas:

1. Design Considerations
2. Performance Evaluation
3. Material Development

Variations of the basic Helipak concept and the Fuel Sphere Assembly (FSA) were evaluated with respect to potential performance, weight, and safety advantages. The materials effort aimed primarily at iridium fuel barrier materials and various graphite composites covering fabrication, development and compatibility. The results of the performance tests and analyses were included in evaluating and refining the design of the Helipack heat source. These results are reported in Sections 4.2.2.

Figure 4.2-1. Reference Helipak Design - Ceramic Fuel

Figure 4.2-2. Open Core Heat Source - 2200 Watt SSC Fuel

4.2.1.1 Fuel Sphere Assembly

The fuel sphere assembly consists of the fuel, compliancy members, iridium post impact containment shell and the wound graphite composite impact shell (See Figure 4.2.1-1).

The design concept of the FSA as used in the Helipak provides protection to the fuel sphere throughout its life cycle. The FSA is a modular element of the heat source and the size of FSA determines the size and weight of the overall heat source, specifically, the FSA:

- a. Provides the basic fuel containment for all mission phases.
- b. Provides a means for decontamination
- c. Precludes significant changes in fuel stoichiometry
- d. Provides impact capability
- e. Provides post-impact containment

At the initiation of the Base Program, the two FSA concepts utilized PPO and SSC fuel spheres of 1.53 and 1.50-inch diameters respectively (See Figures 4.2-1 and 4.2-2). During the early portion of program, reported herein, the SSC fuel form was dropped from consideration in favor of PMC, which is the fuel form being used on the Transit and Pioneer programs.

The fuel sphere size of the PPO fuel form was changed from 1.53 to 1.60 inches with fuel density of 80% T.D. This was required by LASL to ensure release of the Helium and fabricability of spheres.

The further change in PPO fuel sphere diameter was necessitated by revised converter thermal loading requirements, which increased the thermal wattage requirements from 2200 w(t) to 2400 w(t) (See Section 3). To ensure that the thermal wattage of the individual fuel sphere be maintained at 115 watts, or less, which was a limit imposed by LASL as a result of their fuel development studies, the number of spheres was increased

Figure 4.2.1-1. Typical MHW Fuel Sphere Assembly (FSA)

to 21. Each sphere then had a thermal loading of 114.3 watts, which using 80% TD fuel, was equivalent to a 1.564 inch diameter. This was the fuel size used in the Final Base Program reference design, which is described in Section 4.2.1.5.

During these evaluations, most effort was carried out using PPO fuel and evaluation of the data made with respect to applicability to PMC.

The final design of the FSA is dependent upon the results of the impact test program, fuel compatibility and test program and the iridium performance and characterization programs. The impact program will determine the type and thickness of the impact shell and the thickness of the iridium shell. The fuel compatibility tests will determine the need for barriers between the fuel and iridium. Results from earlier non-fuel compatibility tests showed a reaction between molybdenum and iridium which indicates a barrier is required if SSC or PMC fuel is used. Data from Batelle show that PuO_2 and iridium are compatible in the 1300°C to 1500°C range (See Phase I Final Report). Iridium crucibles have also been used by Argonne National Laboratory in testing of PuO_2 at 2000°C for repeated periods of 24 hours with no reaction. Fuel test programs (See Section 4.1.2.6.1) such as the helium release tests and plutonium escape tests will be used to define the vent system in the PICS. The non-fuel compatibility tests will determine the requirements for barriers between the iridium shell and the graphite impact shell. The non-fuel test compatibility tests (see Section 4.2.1.3) indicate that HfO_2 , MgO and BeO are suitable barriers between the fuel and the iridium. These tests also indicated that no barrier is required between the iridium shell and the graphite impact shell at operational temperature (1370°C) and at reentry temperature (2100°C), the only effect being a eutectic melting at (2210°C). The effects of exposure of iridium to graphite at these temperatures will be determined in the next phase of the program.

To accommodate variations in fuel sphere size and to minimize gaps that might exist during vibration that occurs in powered flight, a dimensional compliance member was included as part of the overall fuel sphere (FSA). (See Figure 4.2.1-1.) At the present time, two refractory oxides, MgO and HfO_2 and a formed iridium corrugated strip of thin foil are candidates for the compliancy member. The non-fuel compatibility test data, Section 4.2.1.3, indicate both oxide materials are compatible with iridium at temperatures up to 1370°C .

4.2.1.2 Helipak Assembly Development

The Helipak design effort was concentrated in four areas. The first design consideration involved the evaluation of techniques for assembling the various components into an overall heat source. Detailed weight tradeoff studies were conducted on the basic design configuration; the dimensions of all components were parametrically varied to determine their effect on overall heat source weight.

The third area of design evaluation was concerned with tradeoffs of various alternate fuel configurations formed in spherical elements for integration into a Helipak configuration.

Finally, in addition to the Helipak evaluation, several alternate concepts using refractory metals with fuel pucks, were also evaluated.

4.2.1.2.1 Helipak Assembly Techniques

Design studies were conducted to evaluate various techniques for assembling the Helipak configuration. The three concepts shown in Figures 4.2.1-2, 4.2.1-3 and 4.2.1-4 are the results of these studies. These concepts were then evaluated with respect to system performance and development by both GE and the AEC Fueling Agency (MRC).

The central rod concept shown in Figure 4.2.1-2 represents the assembly that was selected as the most desirable based on thermal performance evaluations as well as Fueling Agency inputs. This concept features a segmented, modular graphite retainer ring which positions the FSA's during assembly, by stacking one row at a time. A graphite centering rod is used to hold the FSA/Retainer ring sub-assembly during the handling and subsequent assembly into the graphite aeroshell. The centering rod also maintains sphere-sphere contact as well as contact with the retaining ring surface to produce an effective heat transfer path.

The compliant sleeve concept shown in Figure 4.2.1-3 stacks the FSA's in the hollow graphite aeroshell, which is lined with a compliant, fibrous graphite sleeve. This concept, although the least complex as far as the total number of components are concerned,

Figure 4.2.1-2. Central Rod Concept

Figure 4.2.1-3. Compliant Sleeve Concept

Figure 4.2.1-4. Screw-Cap Concept

is the least desirable from thermal performance considerations. In addition, it necessitates reaching into a relatively deep cavity with a manipulator when assembling.

Figure 4.2.1-4 displays the Screw-Cap concept. This concept is considered the best thermal approach, but is the most complicated from an assembly standpoint. This assembly would be accomplished by first placing the graphite retaining post in the retainer shell, then seating the FSA's in the spherical cavities in the post, through a hole in the retainer shell. Graphite covers and screws would then be used to hold the FSA's in the retainer shell, thereby assuring good thermal contact. Buildup of the assembly would proceed in a vertical direction and would produce an integral sub-assembly for insertion into the graphite aeroshell.

4.2.1.2.2 Helipak Weight Tradeoffs

Weight analyses were conducted on the MHW heat source designs similar to that shown in Figure 4.2.1-5. The heat source configuration will not be finalized until definitive test data on two of the primary components, the fuel and the impact shell, becomes available. It is necessary, however, to determine the effect that variations in these two components have on the heat source design and the overall implications of these design variations on the RTG itself. To determine the effects on the heat source, a series of parametric trade-off studies were conducted using the central rod concept shown in Figure 4.2.1-2 as a reference.

To provide maximum design data flexibility in these studies, three general arrangements were analyzed, a 3X matrix, a 4X matrix, and a 5 X matrix system. All of the heat source components were varied to provide a wide range of parametric data. The program variables include:

1. Total heat source thermal loading
2. The type of fuel, PPO or PMC (any density)
3. The number of fuel spheres
4. The thickness of the MgO barrier

18 SPHERES
ARRANGED IN A
3 X 6 X 60° MATRIX

PPO FUEL

18 SPHERES
ARRANGED IN A
3 X 6 X 60° MATRIX

PMC FUEL

Figure 4.2.1-5. MHW Reference Designs

5. The thickness of the iridium post impact shell
6. The thickness of the graphite impact shell
7. The thickness of the retaining ring
8. The thickness of the PG insulator
9. The thickness of the aeroshell
10. The thickness of the outer can

The output of the study includes:

1. Length, diameter and weight of the total heat source
2. Fuel power density
3. Fuel sphere diameter
4. The individual weights of all components

Point Design Tradeoffs

Detailed design tradeoff studies were conducted for heat sources of 2200 and 2600 watts (t) with the following groundrules:

1. Thickness of the outer can	$t_{oc} = 0.010 \text{ in.}$
2. Thickness of aeroshell (POCO AXF-5Q)	$t_{AS} = 0.500 \text{ in.}$
3. Thickness of PG insulator	$t_{PG} = 0$
4. Thickness of retaining ring, (Pyrocarb 406)	$t_{RR} = 0.200 \text{ in.}$
5. Thickness of post-impact shell (iridium)	$t_{Ir} = 0.020 \text{ in.}$
6. Thickness of MgO barrier	$t_{MgO} = 0.020 \text{ in.}$

Variables considered include:

1. Type of fuel - PPO and PMC
2. Fuel Power Density - PPO: 3.48 w/cc
3.79 w/cc
PMC: 3.20 w/cc
3. Number of spheres
4. Impact shell thickness, t_{IS}

Figure 4.2.1-6 presents the dimensional characteristics of the fuel as a function of fuel sphere thermal loading. These tradeoff data, both weight and dimensional characteristics (length and diameter) are presented in Figures 4.2.1-7 through 4.2.1-16 for the 2200 w(t) design and in Figures 4.2.1-17 through 4.2.1-22 for the 2600 w(t) design, for various fuel sphere sizes. The thickness of the impact shell is the most significant variable; a difference of 0.25 inches in this variable can change the heat source weight approximately 11.5 pounds. The following rule of thumb can be used: a 0.050 inch change in the radius of the fuel sphere assembly, FSA, changes the total heat source weight $\approx$ 2.0 pounds. For a given impact shell thickness, changing from a 3X to 4X, 4X to 5X, increases the heat source weight approximately 4.5 pounds.

If the fuel density of PPO can be increased from 3.48 watts/cc to 3.79 watts/cc, approximately 1.0 pounds weight reduction results for a given sphere size and impact shell thickness. If the fuel changes from PPO (3.48 w/cc) to PMC (3.2 w/cc), a 3.5 pound weight increase results.

The overall result of this study is summarized in Figure 4.2.1-23. The previous Figures 4.2.1-6 through 4.2.1-22, provided data of the characteristics of the heat source with varying impact shell thickness for each fuel sphere size. In reality, the impact shell thickness is reduced as the fuel sphere size is reduced. The data presented in Figure 4.2.1-23 are based on a calculated required impact shell thickness for each sphere size. The data were developed using a semiempirical energy balance technique.

Figure 4.2.1-6. PPO Fuel Sphere Characteristics

Figure 4.2.1-7. Effect of Fuel Sphere Size on Total Heat Source Weight

Figure 4.2.1-8. Heat Source Geometry

PPO FUEL -3.48 W/CC (76.2% T.D.)

4X MATRICES

2200 W_t

Figure 4.2.1-9. Effect of Fuel Sphere Size on Total Heat Source Weight

Figure 4.2.1-10. Heat Source Geometric Characteristics

Figure 4.2.1-11. Effect of Fuel Sphere Size on Heat Source Weight

Figure 4.2.1-12. Heat Source Geometric Characteristics

Figure 4.2.1-13. Effect of Fuel Sphere Size on Total Heat Source Weight

Figure 4.2.1-14. Heat Source Geometric Characteristics

Figure 4.2.1-15. Effect of Fuel Sphere Size on Total Heat Source Weight

Figure 4.2.1-16. Heat Source Geometric Characteristics

PPO FUEL -3.48 W/CC (16.2% T.D.)
 3X MATRICES
 2600 W_t

Figure 4.2.1-17. Effect of Fuel Sphere Size on Heat Source Weight

Figure 4.2.1-18. Heat Source Geometric Characteristics

PPO FUEL -3.48 W/CC (76.2% T.D.)
 4X MATRICES
 2600 W_t

NUMBER OF SPHERES:	(4X20)	(4X15)	(4X10)	(4X8)	(4X7)	(4X6)	(4X5)
SPHERE DIAMETER:	1.028	1.132	1.300	1.400	1.459	1.536	1.632 (INCHES)
FUEL WEIGHT:	0.179	0.238	0.358	0.447	0.511	0.596	0.715 (LBS)

Figure 4.2.1-19. Effect of Fuel Sphere Size on Heat Source Weight

Figure 4.2.1-20. Heat Source Geometric Characteristics

Figure 4.2.1-21. Effect of Fuel Sphere Size on Heat Source Weight

Figure 4.2.1-22. Heat Source Geometric Characteristics

Figure 4.2.1-23. Effect of Fuel Sphere Size on Heat Source Weight
with Required Impact Shell Thickness

Design tradeoff studies on the converter and RTG indicate that the heat source length should be no larger than 18 inches. The effect of this limit is also shown in Figure 4.2.1-23. All designs to the right of cross-hatched line are below 18 inches, while all designs to the left of the line are larger than 18 inches. If the fuel sphere size is decreased from the present 122 watts (t) to below 105 watts (5), a 3X matrix cannot be used, if the length is held to 18 inches. Changes in the matrix from 3X to 4X (90 watts (t)/sphere) result in a 2.5 pound increase in weight, while a change to a 5X matrix (55 watts/sphere) results in a 4.5 pound weight increase. Changing to PMC fuel (3X6) would increase the weight 3.6 pounds.

The results of these studies provide an insight into the effect of component and design variations (fuel size, impact shell thickness and matrix buildup) on the heat source weight and geometries. In addition to weight variations and the resulting effect on the specific power of the RTG, it is estimated that increasing the number of spheres from 18 to 40 also increases the cost of producing the heat source by 75 percent.

Effect of Heat Source Thermal Loading

An additional tradeoff study showing the effect on heat source characteristics, weight and geometry, of varying fuel loading using a 3X matrix system, was made. These data are presented in Figures 4.2.1-24 and 4.2.1-25. For a given impact shell thickness, the variations of weight and geometry with fuel loading, are linear. For a 3X matrix system, for the larger thermal loadings 2400 w(t), the lengths increase rapidly. If higher powered systems are required, a system other than a 3X matrix would be desirable.

An alternate design approach is presented in Figure 4.2.1-26. The lower curve, optimized designs, is the 0.5 inch, t_{IS} curve from Figure 4.2.1-25. The upper curve assumes a fixed geometry configuration, being compensated with additional thicknesses of the dimensional compliance shell. As can be seen, there would be a 10 pound weight penalty, using this technique, to obtain a 2200 watt(t) heat source with a system designed to accommodate 2800 watts. This approach, fixed geometry for varying heat source thermal loading, is an effective design technique to compensate for small variations (± 50 watts (t)) in heat source thermal loading requirements, and provide design flexibility with nominal design change, but should

Figure 4.2.1-24. Effect of Impact Shell Thickness on Heat Source Weight

Figure 4.2.1-25. Effect of Impact Shell Thickness on Heat Source Size

Figure 4.2.1-26. MHW Heat Source Weight Tradeoff

not be used when larger variations are desired. If wide variations in thermal loading are required, a point design approach should be followed to minimize weight and thereby produce the most effective RTG specific power.

4.2.1.2.3 Alternate Spherical Fuel Configurations

Based on the potential problem of thermal stress failure of large PPO spheres, a study was conducted to evaluate the effect of varying the size and configuration of fuel packaged in an effective sphere of 1.60 inch diameter. The types of configurations considered included:

1. Small spheres (0.010 to 0.80 inch diameter)
2. Segmented spheres - included "orange slice" arrangement (2-8 pieces), as well as small sphere encased in two concentric hemispheres.
3. Stacking of interlocked pucks

Comparative data was obtained relative to overall heat source configuration and weight due to variations in the fuel configuration. Figure 4.2.1-27 illustrates the extent of the effort as well as some of the imposed limitations.

The effectiveness of the multiple spheres per shell concept is directly related to the effective packing fraction attainable within a 1.60 inch diameter spherical shell. A low packing fraction reduces the effective power density of each fuel sphere assembly with a corresponding weight penalty. Figure 4.2.1-28 shows the variation of this packing fraction (P.F.) as a function of the sphere size within a constant 1.60 inch shell. Discrete graphical solutions were obtained for less than ten spheres per shell; empirical data was used for the small spheres. The results of study indicate that the best P.F that could be achieved, even with minute spheres, is about 70 percent. For the sphere diameters, a P.F of 55 percent was concluded as being representative. With a 55 percent packing fraction, the number of fuel sphere assemblies required would be approximately doubled, with the size and weight of the heat source increasing accordingly.

Figure 4.2.1-27. Helipak Arrangement

The segmented sphere approach was evaluated by varying the iridium clad thickness from 21 mils to 60 mils, in 10 mil increments, to increase the clad capability relative to shear loading at impact, should it be required. Table 4.2.1-1 summarizes these results.

Table 4.2.1-1. Weight Effect of Varying Ir Clad Thickness

Matrix	Weight	Crush-up Thickness (in.)	Ir Thickness	ΔWeight-Pounds (Over Reference Design)
3 x 6	2200	0.400	0.20	0 (ref)
3 x 6	2200	0.400	0.30	+ 1.7
3 x 6	2200	0.400	0.40	+ 3.4
3 x 6	2200	0.400	0.50	+ 5.2
3 x 6	2200	0.400	0.60	+ 7.0

Figure 4.2.1-28. Packing Fraction vs Number of Spheres/Shell

The stacking of pucks in a spherical shell makes the packing fraction a function of the number of different size pucks within the shell. The feasibility of truncating the cylindrically shaped puck to obtain maximum utilization of volume is also an important factor. Examples of the extent of packing fraction variations are shown in Figure 4.2.1-29a, b, c and d. For this concept, a P.F of 85 percent was used, since some crush-up volume within the shell would provide some beneficial effects at impact. Table 4.2.1-2 summarizes the weights of these stacked puck systems with the Ir clad thickness increased to 40 mils to accommodate potential shearing effects of the pucks at impact.

Table 4.2.1-2. Stacked Puck Helipak Weights

Matrix	Weight	P. F. (%)	Thickness Ir (in.)	Δ Weight (Pounds*)
3 x 6	2200	85	0.040	+ 3.5
3 x 7	2200	85	0.040	+ 4.8
3 x 8	2200	85	0.040	+ 6.1
3 x 9	2200	85	0.040	+ 7.4
3 x 10	2200	85	0.040	+ 8.6

* (Over single 1.60 diameter fuel sphere)

To alleviate the potential shearing effects of the pucks at impact, the pucks could be interlocked using male and female wedges in each puck. This concept would, however, have to be evaluated to determine its feasibility before an assessment of its potential advantages could be made.

4.2.1.2.4 Alternate Heat Source Concepts

In addition to the reference Helipak designs discussed above, several alternate concepts, using standard fuel pucks were also evaluated for MHW application. These included:

- 4 SIZES
- 510 PUCK SHAPES

$$\underline{P.F. = 81\%}$$

a)

- 3 SIZES
- 1 MODIFIED PUCK

$$\underline{P.F. = 85\%}$$

b)

- 2 SIZES
- 2 MODIFIED SHAPES

$$\underline{P.F. = 91\%}$$

c)

- 4 SIZES
- ALL MODIFIED SHAPES

$$\underline{P.F. = 93\%}$$

d)

Figure 4.2.1-29. Puck Quantity and Shape Variations as Affecting Packing Fraction

1. Individually Protected Fuel Pucks (IPP)
2. Refractory Metal Design
3. Graphite Noble Metal Concept (GNOME)

Individually Protected Fuel Pucks

Several IPP configurations were evaluated for use with the reference MHW converter design. The first IPP design concept is that shown in Figure 4.2.1-30(a), which is the standard 40-watt SSC fuel puck. Fifty-five of these discs would be required to attain the 2200 watt thermal loading for MHW operation. A "stacked" heat source configuration, as shown in Figure 4.2.1-31(a) would be 43.5 inches long which is beyond the allowable heat source envelope for the MHW converter. An effort was made to reduce this length by considering two 40-watt pucks in a single re-entry shall. While this concept significantly reduces the overall heat source length, as shown in Figure 4.2.1-31(b), it is still too long for integration with the present MHW converter design.

The second IPP concept considered was the "Domino" configuration shown in Figure 4.2.1-30(b). This configuration utilizes the most effective radiative area of the pucks by orienting them in a planar array facing the thermoelectric hot shoes. To maintain the same thermal loading (watts/inch) within the converter, as the Helipak concept, it was necessary to use four 46 watt pucks rather than the standard 40 watt SSC pucks in a single heat shield. Figure 4.2.1-32 shows the "Domino" blocks integrated into a heat source. This concept integrates quite well into the present MHW converter envelope with reasonable system weights.

Another IPP configuration considered was the "Four Leaf Pallet" concept shown in Figure 4.2.1-30(c). This concept utilizes the standard 40 watt SSC fuel puck and is integrated into the present MHW converter, as shown in Figure 4.2.1-33. The weight of this concept, is, however, approximately 20 percent higher than the "Domino" concept.

Integration of these IPP designs into a heat source pose a problem since the main advantage of the IPP lies in its inherent lower environmental (both re-entry and impact) effects, which, in turn, requires that release of the individual pucks must occur prior to re-entry.

Figure 4.2.1-30. Conceptual Designs of Individually Protected SSC Fuel Pucks

(a)

(b)

Figure 4.2.1-31. Conceptual Heat Source Designs Using 40W and 80W Discs

ESTIMATED WEIGHTS

12 DOMINO FUEL BLOCKS	34.0*
CLAMP STRUCTURE (LESS TIE ROD)	1.0*
CENTERING TUBE	1.4*
TOTAL WT IN GENERATOR	36.4*
TIE ROD	7*
TOTAL HEAT SOURCE WT	43.4*

'DOMINO' CONCEPT
HEAT SOURCE

Figure 4.2.1-32. Conceptual Design of 2200 W Heat Source Using "Domino" Fuel Blocks

Figure 4.2.1-33. Conceptual Design of 2200 W Heat Source Using "Pallet" Fuel Blocks

Re-entry performance of the individually protected pucks (Figure 4.2.1-30(a)) were evaluated considering both standard 40-watt pucks and new 92-watt pucks in single reentry shells. The pucks were evaluated for both edge-on and side-on orientations. These results are presented in Section 4.2.2.

The effect of the integration of IPP concepts into a heat source is significant. The MHW-RTG has a ballistic coefficient of 35-50 psf, depending on the particular design, while the pucks (face-on) have β which produces a relatively low re-entry thermal environment thereby making the individual puck concept feasible. If, however, during re-entry, the generator does not disassemble until late in the heating pulse, the individual puck could be exposed to a potentially severe thermal shock situation.

For the possibility where the generator would remain an entity but with large holes in the outer shell exposing the fuel puck flat surface to a heating environment by a β of 35-50, which is 3-7 times greater than that of the fuel puck, liner overtemperature and possibly puck aeroshell burn-through would result.

Structural integration requirements must also be considered. These specify that the RTG (and hence the heat source) must be capable of surviving the dynamic launch environment. Therefore, an integrated RTG with a β of 35-50 could remain an entity during re-entry, thereby producing a situation similar to that discussed above.

The Helipak and GNOME (discussed below) designs are not sensitive to these situations since their β 's are 40-50 (comparable to the RTG); these heat sources are designed for that re-entry thermal environment.

Table 4.2.1-3 summarizes the advantage and disadvantages of the IPP concepts.

Table 4.2.1-3. Advantages and Disadvantages of Individually Protected Fuel Pucks

Advantages	Disadvantages
<ul style="list-style-type: none"> • Less severe re-entry heating environment in flat-on, flat-on tumbling and edge-on spinning modes. • Significantly reduced impact velocity. • Flexibility and modularity. • Lends self to testing in existing facilities. (Including superorbital arc testing.) • Potential to be light weight heat source. • Post-impact temperatures are reduced. 	<ul style="list-style-type: none"> • Subsonic behavior (temperature and recession) requires considerable investigation. • Subsonic flight time and impact point are very difficult to predict (latter means large possible area of impact). • Potential thermal stress failures due to gradients in two directions. • Aerodynamic behavior must be established with emphasis on possibility of edge-on stable re-entry. • Internal gas management is difficult and one PRD per puck would be required if helium internal control was required. • Substantial amount of testing required. • Integration is difficult <ul style="list-style-type: none"> • Positive release required • Low β into high β.

Graphite Noble Metal Concept (GNOME)

Based on the successful impact test results of spherical elements. The impact absorption capability of composite graphites was demonstrated. A design incorporating a composite graphite system, Pyrocarb 406, as the strength member with an iridium liner adjacent to the fuel, and POCO as the ablator was evolved. A design utilizing this approach is shown in Figure 4.2.1-34. The graphite crushup thickness is estimated to be adequate to successfully bring the iridium through impact velocities up to 380 fps. The thermal performance of this concept is discussed in Section 4.2.

Figure 4.2.1-34. GNOME Impact Protection Concept 2200 Watts

Because of the crushup materials high thermal diffusivity, the iridium liner temperatures during reentry were found to be relatively low and insensitive to changes in crushup thickness. It may be possible therefore to use Pt30Rh as an alternate fuel liner if at least 1.00" of crushup is used; the use of iridium, however, reduces the amount of side crushup material required for impact protection.

Figures 4.2.1-35 and 4.2.1-36 show the size and weight variations that result from varying the aeroshell configuration (single vs double) as well as the applicable crushup thickness for both SSC and PPO fuel pucks.

The GNOME is the recommended backup design to the Helipak heat source.

4.2.1.2.5 Reference Design

Based on the results and evaluation of analyses and tests reported in this section, the reference design was updated to the configuration shown in Figure 4.2.1-37 for PPO and

Figure 4.2.1-35. GNOME Heat Source Configuration-Single vs. Double Aeroshell - SSC Fuel

- PYROCARB END CRUSH-UP (1.65")
- LENGTH - 20.56"
- WEIGHT: HEAT SOURCE - 43.16 LB
CONVERTER - 32.00
75.16

- POWER - 159 WATTS
- SPECIFIC POWER - 2.12 WATTS/LB
(MUST USE IRIIDIUM)

- PYROCARB END CRUSH-UP - 1.25"
- LENGTH - 16.32"
- WEIGHT: HEAT SOURCE - 32.6 LBS
CONVERTER - 32.0 LBS
64.6

- POWER - 159 WATTS
- SPECIFIC POWER - 2.46 WATTS/LB
(MUST USE IRIIDIUM)

Figure 4.2.1-36. GNOME Heat Source Configuration-PPO vs. SSC Fuel

Figure 4.2.1-37. MHW Reference Design 2400 Watts

PMC fuel forms. Details of the PPO heat source are shown in Figure 4.2.1-38. A weight breakdown is given in Table 4.2.1-4.

The PPO fuel sphere size used during most of the evaluation work was 1.60 inches diameter, 122 watts loading based on 80% nominal density as defined by the fuel fabrication development agency, LASL. This was based on a heat source thermal load of 2200 watts, utilizing 18 spheres in a 3 x 6 matrix. When the converter requirements dictated an increase to 2400 watts, the fuel sphere diameter was reduced to 1.564 inches with a thermal load of 114.3 watts. The spheres were arranged in a 3 x 7 matrix. Maintaining the 3 x 6 matrix for a 2400 watt thermal loading would have meant using a sphere of 133.3 watts and a diameter larger than 1.60. Development and fabrication of the fuel spheres at LASL indicate that this latter size is not advisable since it causes fabrication problems and increased thermal stress problems (sphere cracking potential is increased).

Table 4.2.1-4. Weight Summary

Component	Weight (lbs)
Fuel (21 @ 0.629 lb each)	13.42
Impact Shell (21 @ 0.283 lb)	5.94
Post-Impact Shell (21 @ 0.138 lb)	2.85
MgO (21 @ 0.0203 lb)	0.42
Aeroshell (10.40 in.)	10.58
Insulator (0.050 in.)	1.18
Retaining Ring	2.38
Outer Can	3.63
End Crushup	1.30
Miscellaneous Hdwe	<u>0.48</u>
Heat Source Total Weight	42.78

Figure 4.2.1-38. 2400 Watt Helipak Design - 3 x 7 Matrix

The iridium shell was maintained at 0.02 inches thick based on the successful impact tests without breaching. (See Section 4.2.1.3.2.4). The shell was sized to accommodate the largest possible PPO fuel sphere (lowest density of 76.5% made by LASL) so that no thermal stresses occur in the iridium during reentry. No internal or external barriers are required as explained in Section 4.2.1.1. Since compliancy is desired to minimize dynamic loading, two types were considered, 1) an iridium spring made of corrugated strips of foil which could provide true compliancy and 2) refractory oxide "shims" which only reduce the gap between the fuel and iridium to some minimal value. HfO_2 was considered best from the compatibility standpoint, but MgO was actually the prime candidate since it is easier to fabricate than HfO_2 . Various diameters and thicknesses would have to be made and stocked so that they could be selectively used as required by dimensionals of the individual fuel spheres and iridium shells. The iridium corrugated spring is the desired approach. Because of the manufacturing and assembly difficulties the MgO and HfO_2 "shim" size shells are considered backups. The evaluation of the compliancy member will be completed during the next program phase.

The helium will be vented from the iridium post impact containment shell through two (2) holes each less than 1 mil in diameter. The size of this hole is also sufficient to maintain an internal oxygen pressure inside the shell thus keeping the fuel stoichiometry from changing. It also prevents the loss of plutonium by vaporization to outside the shell. Particulate containment will be investigated during the next program phase but the current concept is to have a refractory oxide coating in and over the vent hole with an iridium shield over the oxide to prevent a direct view from the fuel to the outside. A conceptual drawing of this approach is shown in Figure 4.2.1-39. Plutonium escape tests will be conducted during the next program phase to determine the design sophistication required, before a design is finalized.

The impact shell size changed during this period as additional data from the impact tests became available. The thickness of 0.400 inches is based on the evaluation of impact test on the successes of bare and iridium clad ThO_2 fuel simulant spheres with a thickness of 0.50 inches and the failure at 0.375 inches. The evaluations of types of material impacted

Figure 4.2.1-39. Post-Impact Subsystem

led to the selection of a T-50 wound yarn graphite composite. The evaluation of infiltration and impregnation as the fabrication method, additional types of high strength yarn, bulk density and fabrication techniques will be made during the next program phase.

The graphite integration hardware consisting of the compliance pads, the retaining ring, the centering rod, and the end crush-up pads are as shown in Figure 4.2.1-38. Only the compliance pads have been evaluated (See Section 4.2.1.3, 4.2). The material to be used is layered cloth woven from two different moduli graphite fibers. The other components will be evaluated during actual hardware assembly evaluation and in the case of the end crushup, during the heat source system impact tests.

The POCO AXF-5Q/aeroshell has been reduced in thickness to 0.400 inches since the increase in diameter from the Phase I Helipak design results in lower heating and ablation. An insulator or pyrolytic graphite (PG) 0.050 inches thick has been added to reduce the maximum iridium post-impact shell to a temperature of $< 2090^{\circ}\text{C}$. This was necessary since a eutectic melt of iridium/graphite at 2210°C was identified and a minimum margin of 110°C on iridium melt is desired. The PG insulator will be deposited directly on the inside wall of the POCO aeroshell. A backup design would utilize a separately formed shell.

The outer can will be an iridium shell, 0.010 inches thick. The refractories such as WC3015, T-111, Ta 10W, Mo50Re and TZM are not as desirable because of low oxidation resistance or incompatibility with graphite which would require barrier coatings. These materials will be further evaluated, along with iridium, during compatibility tests with the SiGe thermocouples. One end of the outer can will have an accommodation for handling and a pressure relief device which will vent helium from the can.

These items are not shown in Figure 4.2.1-38. The side walls of the outer can will have a coating of HfO_2 iridium cermet to provide an emissivity of > 0.80 . The venting will not be into the converter internal cavity but through a tube, which will penetrate the converter wall, to outside the converter. The vent will be a capillary type which will maintain an internal helium pressure of 0.1 to 0.4 atmospheres and will contain particulate. The multiple disc type with minute holes is the selected design with a spiral capillary cut into a thin disc as the backup.

The present status of the design does not reflect the final system weight and dimensional envelope. These items will be finalized after completion of the optimization impact tests. These tests will define the iridium post-impact shell thickness and the graphite impact shell, the latter being the most critical parameter with respect to weight and size.

4.2.1.3 Heat Source Materials Development

4.2.1.3.1 Post-Impact Shell Development

The MHW-RTG Phase I Final Report (Reference 4-1) specified Iridium as the primary candidate for the post-impact containment shell. The use of Iridium in this application improves the overall performance capability of the heat source, as its melt temperature is well in excess of 4000⁰F. Previously available test data on Iridium, from Battelle Memorial Institute⁽¹⁾ indicated that Iridium was compatible with the ceramic fuel form (PPO), thereby eliminating the requirement for a barrier at this interface. The BMI data indicated that for either PMC or SSC fuel forms, which contain molybdenum, a barrier would be required between the fuel and the Iridium. To ensure that the conclusions drawn by BMI are valid at MHW re-entry temperatures, as well as to evaluate the performance of the overall combinations of candidate heat source materials with respect to each other, two separate series of material compatibility tests, with and without fuel, were initiated. These tests were designed to provide data not only on the compatibility of the prime heat source materials with respect to the fuel and with each other, but also on the ability of several candidate barrier materials to eliminate or reduce intermaterial reactions, should they occur. The non-fuel materials compatibility evaluation was performed at GE while the fueled material tests were performed at Los Alamos Scientific Laboratories (LASL) and Donald W. Douglas Laboratories (DWDL).

In addition to the basic materials compatibility evaluation between components and the fuel during normal operation and re-entry, the ability of the component materials to perform under the environmental conditions encountered during ground handling and post-impact were also evaluated. To determine the performance of the candidate materials oxidation and emissivity tests, in the temperature regimes associated with these conditions, were performed. Furthermore, as part of the evaluation of candidate outer can materials, a compatibility test series with the thermoelectric material, silicon germanium, was also performed by RCA.

¹ Poberskin, M., "Mid-Year Report on Development Program for Fabrication of Composite Fuel Form of ²³⁸PuO₂," BMI-1831, Battelle Memorial Institute, Columbus Laboratories March, 1968

4.2.1.3.1.1 Materials Compatibility. As noted above, the material compatibility tests were divided into two phases: non-fueled and fueled. In both phases, Iridium was the primary component material, with Pt30Rh as a backup. The compatibility investigations were directed toward evaluating the compatibility of these base materials with graphite, which is the impact and re-entry material, with several potential barrier material candidates, with several refractory alloys of molybdenum and molybdenum itself. The results of the non-fuel compatibility tests are discussed below; the fuel material test program has not yielded any results during this reporting period. The fuel materials program was discussed in Section 4.1.2. A total of 20 non-fuel, material compatibility crucibles were tested for 500 hours at 2500⁰F (normal heat source operating temperature).

The non-fuel material test matrix is shown in Table 4.2.1-5 , Figures 4.2.1-40 and 4.2.1-41 show the configurational stack-ups for the test crucibles. Table 4.2.1-6 outlines the test objectives for the individual crucibles.

All crucibles were sectioned so that the individual component interfaces could be metallographically examined at high magnification. Detailed discussions on test configurations and the performance of the various materials are presented below.

A long time objective of the MHW program has been the identification of materials having compatibility with graphite at elevated temperatures (~ 2500⁰F), as well as having oxidation resistance at temperatures representative of pre-launch and post-impact conditions (~ 1600⁰F). Only the noble metal family of materials fulfills these basic requirements.

Pt30Rh was originally considered as the primary capsule material. A compatibility program was conducted during Phase I to study the behavior of this material when coupled with carbon or refractory metals (T-111, TZM and silicide coated WC3015), both in direct contact, as well as when shielded from these materials by plasma-sprayed refractory oxide barriers.

Table 4.2.1-5. Compatibility Crucibles

Crucible No.	Matrix					
	1	2	3	4	5	
1	ThO ₂	ThO ₂	Ir	ThO ₂	--	
2	Mo	--	Ir	Mo	3 TZM spacers	
3	BeO	BeO	Ir	BeO	--	
4	MgO	MgO	Ir	MgO	--	
5	ZrO ₂	ZrO ₂	Ir	ZrO ₂	--	
6	HfO ₂	HfO ₂	Ir	HfO ₂		
7	Mo50Re ⁽¹⁾	--	Ir	Mo ⁽¹⁾	3 TZM spacers	Mo50Re Pt Mo
8	Mo50Re	--	Ir	Mo50Re	3 TZM spacers	
9	Mo50Re/TiC ⁽²⁾	--	Ir	HfC/Mo50Re ⁽²⁾	3 TZM spacers	Mo50Re TiC Ir Mo50Re HfC
*10	Graphite	--	Ir	Graphite	--	
*11	TaC	TaC	Ir	TaC	--	
12	TiC	TiC	Ir	TiC	2 graphite discs top & bottom between TiC and TZM	
13	ZrC	ZrC	Ir	ZrC	2 graphite discs top & bottom between ZrC and TZM	
*14	Graphite	--	BeO Disc	Graphite	--	
15	Graphite	--	HfO ₂ Disc	Graphite	--	
16	TiC	TiC	Mo50Re	TiC	--	
17	TaC	TaC	Pt30Rh	TaC	2 graphite discs top & bottom between TaC and TZM	
18	TiC	TiC	Pt30Rh	TiC	2 graphite discs top & bottom between TiC and TZM	
19	ZrC	ZrC	Pt30Rh	ZrC	2 graphite discs top & bottom between ZrC and TZM	
20	BeO/Ir	--	Pt30Rh	Ir/HfO ₂	--	BeO Ir Pt30Rh Ir HfO ₂

Notes:

Crucibles 1 to 9, incl., 12 and 13, 16 to 20, incl., are TZM

*Crucibles 10, 11, 14 and 15 are graphite

(1) Both Mo and Mo50Re contain a MoPt intermetallic layer adjacent to Ir

(2) Ir and Mo50Re interfaces contain sputter coated TiC and HfC as indicated

Test conditions 500 hrs at 2500 °F in 10⁻⁶ torr vacuum

Figure 4.2.1-40. Crucible and Component "Stack-Up" for the Following Test Numbers - 1, 3-6, 11-13, and 16-19

Figure 4.2.1-41. Crucible & Component "Stack-Up" for Crucibles Numbers - 2, 7-10, 14, 15 and 20

Table 4.2.1-6. Test Objective for Short Term Compatibility
Testing Program

1. Evaluation of Direct Interaction between Components
 - (a) Mo and Ir: Crucible #9
 - (b) Graphite and Ir: Crucible #1
 - (c) Mo50Re and Ir: Crucible #10
2. Evaluation of Barriers Preventing Interaction between TZM and Graphite
BeO: Crucible #11 and #20
3. Evaluation of Barriers Preventing Interaction between Noble Metal and Graphite
 - (a) TaC High Density : Crucible #2 and #3
 - (b) TiC : Crucibles #4 and #5
 - (c) ZrC Pressed & Sintered : Crucibles #6 and #7 and #19
 - (d) ZrO₂ : Crucible #16
 - (e) HfO₂ Coupons : Crucible #17 and #20
4. Evaluation of Barriers Preventing Interaction between TZM and Noble Metal
 - (a) BeO: Crucibles #12 and #13
 - (b) TiC: Crucible #14
 - (c) MgO: Crucibles #15
 - (d) Ir/Mo Intermetallic Coating: Crucible #18
 - (e) ThO₂: Crucible #8

For these evaluations, a series of crucibles were exposed to a 10^{-6} torr vacuum at 1540°C for 500 hours and metallographically evaluated. The pertinent results of the experiment are as follows:

1. A barrier is required to prevent interdiffusion between Pt30Rh and carbon or refractory metals.
2. Plasma sprayed refractory metal oxides (HfO_2 , and ThO_2) do prevent diffusion between refractory metal alloys but not between Pt30Rh and the refractory metal or carbon.
3. The melting point of Pt30Rh alloy is drastically reduced when contaminated by small quantities of silicon present in the oxides or by silicon and iron present in a silicide coating (Table 4.2.1-7).
4. Pt and Rh vapors appear to assist transport of carbon.
5. A Pt_3Mo intermetallic barrier on a Mo alloy is an acceptable barrier to the diffusion of platinum.

These test data coupled with other data obtained from TRW (Table 4.2.1-8) appear to make use of a Pt30Rh alloy, a high risk for MHW application.

For the base program, testing of Pt30Rh was continued but on a limited basis to determine the effect of using higher density oxides as barriers, rather than the relatively porous oxides that were plasma-sprayed during Phase I.

Reactions might also be minimized by interfacing the Pt30Rh with carbides rather than directly with carbon. Table 4.2.1-9 indicates the type of capsule interfaces visualized at time of Series 2 compatibility test initiation. In general, test coupons were 0.500 inch diameter and 0.020 inch thick. Barrier materials were fabricated to prevent solid contact between test coupons and the crucible walls. All oxides were pre-fired in air at 2000°F to assure removal of surface impurities.

The purity of starting material oxides were 99.97 percent, or better. Pressing and sintering developed densities greater than 90 percent, theoretical. The purity of all TiC carbide

Table 4.2.1-7. Compatibility and Characteristics of Materials of Interest to the MHW Capsule Program

Observation - Temperature (°C)							
COUPLE		REACTION	NO REACTION	EUTECTIC	SOLID SOLN.	OXIDE MELT TEMP.	OXIDE THERMAL EXP. ($\times 10^{-6}/^{\circ}\text{C}$)
(1) PuO ₂ vs.	MgO			2250-2300	1750	2800	13.5
	BeO	1750		2000		2570	8.9
	HfO ₂	2000*		2500	1500	2760	8.5
	ThO ₂		1250		1500	3300	9.0
	Pt10Rh	1200				~1870	8.9
	Mo		2000			2625	5.6
	Ir	1500	1250			4450	6.8
(2)(4) Mo vs.	Pt	1095		1925			
	Ir	1370 ⁽⁵⁾		2080			
	MgO	1600-1800					
	BeO	1700-1900					
	ThO ₂	2135					
	HfO ₂		2300				
(3) C vs.	Pt	1205		2180			
	Ir		2205	2295			
(3) Si vs.	Pt			830			
	Ir			1470			
(3) B vs.	Pt			800			
	Ir			1046			

(1) W. M. Pardue, et al, BMI-1869, Battelle Final Report

(2) S. J. Burnett, "Properties of Refractory Metals," AERE-R-4657, JKAEC, 1969

(3) J. Matthey Bishop, Vol. 1 No. 4, 1957, and Vol. 10 No. 1, January 1966

(4) W. D. Conrady, Technical Doc. Report No. ML-TDR 64-204, Vol. 1, AD627139, April 1965, Wright Patterson

(5) IPSO Compatibility Program

Table 4.2.1-8. Literature Search for Compatibility of Candidate Fuel Capsule Systems

REACTION ⁽¹⁾ (°F)		1388	1388	1600	1950	2000	None		2000 PtAl ₂ O ₃ 1200	
TRANSIT	Fuel	Ta10W	T-111	Pt20Rh	PtAl ₂ O ₃	Graphite Ins	Graphite Ab1	Inconel-X		
OPER. (°F)	1602	1489	1453	1424	1337	1266	952	942		
REENTRY (°F)	2641	2534	2559	2600	2600	2660	3954			
IMPACT			2372							
REACTION (°F)		1388	1388	1600	2200					
PIONEER	Fuel	Ta10W	T-111	Pt20Rh	ZrO ₂ Ni	Graphite				
OPER. (°F)	1646	1535	1503	1476	1151	1097	Vent and Switch			
OPER. (°F)	1755	1640	1605	1570	1275	1085	Vent Only			
REENTRY (°F)		3103	3133	3198	3384 (internal)	3978 (internal)				
REACTION (°F)	1	1388	1388	1600	1600	1950	1600	3000	2400**	3200*
IBHS	Fuel	Ta10W	T-111	PtAl ₂ O ₃	Pt20Rh	PtAl ₂ O ₃	Ta Felt 2183 2946 1/32 in. gap	TZM 2183 2946	ZrO ₂ Ni	AXE(as) Q
OPER. (°F)	2255	2240	2193	2183	2183	2183	2183	2183	2136	2081
REENTRY (°F)	2423	2723	2818	2946	2946	2946			3368	4318

* Ni incompatible with carbon at 1300°F

** Ni incompatible with Ta at 1300°F

Table 4.2.1-9. Candidate Fuel Capsule Interfaces

SYSTEM MODEL:

COMPONENT LAYERS PROGRESSING TOWARD GRAPHITE

NOTE: M = MELT R = REACTION ϵ = EUTECTIC

specimens was better than 99.5 percent, while the remaining carbides were 99.8 percent pure (refractory metal plus oxide). The Iridium specimens were 99.95 percent pure; the maximum non-noble metal impurity in the Iridium is iron, at 150 ppm. The Iridium test coupons were received in a slightly bowed condition, which made 100 percent solid/solid contact impossible. The surfaces of the Iridium were in the as-fabricated condition (wavy) and the edges were slightly delaminated as a result of sizing to order requirements. The surfaces of the PtRh test specimens were flat with a finish of 16 RMS, or better; non-noble metal contaminants totaled less than 500 ppm with no single containment greater than 100 ppm. Graphite crucibles were fabricated from 99.9 percent pure Pyrocarb 406 that was outgassed in vacuum at 1500°C, prior to test.

Full contact between crucible and test coupons was obtained by handtightening threaded crucible caps. Crucible threads and vent holes allowed escape of vapors, developed during test, to simulate actual capsule conditions. The tests were performed in a Brew Furnace which maintained crucibles at a temperature of 1370°C and a partial pressure of approximately 10^{-6} torr for 600 hours. Tantalum sheet was used (external to crucibles) to getter crucible vapors, thereby exaggerating the type of reducing atmosphere expected during MHW operation.

4.2.1.3.1.1.1 Test Results

Crucible #1 (ThO_2 -Ir- ThO_2) - Figures 4.2.1-42 through 4.2.1-45 show the metallography and electron-probe results obtained from the destructive evaluation of Crucible #1.

Diffusion of ThO_2 (Th) into Iridium is evident; there was no evidence of significant diffusion of Ir into ThO_2 . It appears that ThO_2 is stable with TZM at 1370°C, in vacuum.

In regions of contact, between Ir and ThO_2 , attack, by the oxide, seems to have progressed through the bulk of the material rather than through grain boundaries. Data in Table 4.2.1-10 suggests that although ThO_2 might cause Ir to melt prematurely, no reaction was predicted at 1370°C.

$\text{ThO}_2\text{-Ir}$

(a) As-Sectioned Crucible #1 (2X)

(b) As-Polished Crucible #1 (6X)

Figure 4.2.1-42. Crucible #1

(a) As-Polished Ir-ThO₂ Interfaces (60X)

(b) As-Polished Ir-ThO₂ Interface (375X)

SPECIMEN CURRENT

Ir X-RAY

ThO₂ X-RAY

(c) Electron-Probe X-Ray of Ir-ThO₂
(Top) Interface (225X)

Figure 4.2.1-43. Crucible #1

NEG #69

93.75X

(a) As-Etched Iridium Coupon Showing Area that is Unreacted with ThO_2

(b) As-Etched View of ThO_2 -TZM (Crucible Base)

Figure 4.2.1-44. Crucible #1

SPECIMAN CURRENT

MO X-RAY

Th X-RAY

Figure 4.2.1-45. Specimen No. 1 TZM -ThO₂ Interface Magnification = 500X

Table 4.2.1-10. Ir-Carbon Compatibility with
Materials of Interest to the MHW Program

Reactant	Ref	Ir		BeO		HfO ₂		Os		MgO		ThO ₂		TaC, ZrC, HfC, TiC
		Start Melt (°C)	Start Reaction (°C)	Start Melt (°C)	Start Reaction (°C)	Start Melt (°C)	Start Reaction (°C)	Start Melt (°C)	Start Reaction (°C)	Start Melt (°C)	Start Reaction (°C)	Start Melt (°C)	Start Reaction (°C)	Start Reaction (°C)
Carbon	(1)	2296	No reaction after 400 hr at 1750				No reaction after 90 min at 2300							
	(2)	2290					No reaction after 2 hr at 2000							
	(3)	2110			1295		1734 to 1776						1640	1200
	(4)	2302												
	(5)	2200	No reaction after 600 hr at 1370		No reaction after 600 hr at 1370		Reduction in 600 hr at 1370				No reaction 600 hr at 1370			
	(8)							2732						
	(9)				Stable to 2300 Vac.						Stable to 1800 Vac		Stable to 2000 Vac.	
	(7)				2300						1800		2000	
	(10)				2300						1800		1950	
Ir	(6)				2-45 min cycles to 2371 and no reaction		2-45 min cycles to 2371 and no reaction					Ir melted after 2-45 min cycles to 2371		
	(5)				Slight discoloration after 600 hr at 1370		No reaction after 600 hr at 1370				No reaction after 600 hr at 1370		Significant reaction after 600 hr at 1370	Significant reaction after 600 hr at 1370
	(7)				No reaction after 90 min at 2300		No reaction after 90 min at 2300				No reaction after 90 min at 2300	Melt point of Ir lowered by ThO ₂	No reaction after 90 min at 2300	

LIST OF REFERENCES

1. S J. Burnett, UKAEA, "Properties of Refractory Materials," AERE-R4657, 1969.

2. High Temperature Protective Coatings for Graphite, Union Carbide Corp., MLTDR 64-173, Parts I through IV, Air Force Matl's Lab., Wright-Patterson Air Force Base, Ohio.

Iridium Base Alloys and Their Behavior in the Presence of Carbon, AFML-TR-66-290, Air Force Matl's. Lab., Wright-Patterson Air Force Base, Ohio

3. J.M. Criscione, R.A. Mercuri, E.P. Schram, A.W. Smith, and H.F. Volk, ML-TDR-64-173, Part II, October 1964.

4. D.P. Harmon, AFML-TR-66-290, "Iridium Base Alloys and Their Behavior in the Presence of Carbon, Aerojet Gen. Corp., February 1967.

M.R. Nadler and C.P. Kempter, J. Phys. Chem., 64 (1960), 1468.

5. IPSO, GE MHW Program.

6. E.W. Filer, "Metallographic Studies of the Compatibility of High Temperature Thermocouple Components, Nuclear Matl's. Propulsion Oper., GE, June 22, 1962.

7. ML-TDR-64-204, Vol. I, "Evaluation of Thermal Protective Systems for Advanced Aerospace Vehicles, April 1965.

8. J. F. Collins, F.F. Juenke, Monthly Technical Progress Report #7, May 3 to May 30, 1971, NSP, GE

Reference #4, above.

9. E. Campbell, "High Temperature Technology," Battelle Memorial Inst. Library of Congress, Catalog Card Number 56-6480.

10. Englehard Industries Technical Bulletin, Vol. VIII, Number 3, December 1967.

Matthey Bishop⁽¹⁾, however, has found that ThO_2 and ZrO_2 and Al_2O_3 tend to dissociate, in the presence of Pt., allowing free metal to react with Pt in an oxygen-free environment at 1200°C ; this does not appear to happen when MgO is in a similar environment. The reaction experienced during these tests (ThO_2/Ir) might have duplicated the results experienced at Matthey Bishop since excellent oxygen getterers were in the system. ThO_2 contaminated Ir appears to be brittle in nature, since the reacted material broke free of the Ir specimen during cool down from test temperature.

After 600 hours at 1370°C , only two or three Ir grains occupy the full 0.020 inch thickness of the starting material.

Crucible #2 (Mo-Ir-Mo) - Figures 4.2.1-46 and 4.2.1-47 show the metallography performed on crucible #2.

There appears to be mutual interdiffusion between Ir and Mo with the movement of Mo into Ir being favored. Ir grain boundaries do not appear to be preferentially attacked by Mo. The 0.020-inch thickness of the Ir appears to be occupied by three or four grains.

Crucible #3 (BeO-Ir-BeO) - The metallography performed on crucible #3 are shown in Figures 4.2.1-48 and 4.2.1-49.

There is evidence of some surface staining and roughening of the Ir which appears to be caused by a superficial surface reaction between BeO and Ir.

The crucible alloy (TZM), which was in contact with BeO throughout the test, appears to have reacted slightly with the oxide. There is evidence that TZM grain boundaries are selectively attacked by the apparent reduction of BeO.

Crucible #4 (MgO-Ir-MgO) - There is no evidence of reaction between MgO and Ir (Figure 4.2.1-50). The 0.020-inch thickness of the Ir specimen is occupied by 3 to 4 grains.

¹ A. S. Darling, G. L. Selman, and R. Rushforth, Johnson Matthey & Co., "Platinum and the Refractory Oxides"

(a) As-Polished Cross-Section of Crucible No. 2 (6X)

NEG #40
93.75X

(b) As-Polished View at Middle of Specimen Length Showing Reaction of Ir and Mo

NEG #41
375X

(c) As-Polished View at Center of Reaction Areas Shown in Figure

Figure 4.2.1-46. Crucible #2

(a) Etched View of Ir-Mo Reaction Zone

(b) Etched View of Non-Reacted Portion of Ir Specimen From Crucible No. 2

Figure 4.2.1-47. Crucible #2

(a) As-Polished View Showing Crucible No. 3 Sandwich

(b) As-Polished View Showing Slight Reaction Zone Between Ir & BeO

(c) As-Polished View of BeO-TZM Crucible Interface

Figure 4.2.1-48. Crucible #3

Figure 4.2.1-49. Etched Section of Ir from Crucible No. 3
that Did Not React with BeO

(a) As-Polished Cross Section of BeO-Ir Sandwich
in Crucible No. 4

(b) Enlarged View of As-Polished Ir-MgO Interface

(c) Etched View of Ir-MgO Interfaces

Figure 4.21-50. Crucible No. 4

MgO reacted slightly with the base of the TZM crucible but there is no observable damage to alloy integrity.

Crucible #5 (ZrO₂-Ir-ZrO₂) - Figure 4.2.1-51 and 4.2.1-52 present the metallography performed on crucible #5.

Y₂O₃ stabilized ZrO₂ is reduced by Ir in vacuum. It appears that zirconium metal is formed insitu permitting interdiffusion of Zr and Ir. The general direction of diffusion appears to be toward the oxide. Less than 0.001 inch of the surface Ir can be detected to have entered into reaction with the oxide. It was not established if the stabilizing agent or the ZrO₂ initiated the interaction; electron probe analysis on a Y₂O₃ stabilized HfO₂, Ir couple (crucible #16, to follow) suggests that the Y₂O₃ is probably the more active material.

Crucible #6 (ZrO₂-Ir-ZrO₂) - Figures 4.2.1-53(a) through (h) illustrate the metallography performed on crucible #6.

This evaluation suggests that unalloyed HfO₂ is stable with Ir, but that Y₂O₃ stabilized HfO₂ and Ir react significantly at 1370°C, in vacuum. Electron-probe rays suggest that the reduction of Y₂O₃ to yttrium metal is followed by the reduction of HfO₂ to hafnium; both metals diffuse into Ir with hafnium probably having the greater mobility. Iridium does not diffuse into the oxide. There appears to have been no reaction between Mo and the oxides.

Crucible #7 (Pt₃Mo Coated Mo and Mo50Re vs. Ir) - Figures 4.2.1-54(a) through (f) summarize the metallography performed on crucible #7.

Prior to compatibility testing, barrier foils were produced by reacting Pt with layers of Mo and Mo50Re foils at 1800°C, for one-minute. Heating was accomplished within the confines of a water-cooled copper coil housed inside an evacuated bell-jar. (Heating to temperature was accomplished in a three-minute period). The refractory metal foils

(a) As-Polished Cross Section of Y_2O_3 Stabilized ZrO_2 - Ir Interfaces

(b) Enlarged View of As-Polished ZrO_2 : Ir Interface at End of Specimen

Figure 4.2.1-51. Crucible No. 5

(a) Enlarged View of As-Polished ZrO_2 :Ir Interface
at Top Center of Specimen

(93.75X)
NEG #82

(b) Etched View of ZrO_2 :Ir Interfaces
at Center of Specimen

Figure 4.21-52. Crucible No. 5

(a) Ir Sandwiched Between Pure HfO_2 and Y_2O_3 Stabilized HfO_2 in Crucible #6.

(b) Enlarged View of Center Portion of Specimens Shown in Figure 14a

(c) Further Enlargement of Stabilized HfO_2 -Ir Reaction Zone Shown in Figure (b)

(d) Enlargement of Pure HfO_2 -Ir Interlace Shown in Figure (b)

(e) Etched but Unreacted Regions of Ir Showing Typical Grain Size of Specimen in Crucible No. 6.

(f) Stabilized HfO_2 -TZM (Crucible No. 6) Interface

Spec #6 HfO_2 -Ir Interface 225X

Spec #6 Y_2O_3 Stabilized HfO_2 -Ir Interface 225X

(g) Electron Probe Analysis of Ir/Oxide Interfaces in Crucible No. 6

SPECIMEN #6 TZM- Y_2O_3 STABILIZED HfO_2 INTERFACE. MAGNIFICATION = 500X

(h) Electron Probe Analysis of Crucible No. 6

(a) Crucible No. 7 Ir Captured Between Mo and Mo50Re Pre-Reacted with Pt.

(b) Enlarged View of Center Specimen Interfaces

SPECIMEN #7. Pt PLATED mo-Ir INTERFACE MAGNIFICATION = 270X

(c) Electron-Probe Analysis of Crucible No. 7

(d) Pt Plated MoRe-Ir Interface Electron Probe: Magnification = 270X

(e) Reaction Between Pt/Mo50Re Intermetallies and TZM Spacer in Crucible No. 7

(f) Typical View at Bottom of Crucible No. 7 Showing No Reaction Between TZM and PtMo3 Intermetallic Coated Mo

(e) Reaction Between Pt/Mo50Re Intermetallies and TZM Spacer in Crucible No. 7

(f) Typical View at Bottom of Crucible No. 7 Showing No Reaction Between TZM and PtMo3 Intermetallic Coated Mo

Figure 4.2.1-54. Crucible No. 7

were converted to intermetallics (Pt_3Mo , some Pt_2O and PtMo) with some solid solutions between Pt, Mo, or Re.

After interfacing with Ir for 600 hours at 1370°C there was no evidence of reaction between the intermetallic barrier specimens and Ir. This is a significant result since previous experiments (Reference 4-1) have shown that Ir and Mo, as well as Ir and Mo50Re, tend to interdiffuse at 1370°C .

In addition, it was found that the intermetallic coated Mo50Re specimen reacted with a TZM spacer. The coating appears to have been consumed by the TZM and diffusion was in the direction of the TZM.

Crucible #8 (Mo50Re-Ir-Mo50Re) - Figures 4.2.1-55(a) through 4.2.1-55(d) indicate that Ir and Mo50Re react, in vacuum at 1370°C .

Depth of penetration of Mo and Re into Ir averages approximately 0.001 inch; a maximum penetration of approximately 0.003 inch was noted locally.

As was the case with the other Ir specimens tested, 2 to 4 grains occupy total wall thickness (0.020 inch).

Crucible #9 (Ti and HfC Sputter Coated Mo50Re vs. Ir) - Figures 4.2.1-56(a) through 4.2.1-56(d) show that reactions occurred between TiC and HfC sputter coated Mo50Re and Ir. Reaction of Ir with the TiC coated specimen was more extensive than with HfC; the mode of interaction was different in each case. While it cannot be confirmed, the brittle TiC coating appears to have become dislodged locally during crucible assembly and bare Mo50Re was probably exposed to Ir during test.

Crucible #10 (Ir-C-Ir) - Figures 4.2.1-57(a) through 4.2.1-57(c) show that carbon does not interact with Ir at 1370°C .

(a) Unetched Cross Section of Crucible No. 8

(b) Portion of Unreacted Region of Ir from Crucible No. 8

Figure 4.2.1-55. Crucible No. 8

NEG #475

(500X)

(c) Enlargement of Reaction Region Shown in (d), below

NEG #474

(125X)

(d) Reaction Region Between Ir and Mo50Re

Figure 4.21-55. Crucible No. 8 (Cont)

(a) Unetched Cross Section of Crucible No. 9

NEG #468

(b) Enlarged View of Center Section of Ir
Vs Carbide Coated Mo50Re shown in (a)

Figure 4.2.1-56. Crucible No. 9

(500X)
NEG #469

(c) Enlarged View of TiC Sputter Coated on Mo50Re/Ir Interface
from Crucible No. 9

(500X)
NEG #473

(d) Enlarged View of HfC Sputter Coated on Mo50Re/Ir Interface
from Crucible No. 9

Figure 4.2.1-56. Crucible No. 9 (Cont)

(a) Cross Section of Crucible No. 10

(b) Enlarged View of Ir-C Interface at Top Center of Figure (a)

Figure 4.2.1-57. Crucible No. 10

AS RECEIVED IR
(500X)

- a. FIBROUS STRUCTURE
- b. SMALL ETCHING PITS

PHOTO NO. 136

IR AFTER HEAT TREATMENT
WITH MgO FOR 600 HRS AT 2500°F

- a. LARGE GRAINED
- b. UNIDENTIFIED MATERIAL
CAUSING PITTING DUE TO
ETCHING (500X)

PHOTO NO. 137

IR AFTER HEAT TREATMENT WITH
PYROCARB 406 FOR 600 HRS AT
2500°F

- a. LARGE GRAINED
- b. UNIDENTIFIED MATERIAL
CAUSING PITTING DUE TO
ETCHING

PHOTO NO. 135

(c) Comparison of Ir Specimens Used to Evaluate Etching Pits
in Ir Heat Treated with Carbon

Figure 4.2.1-57. Crucible No. 10 (Cont)

These experimental observations agree with the results found in Table 4.2.1-10 where no graphite/iridium reaction was reported below 1700°C. The vast majority of experiments suggest that Ir and carbon are stable to eutectic temperature ($\approx 2296^{\circ}\text{C}$).

Electron-probe and gas analyses were performed on the compatibility specimens to confirm the metallographic evaluation; all evaluations indicate that there was no carbon pick-up after 600 hours at 1370°C.

Additional studies performed by GE, which are discussed in Section 4.2.1.3.1.2, show that no carbon-Ir reaction is observable below eutectic temperature.

Crucibles 11, 12, and 13 (Carbides, TaC, TiC and ZrC vs. Ir) - Figures 4.2.1-58 thru 60 show the metallography performed on crucibles containing carbides in contact with iridium. J. M. Criscione, et. al., ⁽¹⁾ also report that metallic carbides react with Ir at 1200°C (and above) to form intermetallic compounds with Ir with subsequent release of carbon.

Of the three carbides tested, TaC attacked Ir more seriously than ZrC or TiC. The attack by TaC was intergranular in nature causing grain boundary cleavage during cool-down from test temperature. Attack by both TiC and ZrC was local (as noted earlier, Ir coupons were bowed and probably only had point contact with the carbides) and seems to have progressed through the bulk of Ir rather than through grain boundaries.

Crucibles 14 and 15 (BeO and HfO₂ vs Ir) - Figures 4.2.1-61 and 62 show that BeO, but not HfO₂, is stable with carbon (at 1370°C). In general, these findings are supported by the literature in Table 4.2.1-8.

Crucible 16 (TiC-MoRe-TiC) - Figure 4.2.1-63 shows that TiC is reduced by Mo50Re, but that the attack was not catastrophic. Rhenium does not form a carbide in the present carbon and there are no reported eutectics between the two materials.

¹J. M. Criscione, et. al., "High Temperature Protective Coatings for Graphite", ML-TDR-64-173, Part II, Union Carbide Corp., October 1964

(b) Exploded View at Right Center Portion of Crucible No. 11

Figure 4.2.1-58. Crucible No. 11

(c) View of Ir in Crucible No. 11 Showing Grain
Grain Boundary Separation

(d) View of Relatively Unattached Ir in Crucible No. 11 Showing
Typical Grain Size after 600 Hr @ 1370°C

Figure 4.2.1-58. Crucible No. 11 (Cont)

(a) Composite View of Crucible No. 12

(b) Region of TiC-Ir Interaction

Figure 4.21-59. Crucible No. 12

NEG 163

(500X)

(c) Exploded View of TiC:Ir Reaction Zone

NEG 175

(125X)

(d) Typical Ir Grain Size in Unreacted Region of Ir:TiC Specimen in Figure (c) above

Figure 4.2.1-59. Crucible No. 12 (Cont)

(a) Composite View of Crucible No. 13 Cross Section

(b) Typical Grain Size in Unreacted Region of Ir Specimen
in Crucible No. 13

Figure 4.2.1-60. Crucible No. 13

(c) Exploded View of Reaction Shown in (d) below

(d) Central Region of Ir:ZrC Interfaces Shown in (a)

Figure 4.2.1-60. Crucible No. 13 (Cont)

(a) Composite View of Crucible No. 14 Cross Section

(b) Enlargement of Typical C-BeO Interface Showing
No Significant Reaction

Figure 4.2.1-61. Crucible No. 14

(a) Composite Cross Section of Crucible No. 15

(b) View of Reaction between Components in Crucible No. 15

Figure 4.2.1-62. Crucible No. 15

(a) Composite Cross Section of Crucible No. 16

(b) Typical Grain Size in Mo50Re Specimen
after 600 Hr at 1370°C

Figure 4.2.1-63. Crucible No. 16

(c) View of Crucible No. 16 Specimens Showing Reaction Zone Along Lower Edge

(d) Enlarged View of TiC-Mo50Re Reaction Area Mid-Span of (c)

Figure 4.2.1-63. Crucible No. 16 (Cont)

Table 4.2.1-11, however, indicates that Mo and carbon melt incipiently at approximately 1500°C. Reactions noted at 1370°C between Mo50Re are sufficiently severe to discourage the direct combination of these two metals at 1370°C.

Crucibles 17, 18 and 19 (Carbides (TaC, TiC and ZrC) vs. Pt 30Rh - Figures 4.2.1-64, 4.2.1-65 and 4.2.1-66, respectively, indicate that Pt30Rh is catastrophically attacked by TaC, TiC and ZrC. Of the three carbides, ZrC appears to be the least reactive.

These reactions, when compared to those observed between the same carbides and Ir, are indicative of the higher temperature "sluggishness" of Ir relative to Pt 30Rh. Reactions that occur with Pt and Rh seem to be duplicated with Ir but at significant higher temperature and over long periods of time.

Crucible 20 (BeO-Ir-Pt30Rh-HfO₂) - The metallography of crucible #20 as shown in Figure 4.2.1-67 indicates that at 1370°C:

1. Ir and Pt30Rh interdiffuse (solid solutioning)
2. BeO appears to be compatible with Ir
3. Y₂O₃ stabilized HfO₂ did not react with Ir as seriously as it did in crucible #6
4. Y₂O₃ stabilized HfO₂ interacted with the base of the TZM crucible.

The minimal reaction found between Y₂O₃ stabilized HfO₂ and Ir is somewhat inconsistent with the results obtained in crucible #6. More detailed analysis would be required to resolve these differences.

4.2.1.3.1.1.2 Series II Short Term Compatibility Test Conclusions

Based on the evaluation of the non-fuel compatibility crucibles (Table 4.2.1-12) the following conclusions can be drawn:

1. There appears to be no interaction between carbon and iridium as determined by either metallography, electron probe, or spectrochemical analyses.
2. No interaction was found between MgO, unstabilized HfO₂, or Mo, or Mo50Re, that has been protected by a Pt₃₀Mo intermetallic coating, formed in situ, prior to compatibility testing.
3. Slight reaction was noted between BeO and Ir, as determined by metallography.
4. Serious interaction was found between Ir and the following materials (as evidenced by metallography): refractory metal carbides, Mo, TZM, and Mo50Re.
5. Catastrophic reactions occur between Pt30Rh and refractory metal carbides.

Table 4.2.1-11. Observed Melting Temperatures of Refractory Metals with Carbon and Ir (1) (2) (3)

Material	Observed Melting Temperature					
	Unalloyed		Eutectic		Incipient	
	C ^o	F ^o	C ^o	F ^o	C ^o	F ^o
Ta	2996	5430			None Reported	
Ta + C			2825	5120		
Ta + Ir			1948	3540		
W	3410	6200			1499	2730
W + C			2710	4910		
W + Ir			2305*	4175*		
Mo	2610	4730			1499	2730
Mo + C			2205	4000		
Mo + Ir			2080	3776		
Nb + Ir			1840	3344		

* Conflicting data suggests that there may be no reaction between Ir and W after 35 min. @ 2427^oC (4400^oF)

REFERENCES

- (1) S.J. Burnett, "Properties of Refractory Metals", AERE-R-4657, JKAEC, 1969.
- (2) ML-TDR-64-204, Vol. I, "Evaluation of Thermal Protective Systems for Advanced Aerospace Vehicles", April, 1965
- (3) Passmore, E.M., Arco Corp. Boyd J.E., Battelle, Man Labs, Inc., A66-36142, 1965.

Figure 4.2.1-64. Crucible No. 17

34

(a) Composite Cross Section of Crucible No. 18

(b) Cross Section of Pt30Rh in Crucible No. 18 Showing Grain Size and Kirkendall Void Formations

Figure 4.2.1-65. Crucible No. 18

(325X)

NEG 492

NOTE: SQUARE
ON GRID = 0.001 IN.

(c) Exploded View of Reaction Between
Pt30Rh and TiC in Crucible No. 18

(93.75X)

NEG 491

NOTE: SQUARE
ON GRID = 0.004 IN.

(d) Reaction Between TiC and Pt30Rh after
600 Hr at 1370°C

Figure 4.2.1-65. Crucible No. 18 (Cont)

(a) Composite Cross Section of Crucible No. 19

(b) Typical Grain Size in Pt30Rh Specimen Exposed to ZrC for 600 Hr at 1370°C

Figure 4.2.1-66. Crucible No. 19

(325X)

NEG 495

(c) Exploded View of Reaction Shown in (d)

(93.75X)

NEG 494

(d) As-Polished Cross Section in Reaction Area in Crucible No. 19

Figure 4.2.1-66. Crucible No. 19 (Cont)

(a) Composite Cross Section of Crucible No. 20

(b) Interface Between Ir & Pt30Rh after
600 Hr at 1370°C

Figure 4.2.1-67. Crucible No. 20

(c) Interface Between BeO & Ir in Crucible No. 20

(d) Interface Between Y_2O_3 Stabilized HfO_2 and Ir
in Crucible No. 20

Figure 4.2.1-67. Crucible No. 20 (Cont)

(e) Reaction Observed Between Y_2O_3 Stabilized HfO_2 and the Base of TZM Crucible No. 20

Figure 4.2.1-67. Crucible No. 20 (Cont)

Table 4.2.1-12. Compatibility Test Results

Compatibility Couple	Crucible No.	Excellent	Good	Fair	Poor	Major Direction of Diffusion	Electron-Probe Results
<u>(Oxides-Ir)</u>							
ThO ₂ :Ir	1				x	Into Ir	Interdiffusion
BeO:Ir	3 & 20		x			Into Ir	Not evaluated
MgO:Ir	4	x					Not evaluated
HfO ₂ :Ir		x					No interdiffusion
Y ₂ O ₃ . HfO ₂ :Ir	6 & 20			x		Into Ir	Y and Hf diffusion into Ir
Y ₂ O ₃ . ZrO ₂ :Ir	5			x		Into oxide	Not evaluated
<u>(Oxide-Refractory Metal)</u>							
ThO ₂ :TZM	1	x					No interdiffusion
BeO:TZM	3 & 20		x			Into TZM	Not evaluated
MgO:TZM	4		x			Into TZM	Not evaluated
Y ₂ O ₃ . ZrO ₂ :TZM	5		x			Into TZM	Not evaluated
Y ₂ O ₃ . HfO ₂ :TZM	6 & 20		x			Into TZM	Y diffusion into TZM
HfO ₂ :TZM	6	x					No interdiffusion
<u>Carbon-Ir</u>	10	x					No interdiffusion
<u>(Carbides-Ir)</u>							
TaC:Ir	11					Into Ir	Not evaluated
TiC:Ir	12			x	x	Equal flow	Not evaluated
ZrC:Ir	13			x		Equal flow	Not evaluated
<u>(Carbides:Pt30Rh)</u>							
TaC:Pt30Rh	17				x	Equal flow	Not evaluated
TiC:Pt30Rh	18				x	Equal flow	Not evaluated
ZrC:Pt30Rh	19				x	Equal flow	Not evaluated
<u>(Carbides-Misc.)</u>							
TiC, TaC, ZrC	17, 18, 19	x					Not evaluated
TiC - TZM	16			x		Possible Carbides in TZM Grain-Bdry's	Not evaluated
TiC-Mo50Re	16				x	Into Mo50Re	Not evaluated
TiC Sputter	9				x	Mutual Interdiffusion	Not evaluated
Coated Mo50Re-Ir							
HfC Sputter	9			x		Mutual Interdiffusion	Not evaluated
Coated Mo50Re-Ir							
TiC Sputter	9				x	Into TZM	Not evaluated
Coated Mo50Re-TZM							
HfC Sputter	9			x		Into TZM	Not evaluated
Coated Mo50Re-TZM							
<u>(Graphite - Misc.)</u>							
TZM - Graphite	17, 18, 19				x	Into TZM	Not evaluated
BeO - Graphite	14	x					Not evaluated
Y ₂ O ₃ :HfO ₂ - Graphite	15				x	Into Oxide	Not evaluated
<u>(Metal - Metal)</u>							
Mo-Ir	2				x	Into Ir	Not evaluated
Mo50Re-Ir	8				x	Into Ir	Not evaluated
Ir-Pt30Rh	7				x	Mutual Interdiffusion	Not evaluated
<u>(Intermetallics - Metal)</u>							
Mo50Re-Mo:Re:Pt-Ir	7	x					No Ir diffusion - Possible light diffusion of Mo, Pt & Re into Ir-Pt into Mo50Re
Mo-Mo:Pt-Ir	7		x			Into Ir	No Ir diffusion Possible light diffusion of Mo & Pt into Ir-Pt into Mo
Mo50Re-Mo:Re:Pt	7				x	Into TZM	Not evaluated
Mo-Mo:Pt-TZM	7	x					Not evaluated

Notes

- (1) Short term compatibility environment:
Time: 600 hr
Pressure: 10⁻⁶ torr dynamic vac.
Temperature: 1370°C (2500°F).
- (2) Definition of Metallographic terms:
Excellent: No visible interaction or discoloration
Good: Interaction visible by discoloration only. Maximum penetration of 0.001 inch -- no visible intermetallic formation or grain boundary precipitate, or attack.
Fair: Penetration greater than 0.001 inch but no visible separation of grain-boundaries or matrix cracking.
Poor: Penetration greater than 0.001 inch with damage to grain-boundaries and/or matrix. Observations of excellent or good designate a system worthy of further investigation in the presently planned long-term tests. Fair and poor denote system reactions that are probably too severe for MHW consideration. The only exception being the interaction between Ir and Pt30Rh where 100% interaction is even considered acceptable.

4.2.1.3.1.2 Iridium-Carbon Eutectic Experimentation. Iridium is the primary material candidate for both the outer can and the post impact shell. In both functions the iridium has a direct interface with a graphitic material (i. e., the aeroshell and the impact shell, respectively). The overall performance of the heat service can be directly related to the melting temperature of the iridium. Thus, any eutectics that may be formed could compromise the heat source design. To ensure that the heat source design has adequate thermal margins throughout its mission profile an empirical evaluation of the Ir-C eutectic was undertaken as part of the Base Program. The literature, as shown in Table 4.2-1-13 , suggests that an iridium/carbon eutectic exists at 2302°C . To verify these results, as well as to establish allowable design temperatures, an experimental program, as defined in Table 4.2.1-14 , was initiated. The testing procedures are discussed below:

A water-cooled copper coil, housed within a glass bell-jar, was utilized to heat specimens while in a partial pressure of 10^{-6} torr, or better. A graphite crucible, centered within the heating coil, was used to hold an Ir coupon sandwiched between graphite, during test. Time to test temperature was approximately 5 minutes; time at temperature was also 5 minutes. All specimens were tested in the "as-received" condition. All graphite purities were certified to be 99.9 percent, or better, and the Ir coupon purity was 99.95 percent.

The first four tests were run using an optical pyrometer to monitor the surface temperature of the carbon crucibles; Table 4.2.1-14 . A cantilevered graphite rod was used to maintain axial pressure on all specimens during test. When melting was experienced at a temperature as low as 2150°C , both the test procedure and the measurement procedure became suspect, since the commonly accepted minimum Ir-C eutectic is approximately 150°C higher.

A calibrated tungsten lamp was used to evaluate any optical error that may have been induced into the experiment due to sighting through a glass bell-jar. This error was found to be 20°C , leaving another 130°C still to be accounted for. As a check, a small diameter hole was drilled into the crucible and "dead-ended" at a point coincident with the sample

Table 4.2.1-13. Observed Melting Temperatures of Unalloyed and Alloyed Noble Metals*

MATERIAL	OBSERVED MELTING TEMPERATURE					
	UNALLOYED		EUTECTIC		INCIPIENT	
	°C	°F	°C	°F	°C	°F
<u>UNALLOYED</u>						
Re	3067	5584				
Os	3055	5563				
Ir	2443	4430				
Rh	1956	3553				
Pt	1770	3218				
<u>UNALLOYED BUT WITH C</u>						
Re + C			2466	4471		
Os + C			2732	4950		
Ir + C			2302	4176		
Rh + C			1662	3024		
Pt + C			1732	3150		
<u>ALLOYED</u>						
Ir + 20 Re					2550	4622
Ir + 20 Os					2532	4590
Ir + 10 Re					2440	4424
Ir + 10 Os					2434	4414
Ir + 10 Rh					2342	4248
Ir + 20 Rh					2283	4141
Ir + 10 Pt					2273	4123
Ir + 20 Pt					2191	3976
<u>ALLOYED BUT WITH C</u>						
Ir20Re + C			2430	4406		
Ir20Os + C			2380	4316		
Ir10Re + C			2360	4280		
Ir10Os + C			2340	4244		
Ir10Rh + C			2230	4046		
Ir10Pt + C			2200	3992		
Ir20Rh + C			2150	3902		
Ir20Pt + C			2130	3866		

*Reference - D. P. Harmon, TR-AFML-TR-66-290, "Iridium - Base Alloys and Their Behavior in the Presence of Carbon", 1967.

Table 4.2.1-14 . Preliminary Ir-C Experimentation Results

TEST #	SURFACE TEMP. (°C)	CORRECTION FOR "ε" TABLE III	CORRECTION FOR GLASS TABLE III	CORRECTED TEMPERATURE (°C)	OBSERVATION
1	2093	+100 (°C) typ	+20 (°C) typ	2213	No Ir Melt (2)
2	2300	110	20	2430	Ir Melt
3	2200	110	20	2330	Ir Melt
4	2150	110	20	2280	Ir Melt
5	2090	125 (1)	20	2235	Ir Melt
6	2140	110 (1)	20	2270	Ir Melt
7	2090	105 (1)	20	2215	Partial Ir Melt (2)
8	2070	110	20	2200	No Ir Melt

NOTES:

1. Actual Correction Obtained During Test
2. Graphite Coupons Under Hood When Contacting Ir (Test #1)
Graphite Coupons Not Under Hood When Contacting Ir (Test #7 and #8)

placement. This allowed the measurement of both the cavity temperature as well as the temperature at the surface of the crucible to be monitored during the test run. These comparisons were made for run number 5 and are presented in Table 4.2.1-15. Up to $\sim 1900^{\circ}\text{C}$ the temperature differences between specimen and cavity appear to be quite small ($\leq 30^{\circ}\text{C}$); however, at 1900°C there is a 100°C variation between the cavity temperature and the specimen surface temperature. To verify these differences, the sample was cooled and then again raised to test temperature; as shown in Table 4.2.1-15 these same differences appeared.

T. R. Wright, of Battelle, (Reference AFML-TR-68-6, 1968) experienced a similar error during experiments directed toward the production of Ir-C coatings for the oxidation protection of graphite and it has been concluded that a vapor phase phenomena could be responsible for changes in the surface emissivity of the graphite in this temperature regime. The GE tests confirm this conclusion.

Three additional runs (tests 6 through 8) were made using the results and corrections shown in Table 4.2.1-15. Based on these results it appears that the eutectic temperature is around 2215°C (4019°F). The difference between this number and the 2300°C eutectic, reported in literature, is probably due to impurities in the materials used and/or experimental error.

Figures 4.2.1-68 through 4.2.1-76 show the behavior of Ir and C after 5 minutes exposure at temperature ranging from 2200°C to 2280°C . Iridium did not show evidence of interaction with carbon until the eutectic temperature is reached.

Electron-probe analysis of the Iridium was performed to determine if localized impurities were present that might have triggered premature melting of the Ir.

Figures 4.2.1-77 and 4.2.1-78 indicate that concentrations of Fe, Al, and Si were present on the surface of "as-received" iridium but not in the bulk of the base metal. Figure 4.2.1-79 shows that Fe is probably present to a greater depth than either Si or Al. It is probable that the Fe was introduced by the tooling used in iridium fabrication; fabrication

Table 4.2.1-15 . Ir-C Eutectic Measurement (Test #5)

GRAPHITE SURFACE (°C)	GRAPHITE CAVITY (°C)	DIFFERENCE (°C) CAVITY/SURFACE
1271	1295	24
1540	1565	25
1800	1830	30
1888	1910	22
1980	2080	100
1995	2080	85
1995	2078	83
COOL AND REPEAT		
1300	1335	35
1937	2020	83
2100	2202	102
2090	2215*	125
2090	2200	110

* Error due to sighting through bell-jar glass
determined to be 20°C

~ Error due to sighting on surface of graphite:

(< 1900°C) + 25°

(1975° to 2000°) + 90°

(2000° to 2100°) + 100

(2100 to 2200°) + 110

Figure 4.2.1-68. Iridium-Graphite Behavior at Reentry Temperature

Reaction Approximately One-Quarter Inch Away From Start-Melt
End Of Specimen Shown In Figure 4.2.1-68

Reaction At Approximately Same Area Of Specimen Shown Above.

Figure 4.2.1-69. Graphite Iridium Behavior @ 2215°C

Reaction Approximately One-Inch Away From Melt End

Reaction Stops About Two-Inches Away From Melt End.

"Reaction-Free" End Of Ir-C Specimen(Unetched).

Figure 4.2.1-70. Iridium-Carbon Behavior @ 2215 °C

Typical View At Top Of
Specimen Where Carbon Was
Present But No Reaction ...
(No Load On Carbon)

Ir-C Eutectic
Composition

(150X)

Etched

Top Edge Of Specimen Shown At a Distance of
About One-Inch Away From Melt End.

Unreacted Ir.
Ir-C Eutectic
Composition

Partially Reacted
Iridium

Ir-C Eutectic
Composition
Carbon

Cross Section About 1/2 In. Away From Melt End.

Figure 4.2.1-71. Ir-C Behavior @ 2215°C

Top Edge Of Ir Coupon Where Reaction With
Carbon Stops.....

(150X)

Note That Ir
Grain Boundaries
Are Selectively
Attacked By C
At Eutectic
Temperature

Bottom Edge Of Ir Coupon Where Reaction Stops.....

Figure 4.2.1-72. Ir-C Behavior at 2215°C

Ir Having Top and Bottom Surfaces Exposed to Graphite During Test.

(15X)

(Unetched)

Ir Protected From Graphite During Test.

(100X)

(Unetched)

Typical Cross Section of Ir-C Compatibility Coupon

Figure 4.2.1-73. Ir-C Behavior @ 2200°C

Ir-C Compatibility Coupon

Ir Protected From Graphite During Test.

(I20X)

(Etched)

Figure 4.2.1-74 . Enlarged Views of Control Coupon and Ir-C Compatibility Coupon After 5 Min. @ 2200°C

Macro-View of Ir-C Compatibility Stack (Unetched)

Enlarged View of Ir-C Interface (Unetched).

Figure 4.2.1-75 . Behavior of Ir and C @ 2235^oC for 5 Min.

Macro View Of Ir-C Compatibilty Stack (Unetched).

← Ir

Enlarged View Of Ir-C Interface (UNETCHED).

Figure 4.2.1-76 . Behavior of Ir and C @ 2280^oC for 5 Min.

Figure 4.2.1-77 . Electron-Probe Identification of Al and Si on the Surface of "As-Received" Iridium

Figure 4.2.1-78 . Electron-Probe Identification of Fe on the Surface of "As-Received" Iridium

Ir

Fe

Al

Figure 4.2.1-79 . Scanning Electron Probe Photographs from the Cross-Section of an As-Received, 0.5 Inch Diameter Iridium Disc.
Magnification = 1000X

temperature seems to have caused superficial alloying at the iridium surface. The source of the Al and Si could be final grinding or polishing process, wherein these materials are deposited on the surface of the iridium. Fabrication procedures are currently being scrutinized to eliminate future material surface contamination where possible. The existence of impurities on the surface of Ir can be a two fold problem. If the impurities on the surface they may trigger surface reactions or cause premature melting; i.e., an Ir-Si eutectic exists at about 1470°C (2678°F). If the contaminants become homogenized (throughout the bulk of the Ir) due to the MHW operational temperature, surface reaction will be minimized, but the base-metal purity and integrity may suffer, particularly if the contaminants orient selectively in the grain boundaries. This homogenizing effect is evident in Figure 4.2.1- 80 , where no spikes of iron can be identified on the surface of iridium after treatment at 1600°F for a few thousand hours.

Figures 4.2.1- 81 and 4.2.1- 82 show other evidence of Fe, Al, and Si on the surface of "as received" and cleaned iridium. Cleaning by grinding removed some (but not all) of the silicon and aluminum from the iridium surface but does not touch the iron. Even washing with acid and solvents does not guarantee a clean surface. The primary method for eliminating these impurities is to initiate an effort with the material fabricator whereby the amount of impurities introduced into the material would be limited; then, a homogenization treatment could be applied to the material for dispersal purposes, to further reduce the overall quantity of contamination.

Table 4.2.1-11 shows that refractory metals, as well as iridium, are subject to premature melting when interfaced with carbon; Ta appears to be most stable with carbon. Table 4.2.1-13 suggests that although the Os-C eutectic is some 430°C higher than the Ir-C eutectic, the difference is reduced to 80°C if diffusion occurs between Ir and its Osmium coating. Table 4.2.1-13 also indicates that alloying Ir with either Pt or Rh significantly reduces the reentry capability of Ir.

Specimen Current Image

Fe

50 μ m

Figure 4.2.1-80 . Scanning Electron Probe Photographs from an Electro Etched Cross-Section of an Oxidized (1600 $^{\circ}$ F) Iridium Disc. Magnification = 1000X

Specimen Current Image

Ir

Si

Al

Fe

Figure 4.2.1-81 . Scanning Electron Probe Photographs of a Portion of the Surface of a "Cleaned", As-Received, 0.5 Inch Diameter Iridium Disc. Magnification = 1000X

Figure 4.2.1- 82 . Scanning Electron Probe Photographs of a Portion of the Surface of a "Cleaned", As-Received, 0.25 Inch Diameter Iridium Disc. Magnification = 1000X

4.2.1.3.1.3 Iridium Oxidation

4.2.1.3.1.3.1 Iridium Air-Oxidation Tests - Two iridium coupons, one welded and one unwelded, were selected for oxidation testing in flowing air at 815°C and 1000°C. The coupons were weighed and dimensioned then placed in a shallow Al₂O₃ boat, which held the coupons during exposure. The test apparatus consisted of a horizontal tube furnace equipped with a 1" I.D. Al₂O₃ muffle. Dry filtered air was injected in one end, over the test coupons and rejected into the atmosphere at the other end. The air flow was metered at between 8 to 10 SCFH. Temperature was controlled by a Pt/Pt10Rh thermocouple positioned adjacent to the Al₂O₃ boat, inside the furnace muffle. Testing was interrupted at various time intervals to inspect the specimens for oxidation.

During the first 200 hours of exposure at 815°C, neither coupon experienced significant weight change. The coupons were unchanged in appearance except for a light purple tarnish of the surfaces. After oxidation for 200 hours at 815°C, the temperature was increased to 1000°C and testing continued for an additional 100 hours. As shown in Table 4.2.1-16, considerable oxidation occurred during this period, resulting in weight losses of approximately 180 mgs. per cm² of test material. The coupons were grossly eroded and covered with a black powdery oxide which was easily dislodged. A substantial amount of black oxide was carried from the 1000°C test coupons by the air stream during testing and deposited on equipment surfaces at the exit end of the furnace muffle. The rate of oxidation appears uniform over the coupon surfaces whether welded or not.

Metallographic examination indicated the oxidation proceeded intragranularly, with no preference for grain boundaries. Hardness readings of 260 D.P.H. in the base metal and 264 D.P.H. in the weldment compare with the original pre-test as annealed hardness of 260 D.P.H. (100 gm load) and 247, respectively. A photo-micrograph of a typical polished cross

Table 4.2.1-16. Iridium Metal Oxidation Exposure In Flowing Air
At Test Temperatures of 815°C and 1000°C

Milligrams of weight change (loss) during exposure time and temperature indicated. (a)										
Test Specimen ^(b) Description	Pre-test wt., gms.	Time, hrs. at 815°C							Time, hrs. at 1000°C	
		5	24	48	72	100	125	200	72	100
Sheet, 0.021" thick 0.9399cm ²	0.4740	0.5	0.4	0.4	0.8	0.7	0.4	0.4	169.1	190.2
									(-35.7%)	(-40.2%)
Welded Sheet 0.021"thk, 1.7663cm ²	0.9329	0.5	0.4	0.4	0.8	0.7	0.4	0.4	283.0	322.4
									(-30.4%)	(-34.6%)

(a) Testing was continuous, interrupted only at times indicated for weighing.

At completion of 200 hours of test at 815°C the temperature was increased to 1000°C and testing continued.

(b) Both iridium test specimen sections were annealed at 1500°C for 1/2 hr. in vacuum prior to testing.

section through the weldment, after oxidation testing, is shown in Figure 4.2.1-83. From this initial testing, the following conclusions can be made:

- (1) Very slight oxidation takes place at 815°C , with formation of an oxide film, which does not evaporate and which acts as a barrier to continued oxidation.
- (2) The rate of oxidation at 1000°C indicates that 0.030" thick iridium would totally oxidize in approximately 1000 hours, when only the outside surface is being oxidized.
- (3) Post-impact temperatures below 815°C will meet the long term fuel containment requirement.
- (4) There appears to be no difference between the oxidation behavior of welded iridium with a heat affected zone, and unwelded iridium.

In order to more precisely determine iridium oxidation threshold temperature and oxidation rates at temperatures above the oxidation threshold, additional testing was performed.

4.2.1.3.1.3.2 Second Phase Iridium Oxidation Test Specimens and Test Procedure - Three sections of iridium sheet were formed into discs 1/2" diameter x ≈ 0.022 " thick. The specimens were supplied by Englehard Industries and were 99.95 percent pure iridium.

These tests used an electrically heated muffle furnace for the oxidation of the pre-measured and weighed specimens. Each specimen was supported on 0.0625 inch diameter Al_2O_3 rods straddled across an alumina boat, so that all surfaces were exposed to an air oxidizing environment, produced by a convective flow of air through the partly opened furnace door. A chromel-alumel thermocouple was placed in the space between the test specimen and boat to maintain specimen temperature. (See Figure 4.2.1-84.) The test temperatures for each of the three specimens were as follows:

Specimen #1 - 815°C

Specimen #2 - 705°C for first 519 hours then raised to 870°C and held for 1000 hours

Specimen #3 - 830°C

J14041A

100x Mag.
As etched

Figure 4.2.1-83. Photomicrograph of a section of 0.021" thick iridium sheet after oxidation testing in flowing air for 200 hours at 815°C and 100 hours at 1000°C. The area shown is through a welded section.

Figure 4.2.1-84 . Oxidized Iridium

4.2.1.3.1.3.3 Second Phase Iridium Oxidation Test Results - During oxidation of iridium in air at temperatures above 815°C, there is an observed weight loss of metal due to the formation and evaporation of iridium dioxide by the following reactions:

The first reaction can take place below 700°C, forming a blue black surface oxide, which acts as a barrier to continued oxidation. As temperatures increase, further oxidation takes place according to the second reaction, resulting in the formation of a black oxide film. At temperatures between 815°C and 870°C IrO₂ begins to evaporate from the surface at a slow rate. This causes a decrease in the effectiveness of the oxide barrier action and an increase in the rate of oxidation. Upon heating IrO₂ to 1050°C, dissociation to Ir metal, solid phase, takes place in an air environment, at the one atmosphere pressure. This compares favorably with the calculated IrO₂ dissociation temperature of 1015°C in 160 mm Hg oxygen pressure as reported in the literature. At temperatures above 1050°C IrO₂ further oxidizes to IrO₃ gas phase.

Figure 4.2.1-84 is a view of specimen #1 and #3 after heating in air for 2094 hours at 815°C and 993 hours at 870°C. The discoloration of the alumina crucible is due to condensed IrO₂ decomposed to Ir metal. The more intense black color of specimen #3 is the result of more complete oxidation.

Figure 4.2.1-85 presents a plot of mass loss, induced by the oxidation and oxide sublimation of iridium. At test temperatures of 705°C and 815°C virtually no loss of weight is observed. When the temperatures are increased from 705°C to 870°C, as in case specimen #2, the oxide that forms at lower temperatures begins to evaporate at a slow rate. The mechanism of mass loss rate appears to be limited by the rate of evaporation of IrO₂. Specimen #3 mass loss rate is higher than that observed for specimen #2 at the same temperature. This higher rate could be caused by the direct oxidation of Ir to IrO₂ during initial heating and its higher sublimation rate compared to the Ir₂O₃ oxide.

Figure 4.2.1-85 . Iridium Oxidation Test Results

Figure 4.2.1-86 presents the cross section of an "as received" specimen showing the topography of the unoxidized surface. Figure 4.2.1-87 is a view of the cross section of specimen #2 after oxidation for 519 hours at 705°C, followed by oxidation at 870°C for 1000 hours. A thin layer of surface oxide remained on the surface, with a thickness of about 0.0025" in the area of uniform penetration and about 0.004" in the area of nonuniform penetration. The thickness of the unreacted iridium decreased from 0.023" to 0.018", or a loss of 0.0019" of metal from the surface. This indicates that it would take about 10,000 hours to oxidize through 0.023" thick Ir, with only the outside surface exposed to the reaction environment.

4.2.1.3.1.3.4 Conclusions

- (1) TIG welding iridium in air does not cause embrittlement. In fact, the cast metal in the weld zone is slightly more ductile with no increase in hardness in the heat affected zone.
- (2) Annealing of iridium takes place at 1500°C for 30 minutes or longer.
- (3) Oxidation of iridium limits post impact temperatures in an air oxidizing environment to 870°C or below for one year fuel containment in a 0.023" thick Ir liner.

4.2.1.3.1.3.5 Follow-On Testing. It is estimated that fuel sphere impact temperature is about 1370°C. This means that oxidation will start at this temperature. If the rate of oxide evaporation is equal to the rate of oxidation then the surface oxide will leave the surface as fast as it forms in which case the barrier action provided by the surface oxide will not take place and the loss of iridium will proceed at a fast rate. Therefore, it is necessary to test oxidation behavior by starting the reaction at 1370°C and allowing the iridium to cool to post-impact temperatures, with the proper variation of surface emissivity with time to accommodate the iridium temperature stabilization.

IRIDIUM →

Figure 4.2.1-86. Clean Ir Surface 125X

IRIDIUM
OXIDE →

IRIDIUM →

IRIDIUM
OXIDE →

←
IRIDIUM
OXIDE

Figure 4.2.1-87 . Specimen #2, 125X Oxidized at 705°C for 159 Hours and 870°C for 1000 Hours

4.2.1.3.1.3.6 Oxidation Stability of Vented Iridium Clad Thoria Molybdenum Fuel Simulant Spheres. The objective of this program was to determine the behavior of two iridium clad ThO_2 -Mo spheres in flowing air at temperatures of 700° to 900°C . These spheres were vented by drilling holes, 0.005-inch and 0.030-inch diameter through the cladding at opposite poles. One sphere (D-41) with 0.030-inch vent holes was tested at 800°C for 3902 hours with 19 thermal cycles to room temperature for inspections. The second sphere (C-10) with 0.005-inch vent holes was tested at 800° for 1516 hours, at 700° for 859 hours and at 900°C for 1527 hours with a total of 20 thermal cycles. Each sphere was examined periodically for weight and diametral changes. The effluent air was passed through an absolute filter to retain all particulate matter that might be evolved from the test spheres, and at the end of each cycle the filter media was checked with a radiation detector for evidence of thoria particles carried in the air stream. After 3902 hours of testing both spheres were sectioned and examined metallographically for the effects of oxidation on the iridium cladding and the ThO_2 -Mo fuel simulant core.

4.2.1.3.1.3.6.1 Test Facilities. The oxidation testing was conducted in the same furnace facilities that were used for the testing of vented Pt-Rh clad ThO_2 -Mo spheres as described in Section 4.2.1.3.1.8. Two horizontal tube furnaces with wire-wound resistance heaters were set up for this test program. The furnaces were fitted with 2-inch ID alumina tubes (Morganite) of 33-inch length, sufficient to extend 8-inches from both faces of each furnace. Steel pipe fittings were cemented to each end of the alumina tube to permit air-tight attachment of the inlet air line at one end and a fixture for holding the filter media at the exit end of the tube. Standard air flowmeters were installed in the inlet air lines to provide a uniform air flow rate of 2 cfh. Magnahelic pressure drop indicators were installed at the exit end to continuously monitor the pressure drop across the filter media. The exhaust gas was conducted into a ventilated duct equipped with CWS filters to collect any thoria particles that might become airborne and escape from the test furnace.

Each furnace was instrumented with three standardized Type K thermocouples. Thermocouple No. 1 was located in the alumina tube at the center of the hot zone and alongside the test sphere, and was connected to a multi-point strip chart recorder for continuous monitoring

of the specimen temperature. Thermocouple No. 2 was located between the alumina tube and the heating element at the center of the hot zone and was attached to a temperature control instrument to maintain the furnace temperature constant within 20°C . Thermocouple No. 3 was also located next to the furnace element and was used for an overtemperature safety control. All instruments and thermocouples were calibrated and standardized before testing and in addition a portable potentiometer was used to make millivolt readings for periodic check on the specimen temperature.

The absolute filter media used in the exit end of the furnace tubes consisted of 4.7 cm diameter discs of Watman #41 filter paper, commonly used in air sampling devices.

4.2.1.3.1.3.6.2 Specimen Preparation. Two bare spheres of ThO_2 -18 Mo fuel simulant, 1.49-inch diameter were received from GE-IPSO identified as C-10 and D-41. Four hemispherical shells of iridium were also supplied for cladding the fuel simulant spheres. These hemispheres had a nominal thickness of 0.025-inch and inside diameter of 1.51 inches. However, the wall thickness at the poles of the hemispheres had been thinned down by the forming process to 0.018-0.019-inch. Vent holes 0.005-inch diameter had been drilled at the poles of the cladding shell for C-10 and 0.030-inch holes had been drilled in the cladding shells for D-41.

The iridium shells were fitted to the cermet spheres for welding. The C-10 sphere was too large for complete closure of the cladding shells at the equator and was sized by light blasting with zirconia sand. Sphere D-41 was loose inside its cladding shells but this was not unexpected since the polar radius of the D-41 hemispheres measured 0.758-inch compared to 0.747-inch radius of the C-10 hemispheres. The data obtained before and after welding of the hemispheres is given in Table 4.2.1-17.

The iridium shells were chemically cleaned prior to assembly for welding but were given no heat treatment. The EB welding was performed using predetermined parameters that gave full penetration welds of the 0.025-inch cladding. No bands of iridium foil were used under the weld area. Small cracks were detected in the welds of both spheres after welding and

Table 4.2.1-17. Assembly Data for Specimen Spheres C-10 and D-41

	C-10	D-41
As Received ThO_2 -Mo Sphere Weight, gm.	321.0	324.6
After zirconia sand blast to size sphere weight, gms.	277.0599	286.5537
After sizing sphere diameter, inch	1.4782/1.4892	1.4983/1.5029
Iridium Hemisphere Nos.	C-1, C-2	D-1, D-2
Vent Hole diameter, inch	0.005	0.030
Iridium Hemispheres Weight per pair, gms.	59.7887	62.0060
In. Hemisphere Dimensions, Inch		
I. D. at equator	1.504/1.510	1.506/1.510
Radius at poles	0.746/0.747	0.758/0.758
Wall Thickness	0.018/0.0255	0.019/0.0255
Combined Weight of Core & Clad, as assembled, gms.	336.8486	348.5597
Weight after welding and flat prep. - gms.	336.9222	348.5232
Axial Diameter at poles, inch	1.5252	1.5425
Equator Diameter at Weld, 90° apart, inch	1.5602/1.5557	1.5671/1.5685
	D-41 Sphere loose Inside Cladding.	

repair welds were made in these areas. No stress-relief or annealing heat treatment was given these spheres following welding.

Prior to testing each sphere was weighed to the nearest 0.1 mg and measured at three diameters (2 places around the weld girth 90 degrees apart and at the poles over the vent holes) to the nearest 0.0001-inch. Diametral measurements were made with a hand micrometer equipped with a dial indicator reading to 0.0001-inch. Flats were ground at the weld band 90 degrees apart to assist in accurate and repeatable measurements at each periodic inspection. Both spheres were photographed in the pre-test condition as shown in Figure 4.2.1-88. The initial weight and dimensions are given in Tables 4.2.1-18 and 4.2.1-19.

4.2.1.3.1.3.6.3 Test Procedure. After complete inspection of the clad spheres, they were placed on a bed of alumina granules in an alumina furnace boat and inserted into the furnace tube so oriented that the vent holes were aligned parallel to the furnace floor. This orientation was maintained throughout the test so that the same hemisphere faced upstream of the air flow.

The boat carrying the sphere was slowly inserted into the hot zone alongside the thermocouple. The filter media was attached to the end of the furnace tube and the air flow was started and adjusted to 2 cfh, as measured by calibrated flowmeters.

During the first eight hours of testing at 800°C close surveillance was maintained on the pressure differential across the filter media to detect possible accumulation of particulate matter released through the vent holes of the spheres. However, no change in pressure across the filter on either furnace was observed during the first 24 hours of test. Inspection of each sphere was made after 24 hours at 800°C for weight and dimensional changes and photographs were made. A subsequent check of the filter media with a radiation detector* indicated no alpha activity and hence no thorium was airborne.

*An internal gas proportional counter, Model PC-3B, Nuclear Measurements, with a counting efficiency of 50% geometry was used to survey all filter media after a five-day wait for decay of radon.

Figure 4.2.1-88. Vented Iridium Clad ThO_2 -Mo Simulant Spheres (Nos. C-10 and D-41) Prior to Oxidation Testing. No C-10 has 0.005-inch² Vent Holes and No. D-41 has 0.030-Inch Vent Holes in Each Pole

Table 4.2.1-18. Results of Periodic Inspections of Vented Iridium Clad Sphere No. C-10
after Exposure to Air at 700° to 900°C

Time at Temp., hrs.	Weight, gms.	Weight Gain, Mg.		Diameter			Filter Activity		Comments
		Increment	Total	Polar Axis	Weld Girth		Filter No.	Alpha Count*	
Pre-test (800°C) 24	336.9222	0	0	1.5252	1.5602	1.5557	-	-	Dark purple - 2 short cracks in weld. No change visually.
	337.1171	194	194	1.5267	1.5608	1.5569	1 ^a	0	
48	337.2123	95	290	1.5269	1.5609	1.5559	2	0	Third crack in weld.
72	337.2886	76	366	1.5276	1.5620	1.5562	3	0	
96	337.3633	75	441	1.5276	1.5624	1.5566	4	0	
120	337.4630	100	541	1.5279	1.5626	1.5568	5	0	
144	337.5610	98	639	1.5279	1.5635	1.5569	6	0	
168	337.6412	80	719	1.5286	1.5639	1.5570	7	0	Fourth crack in weld - 90° apart.
336	337.9054	264	983	1.5300	1.5645	1.5575	9	0	
508	337.9687	63	1046	1.5307	1.5652	1.5578	11	0	
844	338.0441	75	1122	1.5313	1.5660	1.5581	13	0	
1180	338.1045	60	1182	1.5326	1.5673	1.5588	15	0	
(800°C)1516	338.1892	85	1267	1.5331	1.5693	1.5638 ^b	17	0	Crack #1 widened.
(700°C)1852	338.2094	20	1287	1.5337	1.5692	1.5640 ^b	19	0	
(700°C)2188	338.2189	10	1297	1.5343	1.5693	1.5642 ^b	21	0	
(700°C)2375	338.2224	4	1300	1.5343	1.5694	1.5648 ^b	23	0	
(900°C)2524	338.3346	112	1412	1.5371	1.5722 ^b	1.5703 ^b	25	0	
2860	338.8161	482	1894	1.5400	N.D.	N.D.	27	0	Filter very dark grey. Crack #1 - ~2" long. Cracks #1 and #2 joined; ~3" long. Wide Crack. Weld cracked all way around.
3188	339.2770	462	2355	1.5521	N.D.	N.D.	29	0	
3568	339.8104	533	2888	1.5592	N.D.	N.D.	31	1	
(900°C)3902	340.2503	440	3328	1.5662	1.6521 ^b	1.6562 ^b	33	0	

* Average alpha counts/min. based on 2-minute counting period.

^a Sphere D-41 tested in same furnace tube with C-10 for 168 hours.

^b Crack in weld interferes with measurement.

Table 4.2.1-19. Results of Periodic Inspections of Iridium Clad Sphere D-41
Exposed to Air at 800°C

Time at Temp., Hrs.	Weight, gms.	Weight Gain, Mg.		Diameters, inch			Filter Activity		Comments
		Increment	Total	Polar Axis	Weld Girth		Filter No.	Alpha Count*	
					0°	90°			
Pre-test	348.5232	0	0	1.5425	1.5685	1.5671	-	-	
24	348.5612	38	38	1.532	1.5690	1.5673	1	0	Dark purple oxide except at weld(golden) .030" vent holes clear
48	348.6048	44	82	1.5445	1.5685	1.5672	2	0	
72	348.6325	28	109	1.5473	1.5685	1.5673	3	0	
96	348.6592	27	136	1.5477	1.5684	1.5671	4	0	
120	348.6854	26	162	1.5481	1.5681	1.5669	5	0	Fine crack in weld.
144	348.7199	35	197	1.5475	1.5681	1.5669	6	0	Purple oxide over weld
168	348.7420	22	219	1.5485	1.5682	1.5670	7	0	Transferred to Furnace #2.
336	348.9601	218	437	1.5506	1.5678	1.5665	8	0	Crack in weld wider.
508	349.2250	265	702	1.5524	1.5679	1.5667	10	0	2 new cracks in weld.
844	349.4576	232	934	1.5530	1.5692	1.5680	12	0	Darker oxide than C-10
1180	349.5691	112	1046	1.5547	1.5701	1.5704	14	0	
1516	349.7290	160	1206	1.5594	1.5723	1.5730	16	0	All cracks widened.
1852	349.9306	212	1407	1.5642	1.5743	1.5772 ^a	18	0	
2188	350.1187	188	1596	1.5667	1.5753	1.5780 ^a	20/22	0	
2524	350.2167	98	1694	1.5681	1.5757 ^a	1.5781 ^a	24	0	
2860	350.3064	90	1783	1.5691	N.D.	N.D.	26	0	2 cracks joined; 1" long.
3188	350.3698	63	1847	1.5701	N.D.	N.D.	28	0	
3568	350.4557	86	1933	1.5711	N.D.	N.D.	30	0	
3902	350.5354	80	2012	1.5722	1.5789 ^a	1.5802 ^a	32	0	2 cracks in weld - longest 1.2".

* Average alpha counts/min. based on 2-minute counting period.

^a Crack in weld interferes with measurement.

After the first 24-hour test period the C-10 sphere was placed in the same furnace as D-41 sphere since the second furnace was required to complete the testing of a Pt-Rh clad sphere at 1000°C. The two iridium clad spheres were tested together in the same furnace tube at 800°C for the next six cycles or until 168 hours at temperature was completed. After that time the spheres were tested in separate furnaces. Inspections were made after each 24 hours of test for the initial 168 hours and thereafter regular inspections were made each weekly or bi-weekly period. The data obtained from these periodic inspections are given in Tables 4.2.1-18 and 4.2.1-19.

Sphere D-41 was tested at 800°C in flowing air for a total of 3902 hours (20 thermal cycles to R. T.). It was then given final inspection and subjected to destructive metallographic analysis. Sphere C-10 with 0.005-inch vent holes was tested at 800°C for 1516 hours, at 700°C for 859 hours, and at 900°C for the final 1527 hours. The test temperature was reduced to 700°C to observe the behavior of the sphere when equilibrium was established between the rate of oxidation of SSCS and the rate of evaporation of MoOx. Under these conditions, little or no change in the sphere was observed. However, under the 900°C test conditions the weight gain and diametral changes were greatly accelerated.

After a total of 3902 hours at temperature sphere C-10 was given final inspection and was subjected to destructive metallographic examination.

Copies of the engineering test logs for each sphere are included as an appendix to this report.

4.2.1.3.1.3.6.4 Test Results. Sphere No. D-41 was clad with 0.018 to 0.025-inch thick iridium hemispheres with 0.030-inch diameter vent holes at the poles. This sphere was tested at 800°C in air flowing at the rate of 2 cfh. The effluent air from the furnace tube was passed through an absolute filter and throughout the test no radioactivity was detected on any filter media, indicating no airborne thorium was released from the clad fuel simulant. A dark oxide film formed over the iridium surface in the initial 24-hour test period and over the weld-affected area after 120 hours at temperature (Figure 4.2.1-89). This

Figure 4.2.1-89 . Upstream face of vented iridium clad ThO₂-Mo simulants after 168 hours at 800°C in air. Sphere No. 10 has 0.005" vent holes; Sphere No. 41 has 0.030" vent holes.

oxide film remained stable for the duration of the test (Figures 4.2.1-90 and 4.2.1-91. A single fine crack was noted in the weld seam after 120 hours at 800°C and 5 thermal cycles. This initial crack (Figure 4.2.1-92) opened slowly, and at 508 hours two new cracks were noted in the weldment. The cracks developed in areas that appeared to have been repaired in the initial welding of the hemispheres (Figure 4.2.1-93). Although these cracks gradually widened, no evidence was noted of material emerging from the core. At the conclusion of the test, two of the cracks had joined making a single crack 1.08-inch long by 0.015-inch wide (Figures 4.2.1-94 and 4.2.1-95). The smaller crack measured 0.28-inch long by 0.035-inch wide. The weight of the sphere increased at a fairly constant rate with a total of 2.01 grams gained in 3902 hours (Figure 4.2.1-96). The axial diameter between the 0.030-inch vent holes increased at a steady rate to a total of 0.0297-inch, equivalent to 1.9 percent (Table 4.2.1-19). Diametral increases at the weld band could not be measured accurately after the cracks developed, but up to 1850 hours the increase was less than half that of the axial diameter.

Sphere No. C-10 was also clad with 0.018 to 0.025-inch thick iridium hemispheres with 0.005-inch diameter vent holes at the poles. This sphere was tested at 800°C for 1516 hours, at 700°C for 859 hours and at 900°C for 1527 hours, for a total of 3902 hours at test temperature. No radioactivity was detected on any of the filter media of the effluent air, but a dark gray oxide deposit was noted on the filter when the iridium clad sphere was tested at 900°C. Two short cracks in the weld were noted after the first 24 hours of test at 800°C, a third crack developed after 120 hours and a fourth crack was noted after 508 hours (9 thermal cycles) (Figure 4.2.1-97). These cracks slowly widened but did not lengthen until the temperature was increased to 900°C (Figure 4.2.1-98). Core material, probably molybdenum oxide, coated the iridium surface around one of the cracks (Figure 4.2.1-91). No significant changes were observed while the sphere was on test at 700°C for 859 hours.

After approximately 500 hours at 900°C the largest crack was about 2 inches in length and after 1193 hours (3568 hours TAT) the cracks opened and extended half-way around the sphere in the weld band. Short longitudinal cracks of an intergranular type also developed in the heat affected zone of the weld. At the termination of the test the crack in the weld extended

Figure 4.2.1-90. Appearance of Iridium clad spheres C-10 and D-41 after 800°C oxidation testing for time periods shown. Upstream vent holes are shown in all photos.

Figure 4.2.1-91 . Appearance of Iridium clad spheres C-10 and D-41 after oxidation testing for time periods shown. C-10 was exposed to 900°C air after 2375 hours while D-41 was tested at 800°C. Note crack propagation in weld of C-10.

71-1-12C

24X

Figure 4.2.1-92 . Crack in weldment of Iridium clad Sphere No. D-41 after 168 hours at 800°C in air.

71-2-12C

Figure 4.2.1-93 . Crack in weldment of Sphere D-41 after 844 hours at 800°C in flowing air.

71-7-8K

71-7-8J

Figure 4.2.1-94 . Post-test condition of Iridium clad spheres C-10 and D-41 showing cracks in weld between hemispheres.

Figure 4.2.1-95 . Crack in weld zone of Sphere D-41 after 3902 hours exposure to air at 800°C.

Figure 4.2.1-96. Weight gain and diametral increase for two vented iridium-clad $\text{ThO}_2\text{-Mo}$ spheres exposed to flowing air at 700°-900°C.

71-1-12B

24X

Figure 4.2.1-97 . Crack in weldment of Iridium clad Sphere No. C-10 after 168 hours at 800°C in air.

71-2-12B

Figure 4.2.1-98 . Crack in weld zone of Sphere C-10 after 844 hours at 800°C in flowing air.

completely around the sphere (Figures 4.2.1-94 and 4.2.1-99). The iridium cladding surface had a thin smooth oxide film which changed little throughout the test (Figure 4.2.1-100).

The weight of C-10 increased gradually at 800°C, but at a faster rate initially than that of sphere D-41 with the larger vent holes (Table 4.2.1-18). Very slight weight increases were noted during the 700°C test phase. Increasing the temperature to 900°C resulted in a sharp increase in the weight gain rate (Figure 4.2.1-96). Average weight gain rates in mg/hr were 0.038 at 700°C, 0.835 at 800°C and 1.328 at 900°C. The axial diameter between the vent holes increased gradually at 800°C (9 mils in 1516 hours) only slightly during the 700°C test phase, but increased sharply with the increase in test temperature to 900°C. The total diametral change at the poles was 0.041 inch (2.7%). Measurements at the equator in the weld band could not be made accurately after the 2524-hour mark because the cracks interfered. Up to that time the equatorial diameter growth lagged behind that of the polar diameter. Although the crack in the weldment opened to a width of 0.060-inch maximum and the simulant core was well exposed to air, no evidence was found of thoria loss from the sphere.

Post-test measurements of the vent holes were made by microscopic techniques. The original 5 mil holes in Sphere C-10 had enlarged to 0.015-inch maximum diameter and were out of round. The upstream hole measured 0.011 to 0.015-inch at the cladding surface and 0.007 to 0.010-inch at the bottom of the hole. The downstream hole measured 0.007 to 0.013-inch at the surface and 0.010 to 0.015-inch at the bottom of the hole.

Very little change was detected in the vent holes of D-41. The original 30 mil holes measured 0.032 to 0.033-inch diameter after the 3902 hours of testing at 800°C.

4.2.1.3.1.6.5 Metallographic Examination. For metallographic evaluations both spheres were impregnated with epoxy and then cut in half along the axial diameter in a section through the widest crack. Quarter sections were then cut to permit examination of a lateral plain near the weld band. These sections were polished and etched (Figures 4.2.1-101 and 4.2.1-102). Metallographic examination revealed an oxide layer or coating on the inside surface

Figure 4.2.1-99 . Rupture of weld and cracks in Iridium cladding of Sphere C-10 after 3902 hours exposure to air at 700° to 900°C.

71-7-8A

Figure 4.2.1-100. Iridium clad ThO₂-Mo Spheres Nos. C-10 and D-41 after 3902 hours at 800° and 900°C in flowing air.

K37021-A
Section C-C 90° from half-section
A-A below. (Mag. 3X)

K37011-A
Half Section A-A (Mag 2X)

Figure 4.2.1-101. Sections through axial diameter of Sphere D-41 showing crack in weldment and depth of oxidation penetration into cermet core after 3902 hours exposure to air at 800°C.

K37031-A

Section C-C, 90° from half-section
below (Mag. 3X)

K37031-A

Half Section A-A (Mag. 2X)

Figure 4.2.1-102. Sections through axial diameter of Sphere C-10 showing rupture of weld between hemispheres and depth of oxidation penetration into cermet core.

of the iridium cladding of both spheres. This layer varied in thickness depending on the proximity to the vent holes or the crack in the weld. This layer appeared to be bonded to the inside surface of the iridium hemisphere (Figures 4.2.1-103 and 4.2.1-104) and to have formed from the iridium since the cladding thickness was reduced in proportion to the thickness of the oxide layer at the point. The oxide coating is presumed to be a combination of oxides of molybdenum and iridium. The maximum thickness of this oxide layer was 0.021-inch in the section of D-41 (800°C) and 0.004-inch in examined sections of C-10 (900°C) (Figures 4.2.1-105 and 4.2.1-106).

Beneath the vent holes and also the cracks in the welds there was a layer of thoria particles that had become depleted of the molybdenum matrix. As noted in previous oxidation tests of this cermet, the thoria particles were fractured in layers parallel to the cladding surface (Figures 4.2.1-107 and 4.2.1-108). The depth of this oxidized band ranged from 0.061-inch beneath the vent holes to 0.154-inch beneath the crack in the weld of Sphere C-10. In sphere D-41, with larger vent holes but less cracking of the weldment, depth of oxidation was 0.033-inch beneath the vent holes and a maximum of 0.071-inch under the crack in the weldment. This oxidation of the core in sphere D-41 at 800°C is about half that of sphere C-10 tested at 900°C. Any correlation between the size of vent holes in the two spheres could not be established because of the cracks in the weldments which opened to become the dominant factor in oxidation of the cermet simulant core.

The iridium cladding showed excellent oxidation resistance on the exterior with no measurable oxide layer showing in the polished sections. In the interior of the hemisphere cladding the iridium was oxidized to a maximum depth of 0.006-inch near the vent hole of sphere D-41. However, only 45 degrees from the vent hole the iridium was unaffected, and this was an area where no molybdenum oxide was formed. Generally, the iridium showed minimal grain growth and was resistant to intergranular penetration except in the areas of weldments. The fused metal of the welds and the heat-affected zone had large grains and appeared to have suffered from intergranular oxidation (Figures 4.2.1-109 and 4.2.1-110), with subsequent cracking as the fuel core expanded.

X
Ir cladding at
polar vent

Y
Ir cladding
45° from pole

Z
Ir cladding at
equator near
weldment

Figure 4.2.1-103. Sections of iridium cladding of Sphere D-41 at 30-mil vent, 45° from pole and at weldment. Locations of these sections are indicated in Figure 4.2.1-105.

A.
Cladding at pole,
near 5 mil vent

B.
Cladding 45°
from pole

C.
Cladding at
equator in
weld zone

Figure 4.2.1-104. Sections of iridium cladding of Sphere C-10 at pole, 45° from pole, and at weld band. Locations of these sections are indicated in Figure 4.2.1-106. (Mag. 75X)

Figure 4.2.1-105. Measurements of Iridium cladding thickness, oxide layer, void band and depletion zone of Sphere D-41 after 3902 hours exposed in air at 800°C.

Figure 4.2.1-106. Measurements of Iridium cladding thickness, oxide layer, void band and depletion zone of Sphere C-10 after exposure to air at 700^o-900^oC for a total of 3902 hours.

Figure 4.2.1-107. Longitudinal section through axis of Sphere D-41 at 0.030-inch vent hole, showing oxide layer on inside surface of iridium cladding, wide depletion zone and unaffected ThO₂-Mo cermet. (Mag. 75X)

Figure 4.2.1-108. Longitudinal section through axis of Sphere C-10 near 0.005-inch vent hole showing thin oxide layer on inside surface of iridium hemisphere, wide depletion band and unaffected ThO_2 -Mo cermet core. (Mag. 75X)

Figure 4.2.1-109. Sections of iridium cladding of Sphere D-41 showing structure of heat-affected zone and intergranular failure in weldment. (Mag. 100X, etched)

Figure 4.2.1-110. Sections of iridium cladding of Sphere C-10 showing structure at crack in weld and at heat-affected zone of weld. (Mag. 100X, etched)

4.2.1.3.1.3.6.6 Summary & Conclusions. Two vented iridium clad ThO_2 -Mo fuel simulant spheres were subjected to oxidation tests in flowing air at temperatures ranging from 700°C to 900°C for a total of 3902 hours with approximately 20 thermal cycles to room temperature. Periodic inspections were made for defects in the cladding, weight changes and diametral changes. Post-test metallographic evaluations were performed on sections of each sphere to determine the behavior of the ThO_2 -Mo core and the iridium cladding in relation to the different size vent holes.

Sphere No. D-41 was clad with 0.025-inch thick iridium hemispheres with 0.030-inch vent holes at opposite poles. This sphere was tested at 800°C in air flowing at the rate of 2 cfh. A dark oxide film formed over the Ir surface and became stable after about 120 hours at temperature. A small crack appeared in the weldment after 120 hours and 5 thermal cycles and two additional cracks appeared after 508 hours. These cracks gradually widened to 0.035-inch maximum at the end of the test but no airborne thorium was detected on the filter media. A total of 2 grams weight gain was noted and a diametral increase of 0.030-inch (1.9%).

Sphere No. C-10 was also clad with 0.025-inch thick iridium hemispheres with 0.005-inch vent holes at the poles. This sphere was subjected to a varied test program in the interest of observing behavior under conditions of equilibrium between oxidation and vaporization rates. This sphere was tested at 800°C for 1516 hours, at 700°C for 859 hours and at 900°C for 1527 hours. No alpha activity indicative of ThO_2 was detected on any of the filter media of the effluent air. Cracks developed in the weldment after 24 hours and gradually increased to encircle the sphere at the end of the test. The weight of the sphere increased 3.3 grams total and the diametral growth was 0.041-inch (2.7%).

The effect of the two sizes of vent holes on the behavior of the spheres could not be established since the cracks in the weldments became the dominant factor in oxidation of the core. During the first 1500 hours when both spheres were tested at 800°C the weight gain was nearly equal but the diametral growth was greater for the larger vent hole.

Metallographic examination revealed an adherent oxide layer was on the inside surface of the iridium cladding of both spheres and was heaviest near the vents or cracks. This coating is presumed to be a combination of oxides of molybdenum and iridium.

In those areas that had been depleted of molybdenum as a result of oxidation, the thoria particles were fractured in layers parallel to the cladding surface. The depth of oxidation penetration into the core reached a maximum of 0.071-inch in the sphere tested at 800°C and 0.154-inch in the 900°C tested sphere.

The iridium cladding showed excellent oxidation resistance on the exterior but was attacked by MoO_x on the interior wall to a depth of 0.006-inch in the 800°C tested sphere, but only in those areas where an excess of air or oxygen was present.

By way of comparison, a Pt-Rh clad sphere No. 39 was previously tested at 800°C under the same conditions as the iridium clad sphere D-41, including the same size vent holes. After 168 hours the diametral increase was 10 mils for Pt-Rh and 6 mils for Ir cladding while weight increases were 382 mg for Pt-Rh and 219 mg for Ir clad sphere. From the previous test results, it was concluded that the vented Pt-Rh clad sphere was capable of 2000 hours at 800°C without cladding failure. By the same measures, the iridium cladding at 800°C should be capable of 10,000 hours protection, provided the weldments were properly treated to prevent cracking. At 900°C the oxidation rate of the cermet fuel simulant core is greatly accelerated over the rate at 800°C and the resultant swelling of the fuel core would be expected to cause rupture of the cladding, even if the weldments did not crack.

4.2.1.3.1.4 Iridium Emissivity. Iridium is considered the prime candidate for the post impact containment member in the MHW heat source.

In addition, iridium is now considered the prime candidate for the heat source outer can in place of an oxidation resistance coated columbium base alloy.

Post-impact fuel containment is dependent on the average temperature of a bare iridium fuel sphere on the ground and completely exposed to air. In order that the iridium provide containment for one year or more it is necessary that temperatures do not exceed the iridium oxide sublimation temperatures of 815°C (1500°F). This requirement can be achieved by maintaining surface emissivity above 0.4 for the unburied condition. The effect of emissivity on temperature of the bare PPO fueled sphere is discussed in Section 4.2.2.6.1.

The estimated sphere impact temperature is 1370°C (2500°F) which is then reduced to 1150°C (2100°F) by conductive and convective heat losses, in air and unburied.

At the instant of impact, it is assumed that the graphite crush-up material will crack off and/or oxidize away exposing the clean iridium liner to an oxidizing environment at 1370°C which then decreases to 1150°C . Under this condition surface oxidation will take place, resulting in the formation in a blue-black oxide. If the rate of oxide evaporation is slower than the rate of formation the increase in surface emissivity will result with a proportional decrease in sphere temperature and rate of loss of iridium oxide. If, however, the oxide evaporates the instant it forms, temperatures will not decrease and iridium mass loss will take place at unacceptable rates.

To evaluate the oxidation and emissivity performance of iridium, a series of tests were performed. The objective of the tests are outlined below:

1. Determine the emissivity of oxidized iridium at selected temperatures of oxidation.
2. Establish the stability of surface oxide emissivity at various temperatures.

3. Determine the effect of impact temperature on emissivity of oxidized iridium.
4. Determine the stability of iridium oxide emissivity in vacuum at heat source surface operating temperatures.

Test Specimens and Usage

Group A Specimens - Iridium discs, 59/64" dia x 0.020" thick.

Group A specimens were oxidized over a 24-hour period at the temperatures and conditions tabulated below.

- 1A Oxidize at 815⁰C
- 2A Oxidize at 975⁰C
- 3A Oxidize at 1025⁰C
- 4A Oxidize at 1075⁰C
- 5A Oxidize at 1123⁰C for one hour followed by cooling in argon from 1125⁰C to room temperature.
- 6A Same as specimen 5A but oxidized at 1200⁰C for one hour
- 7A Same as specimen 5A oxidized at 1370⁰C for one hour

Each specimen was weighed before and after oxidation.

Spectral emittance was measured after oxidation and total normal emittance calculated from spectral data.

Group B Specimens - Iridium strips, 8" x 0.250" x 0.020".

Group B specimens were oxidized and measured for total hemispherical emittance. The following specimens were tested:

- (1B) Oxidize at 815⁰C for 24 hours. Measure total hemispherical emittance at 1100⁰C in vacuum. Hold temperature constant at 1100⁰C for a total 168 hours and measure total hemispherical emittance at 24 hour intervals.
- (2B) Life test specimen oxidized at 815⁰C. Heat in vacuum at 1100⁰C and test at the end of each 168 hours.

4.2.1.3.1.4.1 Spectral Emittance of Oxidized Iridium. The group of seven iridium discs, described above, were oxidized in a convective air environment in accordance with the time and temperatures given in Table 4.2.1-20 . Also given is the total normal emittance calculated from measured spectral reflectance data. Figure 4.2.1-111 is a plot of spectral emittance as a function of wavelength. This shows that at temperatures below 975°C and at wavelengths less than 2 microns emittance is greater than 0.5. When normalized to black-body radiation at 975°C, peak energy radiation takes place between 1 and 3 microns and involves 43% of the total radiation. Figure 4.2.1-112, shows a view of the iridium discs after oxidation at temperatures of 815°C and 975°C a blue black IrO₂ oxide forms which acts as a barrier to continued oxidation. At 1025°C and above the surface retains the appearance of unoxidized iridium. The curves for spectral emittance at temperatures given in Figure 4.2.1-111 indicate that a thin film of oxide remains on the surface at temperatures between 1000°C and 1100°C with the transition to a clean surface taking place above 1075°C.

Table 4.2.1-20. Spectral Emittance of Oxidized Iridium

Specimen	Temp. °C	Time - Hrs	Total Normal Emissivity	
			38°C	815°C*
1	815 ^o	24	0.10	0.32
2	975 ^o	24	0.15	0.42
3	1025 ^o	24	0.15	0.23
4	1075 ^o	24	0.14	0.24
5	1125 ^o	1	0.05	0.12
6	1200 ^o	1	0.05	0.12
7	1370 ^o	1	0.04	0.07

*Data taken at 38°C and normalized relative to blackbody temperature of 815°C

4.2.1.3.1.4.2 Total Hemispherical Emittance of Clean and Oxidized Iridium. Measurement of total hemispherical emittance was carried out by the hot filament calorimetric method which utilizes an iridium test specimen 8 units long by 0.250 inch wide and 0.020 inch thick.

Figure 4.2.1-111. Spectral Emittance

Figure 4.2.1-112. Oxidized Iridum

The specimen is instrumented with a Pt-Rh thermocouple at the center and with Pt voltage probes spaced one inch above and below the thermocouple. The specimen is resistively heated to the desired test temperatures in vacuum and at pressures below 10^{-6} torr.

Total hemispherical emittance is calculated as follows:

$$\epsilon_{TH} = \frac{VI}{A \sigma (T_1^4 - T_2^4)}$$

where

- ϵ_{TH} = total hemispherical emittance
- V = voltage drop across the voltage probes, volts
- I = current passing through the test specimen, amperes
- A = total radiating surface between voltage probes, square centimeters
- σ = Stephan-Boltzmann constant, 5.6686×10^{-12} watts/cm² °K⁴
- T_1 = temperature of test specimen, °K
- T_2 = temperature of vacuum chamber internal wall, °K

To date one specimen of iridium has been tested under various environmental conditions. At the outset total hemispherical emittance of clean iridium was determined as a function of temperature in a vacuum of 4×10^{-7} torr. Figure 4.2.1-113 shows emittance ranges from 0.194 at 790°C to 0.187 at 1370°C. The specimen was then resistively heated in air for one hour at 1363°C. This is close to calculated ground impact temperature of 1370°C. The specimen was next tested in vacuum at 1355°C. Total hemispherical emittance at this temperature is 0.203 and is plotted in Figure 4.2.1-113. This value is very close to that determined for clean iridium. From visual observation the specimen surface remained bright and clean but had an etched appearance.

Based on an emittance of 0.203, post impact temperature drops from 1370°C to 882°C. Therefore, oxidation of the iridium specimen was allowed to take place in air for four hours at 882°C followed by vacuum determination of emittance at 882°C. As a result total hemispherical emittance increased from 0.203 to 0.334. A thin film of oxide formed on the surface as evidenced by a light amber color. The specimen was again heated for four hours in air. Total hemispherical emittance increased still further from 0.334 to 0.396. Oxide discoloring remained unchanged from the previous heating. The specimen was again heated in air at 882°C for an additional 26 hours resulting in a further increase in total hemispherical emittance from 0.396 to 0.564. During this heating, surface oxidation takes place with the formation of the characteristic blue-black iridium oxide (IrO_2).

Figure 4.2.1-114, gives the rate of change of emittance as a result of heating in air at selected temperatures. The change is computed by taking the difference between the effective emittance at given time intervals and the effective emittance at time zero. Curve #1 shows that emittance increases 32% as a result of heating 12 hours at 880°C. Adding this to the starting emittance of 0.203 the resultant emittance increases to 0.523 in 12 hours. Curve #3 shows that emittance decreases 4% after heating in vacuum at 882°C for 30 hours. At the end of this time the resultant emittance decreases to 0.483. Curve #2 shows that the rate of change in emittance substantially decreases when the initial effective emittance is 0.56 compared to 0.46. Here the increase in 30 hours is 16% compared to 32% in 12 hours.

Figure 4.2.1-113. Total Hemispherical Emittance

Figure 4.2.1-114. Change in Total Hemispherical Emittance

4.2.1.3.1.4.3 Summary and Conclusions

1. Total hemispherical emittance of clean iridium is 0.18 ± 0.05
2. Emittance increases to 0.2 on heating in air at 1370°C for one hour
3. At 882°C iridium forms a sufficient oxide layer to cause emittance to increase to 0.39 in eight hours and to 0.56 in 26 hours
4. The oxide layer is unstable in vacuum which results in a linear-decrease in emittance with time. Because of this instability it will be necessary to coat an iridium outer can of the heat source with a stable coating having an emittance greater than 0.85. The highest emittance observed for oxidized iridium during this investigation was 0.564.

4.2.1.3.1.5 Iridium Fabrication. In the area of fabrication, feasibility development of iridium in numerous shapes was demonstrated. Iridium hemispheres were fabricated and welded (see Section 4.2.1.3.1.6).

Iridium sheet was fabricated in 2 mil, 5 mil, 10 mil, and 20 mil thicknesses. In addition, the test crucibles for the LASL fuel compatibility tests were fabricated as follows:

24 Iridium Hemispheres (10 with weld lips)
40 Iridium crucibles
40-2 mil and 5 mil Iridium Belly Bands

4.2.1.3.1.6 Iridium Weld Development. Iridium is the primary candidate for the MHW post impact shell. Before the use of this material can be implemented, the fabrication technology must be advanced in the areas of forming and welding.

A development program was conducted to develop gas tungsten arc (GTA) welding procedures for the closure weld of the fuel capsule liners of spherical configuration. The specific goals of the program were:

1. To determine the optimum weld current for various weld speeds.
2. To determine the dimensional changes caused during welding.
3. To determine the weld speed which yielded the optimum weld microstructure for impact resistance.

Test Plan. The Iridium welding program was conducted to establish GTA welding parameters for a butt joint, without and with a filler, as shown in Figure 4.2.1-115. Thoria (ThO_2) was used as the simulated fuel.

Because of the limited data on welding Iridium, a weld speed of 8-1/4 ipm was selected arbitrarily to begin the welding trials. After the initial welding trials, weld feasibility was demonstrated. A program plan was then selected to identify the optimum current range for four weld speeds (6, 10, 15, 20 ipm). This was planned in three phases. The first phase was to determine the current range in which full penetration occurs. The current which yielded the best contour* would be selected as the weld current for the second phase of the program to make a demonstration weld. Also, an initial current taper was selected to achieve a constant weld width. The objective of Phase III was to select the optimum weld speed based on metallographic results, as well as to demonstrate the repeatability of the weld process. Two welds at the optimum weld current for the selected weld speed would be made and fully documented in the pre- and postweld conditions. Figure 4.2.1-116 contains the test plan.

*A weld with slight top and bottom reinforcement and approximately 0.080 inch wide was desired.

Figure 4.2.1-115. Joint Cross Section

Weld Speed (ipm)	Weld Time (sec)	Weld Trials (amps)	<u>Phase I</u>	<u>Phase II</u>		<u>Phase III</u>	
			Current Range (amps)	Demonstration Weld (amps)		Demonstration Weld (amps)	
				<u>Starting</u>	<u>Weld</u>	<u>Starting*</u>	<u>Weld Level</u>
8 1/4	33	60					
6	45		60-45	42 ¹	30		
10	30		59-42	53 ²	45		
15	20		72-50	70 ³	66		
20	15		82-71	77 ⁴	77	77 ⁴	77

* Current tapered.

¹ 56-30 amps over first 12.5 seconds.

² 53-45 amps over first 10 seconds.

³ 70-66 amps over first 2.5 seconds.

⁴ Constant throughout weld cycle.

Figure 4.2.1-116. Test Plan

Apparatus. The welding was conducted in a four (4)-foot-diameter welding chamber. The joint alignment was achieved and maintained with a welding fixture which is shown in Figure 4.2.1-117. The fixture was designed to fix the Iridium rings in the Y and Z planes. Also, the fixture had a live center which was spring-loaded to take slack in the Y direction due to weld shrinkage. A rotational fixture was used which was built specifically for General Electric. It has a rotational speed range of 4 to 80 ipm. A Miller ESR Model 400 welding machine was used which has the capability for either automatic or manual GTA welding. Three control modules programmed the up slope, welding, and down slope current during the weld cycle. Also, there is a control to vary current during the weld cycle.

Welding Procedure. A process control record form was kept on each weld which documented preweld physical properties; assembly, tacking, and welding procedures; and postweld physical properties of the Iridium cylinders. Also, the physical measurements of the weld joint, the external and internal soundness of the weld joint, the visual appearance of the ThO₂ disc, Iridium foil and rings, and the microstructure of the weld and heat-affected zones were documented in the process control record form. A numbering system was devised to be used in conjunction with the process control record form sheet. The number identified the material, type of weld, thickness of foil, and the number of welds made: i. e., IB2-3 means Iridium butt weld 0.002-inch foil and the third weld of that type.

Automatic GTA welding was performed in a four (4)-foot weld chamber which was evacuated to 5×10^{-4} torr and then backfilled with welding grade argon gas. Iridium is amenable to welding in air; however, for the welder's protection from the thorium, welding was performed in an enclosed vessel. Tack welding of the Iridium strips into rings was initially performed on the bench in air with small tacks; however, cracking of the tacks was encountered due to the welding stresses. This problem was solved by laying a continuous weld bead across the ends of the strips. This procedure was adopted for the remainder of the program. However, the rings would distort from this welding; therefore, the rings were sized on an expansion mandrel to minimize ovality. Before sizing, the edges of the ring were squared up on a metallographic grinding wheel, then ultrasonically cleaned to remove any Al₂O₃ particles picked up during the grinding. High-magnification (3-10X) inspection of the rings was used to assure that no Al₂O₃ particles remained prior to welding.

Figure 4.2.1-117. Welding Fixture

Even with sizing, considerable misalignment was still encountered due to the difference in diameters. Initially, two different sizes of rings, small and large inside diameters, were made to accommodate the two different sizes of foil (small, 1.530 inch; large, 1.535 inch). However, the variance was so great that some of the rings intended to be small were larger than the large rings. To minimize the misalignment effect, the rings were matched together by diameters. However, due to the large inside diameter variance (1.527 - 1.544 inches) the rings used in the latter portion of the program contain considerable mismatch.

Tack welding of two rings together was initially performed on the bench with four (4) tacks. Tack weld cracking was encountered due to misfit of the edges and misalignment of the diameters. This problem was solved by tack welding in the weld fixture in the weld chamber. This procedure complicated the preweld cylinder width measurements. To take this measurement accurately, the tacked cylinder would have to be removed from the weld chamber before welding. This would have required another pumpdown cycle. Also, the risk of cracking the tack welds would have been encountered. Therefore, the preweld width measurements were deleted for most of the welds.

Because burnback of the edges of the strip was experienced, the edges of both rings to be joined were aligned together so that only one defect and not two was produced during the welding cycle (see Figure 4.2.1-118).

The welding consisted of a dwell time, weld cycle, and down slope. The dwell time was not programmed but was controlled manually by the operator upon onset of a weld puddle. There was no up slope; the operator immediately went to maximum current. The weld cycle was programmed and confirmed before welding on a molybdenum block. The tungsten-2% thorium, 1-16-inch-diameter electrode was tapered and the arc length set with a 0.040-inch shim. Relative alignment of the electrode to the ring joint line was confirmed by visual observation by going through a dummy run. To conserve iridium rings, a procedure was initiated as shown in Figure 4.2.1-118. Two welded cylinders were used to make another weld, thereby increasing the efficiency of the rings to three welds per four rings.

Figure 4.2.1-118. Two Iridium Rings Welded Together to Produce a Third Joint

Welding Trials. The summary of the weld study is contained in Table 4.2.1-21 . During the weld trials, two tee welds were made; however, it soon became apparent that holding of the square filler wire in place would be a problem. The filler wire had a tendency to lift up from the rings which caused weld defects in the form of blow holes. The filler wire could not be tacked in place nor could it be easily formed; therefore, the tee welds were deleted from the remainder of the program.

During the welding trials, problems of tack weld cracking and weld joint cracking were encountered. Both the tack and weld joint cracks gave the appearance of a brittle tensile failure. The tack welds were made larger and partial relief from cracking was obtained. It was hypothesized that misfit between the two rings was the cause of cracking. The solution to this problem was tack welding in the weld fixture and welding immediately thereafter. The parameters for a reliable tack weld were found to be a minimum of 55 amps and a minimum hold time of 2 seconds.

The weld joint cracking problem was the result of too little gap clearance between the ThO_2 foil assembly and the Iridium rings. A plot of the minimum diametral clearance versus observed ThO_2 disc cracks is shown in Figure 4.2.1-119. (The solid circles represent minimum diametral clearance of welding performed in Phase I after a 0.020-inch minimum diametral clearance had been determined.) It was noted that the Iridium rings had shrunk tightly down upon the ThO_2 disc in every case that cracking was observed. Due to the lack of clearance, the ThO_2 disc cracking probably is a result of thermal shock because there was no gas insulating layer between the ThO_2 and weld puddle. Also, there are two other factors which would favor joint cracking during cooldown: (1) the diameter of the rings would decrease at the weld joint line due to shrinkage which occurred upon solidification and (2) the rate of contraction of the Iridium rings is faster during cooling than ThO_2 disc due to the larger expansion coefficient of the Iridium. Based on the results obtained in the welding trials, all the ThO_2 discs were ground to provide a minimum of a 0.020-inch diametral clearance. No other ThO_2 failures were encountered except for one; drippings from a tee weld, where excessive blow holes were experienced, took up the

Table 4.2.1-21 . Summary of Weld Study

S/N	Type of Weld	Thickness of Foil (in.)	ThO ₂ Disc	Welding Trials		Postweld Dimensions	Fluorescent Radiography	Metallography
				Weld Speed(ipm)	Weld Current(amps)			
IB0-1	Butt	None	None	8 1/4	60	X	-	- X
IB5-1	Butt	0.005	C-3*	8 1/4	60	-	-	- X
IB5-2	Butt	0.005	C-8	8 1/4	58	X	X	X X
IB2-1	Butt	0.002	C-7	8 1/4	60-58	X	-	- -
IB2-2	Butt	0.002	C-6*	8 1/4	60-58	-	X	- -
IT5-1	Tee	0.005	C-9*	8 1/4	60	X	-	- -
IT5-2	Tee	0.005	C-8	8 1/4	60	X	X	X X
<u>Phase I</u>								
IB2-3	Butt	0.002	C-2	6	60-45	X	X	X X
IB2-4	Butt	0.002	C-2	10	59-42	X	X	X X
IB2-5	Butt	0.002	C-2	15	72-50	X	X	X X
<u>Phase II</u>								
IB2-6	Butt	0.002	C-2	15	70-66	X	X	X X
IB2-7A	Butt	0.002	C-2	10	53-45	-----too cold-----		
IB2-7B	Butt	0.002	C-2	10	58-50	-----too cold-----		
IB2-7C	Butt	0.002	C-2	20	88-82	-	-	- -
IB2-8A	Butt	0.002	C-2	6	56-30	-----too cold-----		
IB2-8B	Butt	0.002	C-2	20	82-71	-	-	- -
IB2-3/4	Butt	0.002	C-2	20	77	X	X	X X
IB2-5/6	Butt	0.002	C-2	15	66	X	X	X X
<u>Phase III</u>								
IB2-9	Butt	0.002	C-2	20	77	X	X	X X
IB0-2	Butt	None	C-2	20	77	X	X	X X
* ThO ₂ disc broken during welding run.

Figure 4.2.1-119. The Effect of Diametrical Clearance on ThO₂ Disc Cracking

clearance and caused the ThO_2 to crack. During the weld trial portion of the program, the weld profile was not ideal. Undercutting and suckup were occurring; however, it was decided to proceed to Phase I of the program.

Phase I. The objective of the Phase I portion of the program was to determine the welding current range which achieved full penetration for various welding speeds. Three welding speeds were selected: 6, 10, and 15 ipm or 45, 30, and 20 seconds of welding time, respectively.

A welding current range of 60-45 amperes was selected for the 6 ipm which achieved full penetration over the entire current range. Figure 4.2.1-120 shows the plot of welding current as a function of welding time. Note that a break in the curve occurs at 53 amps, 21 seconds for the 6 ipm. This occurred because the electrode touched the molten pool. It is hypothesized that the molten pool grew into the electrode. However, more significant is that after a two-minute interlude to reset the electrode, a weld puddle was initiated and maintained at 50 amps and less weld current. (Again the technique of dwell time determined by sight of weld puddle was used.) During the weld trials, a minimum of 55 amps for 2 seconds was required to make a good tack weld. It appeared that the slow speed allowed sufficient preheat to initiate a weld puddle at this low current. Actually, the appearance of the weld indicated a much lower power input would be required to achieve a weld of the desired profile.

Based on the results of the 6 ipm, the power input for the 10 ipm was reduced approximately by one-third as shown in Figure 4.2.1-121. The current was varied continually over 59-42 amps and full penetration was achieved over the entire range. Again, the weld width was larger than desired and, therefore, the power input was slightly reduced for the 15-ipm welding experiment.

The 15-ipm welding experiment resulted in full penetration over the entire range of welding current (72-50 amps). The results were so encouraging that a 20-ipm welding speed was inserted in the Phase II portion of the program.

Figure 4.2.1-120. Current Input as a Function of Weld Speed

Figure 4.2.1-121. Percent of 6 ipm Power Input

It was observed that the weld width of all samples, regardless of weld speed or if the current was constant or varying, increased to a maximum some finite distance from the weld start, as shown in Figure 4.2.1-122. Figure 4.2.1-122 is a plot of the weld width of IB2-4 10-ipm weld speed with weld current varying from 59-42 amperes. Also, the maximum weld width shifted towards the weld start with increasing weld speed, and the maximum weld width decreased with increasing welding speed (for the case of Phase I increasing weld speed represented decreasing power input). Note the maximum weld width point in Figure 4.2.1-122 for 6 ipm. Therefore, in Phase II, an initial current taper was planned to achieve a constant weld width.

Phase II. In Phase II, a welding cycle was selected based upon attaining a constant weld width of approximately 0.080 inch. An initial taper of the current was planned to achieve a constant weld width over the entire weld. Also, a 20-ipm weld experiment was included because of the encouraging results of the 15-ipm weld. The following weld current parameters were selected, as was shown in Table 4.2.1-21 :

- 6 ipm, 56-30 amps, taper to 30 amps over first 12.5 seconds;
- 10 ipm, 53-45 amps, taper to 45 amps over first 10 seconds;
- 15 ipm, 70-66 amps, taper to 66 amps over first 2.5 seconds;
- 20 ipm, 88 amps, no taper.

As shown in Table 4.2.1-21 , the 6 ipm and 10 ipm were too cold to cause full penetration. The same was true when the 10 ipm was rerun at 5 amps higher (58-50). The results for 10 ipm were most confusing based on the results of Phase I where the current range of 59-42 amps achieved full penetration (IB2-4). The analysis of the 6 ipm was more definitive. In Phase I, a weld puddle was maintained at 45 amperes after a significant amount of power had been put into the weld which would cause a large amount of preheat. For the 6-ipm weld in Phase II, only 1/10 of the power had gone into the weld by the time 45 amperes was reached (the minimum current in Phase I) or conversely about 1/10 of the preheat. Thereby, it was concluded that at the low amperage levels preheat is required to maintain a weld puddle.

Figure 4.2.1-122. Weld Width Variance Along Weld

For the 10 ipm, the above reasons can account for less preheat; however, not as dramatically as for the 6 ipm. Postweld inspection indicated that the ThO₂/foil assembly could have touched the Iridium rings during welding. Apparently, due to mismatch the diametral clearance was taken up as the ThO₂ disc revolved in the signs. The ThO₂ disc could have caused a local chilling of the weld puddle when it touched and due to the combination of low preheat and low current level, the weld puddle was frozen off and could not be restarted. The frozen weld puddle which would stop weld shrinkage would also explain why the ThO₂ disc was not cracked.

The 15-ipm results were very encouraging; full penetration was achieved on IB2-6 and the weld width range was reduced to 0.067 to 0.095 inch. The average of weld width was 0.081 inch. The weld profile also indicated reinforcement on the top and bottom of the weld. Another weld was tried at 15 ipm at a constant 66 amps to further reduce the weld width variance; however, due to misalignment of the Iridium rings the weld width variance increased by reducing the weld width.

The optimum weld current range of the 20 ipm had to be determined. The range of 88-82 amps appeared too hot; therefore, another weld at 82-72 was conducted. From that experiment 77 amps was selected. The results of this weld (IB2-3/4) indicated that 77 amps produced a weld width larger than 0.080 inch. Also, mismatch influenced the weld width variance of the Iridium rings. The weld profile indicated top and bottom surface reinforcement. Therefore, due to the uncertainty resulting from mismatch, 77 amps was selected for the welding current for the Phase III studies.

Phase III. The objective of Phase III was to demonstrate the repeatability of the 20-ipm welding speed to achieve high-quality joints; also, two other objectives were added (1) to determine if the foil protected the ThO₂ disc from thermal shock and (2) to determine if a 0.005-inch-diameter hole could be closed by welding (with no filler metal).

The welding parameters were the same as established in Phase II for a 20-ipm welding speed: 20 ipm, 77 amps, no taper. Two welds were made, IB2-9 and IB0-2. The

diameters of the rings for IB2-9 differed in places as much as 0.010 inch, and the edges of IB0-2 were not square; a maximum gap of 0.004 inch was measured. However, of the available Iridium rings, the four chosen were the least objectionable. It was decided that the 0.004-inch gap of IB0-2 would be used instead of the 0.005-inch-diameter machined hole.

The welds of both IB2-9 and IB0-2 appeared sound. The weld width for IB2-9 as shown in Figure 4.2.1-123 varied from 0.078 to 0.092 inch. The occurrence of the maximum weld width confirmed the results of Phase II that the maximum weld width shifted towards the weld start with increasing weld speed. Also, the curve indicated that at constant amperage, the weld width became sinusoidal around 0.080 inch. Both welds, IB2-9 and IB0-2, exhibited top and bottom reinforcement. Therefore, it was considered that the welding speed of 20 ipm produces welds of good reproducible quality.

Iridium cylinder, IB0-2, was used to characterize the role of the foil and to determine if welding could close up a 0.005-inch hole located in the weld joint. The foil was removed from ThO₂ disc, C-2; it was questioned if the foil was a heat shield along with functioning as a spatter shield. The criteria was if or if not the ThO₂ disc cracked. The ThO₂ disc did not crack. The only discoloration observed was at the weld start where burnback of the Iridium edges was observed.

The 0.004-inch gap was welded and appeared sound by fluorescent penetrant inspection. However, radiography indicated cracks in the areas of excessive gap. Also, radiography indicated two cracks in IB2-9. Apparently, due to edge misalignment and mismatch of the diameters, sufficient stresses were produced in these welds to cause failure.

This one experiment does not inclusively indicate that a 0.005-inch-diameter hole cannot be welded shut successfully. In our case, the 0.004-inch gap, which had length, required more volume to be filled than a 0.005-inch-diameter hole and thereby would set up greater welding stresses.

Figure 4.2.1-123. Weld Width of IB2-9

POSTWELD DIMENSIONAL INSPECTION

Diametral Change. The diametral change, both OD and ID, is summarized in Table 4.2.1- . The OD change ranged from +0.001 inch to +0.024 inch; however, if the extremes of the range are deleted from the data, the range would narrow to + 0.006 inch to + 0.015 inch. The ID change was influenced by the inspection technique. The initial data indicated that the ID decrease ranged from 0.025 inch to 0.031 inch. However, the ThO₂ disc, C2, was used successfully throughout twelve welding runs, and the measured diametral clearance varied from 0.020-0.024 inch. Therefore, the ThO₂ disc, C2, should have failed.

The postweld ID was measured with tapered plugs and apparently this technique, which depends on feel, was too crude to be accurate. Therefore, the rings which had not been modified such that a valid ID measurement could be made were remeasured with a depth gage. The depth gage, unlike the tapered plugs, can be placed right at the weld joint.

Table 4.2.1-22, which contains the depth gage measurements, indicates that the ID decrease ranged from 0.026 to 0.003. However, as was mentioned previously, the welds made during the welding trial phase experienced suckup. The maximum decrease in ID occurred from cylinders made during the welding trials (0.026 inch, 0.023 inch). The maximum ID decrease for Phase II and Phase III (no accurate measurements made on Phase I samples) is 0.014 inch. There appears to be a correlation with power (heat) input and ID change. As was shown in Figure 4.2.1-121, the weld joints made during the welding trials had a power input of twice that of the weld joints made in Phase II and Phase III.

Width Change. Over an approximately one-inch width, the data indicated that 1.2 to 0.5 percent shrinkage occurred. As mentioned previously, limited data was gathered on the postweld width of the cylinders.

Weld Thickness. The weld thickness varied from 0.0235 to 0.033 inch. There did not appear to be a correlation with weld thickness and power input.

Table 4.2.1-22 . Diametral Change Due to Welding

S/N	Preweld Dimensions		Postweld Dimensions		Change	
	Avg. OD	Min. ID	Avg. OD	Min. ID	Avg. OD	Avg. ID
IB0-1	1.583	1.538	1.591	1.512	+ 0.006	- 0.026
IT5-2	1.584	-	1.585	-	+ 0.001	-
IB5-2	1.570	-	1.584	-	+ 0.014	-
IB2-1	1.577	1.533	1.586	1.510	+ 0.009	- 0.023
IB2-3	1.575	-	1.587	-	+ 0.008	-
IB2-4	1.572	-	1.580	-	+ 0.008	-
IB2-5	1.569	-	1.593	-	+ 0.024	-
IB2-3/4	1.573	-	1.580	-	+ 0.007	-
IB2-5/6	1.570	-	1.584	-	+ 0.014	-
IB2-7	1.570	1.531	1.575	1.522	+ 0.005	- 0.009
IB2-8	1.568	1.527	1.581	1.513	+ 0.013	- 0.014
IB2-9	1.578	1.533	1.582	1.530	+ 0.004	- 0.003
IB0-2	1.575	1.523	1.576	1.516	+ 0.001	- 0.007

Visual Inspection. Before the diametral clearance was controlled to a minimum of 0.020 inch, failure of the ThO₂ discs and partial melting of the foil occurred. However, control of the diametral clearance solved both of these problems. Twelve welds were made with one ThO₂/foil assembly, C2, with only superficial stains resulting on the foil and with no apparent physical effects to the ThO₂. On the last Phase III weld made, the foil was not included and only a stain resulted on the ThO₂ at the area right below the blow hole.

Fluorescent Penetrant. Only scattered porosity was detected on both OD and ID surfaces. No defects were detected with a depth sufficient to hold penetrant after a dip (no soak time) in trichlorethylene.

Radiography. Spheroidal porosity was observed in every weld that was radiographed. The maximum diameter void observed was 0.008 inch which occurred on a 20-ipm weld; however, typically the voids were less than 0.002 inch. Also it appeared that porosity increased with increasing welding speed. However, the mismatch of the Iridium rings was a maximum for the 15- and 20-ipm welds and the porosity was greater in areas of mismatch. At this time, it is not clear which variable is controlling for porosity, weld speed or mismatch. For example, the last two welds, IB2-9 and IB0-2, were made on rings that contained both diametral and edge mismatch. IB0-2 contained a 0.004-inch gap where on previous welds, the gap had been less than 0.001 inch. For the first time, of all the welds made the radiographs indicated cracks in the weld joint on both rings (IB2-9, IB0-2).

Metallographic Evaluation. A metallographic evaluation was performed on the 6-, 10-, 15-, and 20-ipm welds. The metallographic procedure is shown below. The results indicated that the heat-affected zone decreased as weld speed increased. Also, the microstructure of the weld went from columnar grains to equiaxed grains as the weld speed increased.

Figures 4.2.1-124 to 4.2.1-128 show the microstructure of the parent material, heat-affected zone, and the weld 180° from weld start for the various weld speeds.

(K01011C)

50X

Figure 4.2.1-124. Parent Metal (As Received)

(J99011A)

50X

(J99011B)

50X

Figure 4.2.1-125. Weld and Heat-Affected Zone of IB2-3 (Weld Speed 6 ipm)

(K02011A)

50X

(K02011B)

50X

Figure 4.2.1-126. Weld and Heat-Affected Zone of IB2-4 (Weld Speed 10 ipm)

(J98012A)

50X

(J98012B)

50X

Figure 4.2.1-127. Weld and Heat-Affected Zone of IB2-5/6 (Weld Speed 15 ipm)

(K01011A)

50X

(K01011B)

50X

Figure 4.2.1-128. Weld and Heat-Affected Zone of IB2-3/4 (Weld Speed 20 ipm)

The microstructure of the parent material (as received) was duplexed with a band of coarse grains extending from the core to the 1/4 section (towards ID). The microstructure appeared to be approximately 95 percent recrystallized.

It was quite difficult to achieve a uniform etch and almost impossible to polish scratch-free the microstructures of the welds and heat-affected zones; however, even with these difficulties a trend from columnar to equiaxed grains in the weld and from coarser to finer grain size in the heat-affected zone is seen as weld speed increased. Please note Figure 4.2.1-127 where diameter mismatch (~ 0.010 inch) is dramatically shown.

Also, the entire joint of IB0-2 was metallographically evaluated in the longitudinal view to determine if the microstructure underwent any significant changes from weld start to finish. Except for the double-pass portion which contained coarse equiaxed grains, the microstructure was essentially the same from weld start to finish.

The equiaxed grains in the 20-ipm weld would be more favorable from the standpoint of impact resistance.

Iridium Metallographic Procedure

- Grind: No. 240 thin - No. 600-grit SiC papers.
- Polish: 6-micron diamond on nylon cloth;
1-micron diamond on nylon cloth;
 H_2O_2 + slurry of Linde B (Al_2O_3) on micro cloth.
Slurry of Linde B (Al_2O_3) + 1/2-micron diamond on Texmet.
- Etch: 6 volts AC for minimum of 2 minutes in 50 percent HCl.

RESULTS AND CONCLUSIONS

1. A minimum current of 55 amps for a minimum time of two seconds was required to make a reliable tack weld.
2. A minimum diametrical clearance of 0.020 inch between the ThO_2 /foil assembly and Iridium rings is required to produce a sound weld. This clearance allowed for weld shrinkage and thermal expansion differences.
3. The weld width decreased with increasing welding speed or conversely the weld width decreased with decreasing power input. Also, mismatch of the rings had a tendency to reduce the weld width.
4. Apparently for the low welding current levels (~ 50 -30 amps), preheat is required to maintain a weld puddle.
5. The weld profile for both the 15 and 20 ipm indicated top and bottom reinforcement.
6. The OD changed from + 0.006 inch to + 0.015 inch. The ID decreased from - 0.026 inch to - 0.003 inch with the maximum decrease occurring at the higher power levels.
7. Fluorescent penetrant inspection indicated no defects of any depth regardless of weld speed.
8. Radiography indicated that porosity increased as a function of weld speed. Also, mismatch of the rings influenced porosity and in excessive conditions caused cracking.
9. Metallographic results confirmed the larger amount of porosity in the 15- and 20-ipm weld experiments; however, the microstructure was more refined in the form of a minimum heat-affected zone and equiaxed grains in the weld.

4.2.1.3.1.7 Iridium Material Properties

4.2.1.3.1.7.1 Iridium Bend Ductility. The key to long-term (one year or more) post impact containment and immobilization of the MHW radio isotope fuel is the provision of a fuel liner containment member, which will not breach as a result of impact, at impact temperatures (1370°C), and which resists catastrophic oxidation at post impact temperatures. For a 122 watt bare fuel sphere, post impact temperatures can range from 540°C (1000°F) to 925°C (1700°F) for surface emissivity of 0.8 to 0.2, respectively.

The prime candidate fuel liner materials are Pt-30 or 40 Rh and pure unalloyed iridium. The latter has the disadvantage of being less oxidation resistant than PtRh alloys, but has the advantages of higher strength at temperature, melt temperature above 4000°F , and a lower vapor pressure. It was necessary to establish a threshold oxidation temperature and the rate of oxidation of iridium above the threshold in order to establish the temperature level at which containment can be assured. It is also necessary to determine the effect of welding on oxidation and ductility.

Initial exploratory bend-ductility and oxidation rate tests were conducted on small TIG welded iridium specimens and iridium sheet metal.

4.2.1.3.1.7.1.1 Iridium Test Specimen Preparation. Three (3) sections of iridium sheet, 0.021" thick x 0.25" wide x 2.5" long, were obtained from existing laboratory stock. Two strips were joined along the long edge to provide a single sheet approximately 0.5" wide x 2.5" long. Joining was accomplished using the tungsten-inert-gas (TIG) welding method. Fixturing was not required and the strips were welded on an open air bench.

The welded strip and other remaining unwelded strip were sectioned across the width to provide coupons approximately 0.5" long. A wet silicon-carbide wheel was required for sectioning to alleviate edge cracking which occurred during trial shearing. The original rolling direction of the sheet was across the narrow width, or opposed to the welded joint.

4.2.1.3.1.7.1.2 Iridium Bend-ductility Tests. All bend tests were conducted at room temperature using a 4T channel and punch set. An initial bend test conducted using an unwelded coupon indicated that sheet was very brittle, fracturing after about 10° bend.

Since the original procurement history of the selected sheet was uncertain, it was decided to anneal one coupon at 800°C and another at 1500°C for 30 minutes in vacuum, then bend test both heat treated specimens. Table 4.2.1-23 gives the results of microhardness tests. From these tests, it can be concluded that

- (1) Very little annealing takes place at 800°C. This is substantiated by no evidence of recrystallization (see Figure 4.2.1-129).
- (2) Annealing takes place at 1500°C and near complete recrystallization induced in 30 minutes at temperature (see Figure 4.2.1-131).
- (3) Welding does not increase the brittleness or hardness in the cast metal of the welded zone.
- (4) An oxidizing environment does not increase brittleness or hardness in base metal and welded zones.

Table 4.2.1-23
Hardness of Heat Treated Iridium

Sample	Average DPH - 100 gm Load at room temperature	
	Base Metal	Weld Zone
Annealed - 800°C	825	---
Annealed - 1500°C	286	---
Annealed - 1500°C	260	247
Annealed - 1500°C Oxidation Tested	260	264

The welded coupon annealed at 800°C fractured at about 30° bend. Micro-hardness readings taken in the bend area indicate an average hardness of 525 D. P. H. at 100 gm. loading. The structure was still fibrous, indicating recrystallization had not taken place. Figure 4.2.1-129 shows a photomicrograph of the fractured zone. This is a classical fracture mode with crack propagation limited to the zone in tension and shear.

J14031A

100x Mag.
as etched

Figure 4.2.1-129. Photomicrograph Showing a Fracture Area in 0.021" Thick Iridium Sheet After a 30° 4T Bend Test.
Sheet Condition: As Annealed 1/2 Hr. -800°C - Vacuum

J14021A

100x Mag.
as etched

Figure 4.2.1-130. Photomicrograph Showing a Fracture Area in 0.021" Thick Iridium Sheet After a 45° 4T Bend Test.
Sheet Condition: As Annealed 1/2 Hr. - 1500°C - Vacuum

J14011A

J14011B

75x Mag.
as etched

Figure 4.2.1-131. Composite Photomicrograph of Typical Grain Structure in a TIG Welded Section of 0.021" Thick Iridium Sheet After Annealing for 1/2 Hr. at 1500°C in Vacuum. Weldment is Shown on the Left Side in the Area of Larger Grain Structure.

The other welded coupon annealed at 1500°C, for 30 minutes, fractured after approximately a 45° bend. Both coupons were bent transversely to the weld. Metallographic examination of this coupon revealed recrystallization had taken place during the 1500°C annealing treatment and microhardness in the bent area averaged 286 D.P.H. at 100 gm. load. The fractures were not as extensive as in the 800°C annealed specimen and were inter-granular in nature. All fractures were in the base metal and none were observed in the welded joint. A photomicrograph of a polished cross-section through the fractured zone is shown in Figure 4.2.1-130. All remaining Iridium coupons were annealed at 1500°C for 30 minutes in vacuum. Figure 4.2.1-131 presents a composite photomicrograph showing the grain structure adjacent to, and in the weld area of a coupon after annealing at 1500°C.

4.2.1.3.1.8 Oxidation Testing of Vented Pt-30Rh Clad ThO₂-Mo Fuel Simulant Spheres at 800°C and 1000°C

Introduction. The objective of this program was to determine the behavior of two Pt-30Rh clad ThO₂-Mo spheres in flowing air at elevated temperatures. The spheres were vented by drilling two holes, 0.030-inch diameter through the cladding, approximately 180 degrees apart. One sphere was tested at 800°C for 168 hours and the second sphere was tested at 1000°C for 336 hours in flowing air. The test was periodically interrupted and the spheres were examined, weighed and measured at three diameters. The effluent air was passed through an absolute filter to retain all particulate matter and at the end of each test interval the filter media was checked with a radiation detector to determine if thorium had been carried in the off-gas. At the end of the test both spheres were sectioned and examined metallographically for the effects of oxidation on the fuel core and cladding.

Test Facilities. Two horizontal tube furnaces with wire-wound resistance heaters were set up for this test program. The furnaces were fitted with 2-inch ID alumina tubes (Morganite) of 33-inch length, sufficient to extend 8-inches from both faces of the furnaces. Steel pipe fittings were cemented to each end of the alumina tube to permit air-tight attachment of the inlet air line at one end and a fixture for holding the filter media at the exit end of the furnace tube. Standard air flowmeters were installed in the inlet air line to provide and maintain a

uniform air flow rate of 2 cfh. Magnahelic pressure indicators were installed at the exit end to continuously monitor the pressure drop across the filter media. The exhaust gas was conducted into a ventilated duct equipped with CWS filters to collect any thorium particles that might become airborne and escape from the test furnace.

Each furnace was instrumented with three standardized Type K thermocouples. Thermocouple No. 1 was located in the alumina tube at the center of the hot zone and alongside the test sphere, and was connected to a multi-point strip chart recorder for continuous monitoring of the specimen temperature. Thermocouple No. 2 was located between the alumina tube and the heating element at the center of the hot zone and was attached to a temperature control instrument to maintain the furnace temperature constant within 20°C . Thermocouple No. 3 was also located next to the furnace element and was used for an overtemperature safety control. All instruments and thermocouples were calibrated and standardized before testing; in addition a portable potentiometer was used to make millivolt readings for a daily check on the specimen temperature.

The absolute filter media used in the exit end of the furnace tubes consisted of 4.7 cm diameter discs of Watman #41 filter paper, commonly used in air sampling devices.

Specimen Preparation. Two Pt-30Rh clad ThO_2 -Mo fuel simulant spheres 1.58-inch diameter identified as serial numbers 37 and 39 were used. These spheres had previously been impact tested and No. 39 showed the effects of impaction by indentations of the cladding on one side. Both spheres had a layer of zirconia under the cladding, one applied by spraying and the other filament wound and impregnated with a zirconia slurry.

Before testing, two vent holes 0.030-inch diameter were drilled through the cladding of each sphere, approximately 180 degrees apart and 90 degrees from the welded girth. The drill barely touched the zirconia coating and did not penetrate to the ThO_2 -Mo core. All burrs were carefully removed from the drilled holes.

Prior to test, each sphere was weighed to the nearest 0.1 mg and measured at three diameters (2 places around the welded girth 90 degrees apart and at the opposing poles or vent holes) to the nearest 0.0005-inch. Diametral measurements were made with a hand micrometer equipped with a dial indicator reading to 0.0001-inch. Flats were ground at the weld line 90 degrees apart to assist in accurate and repeatable measurements at each periodic inspection. Both spheres were photographed in the pre-test condition as shown in Figure 4.2.1-132. The initial weight and dimensions are given in Tables 4.2.1-24 and 4.2.1-25 .

Test Procedure. Sphere No. 39 was tested at 800^oC and Sphere No. 37 was tested at 1000^oC. For insertion into the respective furnace tubes the spheres were supported by a bed of alumina granules in an alumina boat. The spheres were positioned so that the vent holes were aligned parallel to the furnace floor and the orientation was maintained so that the same hemisphere faced upstream throughout the test.

The boat carrying the sphere was slowly inserted into the center of the furnace hot zone, alongside the thermocouple. The filter media was attached to the end of the furnace tube and the air flow was started and adjusted to 2 chf, as measured by calibrated flow meters.

During the first eight hours of testing close surveillance was maintained on the pressure differential across the filter media to detect possible accumulation of particulate matter released through the vent holes of the spheres. The pressure differential did not exceed 0.75-inch of water for the 800^oC test over the entire test period. The pressure drop across the filter on the 1000^oC test increased to 2.8 inches of water after 7 hours and the filter was replaced. However, a subsequent check of the filter media with a radiation detector* indicated no alpha activity. Apparently the material collected on the filter was molybdenum oxide.

*An internal gas proportional counter, Model PC-3B, Nuclear Measurements, with a counting efficiency of 50 percent geometry was used to survey all filter media after a five-day delay for decay of radon.

Figure 4.2.1-132. Pt-30Rh Clad ThO_2 -Mo Fuel Simulant Spheres Prior to Testing Showing 0.030-inch Vent Holes Drilled at the Poles of Each Sphere

Table 4.2.1-24 . Results of Daily Inspections of Vented Pt-Rh Clad Sphere No. 39
Oxidation Tested at 800°C

Time at Temp., Hr.	Weight, gm.	Weight Gain-Mg		Diameter			Filter Activity			Comments
		Increment	Total	Polar Axis	Weld 0°	Girth 90°	Filter No.	Duration, Hr.	Alpha* Count	
Pre-test	337.7253	0	0	1.5800	1.5791	1.5987	1	7	0	
24	337.7410	15.7	15.7	1.5817	1.5790	1.5987	2	17	0	Blue tint on upstream face; vents clear.
48	337.7908	59.8	65.5	1.5829	1.5786	1.5988	3	24	0	General darkening of surfaces.
72	337.8461	55.3	120.8	1.5844	1.5782	1.5982	4	24	0	Blue tint general except around vent holes.
96	337.9105	64.4	185.2	1.5858	1.5780	1.5979	5	24	0	No change.
120	337.9665	56.0	241.2	1.5874	1.5777	1.5976	6	24	0	No Change.
144	338.0305	64.0	305.2	1.5885	1.5770	1.5982	8	24	0	No change.
168	338.1072	76.7	381.9	1.5899	1.5771	1.5987	9	24	0	Uniform Blue tint except around vents.

* Average alpha counts per minute based on 2-minute counting period.

Table 4.2.1-25 . Results of Periodic Inspections of Vented Pt-Rh Clad Sphere No. 37
Oxidation Tested at 1000°C

Time at Temp., Hr.	Weight, gm.	Weight Gain, mg		Diameter			Filter Activity			Comments
		Increment	Total	Polar Axis	Weld 0°	Girth 90°	Filter No.	Duration, Hr.	Alpha Count	
Pre-Test	339.7355	0	0	1.5769	1.5915	1.6006	1	6.9	0	
24	339.6721	- 63.4	-63.4	1.5786	1.5915	1.6004	2	17.1	0	
48	339.7889	116.8	53.4	1.5814	1.5912	1.6007	3	24	0	
72	339.8932	104.3	157.7	1.5826	1.5908	1.6014	4	24	0	
96	339.9993	106.1	263.8	1.5849	1.5902	1.6023	5	24	0	Cracked furnace tube replaced
120	340.1374	138.1	401.9	1.5877	1.5911	1.6037	6	24	0	Dark tint on upstream face of sphere.
144	340.2690	131.6	533.5	1.5894	1.5918	1.6047	8	24	0	
168	340.4438	174.8	708.3	1.5932	1.5927	1.6066	9	24	0	
336	342.4710	2027.2	2735.5	1.6290	1.6245	1.6390	10	168	0	*

* Pt-Rh cladding of upstream hemisphere cracked and ruptured, exposing core. Welded girth also cracked about 180 degrees. Downstream hemisphere surface shows strain.

After 24 hours at temperature the spheres were pulled into the cold zone and when cool were removed for inspection and photography. When the spheres were returned to the test furnace a new filter disc was installed at the exit end of each tube and the spent filter was carefully saved for alpha counting after allowing for decay of radon. The test was interrupted after each 24-hour period for inspection and photography until each sphere had been subjected to oxidation testing for a total of 168 hours. Sphere No. 39 tested at 800°C was then subjected to metallographic examination. Sphere No. 37 was continued on test at 1000°C for an additional 168 hours uninterrupted, and it was then inspected and subjected to metallographic examination. During this second 168-hour test period the magnehelic pressure differential across the filter increased to 2.5 inches on the second day, to 3.5 inches on the sixth day, and to 4.25 inches prior to termination of the test. Upon removal of the sphere it was observed that the Pt-Rh cladding on the upstream face of No. 37 had cracked and ruptured severely, exposing the fuel core as shown in Figure 4.2.1-139.

Test Results. Sphere No. 39 tested at 800°C in flowing air for 168 hours showed no visual change except a blue tint which gradually covered the sphere surface, uniformly. The total weight gain was 381.9 mg as shown in Table 4.2.1-24. The diametral increase was 0.0099-inch between the poles or vent holes, while a slight decrease in diameter occurred at the welded girth. The pressure drop across the filter media did not exceed 0.75-inch of water as indicated by the magnehelic gauge and none of the filters contained any radioactive material as shown by alpha counting. The appearance of the test spheres at each 24-hour inspection is shown in Figures 4.2.1-133, 134 and 135. The 0.030-inch vent holes remained clear throughout the test and the underlying core material appeared white through the vent holes as shown in Figure 4.2.1-136. The gross weight gain and increases in polar diameter as a function of time are shown plotted in Figure 4.2.1-137.

The testing of Sphere No. 39 was terminated after 168 hours at 800°C and the sphere was vacuum impregnated with epoxy in preparation for cutting sections for metallographic examination.

Pre-Test

After 24 hours

Figure 4.2.1-133. Vented Pt-Rh clad ThO_2 -Mo Fuel Simulants Shown at Each Daily Inspection
 Sphere No. 37 was Tested at 1000°C and Sphere No. 39 at 800°C

Figure 4.2.1-134. Vented Pt-Rh Clad ThO_2 -Mo Fuel Simulants Shown at Each Daily Inspection
 Sphere No. 37 was Tested at 1000°C and Sphere No. 39 at 800°C in Flowing Air

Figure 4.2. 1-135. Vented Pt-30Rh Clad ThO₂-Mo Simulants After 168 Hours in Flowing Air
at 1000°C (#37) and 800°C (#39)

Downstream Face

Upstream Face

Figure 4.2.1-136. Vent Holes (0.030") in Pt-Rh Clad Sphere No. 39 After 168 Hours (7 Cycles) at 800°C

Figure 4.2.1-137. Weight Gain and Diametral Increase of Vented Pt-Rh Clad ThO_2 -Mo Simulant Sphere No. 39 Tested in Flowing Air at 800°C

Sphere No. 37 was tested at 1000°C for 168 hours with daily inspections for weight gain and dimensional changes. The Pt-Rh cladding remained bright for the first three cycles (72 hours) and then developed a dark tint as shown in Figures 4.2.1-133, 4.2.1-134 and 4.2.1-135. The weight gain and diametral growth data are given in Table 4.2.1-25 and plotted in Figure 4.2.1-138. After 168 hours at 1000°C the weight gain was 708.3 mg compared to 381.9 mg for the companion sphere tested at 800°C. The increase in polar diameter was 0.027-inch versus 0.010-inch for the 800°C tested sphere.

The testing of sphere No. 37 was continued for an additional week (168 hours) without interruption. As the oxidation testing proceeded, the pressure drop across the filter media increased steadily from 1.6 inches of water to 4.25 inches after 336 hours total test time. The filter paper was stained light yellow indicating an accumulation of molybdenum oxide was present.

When the sphere was removed from the furnace it was observed that the Pt-Rh cladding had ruptured on the upstream face near the vent hole, and cracks extended to the weld zone as shown in Figure 4.2.1-139. The fuel core was exposed beneath the cracks as shown in Figures 4.2.1-140 and 4.2.1-141. The cracks in the welded girth extended about half way around the sphere and were of the type shown in Figure 4.2.1-142.

The incremental weight gain in the last 168-hour cycle was 2.03 gm and the diametral increase was 0.036-inch as shown in Table 4.2.1-25. The filter media contained no radioactive thorium as indicated by alpha counting with a sensitive instrument. After the test, the inside of the furnace tube was smeared and no alpha or beta count was found.

Metallographic Examination. After final examination for dimensional and weight changes and photography of the post-test condition, the tested spheres were impregnated with liquid epoxy by evacuation. Sphere No. 39 was sectioned adjacent to the vent hole on the upstream side and a quarter section was removed. This section was mounted and polished on the longitudinal face to sufficient depth to view the 0.030-inch vent hole in cross-section.

Figure 4.2. 1-138. Weight Gain and Diametral Increase of Vented Pt-Rh Clad ThO_2 -Mo Simulant Sphere No. 37 Tested in Flowing Air at 1000°C

37

71-1-2C

Figure 4.2.1-139. Vented Pt-Rh Clad ThO₂-Mo Sphere No. 37 After 336 Hours
at 1000°C in Flowing Air; Upstream Face

71-1-13B

Figure 4.2.1-140. Crack in Pt-Rh Cladding of Sphere No. 37 Near 0.030" Vent Hole on Upstream Face; After 336 Hours at 1000°C (Magnification 24X)

71-1-13C

Figure 4.2.1-141. Crack in Pt-Rh Cladding of Sphere No. 37 Midway Between Vent Hole and Weld on Upstream Face; After 336 Hours at 1000°C (Magnification 24X)

As shown in Figure 4.2.1-143, the vent hole was drilled barely through the 0.025-inch Pt-Rh cladding and did not penetrate the zirconia coating over the ThO_2 -Mo core. Under the 0.012-inch layer of zirconia was an oxidized layer of Mo- ThO_2 about 0.015-inch thick and a second layer of crumbly thorium particles about 0.010-inch thick. Measurements made of the various layers of oxide under the cladding are shown in the sketch of Figure 4.2.1-146.

In the area of the vent hole, the zirconia layer was channeled through to the ThO_2 -Mo core as shown in Figure 4.2.1-147A. Beneath the zirconia layer the molybdenum matrix was oxidized and the thorium particles were fractured or crumbly in the band next to the unaffected core material. The depth of oxidation was observed to narrow down at 45 degrees from the vent hole and at the welded girth there was no oxidized layer. Examination of the Pt-Rh cladding adjacent to the weld revealed some evidence of grain boundary penetration as shown in Figure 4.2.1-147B. This suggests that vapors of molybdenum oxide may have penetrated the zirconia layer and entered the grain boundaries of the cladding. However, no indications of early failure of the cladding were observed, except what appears to be the initiation of a small crack which appears to have penetrated about 0.003-inch into the Pt-Rh cladding.

Sphere No. 37 tested at 1000°C for 336 hours was impregnated with epoxy and was then half-sectioned on a plane near the vent holes and through the crack in the cladding. One section was ground down to the vent hole in the upstream face and polished to provide the half section shown in Figure 4.2.1-148A. The oxidized layer completely encircled the ThO_2 -Mo core and varied in thickness from 0.040 to 0.102-inch. The measurements made in the area of the cladding rupture are shown in the sketch of Figure 4.2.1-149.

The microstructure of the Pt-Rh cladding in the hemisphere with the intergranular cracking was compared with the microstructure of the Pt-Rh in the hemisphere which was intact, as shown in Figures 4.2.1-148B and 4.2.1-148C. There was no evidence of attack on the grain boundaries in either hemisphere although the failure did appear to be intergranular in nature as shown in Figure 4.2.1-148D.

Macrophotos were made of the cladding and the oxide layers at the vent hole, 45 degrees from the vent and at the weld. These are shown in Figures 4.2.1-150 and 4.2.1-150(a) and in each the cladding appears to have cracked or ruptured in the grain boundaries as a result of oxidation of the underlying molybdenum and the accompanying expansion of the core.

Tests previously conducted on unjacketed ThO_2 -Mo fuel simulant specimens in air at 800° and 1000°C indicated that after 48 hours at 1000°C the oxidation had proceeded to a depth of 0.50-inch or at a rate of 0.010-inch per hour. This compares to a rate of 0.0003-inch per hour for the vented Pt-Rh clad sphere tested at the same temperature. Furthermore, the particulate matter collected on the filter media for the unclad specimen in 16 hours had 20 to 40 alpha count per minute, while the filters for the vented clad sphere had no alpha count, even for 168 hours of continuous testing.

The microstructure of the previously tested unclad ThO_2 -Mo specimen showed oxidation in circumferential bands, resulting in fracturing of the ThO_2 agglomerates in planes parallel to the surface. Within the oxidized layer the ThO_2 particles were fractured into sub-micron particulates which could become airborne. Similar fracturing of the ThO_2 particles occurred within the clad sphere as shown in Figure 4.2.1-147A but the fine particles did not escape through the vent or the rupture in the cladding.

Summary and Conclusions. Two Pt-30Rh clad ThO_2 -Mo fuel simulant spheres, each vented with 0.030-inch holes drilled through the cladding at the poles, were subjected to oxidation tests at 800° and 1000°C .

One sphere tested at 800°C for 168 hours (7 thermal cycles), was visually unaffected. The maximum diametral increase was 0.010-inch and the weight gain was 381.9 mg. No radioactive material (thoria) was collected on the absolute filter as indicated by no alpha count. The vent holes remained clear throughout the test. The maximum depth of oxidation into the ThO_2 -Mo core was 0.026-inch beneath the vent hole. Based on the relatively small changes observed after 168 hours at 800°C , the Pt-Rh clad fuel simulant sphere appeared to be capable of 1000 to 2000 hours at 800°C in air without failure of the cladding or release

of airborne thoria provided, of course, the small 0.003-inch deep crack propagates no further.

The sphere tested at 1000°C for 336 hours (8 thermal cycles) was visually unaffected after 168 hours but failed by cladding rupture after an estimated 300 hours at 1000°C in air. The maximum diametral increase was 0.063-inch and the weight gain was 2.74 gms. in 336 hours. No radioactive material became airborne from the ruptured sphere as indicated by zero alpha count on the filter media. The depth of oxidation into the ThO₂-Mo core ranged from 0.040-inch to 0.102-inch at the area of the cladding rupture. The cracking and subsequent rupture of the Pt-Rh cladding was a result of expansion of the oxidized ThO₂-Mo cermet core. The appearance of the rupture indicated the Pt-Rh had low ductility and the failure was of an intergranular type. Yet no foreign material was found in the grain boundaries.

It is believed that embrittlement may have been caused by the solid solutioning of Mo since Pt30Rh has a normal ductile elongation of $\approx 60\%$ at 1000°C. Electron probe analysis is needed to confirm this conclusion.

Oxidation of the ThO₂-Mo cermet core was reduced by the cladding by a factor of 30 when compared to previously tested unclad ThO₂-Mo cermet disc specimens at 1000°C. Furthermore, the cladding prevented release of airborne thoria particles, even after the cladding had cracked and ruptured in one area.

71-1-13A

Figure 4.2.1-142. Crack in Pt-Rh Cladding and Weldment of Sphere No. 37 After 336 Hours at 1000°C. (Magnification 24X)

71-1-11C

Figure 4.2.1-143. Oxidation of ThO₂-Mo Core Under Zirconia Coating at Upstream Vent Hole Sphere No. 39 After 168 Hours at 800°C. (Magnification 24X)

71-1-11A

Figure 4.2.1-144. Oxidized Layer on ThO_2 -Mo Core 45 Degrees Away From Vent Hole. Light Band Under Pt-Rh Cladding is Zirconia. Oxidation of Core Proceeded to a Depth of 0.025-inch. Sphere No. 39 After 168 Hours at 800°C . (Magnification 24X)

24X

71-1-11B

Figure 4.2.1-145. Oxidation of ThO_2 -Mo Core Proceeding From Vent Hole Toward Weldment. Note Change in Color of Zirconia Coating Under Pt-Rh Cladding. Sphere No. 39 After 168 Hrs. at 800°C .

Figure 4.2.1-146. Measurement of Oxide Layers on ThO₂-Mo Core of Sphere No. 39 After 168 Hours at 800°C.

A. Section at .030" Vent Hole showing channelling of Zirconia layer and loss of Mo from cermet core.

B. Cladding near weld where zirconia coating changed from white to black.

Figure 4.2.1-147. Sections of Pt-Rh Clad Sphere No. 39 After 168 Hours at 800°C.
(Electro Etched; Magnification 75X)

J47011D 2X
Figure 4.2.1-148A. Half Section Through Vent Holes
Showing Depth of Oxidized Layer in Sphere No. 37

J47011A 100X
Figure 4.2.1-148B. Area 1 of Pt-Rh Cladding
in Sound Hemisphere

J47011B 100X
Figure 4.2.1-148C. Area 2 of Pt-Rh Cladding Near
Upstream Vent Hole of Ruptured Hemisphere

J47011C 100X
Figure 4.2.1-148D. Area 3 of Pt-Rh Cladding
in Ruptured Hemisphere

Figure 4.2.1-149. Measurement of Oxide Layers Formed on ThO₂-Mo Core of Sphere No. 37 After 336 Hours at 1000°C

.030" Vent Hole - Upstream Face

Cladding Crack 45° from Vent

Figure 4.2.1-150. Pt-Rh Clad Sphere No. 37 After 336 Hours at 1000°C
(Magnification 24X)

Crack in Pt-Rh Cladding 45° from Vent Hole

Crack in Weldment

Figure 4.2.1-150(a). Pt-Rh Clad Sphere No. 37 After 336 Hours at 1000°C
(Magnification 24X)

4.2.1.3.2 Impact Shell Design Requirement

The design of the impact shell for the MHW fuel sphere was directed toward the development of an impact protection system that will provide for the intact impact of a fuel sphere against a granite block with an impact velocity ten percent greater than the minimum sea level terminal velocity, at a temperature of 2500°F. The impact shells considered utilized mechanical points, thereby eliminating the requirement for hot winding. In addition since design tradeoff studies have indicated that the overall heat source weight is directly related to impact shell thickness, the design effort was directed toward the development of systems utilizing Thornel 50 fibers or cloth.

4.2.1.3.2.1 Impact Shells Systems. Wound Thornel-50 shells with CVD infiltration provided the initial reference from which the impact protection system concepts evolved. While this concept has proven impact capability which meets system design requirements, the CVD infiltration process is costly and the resulting soft interior makes it extremely difficult to fabricate a mechanical closure with full material integrity through the overall shell thickness.

A total of 26 impact tests were performed at the GE Morgantown Impact Test Facility, during the Base Program, to identify the type and characteristic of various impact shell materials best suited to provide intact impact protection of the fuel.

The impact shell concepts that were tested were based on design data developed as part of the Phase I Program.

A wide range of material/design concepts have been evaluated during both phases of the Program. Table 4.2.1-26 summarizes the total number of various concepts that have been evaluated.

Table 4.2.1-26. Impact System

Material Type		Construction and Process		Meets Design Requirement
1	Thornel 50 yarn	Wound	Uninfiltrated	No
2	Thornel 50 yarn	Wound	CVD infiltrated	Yes
3	Thornel 50 yarn	Cloth	Resin Impreg.	No
4	Thornel 50 yarn	Wound	Resin Impreg.	Yes
5	Pyro Carb 406	Cloth	Resin Impreg. and CVD Infiltrated	No
6	Pyro Carb 500	Cloth	Resin Impreg.	No
7	Compression Molded	Cloth Patch	Resin	No
8	Carbitex	Wound	Resin Impreg.	No

4.2.1.3.2.2 Test Facility. All of the impact tests to date have been performed at the GE Impact Test Facility in Morgantown, Pa. The facility consists of a pneumatic catapult and a high temperature controlled environmental furnace.

4.2.1.3.2.3 Test Conditions. The impact tests are conducted are 2500⁰F at impact velocities between 310 to 320 fps. An inert atmosphere of Argon was maintained during heat-up of the impact sphere to minimize the possibility of graphite oxidation.

4.2.1.3.2.4 Impact Test Results. The impact tests conducted during the Base Program are listed in Table 4.2.1-27. Analysis of these test results was divided into two categories; system and component information. From Tables 4.2.1-26 and 4.2.1-27, it can be seen that cloth systems using Thornel-50 yarn and Resin Impregnation (Tests 1 and 3) are not adequate even at thicknesses up to 0.70 inch. (See Figure 4.2.1-151.) From test number 2 it was concluded that the compression molded shell systems were also not adequate, since post test analysis revealed greater damage to the fuel simulant, as can be seen by a comparison between photos a, b, and c.

Table 4.2.1-27. Elevated Temperature Sphere Impact Data

Impact Temperature 2500 °F								
Specimen Number	Bare Ball Dia. in.	Cover Thick. inches	Wt. Gms. Before	Simul. % TD	V _i -fps IMPACT	Impact Energy ft-lbs	P	F
SN-101 (1)	1.5 T-50 ReImp + .020 Pt-30Rh	.400	459	98.2	295	1366.1		X
SN-116 (2)	1.5 Comp Molded + .020 Pt-30Rh	.400	443	98.4	253	969.8		X
SN-104 (3)	1.5 T-50 ReImp + .020 Pt-30Rh	.700	629	98.1	300	1936		X
SN-32* (4)	1.25 Infiltrated Thornel Only	.25	201	98.2	297	606.4	X	
SN-33* (5)	1.5 Infiltrated Thornel (Split Seam)	.50	254	98.0	291	736		X
SN-106 (6)	1.5 PyroCarb 406	.50	513.5	98.5	291	1488		X
SN 252 GE 161 (7)	1.60 T-50 Contin & Infiltrated	.50	540.5	98	315	1833	X	
SN 248 GE 141 (8)	1.60 T-50 Contin & Infiltrated	.50	478.5	83.6	311	1581	X	
SN 253, 244 GE 111 (9)	1.52 T-50 Infilt. Polar Plug	.50	408	98	2812	1100		X
SN 256 GE 120 (10)	1.47 T-50 Continuous & Lig. Impreg.	.50	381	98.4	332	1435	X	
SN 150 (11)	1.60 Bare	---	281.3	80.7	---	---		abort TSF
GE 151 (12)	1.60 Bare	-----	281.4	78.1	16	2.47	X TSF	
GE 114 (13)	1.52 Bare	-----	322	98.2	16	2.82		X 6 pcs
GE 123 (14)	1.47 Bare	-----	269.8	98.5	16	2.36		X 10 - 1 pcs
GE 124 (15)	1.47 Bare	-----	269.4	98.5	--	---		abort TSF
GE 115 (16)	1.52 Bare	-----	322.0	98.1	12	1.58		X 6 pcs
GE 126 (17)	1.47 Bare	-----	269.8	98.4	12	1.33		X 3 pcs
GE 127 (18)	1.47 Bare	-----	270.0	98.6	8	.59	X TSC	
GE 112 (19)	1.52 Bare	-----	322.0	98.2	8	.70	X	
GE 128 (20)	1.47 Ir clad	.020	333.5	98.6	301	1033		X
GE 107 (21)	1.52 Pt-30Rh + C-1 Carbitex	.50	523.1	98.4	300	1610		X
<u>SN 258/257B</u>								
GE 138 (22)	1.47 Ir clad + T-50 continuous, Inf. + Polar Plug	.50	432.3	98.6	311	1430	X	
GE 162 (23)	1.60 ----	-----	347.2	98.5	16	3.04		X
<u>Hitco 101</u>								
GE 138 (24)	1.47 T-50 cloth RE Impreg.	.20	377.3	98.6	309	1232		X
<u>S/N 260/277A</u>								
GE 143 (25)	1.60 T-50 Cont. Inf. and Plug	.50	436.5	81.6	310	1627.4	X	
<u>SN 287/286</u>								
GE 144 (26)	1.60 T-50 cont. Double Infl. and Polar Plug	.375	430.05	80.7	311	1415		X

F = Failure
P = Passed
TSF = Thermal Stress Failure

*Impacted Twice

(a) SN-101

(b) SN-104

(c) SN-116

(d) SN-106, Pyrocarb 406

Figure 4.2.1-151. Impact Specimen

Test number 6 also demonstrated that PyroCarb 406 material is not adequate to meet the MHW system design requirements. Post test photograph of this test is shown in Figure 4.2.1-151(d).

The only impact systems that provided the protection required to meet the intact impact requirement were: (1) the wound Thornel-50 with CVD infiltration, and (2) the wound Thornel-50 with resin impregnation, during winding. Test specimens 7, 8, and 25, verify that the wound Thornel-50 shells with CVD infiltration can meet the system requirements. Test specimen number 10 is the verification that the wound Thornel-50 shell with resin impregnation during winding can also meet the system requirements.

4.2.1.3.2.5 System Analysis. A rigorous structural analysis of the sphere impact phenomena was not conducted during the Base Program, as part of the initial material screening phase of the program. Some preliminary analyses were performed to obtain an understanding of the primary impact parameters. The results of these analyses are presented in Figure 4.2.1-152 which presented the maximum radial stress developed in the sphere at the point of impact, as a function of impact velocity and impact surface modulus. The analysis indicates that the stress induced in the fuel simulant at impact is directly proportional to the modulus of the impacted surface. The analytical analyses confirms the experimental results where it has been determined that while a bare fuel simulant breaks at 16 fps when dropped on granite survived at velocities greater than 300 fps are possible when the spheres are enclosed in a cover system.

Based on the experimental impacts conducted as part of the Base Program, the following system characteristics have been identified as being desirable: an impact shell wound with Thornel-50 yarn; resin impregnated or CVD infiltrated, such that the modulus of the inner shell surface is $\approx 1 \times 10^6$ psi or less. It should be also known that the inner surface modulus is a function of material density and noted, there is a different modulus/density relationship for CVD infiltration and resin impregnation. Additional duties on these relationships will be obtained during the next phase of the MHW program.

Figure 4.2.1-152. Stress Induces in a Bare Sphere Impacting into Surfaces of Different Elastic Modulii

The development of component design data has been found to be related to the following parameters: shell thickness; effect of mechanical closures; dimensional tolerance information; thermal growth; bulk density; and density gradient.

The wall thickness of an impact shell is a direct function of the total mass to be decelerated, the impact velocity, impact temperature of fuel simulant and to a small degree, the graphite temperature, the impact surface material, and the bulk density of the impact shell and density gradient, through the shell.

Thermal analyses have established that the temperature of the sphere at impact is 2500^oF; the impact surface material is smooth granite, therefore, the wall thickness is primarily a function of decelerated mass, impact velocity, and wall densities.

Post impact evaluation of the tests performed to date on successful impacts of both the CVD infiltrated and resin impregnated systems have revealed a characteristic cone crater, on the impact surface. To correlate the volume of material contained in this crater with the success or failure of the system, a study was initiated to determine the energy absorption or dissipation that occurs in the shell material at impact.

The impact phenomena is basically one of energy dissipation; but, the problem is in how that energy is dissipated. Energy, at impact, can be dissipated in the following modes:

1. Crushing of the graphite
2. Generation of heat and light
3. Ejecta
4. Sphere rebound
5. Energy transmitted into the granite block
6. Elastic energy in graphite and ThO₂

The energy dissipated in items 4, 5, and 6 appear to be small. The energy dissipated in the granite block can be determined from conservation of momentum, where the mass and velocity of the incoming ball is given by M_1 and V_1 , respectively, the momentum prior to impact becomes $M_1 V_1$. Rebound velocity of the impact sphere has been measured at about 0.10 of the initial velocity and the mass after impact has been found to be approximately 0.80 of the initial value, for a successful impact. The granite block is a 2 foot cube; thus, the velocity imparted to the granite at the instant of impact (for a 1.0 lb sphere at $V_1 = 330$ ft/sec) is $V = 0.23$ ft/sec. This corresponds to an energy of about 1.1 ft/lbs. The energy of the rebounding ball is about 13.5 ft/lbs. The sum of these energies as well as the elastic energy in the ball and ThO_2 simulant is found to be less than 1% of the initial energy.

The ejecta material from the crushed graphite was also thought to dissipate a considerable portion of energy at impact, however, recent fastex film analysis revealed that ejecta velocities are on the order of the rebound velocities and therefore, also amount to less than 1% of the total impact energy. The major portion of the impact energy must then be dissipated through heat and in crushing of the graphite.

An accurate determination of the percent of distributions of heat and crushing energy losses is not possible; therefore, the analyses conducted to date, utilized only the crushing phenomena. It is believed that the ratio of heat energy to crushing energy would be constant over the range of test parameters considered therefore the concept of effective stress is considered a reasonable assumption. In this approach, the energy dissipated at impact is a product of the volume of crush material involved and the effective stress. The results of compression tests (see Paragraph 4.2.1.3.2.6) indicate that material can be crushed to about 80% of its original thickness before going into the bulk compression region at which point little additional work done. The energy capacity of the crushing graphite can be defined by:

$$E = 0.8 \frac{\sigma_e v}{12} \quad (\text{ft-lbs})$$

$$E = 0.666 \sigma_e v \quad (\text{ft-lbs})$$

Using these relationships, the data shown in Figure 4.2.1-153 was developed. Using the energy and the volume as determined from Figure 4.2.1-153 for the tests performed to date, the relationship between bulk cover density and cover effective strength is illustrated in Figure 4.2.1-154 for CVD infiltrated systems. A similar curve could be generated for resin impregnated systems as well.

These curves are preliminary in nature and are based on limited data. To use these curves for preliminary design, caution must be exercised because effect of density gradient also applies. Typical gradients found in shells already fabricated between densities of <1.6 gm/cc on the outer portion of the shell down to as-wound - 0.85 gm/cc on the inside. The density gradient performs two functions: 1) attenuation of the shock wave generated at impact and 2) distribution of the load over the hemisphere at impact; provided the inner density is low enough. Test number 26 is an example of the effect of too high an interior density, which caused the simulant to fracture. Additional tests will be conducted during the next program phase to evaluate in more depth the effect of high interior density.

During the early phase of the impact program there were no spheres impacted with mechanical joints. Impact test number 4, shown in Figures 4.2.1-155 and 4.2.1-156, demonstrated continuous impact shells were not required for successful impacts. It remains to be shown how the material will behave when impacted on or at some angle relative to mechanical joint. Tests to evaluate these effects are planned for the next phase of the program. In addition to the experimental programs planned for the next phase of the MHW Program, further analytical studies are planned which will be directed toward attaining an accurate model of the impact phenomenology.

4.2.1.3.2.6 Compression Testing of Various Graphite Materials. Compression test data is presented for various graphite materials. The tests have been conducted with the intent of establishing the stress-strain behaviour following the ultimate compressive load, so that some indication of the energy absorbed during "crush-up" could be obtained.

Figure 4.2.1-153. Correlation of Crush Volume with Impact Shell Thickness

Figure 4.2.1-154. Effective Cover Strength vs Cover Density

SN-32, .25 in. thick infiltrated T-50
cover
1.250 in. diameter fuel simulant
First Impact $V_{\text{impact}} = 338 \text{ fps}$

Figure 4.2.1-155. Test No. 4 Before Second Impact

SN-32, .25 in. thick infiltrated T-50
cover
1.250 in. diameter fuel simulant
 $V_{\text{impact}} = 297 \text{ fps}$

Figure 4.2.1-156. Test No. 4 After Second Impact

Method and Data. Compression tests have been performed on various graphite materials with the intent of demonstrating their ability to absorb impactive loads. As such the tests have been continued following ultimate load in those cases where catastrophic failure did not occur to determine the point at which bulk compression of the material occurs. It must be appreciated however that following the ultimate load the dimensions of the specimen will have changed.

Compression testing was performed on specimens essentially in the form of cubes, the exact dimensions are given in the data table. These were pressed between flat steel compression platens on a 10,000 lb capacity Instron Universal Testing Machine. The applied load was obtained from the regular Instron load cell and strain was taken from a record of machine crosshead motion. This technique is quite applicable for the measurement of strain to failure and total strain if a correction is used for the compression of the load train without the specimen. In those cases where the elastic portion of the stress-strain curve is steep i. e., high modulus, it is not possible to obtain sufficiently accurate values of the modulus and this has not been quoted. However for soft materials the modulus is accurate enough. The correction for machine deflection is negligible when compared to total strain on those specimens which did not fail catastrophically. The strain rate for each of the specimens is given in the data tables.

The data is shown in Table 4.2.1-28. An explanation of the tabulated results can be obtained by reference to Figure 4.2.1-157 which is an idealized stress-strain curve.

Discussion. Since the intent of the test series was to demonstrate the energy absorbing capabilities of the various materials following initial failure, it follows that those materials such as the polycrystalline graphite and pyrolytic graphite which shattered at ultimate load have zero capability. The energy absorbed up to the initial failure is a small proposition of that obtained during the crushing up period. The area under the stress strain curve has been evaluated by J. Poland of IPSO. The fiber reinforced materials, both the laminates and the Avco 3D material do not fail catastrophically at an ultimate compressive load however. As stated previously it is not possible to calculate the loading area following failure since it is

changed by failure mode. The laminate materials tended to shear between the laminations, particularly if the laminations were not exactly perpendicular to the applied load. The 3D material tended to crack parallel to the applied load down the fiber bundles. Those bundles at the side of the specimen tended to peel away.

An examination of the data shows, that as one may expect, the highest compressive strength is shown by the highest density laminate material. This is not the case for the 3D material where the lower density specimen exhibits an appreciably higher strength. This is an area which could bear further examination if the 3D material is a viable concept for the re-entry capsule. Both the 3D material and the high density laminates exhibit large total strains and probably the best crush up behavior. It must be realized however that this data has been obtained under conditions in which the sides of the specimen are free. In the actual application where it would be expected that there is a cone of deformation, the sides of which are restricted, i. e., a plane strain condition, the behavior of these materials may be significantly different. It may be possible to simulate this condition for similar loading rates to the above using the Instron test machine and an appropriate test chamber.

Table 4.2.1-28. Compression Test Data - Graphite Material

Specimen	Density gm cm ⁻³	Specimen Dimension Ht x Area in x in		Strain Rate ins min ⁻¹	Ultimate Comp. Strength 10 ³ psi	Elastic Modulus 10 ⁶ psi	Failure Strain %	Total Strain %
Pyrocarb 406 (perpendicular to laminates)	1 1.28	0.406	0.268	2.0	14.96	0.19	11.8	57.1
	2 1.29	0.406	0.266	2.0	17.07	----	----	----
	3 1.32	0.407	0.278	0.002	12.50	0.59	7.9	70.2
	4 1.30	0.408	0.286	20.0	14.97	----	----	----
Pyrocarb 300 (perpendicular to laminates)	1 1.20	0.460	0.238	2.0	10.84	0.10	13.9	65.6
	2 1.19	0.500	0.241	2.0	9.25	0.09	15.4	73.6
	3 1.15	0.479	0.242	0.002	9.81	0.09	15.3	----
	4 1.20	0.456	0.245	0.002	9.80	0.10	14.5	70.5
Pyrocarb 500 (perpendicular to laminates)	1 1.37	0.410	0.253	2.0	15.77	0.30	7.3	61.9
	2 1.41	0.413	0.252	2.0	17.14	0.26	9.3	64.0
	3 1.42	0.403	0.247	0.002	17.37	0.31	8.8	73.2
	4 1.44	0.381	0.255	0.002	14.90	0.26	8.1	77.4
Carbitex 500 (perpendicular to laminates)	1 1.35	0.646	0.244	2.0	26.39	0.40	5.8	82.0
	2 1.38	0.375	0.204	0.002	12.35	0.40	6.3	51.2
	3 1.36	0.645	0.241	2.0	25.18	0.53	6.5	79.2
	4 1.37	0.345	0.231	0.002	25.13	0.59	6.2	33.2
Poco AXF 5Q (perpendicular to laminates)	1 1.73	0.471	0.282	2.0	19.15	0.51	7.1	----
	2 1.77	0.459	0.288	2.0	19.75	0.41	7.4	----
	3 1.77	0.464	0.272	0.002	18.97	0.54	6.6	----
	4 1.79	0.450	0.292	0.002	18.62	1.02	3.8	----
Pyrolytic Graphite (perpendicular to laminates)	1 2.25	0.505	0.256	2.0	29.06	1.12	2.1	----
	2 2.25	0.505	0.244	2.0	36.97	1.36	2.6	----
	3 2.25	0.510	0.244	0.002	36.07	1.65	2.5	----
	4 2.25	0.510	0.168	0.002	40.71	1.72	2.8	----
Avco 3D*	A1 1.74	0.517	0.267	0.2	17.0	----	10.3	76.6
	A2 1.74	0.516	0.269	2.0	16.7	----	9.1	74.1
	B1 1.50	0.518	0.274	2.0	24.9	----	4.8	66.0
	B2 1.50	0.516	0.273	2.0	24.7	----	5.1	62.9
	C1 1.70	0.500	0.250	2.0	17.2	----	10.2	77.4
	C2 1.70	0.503	0.253	0.2	18.6	----	----	----

* Elastic Modulus too high for accurate measurement by this technique.

Figure 4.2.1-157. Schematic of Typical Load Deflection Curve (Compression)

4.2.1.3.3 Outer Can Materials Development

To evaluate candidate outer can materials under environmental conditions representative of those encountered during the operational life of the MHW-TRG a materials screening program was defined. The proposed evaluation program will be defined herein, but no test results were obtained during this reporting period.

Table 4.2.1-29 defines the design requirements for the MHW outer can material. Figure 4.2.1-158 shows the melting points of the various candidate materials.

The use of additional thickness on the graphite aeroshell to perform the function of the outer can is not possible for the following reasons:

1. An impervious coating would have to be developed to:
 - a. resist oxidation prior to launch
 - b. resist uncontrolled escape of helium during operation
 - c. resist carburization of neighboring generator components
2. Thickness must be increased to account for maximum total possible ablation occurring from the greatest number of possible skip - reentry cycles.
3. Matrix and coating brittleness lend themselves to possible rupture during handling and launch.

Tungsten, and its alloys, (Figure 4.2.1-158) are not sufficiently fabricable, ductile, or weldable to be considered prime candidate can material. W's high density and low resistance to oxidation makes it even less attractive.

Rhenium is only considered as a possible alloying material because of its poor workability and inferior oxidation resistance.

The melting point of hafnium is almost coincident with that anticipated during a skip re-entry. Its high density and oxygen gettering ability reduce its desirability as an outer can material.

Table 4.2.1-29. Functional Requirements of an MHW Outer Can Material

I. Prelaunch

1. Protect graphite from oxidizing in air to 980°C (1800°F).
2. Maintain .1 psi (min.) helium back pressure.

II. Launch

Sustain dynamic loading without damage

III. Operation

1. Maintain an emissivity of 0.8, or better, while at $\sim 1200^{\circ}\text{C}$ for ~ 12 yr. in 10^{-7} torr, or less, pressure.
2. Maintain 0.1 psi He back pressure.

IV. Skip Reentry

1. Resist melting to at least 2200°C in a partial air environment

SCHEMATIC SHOWING BASIC MHW FUEL CAPSULE
COMPONENTS

The carbides and oxides are not prime can candidates because of their inherent brittleness and transparency to helium at elevated temperature. The carbides oxidize readily at low temperature and the oxides are generally poor thermal conductors and are subject to rupture during rapid heating or cooling.

Alloys of Ta, Mo, Nb and Ir are available for investigation in the MHW program. Of the Mo alloys, Mo50Re is most attractive because it is the most easily fabricated and welded.

Ta10W is probably the best tantalum alloy for MHW can purposes. Its melt temperature is higher than T-111 or T-222 and is more easily obtained to firm specification requirements. In addition, mechanical property advantages obtained with T-111 and T-222 are not required for MHW can serviceability.

Columbium is active at elevated temperature and will oxidize and diffuse readily, unless generously alloyed. The alloy, WC3015, is of interest because hafnium, titanium, and tungsten have been added to enhance high temperature stability and oxidation resistance. The alloy has an inherent capability of forming oxidation resistant glasses on its surface, in air, at temperatures above 2000°F ; this could be an asset during skip re-entry since this type structure also has a high emissivity. However, if this protective film is quickly removed by melting or spalling during skip, the glass formation could be a detriment.

All refractory metal alloys require a coating to enhance emissivity and to provide oxidation resistance during ground handling. If handled in protective atmosphere, the requirement for an oxidation resistant coating disappears.

Silicide coatings provide good emissivity and oxidation resistance to about 2500°F . However, a pinhole in a coating could lead to disastrous failure of the matrix material unless a self-healing capacity is built into the material. Coatings of the Vac-Hyd 109 type are not desirable because they contain the lower melting silicides of Fe and Cr (Figure 4.2.1-158) which also tend to evaporate in vacuum. In addition, this type coating is not readily self healing and is

subject to damage during thermal cycling. A TNV-7 type coating (Ti, W, V, and Mo silicides) is (1) more stable in high temperature vacuum, (2) less subject to failure from thermal cycling, and (3) melts at higher temperature than the Vac-Hyd counterpart. TNV-7 coated Ta10W appears to have a higher temperature capability than a similarly coated Mo or Cb alloy.

Carbide coatings on refractory metal alloys will resist melting at skip reentry temperature and provide high emissivity. However, the most oxidation resistant carbide, probably TiC, is only protective to about 1000°F max., if continuous. During reentry the carbide will change to oxide and melt at slightly lower temperature than the carbide, Figure 4.2.1-158; emissivity diminishes accordingly with HfO₂ and TiO₂ providing the highest emittance (0.7 to 0.8).

Unalloyed iridium is stable in air to 1500°F and resists degradation due to oxidation to 1600°F. During reentry, therefore, unprotected iridium will lose thickness. Since the emissivity required for efficient generator operations. If the coating also reduces the recession of iridium during reentry it will be doubly beneficial. A coating of HfO₂ and Ir powder on an Ir substrate might satisfy MHW requirements; the emissivity should not change from skip to skip since the chemistry (and possibly the structure) of HfO₂ will not change structure in a high temperature air environment. In addition, the oxide will decrease the surface area of Ir that is available for evaporating at high temperature. (Ir, electroplated on W and overcoated with (CaO: ZrO₂) provides full protection to W in air at 2000°C for 5 hours).

4.2.1.3.3.1 Proposed Test Program. Coupons of Ir and refractory metal alloys of Ta, Mo, and Cb were forwarded to RCA for compatibility testing with Si-Ge thermoelectric materials. This is a necessary part of an outer can screening program since the properties of the outer can material might be affected by its operational environment, Figure 4.2.1-159. The candidate materials will be tested for 1000 hr. under conditions shown in Figure 4.2.1-160. The data will be used to identify the type materials that are least affected by the generator environment. Coatings will then be selected to upgrade the emissivity (and

Figure 4.2.1-159. Cross Section Showing Interfacing of Capsule Clad and Thermoelectric System During Operation

Figure 4.2.1-160. SiGe Compatibility Program

possibly the oxidation resistance) of the base material of interest. The effect of generator environment on the emissivity of the coated system will be determined after testing of the type depicted in Figure 4.2.1-160. In addition, the effect of generator melting (on can melt temperature) will have to be investigated.

As discussed above a good outer can material should resist melting during skip re-entry in order to prevent premature spalling of the graphite ablator. The melting temperature of the outer can is dependent upon (1) the aged structure and chemistry of the base material (2) the type material in the coating, (3) interplay between base material, coating, and generator atmosphere during capsule operation and generator melting during skip and (4) the emissivity of the coating during each skip cycle and (5) the changes in base material and coating brought about by cyclic exposure to the high temperature - low oxygen pressure environment developed by high velocity skip through the rarified atmosphere at an altitude of about 400,000 feet.

For reasons of economy, the number of materials and coatings submitted to test were kept to a minimum. In addition, a practical program should consider how the selection of material and coating effects the fabrication feasibility and cost of a "full-up" fueled capsule. A functional priority must also be established to permit weighing of the importance of specific can properties necessary to the selection of the prime can material.

Table 4.2.1-30 summarizes the results of a literature survey used to compare the relative merits of 4 candidate base materials and 3 coating systems. Tables 4.2.1-31 through 4.2.1-36 support the ratings presented in Table 4.2.1-30.

It is assumed that operational reliability is the most important function that an outer can material must satisfy. Pre-launch oxidation resistance and skip-reentry capability are weighted equally while fabrication cost is given lowest priority; it is assumed that all systems are, in fact, fabricable, but that each material will have its own problems, which are solvable.

Table 4.2.1-30. Comparative Ratings of MHW Can Materials

Can Mat'l. Coating	Fabrication Reliability and Component Cost	Handling Reliability and Survi- vability in 980°C Air	Operational Reliability and Emissi- vity at 1200°C (Vac.)	Skip Reentr Survival Capability 2200°C Max. (Partial O ₂ Atms)	Weighted Rating	Rating Normalized To Lowest Weighted Rating
Multiplication Weight Factor	*	*	*	**		
	1	2	3	2		
Ir-HfO ₂ :Ir	1.0	3.0	3.0	4	11	1.2
Ir-Pt-HfO ₂ :Ir	7.25	2.0	3.6	2	8.85	1.0
Ta10W:TiC	1.0	6.0	3.6	6	16.6	1.8
Ta10W:TNV-7	1.38	5.0	9.6	10	25.98	2.9
Mo50Re:TiC	1.25	6.0	3.6	16	26.85	3.0
Mo50Re:TNV-7	1.66	4.6	10.2	16	37.46	3.6
WC3015:TiC	1.0	5.0	3.0	8	15.0	1.7
WC3015:TNV-7	1.5	2.6	9.0	16	29.1	3.3

(*) Multiplication factor for normalized rating listed on tables III, IV and V.

Table 4.2.1-31. Fabrication Reliability and Component Cost

Mat'l	Mat'l Costs		O.D. Coating Application Cost and Difficulty (1)	I.D. Coating Cost for Graphite Stability (2)	Weighted Rating	Rating Normalized To Lowest Weighted Rating
	Welded Can Prior To Coating	Coating Mat'l.				
Property	(1) Least Expensive (or) Most Reliable					
Factor	(4) Most Expensive (or) Least Reliable					
Ir-HfO ₂ :Ir	4	3	1	0	8	1.0
Ir-Pt: HfO ₂ :Ir	4	4	2	0	10	1.25
Ta10W:TiC	1	1	3	3	8	1.0
Ta10W:TNV-7	1	2	4	4	11	1.38
Mo50Re:TiC	3	1	3	3	10	1.25
Mo50Re:TNV-7	3	2	4	4	13	1.66
WC3015:TiC	2	1	2	3	8	1.0
WC3015:TNV-7	2	2	4	4	12	1.5

- (1) Emissivity Coating or Barrier Coating (TiC or HfO₂: Ir) Need only be applied once prior to fueling and welding.

Oxidation Resistant and Emissivity Coating (TNV-7) Must be applied after fueling and welding to seal all surfaces. This requirement increases cost and difficulty significantly. In addition, TNV-7 must be stabilized to prevent evaporation in vac.

- (2) Compatibility data to date indicates that Ir requires no I.D. coating protection. Ir appears to be compatible with graphite at operating temperature.

Table 4.2.1-32. Handling Reliability and Serviceability in Air

PROPERTIES

Can Mat'l	Coating	Function Performance (After thermal cycling 980 C Air)	Function *Performance if Coating damages or non-continuous	Oxidation Resistance Capability (980°C-Air)	Weighted Rating	Rating Normalized To Lowest Weighted Rating
Property Weighting Factor	(1) Most Reliable System (8) Least Reliable System					
Ir	HfO ₂ :Ir	3	2	4	9	1.5
Ir	Pt:HfO ₂ :Ir	2	1	3	6	1.0
TalOW	TiC	8	2	8	18	3.0
TalOW	TNV-7	5	8	2	15	2.5
Mo50Re	TiC	8	2	8	18	3.0
Mo50Re	TNV-7	4	8	2	14	2.3
WC3015	TiC	6	2	7	15	2.5
WC3015	TNV-7	1	6	1	8	1.3

* Ir:HfO₂ TiC Required for Emissivity only

TNV-7 Required for Oxidation Resistance & Emissivity

Table 4.2.1-33. Operation Reliability and Emissivity at 1200°C (Vac.)

Mat'l.	Emissivity *	Evaporation Stability in Vac. @ 1200°C	Diffusion Stability in Vac. @ 1200°C	Weighted Rating	Rating Normalized To Lowest Weighted Rating
Property Wt. Factor	(1) Best Rated System (8) Worst Rated System				
Ir-HfO ₂ :Ir	3	1	1	5	1.0
Ir-Pt:HfO ₂ Ir	3	1	2	6	1.2
TalOW:TiC	2	1	3	6	1.2
TalOW:TNV-7	1	7	8	16	3.2
Mo50Re:TiC	2	1	3	6	1.2
Mo50Re:TNV-7	1	8	8	17	3.4
WC3015:TiC	1	1	3	5	1.0
WC3015:TNV-7	1	6	8	15	3.0

* See Table VI For Emissivity Data

Table 4.2.1-34. Oxidation Resistance and Emissivity of Uncoated and Coated Can Candidate Materials

CAN MAT'L.	MAXIMUM USABLE EXPOSURE TEMPERATURE (°C) IN AIR FOR 100 DAYS (T) AND PROJECTED EMISSIVITY AT 1200 C IN VACUUM (E).						
	UNCOATED		EMISSIVITY COATED			OXIDATION AND EMISSIVITY COATED	
	T	E	T	E	COATING	T	E
Ir	870	.2 to .3	870	.7 to .8	HfO ₂ :Ir	~ 1000	.7 to .8
Ta10W	400	.4 to .5	400	.75 to .85	TiC	> 1000	.85 to .95
Mo50Re	< 400	.35 to .45	< 400	.75 to .85	TiC	> 1000	.85 to .95
WC3015	550	.4 to .5	550	.85 to .95	TiC	> 1000	.85 to .95

Table 4.2.1-35. Melting Points ($^{\circ}\text{C}$) of Can Candidates and Coatings Prior to and After Exposure to MHW Environments*

CLAD & COATING CANDIDATE		MAJOR COATING CONSTITUENTS		THERMOELECTRIC CONTRIBUTION TO CLAD AND COATING	OXIDATION** IN 2200 $^{\circ}\text{C}$ - PARTIAL O_2 ATMS
		CLAD CONTRIBUTION	COATING CONTRIBUTION		
Ta10W:TiC	Clad Melt Pt. 3040 (5495 $^{\circ}\text{F}$)	TaC 3880 (6916 $^{\circ}\text{F}$) Ta ₂ C 3400 (6150 $^{\circ}\text{F}$) WC 2600 (4712 $^{\circ}\text{F}$) W ₂ C 2730 (4946 $^{\circ}\text{F}$)	TiC 3100 (5610 $^{\circ}\text{F}$) ↓	Ta ₉ Si ₂ 2540 (4600 $^{\circ}\text{F}$) WSi ₂ 2200 (3992 $^{\circ}\text{F}$) WP 1450 (2642 $^{\circ}\text{F}$)	Ti ₂ O ₃ (decomposes) Ta ₂ O ₅ 1900 (3452 $^{\circ}\text{F}$) WO ₃ 1480 (2697 $^{\circ}\text{F}$)
WC3015:TiC	2500 (4540 $^{\circ}\text{F}$)	NbC 3500 (6330 $^{\circ}\text{F}$) HfC 3890 (6930 $^{\circ}\text{F}$) WC 2600 (4712 $^{\circ}\text{F}$)	-----	B ₄ C 2350 (4262 $^{\circ}\text{F}$) SiC (decomposes) 2700 (4892 $^{\circ}\text{F}$) TiO ₂ P ₂ O ₅ 1255 (2295 $^{\circ}\text{F}$)	Cb ₂ O ₅ 1490 (2715 $^{\circ}\text{F}$)
Mo50Re:TiC	2550 (4620 $^{\circ}\text{F}$)	MoC 2570 (4658 $^{\circ}\text{F}$) Mo ₂ C 2230 (4046 $^{\circ}\text{F}$) ReC (decomposes) 1600 (2912 $^{\circ}\text{F}$)	-----	MoB - 1450 (3887 $^{\circ}\text{F}$) Nb ₄ Si - 1960 (3560 $^{\circ}\text{F}$) MoSi ₂ - 2030 (3686 $^{\circ}\text{F}$) TiP - 1580 (2876 $^{\circ}\text{F}$)	MoO ₃ 795 (1462 $^{\circ}\text{F}$) Re ₂ O ₅ 295 (565 $^{\circ}\text{F}$)
Ta10W:TNV-7	3040 (5495 $^{\circ}\text{F}$)	TaSi ₂ - 2420 (4390 $^{\circ}\text{F}$) WSi ₂ - 2165 (3925 $^{\circ}\text{F}$) TiSi - 1770 (3218 $^{\circ}\text{F}$)	TiSi (decomposes) 1770 (3218 $^{\circ}\text{F}$) Ti ₅ Si ₃ - 2120 (3848 $^{\circ}\text{F}$)	WSi ₂ 2200 (3992 $^{\circ}\text{F}$)	
WC3015:TNV-7	2500 (4540 $^{\circ}\text{F}$)	Nb ₅ Si ₃ - 2400 (4352 $^{\circ}\text{F}$) NbSi ₂ - 2180 (3956 $^{\circ}\text{F}$) Nb ₄ Si - 1960 (3560 $^{\circ}\text{F}$) Hf ₅ Si ₃ - 2300 (4172 $^{\circ}\text{F}$) HfSi - 2120 (3848 $^{\circ}\text{F}$) TiSi - 1770 (3218 $^{\circ}\text{F}$)	WSi ₂ - 2200 (3992 $^{\circ}\text{F}$) W ₃ Si ₂ - 2350 (4262 $^{\circ}\text{F}$) W ₅ Si ₃ - 2360 (4280 $^{\circ}\text{F}$) V ₃ Si - 2050 (3722 $^{\circ}\text{F}$) V ₅ Si ₃ - 2150 (3902 $^{\circ}\text{F}$) Mo ₃ Si - 2050 (3722 $^{\circ}\text{F}$) MoSi ₂ - 2030 (3686 $^{\circ}\text{F}$) Mo ₅ Si - 2105 (3825 $^{\circ}\text{F}$)	TaP 1660 (3020 $^{\circ}\text{F}$) BP 1130 (2066 $^{\circ}\text{F}$) NbP 1730 (3146 $^{\circ}\text{F}$) TiB ₂ 2980 (5696 $^{\circ}\text{F}$) VP 1320 (2408 $^{\circ}\text{F}$) Nb ₄ Si 1960 (3568 $^{\circ}\text{F}$) TiP 1580 (2876 $^{\circ}\text{F}$) ReP 1200 (2200 $^{\circ}\text{F}$) TiSi 1770 (3218 $^{\circ}\text{F}$)	SiO ₂ 1400 (2551 $^{\circ}\text{F}$) Ti ₂ O ₃ 2130 (3866 $^{\circ}\text{F}$) WO ₃ 1480 (2697 $^{\circ}\text{F}$) MoO ₃ 795 (1462 $^{\circ}\text{F}$) V ₂ O ₅ 1245 (2270 $^{\circ}\text{F}$) Cb ₂ O ₅ 1490 (2715 $^{\circ}\text{F}$) MoO ₃ 795 (1462 $^{\circ}\text{F}$) Re ₂ O ₅ 295 (565 $^{\circ}\text{F}$)
Mo50Re:TNV-7	2550 (4620 $^{\circ}\text{F}$)	Mo ₂ Si - 2100 (3812 $^{\circ}\text{F}$) MoSi ₂ , Mo ₃ Si - 2020 (3668 $^{\circ}\text{F}$) ReSi - 1700 (3092 $^{\circ}\text{F}$)	MoSi ₂ - 2030 (3686 $^{\circ}\text{F}$) Mo ₅ Si - 2105 (3825 $^{\circ}\text{F}$)		
Ir - Ir:HfO ₂	2455 (4450 $^{\circ}\text{F}$)	----- Ir - 2455 (4450 $^{\circ}\text{F}$) ----- ----- HfO ₂ - 2905 (5261 $^{\circ}\text{F}$) ----- Pt:Ir (PPM of Pt in Solid Soln with Ir)		IrB 1046 (1920 $^{\circ}\text{F}$) IrP 1262 (2300 $^{\circ}\text{F}$) IrSi 1470 (2680 $^{\circ}\text{F}$) IrMo 2080 (3776 $^{\circ}\text{F}$) IrGe (Unknown)	IrO ₂ ↑ > 1000 (1832 $^{\circ}\text{F}$) MoO ₃ 795 (1462 $^{\circ}\text{F}$)
Ir - Pt:Ir:HfO ₂	2430 (4400 $^{\circ}\text{F}$)	----- 2250 (4082 $^{\circ}\text{F}$) -----			

*Note that not all Eutectics are considered: lower melt pt's. are to be expected in those instances where other Eutectics are formed.

**As carbides oxidize (during skip reentry) emissivity reduces ~ < 0.5 and temperature rises.

Table 4.2.1-36. Minimum Melting Temperatures ($^{\circ}\text{F}$) as Established by Reactions
Between MHW Components (From Table 4.2.1-6)

CLAD AND MELT TEMP.	COATING	CLAD-COATING	CLAD-COATING AND THERMOELECTRICS	CLAD-COATING-THERMOELECTRICS AND REENTRY ENVIRONMENT
Ta10W (5495)	TiC	WC (4712)	TiO ₂ P ₂ O ₅ (2295) WP----- (2642) WSi----- (3992) TiP ₂ ----- (2876)	WO ₃ (2697) Ta ₂ O ₅ (3452) } Reduced Emissivity
WC3015 (4540)	TiC	WC(4712)	As Above Plus Nb ₄ Si (3560)	WO ₃ (2697) Nb ₂ O ₅ (2715) } Reduced Emissivity
Mo50Re (4620)	TiC	ReC (2912) MO ₂ C (4046)	Same as for Ta10W plus ReP (2200) MO ₂ B (3887) MOSi (3686)	MoO ₃ (2697) Re ₂ O ₅ (565) } Reduced Emissivity
Ta10W	TNV-7	V ₃ Si (3722) WSi ₂ (3925) TiSi (3218) MoSi ₂ (3686)	TiP (2876) V P (2408) TaP (3020) W P (2642)	SiO ₂ (2551) Ti ₂ O ₃ (3866) WO ₃ (2697) MoO ₃ (1462) } V ₂ O ₅ (2270) Emissivity may only change slightly
WC3015	TNV-7	As above plus Nb ₄ Si (3560)	As above plus Nb ₄ Si (3560)	As above plus Cb ₂ O ₅ (2715) } Emissivity change (as above)
Mo50Re	TNV-7	Same as Ta10W Plus ReSi (3092)	TiP (2876) MoP (1650) V P (2408) MoSi ₂ (3686) W P (2642) ReSi (3092)	Same as Ta10W plus Re ₂ O ₅ (565) MoO ₃ (1462) } Emissivity Change (as above)
Ir (4450)	Ir:HfO ₂	Ir (4450)	IrB (1920) IrSi (2680) IrP (2300) IrMo (3776)	IrO ₂ ~ (1800) MoO ₃ (1462) } No change in Emissivity
Ir	Pt:Ir: HfO ₂	Ir:Pt ~ (4000)	As above although a minimum soln. of Pt in Ir will lower melt temp. slightly	As above No change in Emissivity

Ratings listed in Table 4.2.1-30 indicate that Mo50Re does not warrant further investigation for can service; even when coated with materials (TNV-7 and TiC) considered most apt to optimize the substrate for MHW environments. The most serious failing probably occurs during skip reentry where low melting point and highly volatile materials form to risk total destruction of the substrate. For this same reason WC3015, coated with TNV-7, might also be dropped from consideration. TNV-7 coated Ta10W may have the best opportunity of surviving a skip reentry. However, the silicide coating is subject to evaporation during operation unless all the Ti and V are "tied up" by formation of stable compounds. Since the silicide coating materials are subject to migration by diffusion, at operating temperature, the long-life stability should be questioned. It is considered that all silicide coatings be dropped from the program even though a coating of the TNV-7 type appears to offer the best means for enhancing the oxidation resistance of refractory metals.

The list of available materials for can service is, therefore, reduced to:

1. Ir coated with HfO_2 : Ir
2. Ir coated with Pt and overcoated with HfO_2 : Ir
3. WC3015 coated with TiC
4. Ta10W coated with TiC

All four material systems appear to have equal capability during operation. The Ir base systems have an edge during pre-launch since long life in air at 1500°F is predicted, and extensive life in higher temperature air is possible, if the platinum and oxide perform beneficially. In addition, if Ir is compatible with carbon (2500°F and below) as indicated by existing data, a coating need not be applied to the I.D. of the can. The oxide systems will probably have constant emissivity (~ 0.7) during each skip.

The TiC coated WC3015 system should still be investigated because it will probably survive extensive service in air, at 1000°F , maximum, while providing a stable highly emissive coating that will form an oxidation resistant glass during reentry. However, it must be

determined if eutectic formations will occur, at 2200°C or less, which could lead to the total destruction of the substrate. Selection of TiC coated WC3015 will require pre-launch handling in a protective atmosphere and coating of both I.D. and O.D. of the can-body.

The Ta10W outer can with a TiC coating (I.D. and O.D.) could provide the system most apt to survive reentry temperatures; however, the extent of change in emissivity and material integrity resulting from oxidation during skip must be determined. This material system should not however, be exposed to air greater than about 700°F prior to launch.

Table 4.2.1-37 indicates the type experiment planned for outer can material systems of interest. The Table 4.2.1-38 test plan is included to determine how an Ir liner might respond during capture after abort. The referenced tests should provide necessary screening information in a short time span and at nominal cost. All specimens will be about 1/4 inch diameter x .0202 to 0.60 inch thick. Testing will be performed within the confines of a water-cooled copper induction heating coil available in the IPSO lab; special crucibles will not be required and all testing should be completed in a one month period.

Table 4.2.1-39 has been included to compare the predicted performance of new alloys not fully characterized. TRW supports a Pt-Hf alloy called Hafnalloys and Oak Ridge is developing an alloy of Pt-Rh-W.

Limited test data suggests that both alloys are stable with graphite to, at least, 2000°F . Both alloys may have stable high emissivity (0.7 or better) when oxidized. Both should be protective in 1800°F air.

There appears to be 5 major disadvantages for both material systems: (1) the alloys are developmental and may not be made available in time for MHW service, (2) both alloys may have to be coated for protection against low melting constituents formed during generator melt-down, (3) maximum skip reentry temperature must be held to about 1900°C even if the alloys escape contamination during operation and skip reentry, (4) the platinum, in both alloys, is subject to rapid deterioration in a silicon-moly atmosphere and (5) alloy brittleness at low temperature may pose difficult assembly and handling problems.

In summary, Hafnialloy and the Pt-Rh-W alloy should probably not be considered for a skip-reentry can material. If the skip requirement is relegated to a low priority position in the overall program, these new alloys may take on a new dimension in importance. Even so, their development must be expedited for design evaluation and fabrication feasibility demonstration.

Table 4.2.1-37. Skip-Reentry Experiment for Can Materials

TEST CONDITIONS AND CYCLES:

A. Atmosphere: 1 MM O_2 in pure argon (max. oxygen at 400,000 ft.)

B. Cycles: *

1.	2100 F to 4200 F in 100 seconds	
	4200 F to 2500 F in 90 seconds	Tot. of 190 seconds
2.	2500 F to 3900 F in 105 seconds	
	3900 F to 2500 F in 95 seconds	Tot. of 200 seconds
3.	2500 F to 3900 F in 110 seconds	
	3900 F to 2500 F in 100 seconds	Tot. of 210 seconds
4.	2500 F to 3900 F in 115 seconds	
	3900 F to 2500 F in 105 seconds	Tot. of 220 seconds
5.	2500 F to 3900 F in 126 seconds	
	3900 F to 2500 F in 114 seconds	Tot. of 240 seconds
6.	2500 F to 3900 F in 138 seconds	
	3900 F to 2500 F in 127 seconds	Tot. of 265 seconds

* only (6) cycles investigated to keep time within reasonable limits while providing meaningful data.

C. Material "stack-ups" tested per A. & B., above

1. Iridium
2. Ir coated with .001 in. Pt & overcoated with .010 in. $HfO_2:Ir$
3. Ir coated with .006 in. Pt gauze over filled with $HfO_2:Ir$ (TOT. of .010 in.)
4. Hot pressed and sintered $HfO_2:Ir$ pellet
5. Hot pressed and sintered $HfO_2:Ir$ pellet on Pt coated Ir
6. Ir slurry coated with Hf (.001 in.) and Ti (.001 in)
7. wc 3015
8. wc 3015 coated with TiC
9. Ta 10W
10. Ta 10W coated with TiC and HfC

D. Repeat B & C, above, except in vacuum.

E. Evaluation: Metallography (all specimens)

Table 4.2.1-38. Liner Reentry Experiment*

TEST CONDITIONS:

A. Atmosphere: Vacuum

B. Capture Reentry Ramp:

1. 10 minutes @ 2500 F
2. 2500 F to 3000 F in $1\frac{1}{2}$ min.
3. 3000 F to 3500 F in $\frac{1}{2}$ min.
4. 3500 F to 3900 F in 1 min.
5. 3900 F to 3500 F in $2\frac{1}{2}$ min.
6. Hold 3500 F for $2\frac{1}{2}$ min.
7. 3500 F to 2500 F in 5 min.
8. Hold @ 2500 F for 10 min.

Test Duration:
33 Min.

C. Material "stack-ups" tested per A & B, above

1. C-Ir-C-Ir-C-Ir-C
#1 #2 #3 #4

Evaluate #1, test others as follows:

2. HfO₂ - #2 Ir - HfO₂
3. BeO - #3 Ir - BeO
4. MgO - #4 Ir - MgO
5. MgO-Ir-MgO-Ir-MgO
#5 #6

Evaluate #5, test #6 as follows:

6. C - #6 - C
7. HfO₂-Ir-HfO₂-Ir-HfO₂
#7 #8

Evaluate #7 and test #8 as follows:

8. C - #8 - C
9. Bare Ir

* Information will be used to augment that obtained from Can Material testing (Table IX) and long term compatibility testing currently being planned.

Table 4.2.1-39. Data Comparing Noble Metal Alloys with Refractory Metal Alloys

Candidate Cladding & Melt Temp. (C)	Major Chemical Constituents in Cladding	Eutectic Formation with the Following Thermoelectric Elements * (C)				Compatibility with Graphic (or) CO ₂ Vapor (C)		Elemental Vapor Pressure of 10 ⁻⁷ Torr at Indicated Temp. (°C)
		Si	P	B	Mo	Reaction Temp.	Eutectic	
W-25 Re (3100)	W	2165	1450	2920	None	600	2525	
	Re	3092	1200	U	None	U	2486	
Ta-10W (3040)	Ta	2540	3020	3200		500	2902	2100
	W	2165	1450	2920	None	600	2525	
Mo50Re (2550)	Mo	3686	1650	1450	None	700	2210	1700
	Re	3092	1200	U	None	U	2486	
WC3015 (2500)	Cb	1960	1730	3050		400	2328	1900
	Hf	2120	U	3250	1950	1200	2800	1600
	W	2165	1450	2920	None	600	2525	
	Ti	1770	U	2980	None	600	72,400	1150
Ir (2455)		1470	1262	1046	2080	None	2296	1650
Oak Ridge Alloy (2000)	Pt	830	588	800	1900	1100	1734	1370
	Rh	1389	1254	1131	2000	1100	1800	1370
	W	2165	1450	2920	None	600	2525	2260
Cermalloy (2150)	Pt	830	588	800	1900	1100	1734	
	Hf	2100	U	3250	1950	1200	2800	

U = Unknown

* = Elements that may evaporate from the Thermoelectric system and deposit on the surface of Cladding during operation (or) deposited (in mass) during initial melting of generator during first ship.

Note: Effects of Ge & Be not considered due to inadequate data.

4.2.1.3.4 Graphite Materials Development

4.2.1.3.4.1 Sublimation Characteristics of Graphite in Vacuum. Of particular interest to the reentry performance of MHW graphite heat shield is the magnitude of any mass loss rates that may occur due to exposure to a space vacuum environment. To make an assessment of this type of effect on analysis was conducted to evaluate the sublimation characteristics of graphite, in vacuum. The graphite materials considered are representative of either a polycrystalline graphite (such as POCO AXF-5Q) or a CVD pyrolytic graphite. The computations were based on graphite sublimation data developed under contract to Sandia Laboratories⁽¹⁾. Experimental measurements were made for both the vaporization coefficient and the equilibrium vapor pressure.

The sublimation rate in vacuum is given by:

$$\dot{m}_{\text{total}} = \sum_{i=1}^3 \alpha_i P_i \left(\frac{M_i}{2\pi RT} \right)^{1/2}$$

where

α = vaporization coefficient

P = equilibrium vapor pressure (in Atm)

i = number of carbon atoms in the species

M = molecular weight

T = Temperature, °K

$\dot{m}$ is in gm/cm² sec

Estimates of graphite sublimation were made for a temperature range between 1120°C and 2230°C, for exposure times up to 50 years. These data are presented in Figure 4.2.1-161. As can be seen, for the normal operating temperature for the MHW heat source, 1370°C, less than 0.1 mils are lost in one year, while less than 4 mils are lost over a 50 year

(1) Zavitsanos, P.D., "Final Report, Experimental Study of the Sublimation of Graphite at High Temperatures," Sandia Contract Number 16-7870, December 1969.

Figure 4.2.1-161. Graphite Recession Due to Sublimation in Vacuum as a Function of Exposure Time

year period if the heat source aeroshell were directly exposed to a vacuum under this temperature condition. If the operating temperature were to rise significantly above the present operating temperature of 1370°C to 1470°C , the sublimation would be approximately 0.4 mils in one year and approximately 25 mils in 50 years, both of which are quite small. As can be seen, however, there is a significant change in sublimation rates at the extremely high temperature range ($\geq 1720^{\circ}\text{C}$).

The values of sublimation rates shown in Figure 4.2.1-161, represent a worst case situation, in that the graphite is directly exposed to vacuum. For the MHW-RTG, the heat source is always contained within a helium environment, which significantly reduces the sublimation rates. (Note: While direct sublimation data on graphite in a helium environment is not available, it is believed that the sublimation rates in helium should be an order of magnitude below that in vacuum.) The only time the MHW heat source graphite material would be exposed to vacuum, would be in the event of an accident or explosion that would breach the outer clad.

As mentioned earlier, the data contained herein are derived from tests on material similar to polycrystalline and pyrolytic graphites. Although no sublimation data is available on Thornel-50 fiber, the sublimation characteristics of the wound graphite system is not expected to differ very much from the values presented here, since the MHW design anticipates using a graphite sphere which will have a CVD pyrolytic graphite coating on the individual fibers, as well as having a solid PG coating on the outer surface of the spheres.

4.2.1.3.4.2 Development and Evaluation of Compliance Pad Materials for Heat Source Application. A program was developed to evaluate a series of candidate materials for use as compliance pads within the MHW heat source. The program was primarily directed toward evaluating maximum elastic load carrying capacity of these materials. Specifically, each material was tested as follows:

1. Test each specimen in compression to 1000 lbs load, and record load vs deflection.
2. Test each sample in cyclic loading to 10^4 cycles at 2 cps. Load should vary between 600 and 900 lbs compression.

3. Relist each sample in compression to 1000 lb and record load vs. deflection data.

Test Samples. The following number and type of materials were tested.

1. High density felt $t = 0.85$ in. thk, 2 in. x 2 in. square, 2 samples.
2. Low density felt $t = 0.85$ in., 2 in. x 2 in. square, 2 samples.
3. Rayon precursor graphite cloth, square weave, $t = 0.85$ in, thick, 2 in. x 2 in. square, 2 samples.
4. Rayon precursor graphite cloth, 8 harness, $t = 0.85$ in. thick, 2 in. x 2 in. square, 2 samples.
5. 3DGFM $t \cong 0.85$ in. thick, 2 in. x 2 in. square, 2 samples.

Test Procedure

1. Each sample was weighed before and after testing (Photographs were taken before and after).
2. Each sample was tested in compression in the 0.85 in. thickness direction and centered approximately between the plattens.
3. All tests were performed at room temperature.

Discussion. Materials tested were all of carbon fibers and consisted of the following:

1. High Density Felt (Carborundum Co.)
2. Low Density Felt (Carborundum Co.)
3. Stacked Fabric, consisting of several layers of square-weave fabric (rayon precursor fiber) sewn together
4. 3D-GFM

Specimens were roughly two inches on a side. Approximate dimensions are given in Table 4.2.1-40.

The initial test plan called for loading the specimens in compression to 1000 lbs. to obtain load-deflection data. In testing materials of this type there is some question as to the initial thickness because of the very large compliance at low load. Consequently the initial thickness is usually determined at some given pre-load. In the present tests a pre-load of about 2.5 psi was employed and deflection was taken to be zero at this point.

Compliance tests were conducted at room temperature in an "Instron" machine at a cross-head speed of 0.2 inch/min.

In conducting the first cycle loadings, it was found that a load of 1000 lbs. was sufficient to reduce the low density felt to a pile of carbon powder. As a result, the load requirements were reevaluated (by IPSO) and it was decided to load the high density felt to only 300 lbs. The 300 lb. load is still larger than anticipated loads in the end use application.

Representative first cycle loading and unloading curves are shown in the attached figures. All of the materials exhibit considerable hysteresis, probably due to inter-fiber friction, but the crush-up of the low density felt is still apparent in the recovery curve for that material.

After the first cycle loading the specimens were subjected to 10,000 cycles of fatigue loading. Load on the specimen was cycled between 50 lbs. (min.) and 250 lbs. (max.), essentially sinusoidally, at a frequency of 2.0 Hz. Fatigue testing was done on an MTI electro-hydraulic machine, using closed loop servo control of the load. Weight loss due to fiber breakage (crushing), was determined by weighing the specimens before and after fatigue. These weight losses are given in Table 4.2.1-41. Note that the Stacked Fabric specimens showed very little weight loss.

The 3DGFM specimens frayed considerably during fatigue. However, most of these loose yarns still remained attached to the specimens. If they had not, the weight loss would have been much greater. It seems probable that such fraying would have to be controlled for this material to be useful as compliance pads.

Table 4.2.1-40. Specimen Dimensions and Weights

Specimen	Approximate Size (in.)	Initial Wt. (g)	Weight Loss on 1st Cycle
1. Low Density Felt			
A	2 x 2 1/8 x 3/4	5.275	Total
B	2 x 2 x 3/4	.989	Total
2. High Density Felt			
A	2 x 2 x 11/15	7.5	--
B	2 x 2 x 11/16	7.5	--
3. Stacked Cloth (Rayon pre-cursor; square weave)			
A	2 3/4 x 2 1/4 x 3/8	18.463	0.002
B	2 1/4 x 2 1/2 x 3/8	16.262	0.002
4. 3D-GFM			
A	2 1/2 x 2 1/2 x 3/4	23.586	0.013
B	2 1/4 x 2 3/8 x 5/8	22.540	(-0.004)

Table 4.2.1-41. Weight Loss During Fatigue

Specimen	Weight Loss (gms.)
A. <u>Stacked Fabric</u>	
A	0.004
B	0.004
B. <u>High Density Felt</u>	
A	0.035
B	0.038
C. <u>3D-GFM</u>	
A	0.065
B	0.104

After completion of the fatigue cycling, the compliance was again measured. Maximum loads employed in this test were the same as were used for the first cycle loading to facilitate comparison. Typical curves, labeled "post-fatigue", are attached (Figure 4.2.1-162).

In comparing the pre and post-fatigue compliance curves, it may be noted that if the material tends to compact during fatigue the thickness at zero load will decrease and the load-deflection curve will be shifted toward smaller deflections. This is seen in the case of the Stacked Fabric, for example. The magnitude of the shift may be taken as a measure of the degree of compaction which occurred.

If compaction did not occur but the specimen degraded (fiber crushing or powdering), the curve could shift to the right (larger deflections at a given load) if the zero-load thickness does not change. The percent change in the starting thickness would probably be comparable to the percent change in weight (if compaction did not occur) and would be very small even for 3D-GFM.

Considering the shift in the curve at peak load, the high density felt and the 3D-GFM both shifted about 0.06 inch. The stacked fabric showed a shift of only 0.026 inch but this low value is due in part to the smaller initial thickness. Taking the thickness into account, the difference is much less but the Stacked Fabric still appears to be as good, possibly better, in retention of resiliency.

In an attempt to further characterize the square weave candidate compliance pad material the following tests were conducted.

Step I. Load specimen from 0 to approximately 500 lbs. in one cycle increments for a total of 15 cycles. Measure height of specimen to 0.005 in. with 10 lb. dead weight on specimen after each cycle and before the first cycle. After performing this test on both specimens do step II. Record load vs. deflection for each cycle.

Figure 4.2.1-162. Post Fatigue

Step II. Load each specimen between 130 and 500 lbs. for 10^4 cycles.

Step III. Measure height of each specimen with 10 lbs. dead weight to within 0.005 in. after 10^4 cycles. Load each specimen from 0 to 500 lbs. and then reverse chart direction and unload specimen at same strain rate as loading specimen.

Loading Rate. Load and unload each specimen at 0.002 in/in. per minute.

Specimens. Each specimen will be 2" x 2" x 0.125 in. thick with 10 lbs. of dead weight. Specimens were weighed before testing and after each step.

Representative load deflection curves are given in Figures 4.2.1-163 (a) and (b). In both of these figures "zero-deflection" is taken to be the initial pad thickness. During the first fifteen (15) loading cycles, the thickness of the pad (at 2.5 psi) gradually decreases (0.022 inch change for specimen "B" and 0.015 inch for "A"). These are changes of about 15% and 12% respectively and are shown graphically in Figure 4.2.1-164

The thickness of the pad at 500 lbs. load changes very little, the compaction being about 0.0025 to 0.003 inch (both pads) during the first fifteen (15) cycles.

After the first fifteen (15) cycles, the specimens were subjected to 10,000 cycles of loading at a rate of 2 cycles per second. After this fatigue, the load-deflection behavior was again measured. However, a period of approximately 24 hours elapsed between completion of fatigue and this last test. It appears that considerable recovery of the specimens occurred during this period as evidenced by the shift to lower deflections (Figure 4.2.1-163 (b)). As a result, this last curve exhibits a large shift in the "zero-load thickness", very similar to the first cycle run.

Specimen weight changes during testing are given in Table 4.2.1-42. The weight losses are less than recorded previously, but are roughly the same percentage of the starting weight.

Figure 4.2.1-163. Load-Deflection Behavior, Specimen B

Figure 4.2.1-164. Effect of Cyclic Loading on Pad Thickness

Table 4.2.1-42. Variation of Specimen Weight on Cycling

Load Cycles Applied	Specimen A (gms)	Specimen B (gms)
0	6.942	7.744
15	6.9415	7.742
10,000	6.939	7.742
	<u>$\Delta = 3 \text{ mg.}$</u>	<u>$\Delta = 2 \text{ mg.}$</u>

4.2.1.4 Pressure Relief Device (PRD)

Two ventings are required in the heat source:

1. One in the outer can
2. One in each of the iridium post impact shells (P. I. S.) around the fuel spheres.

A. Outer Can Vent

The outer can vent must provide for internal helium pressure to enhance heat transfer. A minimum of 0.1 atmosphere of helium is required when in the normal operating condition. Temperature of the vent is 2000^oF to 2100^oF and venting is to hard space vacuum.

The design of the vent and filter system should be such that no harmful quantity of air gets into the heat source while it is on the pad for a 30-day period. Analysis and material evaluation are required to determine if a shorter limit should be set and appropriate pad procedures defined or whether a burst disc is required.

Containment of all particulate must be assured without blocking the filter. Use of a separate or integral-with-vent filter is acceptable.

Venting is to be to outside of the converter. The venting is to be such that it can be sealed, opened and gases "bled" from the heat source, then subsequently sealed. Capability for a minimum of four sealing and resealing cycles are required while the heat source is in the generator. The resealing should be positive and capable of supporting internal pressures built up during a 3-month storage at 1800^oF heat source surface temperature. This is a system requirement. The heat source will have a straight capillary tube which will extend thru the pre-load bolt in the heat source support structure. Interface with a tube leading to the converter wall will be at this point. This tube should be sufficiently long to allow for a minimum of four opening and resealing cycles prior to being cut to accept attachment to the converter tube. The seal should be capable of supporting internal pressures built up in six months storage at a 1200^oF heat source surface temperature

The materials of the outer can vent, filter and tube must be compatible with the outer can materials to enable installation by welding and with converter materials.

The operating temperatures and life times of the outer can vent are as follows:

Storage	1600 ⁰ F	2 years
Ground Test (in Vacuum)	2100 ⁰ F	1 year
Launch Pad	1800 ⁰ F	30 days
Normal Operating Life	2100 ⁰ F	12 years

Helium release rates will be defined.

Hardware will be built and tested to verify that the performance requirements have been met.

B. Post Impact Vent

The post impact shell must be vented to the heat source internal volume. No direct "see through" is a requirement. This can be provided by a design feature such as a foil plate inside the iridium shell spot welded in place.

Two vents are required, 180 degrees apart.

The vents will be holes thru the iridium with as small a diameter as possible to minimize Pu escape. Pu escape data and/or tests will be provided by LASL by the end of August 1971. In case it may be required, the use of a filter consisting of the foil cover and a refractory oxide on the internal iridium wall is to be investigated. The filter must contain all particulate and not restrict helium venting.

Minimum vent hole size is desired. The size of the bare vent holes and the hole/filter combination are determined by transient helium pressure buildups due to re-entry temperature excursion. The holes and/or filter are to be sized and designed such that this pressure does not destroy the iridium shell or graphite impact shell. Thermal and stress

analysis are required. ΔP and stress are to be determined as functions of hole size and helium release rates near 100 percent and at at 50 percent for periods from one to ten years. Helium release rates will be defined as functions of temperature and time.

Filters are to be fabricated and tested for flow rates at temperature.

The filter must survive re-entry and impact but test effort in this area is limited to a helium pressure during re-entry test if analysis indicates that it is required. An all up FSA is required for this test.

The operating temperatures and life times of the filter are as follows:

Storage	2000 ^o F	2 years
Ground Test	2500 ^o F	1 year
Launch Pad	2200 ^o F	30 days
Operating	2500 ^o F	12 years
Re-entry	3800 ^o F Max.	Re-entry pulse
Impact	2500 ^o F	1 hour (cooled down to post impact temperature)
Post-impact	700 ^o F to 1600 ^o F	Indefinite

C. Burst Disc

A burst disc is required over each of the vents in the PIS.

The burst disc shall be capable of supporting the internal pressure developed while storing the FSA at a temperature of $\sim 1200^{\circ}\text{F}$ for six months. At any time from fabrication to the end of six months the iridium encapsulated fuel will be decontaminated, the burst disc punctured and the sphere assembled into the FSA and the heat source.

4.2.1.4.1 Gap Conductance at Low Helium Pressure

An evaluation was made to determine the relationship between gap conductance and helium pressure for the MHW conditions.

Conductance across a 5 mil gap with helium at 1 atmosphere is about 85% of the maximum; about 40% at 0.1 atmosphere and about 6-1/2% at 0.01 atmosphere. Conductance across a 40 mil gap is about 97%, 83% and 35% of maximum at 1, 0.1, and 0.01 atmosphere respectively.

Curves are given to show the effect of pressure, thermal accommodation coefficient and gap size on the conductance.

Dushman gives an equation in "Vacuum Techniques" for heat transfer in the slip region:

$$q = \frac{KA \Delta T}{d + 2\beta\lambda}$$

where: d = gap width

λ = mean free path

$$\beta = \frac{2 - \alpha}{\alpha} \quad \frac{9\gamma - 5}{2(\gamma + 1)}$$

α = thermal accommodation coefficient

γ = gas constant = 5/3

For continuum conditions $d \gg \lambda$ and the equation is standard

$$q = \frac{KA \Delta T}{d}$$

with the thermal conductivity, K , independent of pressure. For free molecular flow conditions $d \ll \lambda$ and the equation is

$$\dot{q} = \frac{KA \Delta T}{2 \beta \lambda}$$

Since λ is proportional to T/P , the heat transfer in free molecular flow is a linear function of pressure.

With a specified gap size, heat transfer is maximum in the continuum region and decreases as the pressure is decreased in the slip region and free molecular region.

The effect of pressure is shown in Figure 4.2.1-165 which gives the ratio

$$\dot{q}/\dot{q}_{\text{maximum}} = \dot{q}/\dot{q}_{\text{continuum}} = \frac{d}{d + 2 \beta \lambda}$$

for $d = 5$ mils and for $d = 40$ mils, the expected MHW range. The thermal accommodation coefficient is an important parameter which is unfortunately not well known since it is a function of gas, surface material, and condition of the surface (i. e., roughness, cleanliness, oxidized or unoxidized et.). Data for helium on platinum and tungsten (iridium should be similar) indicate that a clean surface may have $\alpha = 0.1$ and that surface condition may double or triple the α .

Figure 4.2.1-165 shows that with $\alpha = 0.2$ to 0.4 , heat transfer at one atmosphere is near the maximum for a 40 mil gap but is only 80% to 90% of maximum for a 5 mil gap.

The reduction in heat transfer due to a low pressure is greater in a small gap. Continuum heat transfer is larger in a small gap than in a large gap through so the parameter of significance is the conductance

$$\dot{q}/A\Delta T = \frac{K}{d + 2 \beta \lambda}$$

shown in Figure 4.2.1-166. The value $K = 0.2 \text{ Btu/HrFt}^2 \text{ } ^\circ\text{F}$ was used to generate the curves. The $\alpha = 0.4$, pressure = 1 atmosphere curve is close to the continuum conductance curve

Figure 4.2.1-165. Pressure Atmospheres

Figure 4.2.1-166. Gap Width - mils

showing predominately the influence of gap (conductance inversely proportional to gap width). At the pressure = 0.1 atmosphere level, noncontinuum effects reduce heat transfer in the small gaps but have small effect on the 40 mil gaps. At pressure = 0.01 atmosphere, conductance is essentially independent of gap size (i.e., free molecular).

4.2.1.4.2 Outer Can Vent Evaluations

One type of outer can vent that could fulfill the design requirements specified above, is a capillary type vent. For the MHW Program, a performance evaluation was conducted on a capillary type vent, fabricated by laser grooving a thin iridium disc, in a spiral fashion, then capping the grooved disc with a solid thin iridium disc, and welding the assembly into the outer can. In order to estimate the capillary length versus disc diameter relationship the spiral groove has been approximated as a series of concentric circular grooves spaced 10 mils apart. This is shown in Figure 4.2.1-167.

Capillary lengths required to maintain various internal pressure levels are shown in Figure 4.2.1-168 for a range of assumed helium release rates, with a single vent at 2000° F and 2200 watts thermal of fuel vented to a vacuum.

A recommended design is a 4 mil capillary, about 20 inches long, which could be contained in a 0.500 inch diameter vent. This capillary would operate at about 5 psi, if 100% of the helium generated is released. At this pressure, flow is in the continuum regime, hence pressure is proportional to square root of length. At lower pressures (free molecular flow), there is a direct proportion, so that pressure would be more sensitive to variations in length. In continuum flow, pressure is also less sensitive to capillary diameter than in free molecular flow. The 5 psi operating pressure provides design margin to allow for lower release rates i.e., with 50% release, the pressure would be 3 psi. If, as has been noted, the length for a particular pressure level should be approximately twice the theoretical value, this 20 inch capillary would hold a 3 psi internal pressure with 100% release, or a 1.8 psi internal pressure with 50% release.

Figure 4.2.1-167. Capillary Length vs Disc Diameter

EQUILIBRIUM

HELIUM GENERATION = 1.025×10^{-4} STD CC/SEC

T = 2000° F

EXTERNAL PRESSURE = VACUUM

CAPILLARY GEOMETRY W=D

Figure 4.2.1-168. Capillary Length vs Internal Pressure

Vent temperature also influences pressure (Figure 4.2.1-169). In the continuum regime, pressure is proportional to temperature to the 0.75 power.

Indications are that it is also feasible to utilize an iridium, SNAP-27 type tortuous path, all metallic filter for the MHW outer can. This type of design should also meet the pressure retention and particle containment requirements as well as to provide for easy integration into the heat source assembly (HSA). This concept will be evaluated as a design back-up; this concept which features a thin disk capillary is presently envisioned. When the number of holes per filter, and the number of filter plates is established emphasis will shift toward making filter assemblies and getting into the verification testing with Ir disks. Simultaneously, capillary disc development will be occurring. This back-up position would be followed if the original flow rate/ ΔP tests show that the SNAP 27 type PRD is not feasible.

4.2.1.4.3 Loss of Oxygen from PuO_2 Fuel Spheres, Through Holes in the Post-Impact Shell

One possible cause of PuO_2 reduction to a substoichiometric composition PuO_{2-X} is vaporization of the fuel as $\text{PuO} + \text{O}_x$ and loss of vapor through the Helium pressure relief holes in the liner. In a sealed container, equilibrium would be reached and vaporization would stop, but loss of one of the vapor constituents from the container would allow vaporization to continue. This analysis concerns loss of O and O_2 through a single 2 mil diameter hole in a liner of 20 mils thickness. Mulford of Los Alamos calculated the loss rate of vapor PuO and PuO_2 with an approach which is essentially the same as that used in this analysis. Agreement in the approaches and results tends to confirm both.

Summary

1. Knudsen flow of the oxygen would require thousands of years in order to result in significant fuel reduction.
2. Knudsen flow exists until Helium partial pressure increases to about 1/10 atmosphere.
 - a. Collisions with helium will be important.

EQUILIBRIUM
 HELIUM GENERATION - 1.025×10^{-4} STD CC/SEC
 EXTERNAL PRESSURE VACUUM
 4 MIL CAPILLARY
 100% RELEASE

Figure 4.2.1-169. Capillary Length vs Internal Pressure

- b. Diffusion of oxygen through the helium is about 10 times larger than mass motion of the oxygen due to entrainment by the flowing helium.
 - c. Helium loss will be only about 1% of that in Knudsen flow.
 - d. Significant fuel reduction will not occur due to oxygen loss including helium effects.
4. Higher fuel temperatures greatly increase oxygen loss rate.

Analysis. Due to the small hole size compared to large fuel sphere surface area, the vapor partial pressures inside the liner may be assumed to be equilibrium. This is a conservative assumption; non-equilibrium pressure due either to rate controlled vaporization or diffusion through the solid would be lower than the equilibrium vapor pressure.

Equilibrium partial pressures for 1800°K are:

Fuel O/Pu	Partial Pressures atm.					
	O	O ₂	Pu	PuO	PuO ₂	P Total
2.0	0.2×10^{-5}	0.29×10^{-3}	0.2×10^{-22}	0.28×10^{-13}	0.20×10^{-8}	0.29×10^{-3}
1.98	0.61^{-8}	0.27^{-8}	0.20^{-17}	0.86^{-11}	0.18^{-8}	0.11^{-7}
1.95	0.19^{-8}	0.26^{-9}	0.20^{-16}	0.26^{-10}	0.18^{-8}	0.40^{-8}
1.92	0.57^{-9}	0.23^{-10}	0.21^{-15}	0.81^{-10}	0.16^{-8}	0.23^{-6}
1.90	0.21^{-9}	0.31^{-11}	0.14^{-14}	0.20^{-9}	0.15^{-8}	0.19^{-8}

Mean Free Path. The O or O₂ flow regime; continuum, slip or free molecular; depends on the the Knudsen number $K_N = \lambda/d$ where d is the hole diameter and λ is the mean free path calculated for O-O or O₂-O₂ collisions.

$$\lambda = \frac{1}{\sqrt{2} \pi \sigma^2 N}$$

λ = mean free path cm

σ = collision diameter cm

N = O or O₂ particle number density 1/cm³

Particle number density is

$$N = P/k T$$

P = partial pressure of O or O₂

k = Boltzmann's constant

For $T = 1800^{\circ}\text{K}$ the number density is

$$N (1/\text{cm}^3) = 4.07 \times 10^{18} P (\text{atm})$$

With $\sigma = 3 \times 10^{-8}$ cm for either O-O or O₂-O₂ collisions.

$$\lambda (\text{cm}) = 5.5 \times 10^{-5} 1/P$$

Free molecular flow exists if

$$K_N > 10$$

For $d = 2$ mils, the flow is free molecular if

$$P < 0.001 \text{ atm}$$

Partial pressures at 1800°K are well below this value so O and O₂ flow will be free molecular (Knudsen flow). Partial pressure of O₂ with PuO₂ at temperatures greater than 1800°K can exceed 0.01 atm so the analysis below has limitations.

Knudsen Flow

Flow rate is

$$\dot{N} = (1/4 N \bar{V}) A$$

$\dot{N}$ = particles lost through hole particles/sec

N = particle density $1/\text{cm}^3$

$\bar{V}$ = average of particle random velocity due to thermal motion $= \sqrt{8 k T / \pi m}$ cm/sec

A = hole area cm^2

m = particle mass

For O atoms at $T = 1800^\circ\text{K}$, $\bar{V} = 1.54 \times 10^5$ cm/sec while for O_2 atoms at 1800°K , $\bar{V} = 1.09 \times 10^5$ cm/sec. With $d = 2$ mils, loss rates are as follows:

O/Pu	$\dot{N}_{\text{O}}$	$\dot{N}_{\text{O}_2}$	$N+2 \dot{N}_{\text{O}_2}$
2.0	0.639×10^{13}	0.655×10^{15}	1.317×10^{15}
1.98	0.194×10^{11}	0.61×10^{10}	1.24×10^{10}
1.95	0.605×10^{10}	0.586×10^9	0.722×10^{10}
1.92	0.182×10^9	0.52×10^8	0.192×10^{10}
1.90	0.67×10^9	0.70×10^7	0.68×10^9

The last column gives the loss rate for O atoms due to both O and O_2 vapor.

NOTE

Markfork used an orifice factor to account for the effect of liner thickness. For a thickness to diameter ratio of 6, he used a factor of 0.18, for a ratio of 60, he used a factor of 0.026. In the present case the thickness to diameter ratio $20/2 = 10$ so the actual loss rates will be less than 18% of those calculated with Knudsen flow.

B. Fuel Reduction

In order to reduce a 335 gram mass of fuel from PuO_{2-X} to $\text{PuO}_{2-(X+0.2)}$ assuming the fuel remains homogeneous throughout) requires a loss of 1.53×10^{22} O atoms. As the loss proceeds and the fuel is reduced, the loss rate changes significantly particularly between $\text{O/Pu} = 2.0$ and 1.98 where the rate decreases 5 orders of magnitude. A conservative estimate of the time required to cause reduction from PuO_{2-X} to $\text{PuO}_{2-(X+0.2)}$ may be based on assuming the loss rate to remain constant at the PuO_{2-X} value. Times calculated on this basis and shown below are shorter than realistic.

Time to change from

PuO_2 to $\text{PuO}_{1.98}$	4 months
$\text{PuO}_{1.98}$ to $\text{PuO}_{1.96}$	38,600 years
$\text{PuO}_{1.95}$ to $\text{PuO}_{1.93}$	67,000 years

Effect of Helium. The results above neglect the presence of helium and are, therefore, representative of only the initial period when helium density is low. Helium has two effects on oxygen loss rate. Oxygen atoms and molecules tend to become entrained by the flowing helium in the vicinity of the hole and be swept through the hole. Superimposed on this mass motion of the helium and oxygen is a diffusion of oxygen through the helium. Both of these effects require a number of O-He or O_2 -He collisions in a dimension equivalent to the hole size.

$$\text{i.e., } \lambda_{\text{O-He}} \text{ or } \lambda_{\text{O}_2\text{-He}} < 1/10 d$$

where

$$\lambda_{\text{O-He}} = 1/\sqrt{2} \pi \sigma^2 \text{O}_2\text{-He } N_{\text{He}}$$

$$\lambda_{\text{O}_2\text{-He}} = 1/\sqrt{2} \pi \sigma^2 \text{O}_2\text{-He } N_{\text{He}}$$

The collision diameters $\sigma_{\text{O-He}}$ and $\sigma_{\text{O}_2\text{-He}}$ are approximately the same as that used in the previous Knudsen number calculation. Helium effects are, therefore, significant only if the helium part pressure is ≥ 0.1 atm.

Since He-He collision diameter is 2.6×10^{-8} (approximately the same as $\sigma_{\text{O-He}}$ and $\sigma_{\text{O}_2\text{-He}}$), helium flow will be continuum at partial pressures of ≥ 0.1 atm.

With a helium generation rate of 5.7×10^{-6} std cc/sec for 335 grams of fuel, an MHW heat source will reach 0.1 atm in a period of a few days. Assuming a 17" x 6-1/2" outer can with 10% void volume and 22-335 gram fuel spheres, calculated time to fill the void volume is two days. Actual time will be somewhat greater due to loss through the outer can vent. Fuel reduction by Knudsen flow during a period of days will be negligible.

Helium Flow Effect. Helium loss from a fuel sphere of 5.7×10^{-6} std cc/sec corresponds to a loss rate of 1.4×10^{14} atoms/sec. With the helium at 0.1 atm and 1800°K, particle number density is 4.07×10^{17} 1/cm³. The He mass loss is given by $m = \rho A \bar{V}$ where $\bar{V}$ is the helium mass average velocity. Number of He particles lost is $N = N A \bar{V}$ which gives a mass average velocity of 17 cm/sec. Oxygen loss rate based on Knudsen flow was $N = N A \frac{\bar{V}}{4}$ with $\frac{\bar{V}}{4} = 3.85 \times 10^4$ cm/sec.

Addition of a mass average velocity for the oxygen due to collisions with the helium could result in at most 17 cm/sec added to the 3.85×10^4 i.e., less than 0.1% increase in oxygen mass loss.

NOTE

Markford cites vapor pressure studies in which a flowing gas is used to collect a vapor (he calls the process transpiration) and concludes that the helium flow rate is three orders of magnitude below that for effective transpiration.

Diffusion Through Helium. Mass flux of O (O_2) through He due to a gradient in O (or O_2) particle density is

$$m_o = D_{O-He} \Delta \rho_o / \Delta X$$

or

$$N_o V_o = D_{O-He} \Delta N_o / \Delta X$$

where D_{O-He} = binary diffusion coefficient

$\Delta N_o / \Delta X$ = O or O_2 density gradient

Assuming that there is no oxygen outside the liner produces the maximum effect

$$\frac{\Delta N_o}{\Delta X} = \frac{N_o}{t}$$

where t is liner thickness. A velocity for this flux is therefore

$$V_o = D_{O-He} \quad 1/t$$

The simple kinetic theory expression for the binary diffusion coefficient is

$$D_{O-He} = \frac{1}{3} \bar{V} \lambda_{O-He}$$

With 0.1 atm of helium at $1800^\circ F$ $\lambda_{O-He} \approx 1/10$ d therefore

$$V_o \approx 1/3 \bar{V} \quad 1/10 \text{ d}/t$$

which with $d/t = 1/10$ gives

$$V_o \approx 1/3 \bar{V} \quad (1/100)$$

Collisions with the helium reduce the oxygen mass loss to about 1% of that predicted by the Knudsen flow equation.

Effect of Oxygen Outside the Fuel Sphere. The calculation above for Knudsen flow and diffusion assume there is no O (or O₂) outside the liner. If there was O (or O₂) then the calculations should both use ΔN_o i. e., N_o inside - N_o outside instead of the N_o value inside. The result would be lower oxygen loss rate. If there were no oxygen "getters" external to the fuel spheres, the outer can would fill to an equilibrium oxygen vapor pressure condition in a matter of hours and oxygen loss would stop.

Effect of Hole Size. Loss rate predicted using Knudsen flow and diffusion are both directly proportional to area of the hole. For larger hole sizes, the Knudsen number is smaller and there may be situations where Knudsen flow will not exist with a large hole. These cases are at O/Pu near 2 and/or at temperatures greater than 1800°K.

Effect of Temperature. Both Knudsen flow and diffusion increase at higher temperatures due to the velocity $V = \sqrt{8 h T / \pi m}$. This is minor though when compared to the change in loss rate due to the change in O or O₂ partial pressure. The partial pressures are:

		Partial Pressures (atm)					
Temperature		1400	1600	1800	2000	2200	2400
O/Pu = 2.0	O	0.15×10^{-12}	0.16×10^{-8}	0.20×10^{-5}	0.62×10^{-3}	0.67×10^{-1}	0.33×10^1
	O ₂	0.32×10^{-13}	0.13×10^{-7}	0.29×10^{-3}	0.89×10^0	0.63×10^3	0.15×10^6
O/Pu = 1.98	O	0.30×10^{-14}	0.11×10^{-10}	0.61×10^{-8}	0.99×10^{-6}	0.63×10^{-4}	0.20×10^{-2}
	O ₂	0.12×10^{-16}	0.59×10^{-12}	0.27×10^{-8}	0.23×10^{-5}	0.56×10^{-3}	0.55×10^{-1}
O/Pu = 1.95	O	0.13×10^{-14}	0.38×10^{-11}	0.19×10^{-8}	0.28×10^{-6}	0.16×10^{-4}	0.48×10^{-3}
	O ₂	0.21×10^{-17}	0.74×10^{-13}	0.26×10^{-9}	0.18×10^{-6}	0.37×10^{-4}	0.32×10^{-2}

At 2000°K the loss rates are from 3000 times (at PuO₂) to 150 times (at PuO_{1.95}) those at 1800°K.

4.2.2 HELIPAK HEAT SOURCE AEROTHERMAL PERFORMANCE EVALUATION

The Helipak concepts recommended for further evaluation in the Phase I Final Report were analytically evaluated to determine their performance during operational, reentry, impact, and post-impact mission phases. Since the PPO fuel configuration was recommended as the primary concept, this configuration was selected as the primary reference configuration for analysis.

To perform this analyses the 2-D thermal model previously used during Phase I, was refined, updated and utilized for most of the analytical effort. In addition, a detailed three-dimensional thermal model was developed and three-dimensional analyses were performed. The results of the 3-D analyses were compared to the 2-D analysis in order to confirm the 2-D analysis accuracy, and permit its continued use in the design development stage. The results of these analyses were used as a base point for further design refinements for the improvement of overall heat source performances.

The following Helipak heat source analyses were performed for the operational and reentry phases:

1. Performance tradeoffs with varying heat source parameters
2. 3-D analyses and comparison of 2-D and 3-D
3. Analysis of the updated reference design
4. Skip reentry

The following additional analyses were performed:

1. Post-impact behavior of FSA's
2. Heat source temperatures in air
3. Analyses of design concepts other than Helipak
 - a. Conventional heat source (TRANSIT/PIONEER) type

- b. Individually protected packs
- c. GNOME (recommended backup to Helipak)

4.2.2.1 Effects of Helipak Parameters on Aerothermal Performance

In the MHW Phase I Final Report, the recommended Helipak heat source using an Iridium fuel liner and PPO fuel was presented but was never analyzed. In this section, the results of initial aerothermal analyses of this heat source are presented (Ceramic/Iridium Helipak Heat Source). The analyses focused on the thermal behavior of this design, and its reentry adequacy. The results of these analyses provide sufficient information to develop a more refined Helipak heat source design.

A series of analyses were performed using approximate thermal models of the MHW Ceramic/Iridium Helipak Heat Source. The analyses examined the effects of: helium filled gaps in the fuel sphere; use of P.G. insulator in the aeroshell; use of PMC in place of PPO fuel; 1100°C and 1000°C RTG hot junction temperatures; and the thermal effect of having a free heat source prior to reentry.

Analyses - Assumptions and Groundrules

At the time this analysis was initiated, there were two prime Helipak candidates. One utilized PMC fuel @ 3.2 W/cc and the other a PPO (or Ceramic) fuel @ 4 W/cc. The latter design was selected as the prime design for analysis because the PPO fuel was considered more desirable (see Section 4.1.2). Figure 4.2.2-1 shows this heat source and the specifics associated with it. This design will be referred to as the Ceramic/Iridium Helipak Heat Source, where the Ceramic refers to the fuel and the Iridium refers to the fuel liner.

Using this design, a thermal model was constructed using the same basic assumptions as in the Phase I Final Report. The thermal model is shown in Figures 4.2.2-2, 3 and 4. Significant points about the model's purpose are:

1. The fuel generation per inch of model is equal the average rate per inch of the design (where the design length is the fueled length).

Figure 4.2.2-1. Ceramic/Iridium Cylindrical Heat Source - 2200 W

Figure 4.2.2-2. Basic Thermal Model of MHW Ceramic/Iridium Heat Source

Figure 4.2.2-3. Basic Thermal Model of MHW Ceramic/Iridium Heat Source

Figure 4.2.2-4. Basic Thermal Model of MHW Ceramic Heat Source

2. The volume of Pyrocarb is such that it represents the average volume of Pyrocarb per inch in the design.
3. The nodal makeup, in particular the angular distribution, is selected to give the best approximation to the actual three-dimensional heat conduction paths.
4. The thermal inertia of the system is fairly well defined due to the good representation of the material volumes involved.

The model underwent some modifications during the analyses to represent design modifications or assumption changes. The four main changes were:

1. The original model has no gap between the Iridium fuel liner and the Pyrocarb, or between the Iridium and the fuel. A later change incorporated a 10 mil helium filled gap in both locations. In the circumferential direction (e.g., Node 32 to 52) and the inward radial direction (e.g., Node 13 to 15) the addition was accomplished using effective conductances and radiation terms due to the absence of actual Iridium nodes (to reduce computer costs).
2. The actual area of contact between a fuel sphere and the Pyrocarb retaining ring is smaller than represented in the original model. This was rectified by changing the connection from Nodes 50 to 48, 90 to 88, and 130 to 128 from conduction to radiation.
3. The original analyses indicated the use of an insulator was required. This was accomplished by using a combined aeroshell of P.G. and POCO, retaining a total thickness of 0.50 inch with simple conduction connecting the two layers -- representing a P.G. layer deposited directly on the POCO. The analyses utilized the increasing P.G., "k" versus "T" discussed in the Phase I Final Report.
4. The original model used PPO fuel. In order to evaluate PMC fuel, a change in fuel properties was made, but only the thermal conductivity ("k") was changed from 1.6 to 3.5 Btu/hr-ft-°F. The $\ddot{q}$ was not changed since the expected fuel form $\ddot{q}$ rates should be the same for PPO and PMC (but lower than that in the model, 3.2 versus 4.0 W/cc). The net effect of the change is to compare the thermal effect of the fuel "k".

With the thermal model defined, a trajectory was selected for use in this initial Helipak analysis. Since a $\beta = 45$, $\gamma = -5.35^\circ$ trajectory was available, it was used in lieu of running a $\beta = 47$, $\gamma = -5.35^\circ$ trajectory. This trajectory used the hypersonic continuum β from 100,000 ft to 0 ft. Later trajectory runs use the more correct

$$C_D = f(\text{altitude}) \begin{matrix} 400,000 \\ 100,000 \end{matrix} \quad \text{and} \quad C_D = f(M_\infty) \begin{matrix} 100,000 \text{ ft} \\ 0 \text{ ft} \end{matrix}$$

The difference in these two methods is about 50 seconds (all in the subsonic) but the trajectory used is the longer of the two, so that it is conservative from a total recession point of view and optimistic for impact temperature.

The reference drag coefficient (C_D) curve used in the trajectory is shown in Figure 4.2.2-5 and the trajectory in Figure 4.2.2-6.

Using the trajectory as defined above, the heating and recession analysis was performed using the ABTON computer code. The results are shown in Figure 4.2.2-7.

The same method was used as in previous analyses, which consists of using the ABTON output, with the thermal model and the THT-D computer program. However, for these cases in addition to analyzing the reentry thermal response, the normal operation steady state profiles were calculated because of the complex heat flow paths involved. Another variable was introduced in these steady state analyses, the average hot junction temperature. Two values, 1100°C (2012°F) and 1000°C (1832°F) were used since both were under consideration for the generator. However, the sink temperature used for the analysis was 20°C higher, which represents the average hot shoe temperature. A third steady state condition was also evaluated, that of a free heat source in space with an equivalent sink temperature of 100°F .

The analyses performed is described below along with the purpose of each analysis.

1. Original Model, no P.G. insulation, no Iridium gaps, oversized sphere/retaining ring contact area
 - Steady state - 1100°C hot junction
 - $\gamma_{\min}$ reentry - initial temperatures @ 1100°C hot junction

Figure 4.2.2-5. Variation of Drag Coefficient with Altitude

Figure 4.2.2-6. Reference MHW Heat Source Trajectory

Figure 4.2.2-7. Reference Heat Source Heating and Recession Behavior

Purpose was to evaluate operational temperature profile and reentry thermal response of Ceramic/Iridium Helipak as shown in Figure 4.2.2-1. Also, to evaluate reentry adequacy of design.

2. Same as 1, but:

- Steady state - 100°F sink temperature
- $\gamma_{\min}$ reentry - Initial temperatures @ 100°F sink

Purpose was to evaluate reentry profile of a heat source free prior to reentry, and determine extent of reduced severity of internal temperatures.

3. Same as 1, but aeroshell is composed of 0.45 inch POCO, with 0.050 inch P.G.

- Steady state - 1100°C hot junction
- $\gamma_{\min}$ reentry - Initial temperatures @ 1100°C hot junction

Purpose was to evaluate operational temperature penalty and reentry benefit resulting from use of 0.05 inch P.G.

4. Same as 3, but Aeroshell is 0.40 inch POCO and, 0.10 inch P.G.

- Steady state - 1100°C hot junction
- $\gamma_{\min}$ reentry - Initial temperatures @ 1100°C hot junction

5. Same as 4, but

- $\gamma_{\min}$ reentry - Initial temperatures @ 1000°C hot junction

Estimated from 1100°C hot junction temperatures (steady state in 4)

6. Same as 4, but PMC fuel ("k" only changed)

- Steady state - 1100°C hot junction
(Actual results high by $\approx 50^{\circ}\text{F}$ - adjusted results only shown.
Error due to bad computer input).
- $\gamma_{\min}$ reentry - Initial temperatures @ 1100°C hot junction
(As indicated above, actually $\approx 50^{\circ}\text{F}$ high)

Results are somewhat high due to initial temperatures ($\approx 20^{\circ}\text{F}$ max) but are conservative

Purpose was to examine effect of PMC fuel versus PPO fuel.

7. Aeroshell is 0.500 inch POCO, Iridium gaps are included, PPO fuel is used, reduced sphere/ring contact area.

- Steady state - 1100^O C hot junction
- $\gamma_{\min}$ reentry - Initial temperatures @ 1100^O C hot junction

Purpose is to provide best estimate of Ceramic/Iridium Helipak design's performance in steady state and reentry.

8. Same as 7, but PMC fuel used ("k" change only)

- Steady state - 1100^O C hot junction
- $\gamma_{\min}$ reentry - Initial temperatures @ 1100^O C hot junction

Purpose is to evaluate use of PMC fuel in place of PPO in Ceramic/Iridium heat source

9. Same as 7, but oversized sphere/retaining ring contact area

- Steady state - 1100^O C hot junction

Purpose was to evaluate only effect of Iridium gaps and compare to results of steady state in 1).

The analyses results are presented in several forms:

1. Nodal maps (similar to Figure 4.2.2-3 are used to show the actual model temperature distribution used for steady state results and also impact temperatures.
2. Design cross sections (similar to Figure 4.2.2-4 are used to show significant reentry node locations and temperatures used for results at significant reentry times, also for steady state.
3. Temperature history curves are used to show reentry response of significant nodes.
4. Tabular form to list significant temperatures of all analyses for comparative purposes.

Figures 4.2.2-8 to 4.2.2-13 show selected steady state results using nodal maps.

Comparison of Figures 4.2.2-8, 9, and 10 illustrates the temperature penalty

- 2200 WATTS_{TH} LOADING
- 6 x 3 FUEL SPHERE MATRIX
- PPO FUEL (4 W/cc)
- STEADY STATE IN GENERATOR-
(1120°C HOT SHOE AVG. TEMP.)
- AEROSHELL
 - 500 MILS POCO

- NO FUEL SPHERE GAPS
- FULL PYROCARB RING/
PYROCARB SHELL CONTACT
AREA

Figure 4.2.2-8. Reference Ceramic/Iridium Helipak Heat Source
Temperature Profile

- 2200 watts_{th} loading
- 6 x 3 Fuel sphere matrix
- PPO Fuel (4 W/cc)
- Steady State in generator - (1120°C hot shoe avg. temp.)
- Aeroshell
 - 450 mils POCO
 - 50 mils P.G.
- No Fuel Sphere Gaps
- Full Pyrocarb Ring/
Pyrocarb Shell Contact Area

Figure 4.2.2-9. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-11. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-12. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

associated with the use of P.G. Figure 4.2.2-11 shows the effect of fuel sphere gaps when compared to Figure 4.2.2-8. Figure 4.2.2-12 shows the effect of the reduced contact area when compared to Figure 4.2.2-11, and of both this and fuel sphere gaps when compared to Figure 4.2.2-8. Figure 4.2.2-13 shows the effect of PMC fuel ("k" only) when compared to PPO as shown in Figure 4.2.2-12.

Figures 4.2.2-14 through 4.2.2-20 show selected steady state results displayed on design cross sections. Figures 4.2.2-14, 4.2.2-15, and 4.2.2-16 correspond to 4.2.2-8, 4.2.2-9, and 4.2.2-10 discussed above. Figure 4.2.2-17, when compared to the results in Figure 4.2.2-16, shows the effect of reducing the hot junction temperature from 1100°C to 1000°C . Figures 4.2.2-18, 4.2.2-19 and 4.2.2-20 correspond to Figures 4.2.2-11, 4.2.2-12, and 4.2.2-13 discussed previously.

The temperature profiles are of interest during reentry, and several representative analyses were selected and profiles at the critical times during reentry are illustrated in Figures 4.2.2-21 to 4.2.2-33. All these profiles are displayed on design cross sections.

Figures 4.2.2-21 through 4.2.2-23 show the profiles of Case 1: (that is, no P.G., no fuel sphere gaps, high sphere/ring contact area) at the time of peak aeroshell surface temperatures, peak Iridium fuel liner temperature, and the peak average fuel temperature (in the sphere located on the stagnation point).

Figures 4.2.2-24 to 4.2.2-26 show the profiles for similar times when 0.050 inch P.G. is added to the aeroshell and Figures 4.2.2-27 to 4.2.2-29 are for 100 mils of P.G. in the aeroshell.

Figures 4.2.2-30 and 4.2.2-31 show the temperature profile for Case 7 (where fuel sphere gaps are included, reduced ring/sphere contact area and no P.G.) at peak aeroshell surface and peak iridium fuel liner temperatures.

Figures 4.2.2-32 and 4.2.2-33 are similar to 4.2.2-30 and 4.2.2-31, except for the substitution of PMC fuel "k" for PPO "k", Case 8.

Figure 4.2.2-14. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-15. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-16. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-17. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-18. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-19. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4. 2. 2-20. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-21. Reference Ceramic/Iridium Heat Source Temperature Profile

Figure 4.2.2-22. Reference Ceramic/Iridium Heat Source
Temperature Profile

Figure 4.2.2-23. Reference Ceramic/Iridium Heat Source Temperature Profile

Figure 4.2.2-24. Reference Ceramic/Iridium Heat Source
Temperature Profile

Figure 4. 2. 2-25. Reference Ceramic/Iridium Heat Source
Temperature Profile

Figure 4. 2. 2-26. Reference Ceramic/Iridium Heat Source
Temperature Profile

Figure 4.2.2-27. Reference Ceramic/Iridium Heat Source
Temperature Profile

Figure 4.2.2-28. Reference Ceramic/Iridium Heat Source Temperature Profile

Figure 4.2.2-29. Reference Ceramic/Iridium Heat Source Temperature Profile

TIME OF MAXIMUM POCO SURFACE TEMPERATURE ($\tau = 107.5$)

Figure 4.2.2-30. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

TIME OF MAXIMUM IRIIDIUM TEMPERATURE ($\tau = 150$ SEC.)

Figure 4.2.2-31. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-32. Reference Ceramic/Iridium Helipak Heat Source
Temperature Profile

TIME OF MAXIMUM IRIIDIUM TEMPERATURE ($\tau = 170$ SEC.)

Figure 4.2.2-33. Reference Ceramic/Iridium Helipak Heat Source
Temperature Profile

Figures 4.2.2-34 and 4.2.2-35 show the temperature profile for Cases 7 and 8 at the time of impact. This is shown on a nodal map because this temperature distribution is very important and the resolution of more temperatures is required.

Table 4.2.2-1 summarizes the cases run, and the significant temperatures resulting from the analyses.

Figures 4.2.2-36 through 4.2.2-40 show the temperature histories of significant cases, 1, 3, 4, 7 and 8 respectively. Plotted are the nodal locations 1, 10 and 12 which are the significant aeroshell, iridium, and fuel nodes respectively.

Table 4.2.2-2 lists the figure numbers, purpose, and case number for reference.

Some conclusions reached as a result of these analyses are:

1. Conclusions as to the accuracy of the two dimensional approach are covered in this section. It can be stated that the two dimensional results are sufficiently accurate for use in design development.
2. The impact temperatures shown in Table 4.2.2-1 are low because the trajectory was not corrected for subsonic drag behavior effects. With this correction, the impact time is changed from 710 to 657 sec. The importance of this can be seen from Figures 4.2.2-36 to 4.2.2-40, where fuel impact temperature differences of 150-200°F are indicated. However, the boundary conditions also change and that effect must be examined. A more refined analysis of this subsonic flight period is included in the analyses reported in Section 4.2.2-4.
3. If the heat source were a free body prior to reentry with temperatures stabilized with a 100°F sink, the reentry and impact temperatures would be approximately 300°F lower than for the standard case of reentry with the heat source temperatures stabilized for a 1120°C (2048°F) hot shoe as the sink.

4. If PMC fuel were used instead of PPO, lower reentry temperatures, lower fuel operational temperatures and lower fuel impact temperatures would be realized.
5. It appears that some P.G. (less than 100 mils) will be required to limit Iridium to 3800°F, however the actual thickness, and if it is required, must be determined from more sophisticated analyses since it is marginal and also sensitive. Further, other decisions as to margin-of-safety must be made.
6. The use of fuel sphere gaps and a reduced retaining ring/sphere contact area can be combined for all future analyses since comparison of these steady state results with those in Section 4.2.2-1, indicated good agreement. The effect of gaps between the Iridium and the Pyrocarb impact shell is not significant (about 30°F) on Iridium temperatures provided they are helium filled. The effect of the contact area between the graphite impact shell and the retaining ring are significant. This change in contact area reduces the re-entry temperature and increases the impact temperature on the order of 200°F (see Table 4.2.2-1).

Table 4.2.2-1. MHW Ceramic/Iridium Helipak Heat Source Thermal Results

Case Number	POCO Thick - In.	P.G. Thick - In.	Fuel Sphere Gaps	Fuel Form	Sphere/Ring Contact Area	Sink Temp (°F)	Stagnation Line Temperatures											
							Operational				Peak Reentry (Time Occurring)				Impact (@ 710 sec)			
							Clad (101)*	Iridium (10)	Outer Fuel (12)	Inner Fuel (13)	POCO (1)	Iridium (10)	Outer Fuel (12)	Inner Fuel (13)	POCO (1)	Iridium (10)	Outer Fuel (12)	Inner Fuel (13)
1	0.5	0	No	PPO	High	2048 ¹	2164	2257	2466	2478	5126 (105)	4449 (140)	3765 (280)	3464 (340)	1800	2150	2680	2810
2	0.5	0	No	PPO	High	100 ²	1100	1200	1423	1469	5068 (107.5)	4205 (145)	3456 (1440)	3179 (475)	-	-	-	--
3	0.45	0.05	No	PPO	High	2048	2163	2296	2504	2515	5155 (1025)	4149 (150)	3633 (300)	3376 (360)	1742	2150	2710	--
4	0.40	0.10	No	PPO	High	2048	2163	2332	2539	2548	5183 (102.5)	3884 (160)	3518 (320)	3305 (410)	1710	2290	2780	--
5	0.40	0.10	No	PPO	High	1868	2009	2178	2385	2394	5177 (102.5)	3821 (170)	3451 (380)	3250 (450)	--	--	--	--
6	0.40	0.10	No	PMC	High	2048	2163	2334	2436	2458	5181 (102.5)	3728 (180)	3509 (260)	3312 (340)	--	--	--	--
7	0.50	0	Yes	PPO	Low	2048	2164	2301	2469	2512	5106 (107.5)	4237 (155)	3884 (280)	3504 (360)	1955	2408	2863	2967
8	0.50	0	Yes	PMC	Low	2048	2164	2311	2414	2451	5096 (105)	4087 (170)	3861 (260)	3552 (320)	1974	2464	2752	2865
9	0.50	0	Yes	PPO	High	2048	2166	2294	2454	2489	--	--	--	--	--	--	--	--

* Node Number 1) $\epsilon = 0.6$ 2) $\epsilon = 1.0$

Table 4.2.2-2. Summary of Figure Numbers Showing Cases Analyzed

Figure No.	
1	Heat Source Detail Drawing
2	Heat Source Basic Thermal Model - Materials and Dimensions
3	Heat Source Basic Thermal Model - Nodal map
4	Heat Source Basic Thermal Model - Design Cross Section
5	Drag Coefficient versus Altitude Behavior
6	Heat Source Reference Trajectory
7	Heat Source Reference Heating and Recession
8	Steady State Temperature Dist. - Nodal map - Case 1
9	Steady State Temperature Dist. - Nodal map - Case 3
10	Steady State Temperature Dist. - Nodal map - Case 4
11	Steady State Temperature Dist. - Nodal map - Case 9
12	Steady State Temperature Dist. - Nodal map - Case 7
13	Steady State Temperature Dist. - Nodal map - Case 8
14	Steady State Temperature Dist. - Design Cross Section - Case 1
15	Steady State Temperature Dist. - Design Cross Section - Case 3
16	Steady State Temperature Dist. - Design Cross Section - Case 4
17	Steady State Temperature Dist. - Design Cross Section - Case 5
18	Steady State Temperature Dist. - Design Cross Section - Case 9
19	Steady State Temperature Dist. - Design Cross Section - Case 7
20	Steady State Temperature Dist. - Design Cross Section - Case 8
21	Reentry Temperature Profile - Max. Aeroshell Surface - Case 1
22	Reentry Temperature Profile - Max. Iridium - Case 1
23	Reentry Temperature Profile - Max. Fuel Average - Case 1
24	Reentry Temperature Profile - Max. Aeroshell Surface - Case 3
25	Reentry Temperature Profile - Max. Iridium - Case 3
26	Reentry Temperature Profile - Max. Fuel Average - Case 3
27	Reentry Temperature Profile - Max. Aeroshell Surface - Case 4
28	Reentry Temperature Profile - Max. Iridium - Case 4
29	Reentry Temperature Profile - Max. Fuel Average - Case 4
30	Reentry Temperature Profile - Max. Aeroshell Surface - Case 7
31	Reentry Temperature Profile - Max. Iridium - Case 7
32	Reentry Temperature Profile - Max. Aeroshell Surface - Case 8
33	Reentry Temperature Profile - Max. Iridium - Case 8
34	Impact Temperature Profile - (Nodal map) - Case 7
35	Impact Temperature Profile - (Nodal map) - Case 8
36	Reentry Temperature History - Case 1
37	Reentry Temperature History - Case 3
38	Reentry Temperature History - Case 4
39	Reentry Temperature History - Case 7
40	Reentry Temperature History - Case 8

Figure 4.2.2-34. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-35. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-36. Reentry Thermal Response of Reference Ceramic/Iridium Heat Source

Figure 4.2.2-37. Reentry Thermal Response of Reference Ceramic/Iridium Heat Source

Figure 4.2.2-38. Reentry Thermal Response of Reference Ceramic/Iridium Heat Source

Figure 4.2.2-39. Reentry Thermal Response of Reference Ceramic/Iridium Heat Source

Figure 4.2.2-40. Reentry Thermal Response of Reference Ceramic/Iridium Heat Source

4.2.2.2 Three Dimensional Analysis

A detailed three-dimensional thermal model of the Ceramic/Iridium Helipak heat source has been completed and is operational. The normal RTG operation condition (1100°C hot junction), reentry and impact conditions have been analyzed and the results were compared with those obtained using the simpler and less costly approximate two dimensional thermal model.

4.2.2.2.1 Thermal Modelling. The thermal modelling was performed using a representative portion of the heat source. The model is 180° circumferentially and axially extends from the center line of one fuel sphere plane to the center line of the next fuel sphere plane. The model has a total of 305 individual nodes. The heat source section selected is shown below in Figure A.

The symmetry of the heat source allows determination of temperature distribution using this model. The only effect not accounted for is the ends but this effect will tend to be small and also a relieving one, resulting in a conservative thermal model. In Figure 4.2.2-41 the modelled section is shown in more detail, with the general nodal breakdown also indicated.

Figure 4.2.2-41. Helipak Ceramic/Iridium Heat Source THTD Model

The individual fuel spheres are divided into nodes as in Figure 4.2.2-42. The quarter sphere at the stagnation line is divided into smaller nodes than the others because during the reentry case this will be the most rapidly responding and thus more accuracy, e.g., smaller nodes, is required. On each side of the iridium liner there is a 10 mil helium-filled gap. The contact coefficient used between nodes across these gaps was $240 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ representing the helium conduction in the gap. These nodes are also connected by radiation. The internal heat generation rate of the fuel is 65.6 watts/in^3 .

From the outer retaining ring surface of the spheres to the inner surface of the Pyrocarb, heat is transferred by radiation only except for that portion of the sphere imbedded in the Pyrocarb retaining ring. In this area there is also a contact coefficient of $240 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$ to represent a 10 mil helium filled gap. The nodes on the surfaces of adjacent spheres are connected to each other by radiation only.

The POCO outer reentry aero-shell is divided circumferentially into five angular sections with the sections nearest the stagnation line being smallest (see Figure 4.2.2-43). The POCO is divided radially into five nodes and the outer clad is one node thick. Separating the inside of the POCO and the outside of the Pyrocarb retaining ring, is a 10 mil He filled gap and again the nodes are connected by radiation and a contact coefficient of $240 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$.

Figure 4.2.2-44 shows the arrangement of the node numbers. Not all the node numbers in the model are shown but those shown are the significant ones required to provide temperature distribution profiles. Figure 4.2.2-45 shows the blocks of node numbers assigned to each part of the model.

The steady state operational temperature profile was calculated with the inside of the RTG having an emissivity of 0.6 and temperature of 2048°F representing the hot shoe average temperature and emissivity. The surfaces of the POCO, Pyrocarb, Iridium and Clad all had emissivities of 0.8.

Figure 4.2.2-42. Fuel Sphere Nodal Breakdown (Typ.)

Figure 4.2.2-43. Helipak Model Angular Breakdown

Figure 4.2.2-44. Helipak Thermal Model Nodal Arrangement

Figure 4.2.2-45. Helipak Detailed Model

The reentry thermal response was calculated for a $\gamma_0 = -5.35^\circ$, $\beta = 45$ trajectory, which is identical to the one used in the analyses in Section 4.2.2.1.

4.2.2.3 3-D Analyses Results and Comparison to 2-D Analyses. The steady state results are shown in Figures 4.2.2-46 and in comparing symmetrically similar pairs of nodes, it is found that they all are within 15°F of each other and the majority are within 8°F . This is a good indication that the modelling was consistent from one section to another. The maximum fuel temperature in the steady state case is about 2630°F and the maximum iridium temperature is 2470°F .

The re-entry results are shown in Figures 4.2.2-47 to 4.2.2-50. An important difference in these analyses is that a fuel thermal conductivity value of $0.92 \text{ Btu/hr-ft-}^\circ\text{F}$ was used as compared to the previous value of $1.6 \text{ Btu/hr-ft-}^\circ\text{F}$ used in the 2D analyses in 4.2.2.1.

To compare 2-D and 3-D results an additional 2-D run was made and the results are presented in Figures 4.2.2-51 to 4.2.2-55. The additional run is identical to Case 7 in 4.2.2.1 except for the fuel "k" change.

The steady state results can be compared by examining the half fuel sphere on the line of symmetry, and we can develop the following table from Figure 4.2.2-46 and Figure 4.2.2-51.

	<u>3D</u>	<u>2D</u>
Outer Can	2162	2164
POCO I. D.	2199	2198
Pyrocarb Cover (Outer)	2278	2234
Iridium (Outer)	2332	2293
Fuel (Outer)	2460	2544
Fuel (Inner)	≈ 2600	2580
Fuel (Max)	2630	2580
Iridium (Inner) (Max)	2470	≈ 2440
Pyrocarb (Inner)	2454	2411

Thus we see that the Iridium maximum in the 2D is some 30°F below the 3D value, and the fuel maximum is 50°F below. These are differences which must be taken into account in future 2D work. These differences could be lessened by more modifications to the 2D model, but this type of reassessment is only valid for one configuration.

Figure 4.2.2-47. Reference Ceramic/Iridium Helipak Heat Source
Re-entry Temperature Profile

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Figure 4.2.2-49. Reference Ceramic/Iridium Helipak Heat Source Re-entry Temperature Profile

Figure 4.2.2-50. Re-entry Thermal Response of MHW Helipak Heat Source Using 3D Detailed Model

Figure 4.2.2-51. Ceramic/Iridium Helipak Heat Source Temperature Profile

TIME OF MAXIMUM POCO SURFACE TEMPERATURE ($\tau = 107.5$)

Figure 4.2.2-52. "Reference" Ceramic/Iridium Helipak Heat Source Temperature Profile

TIME OF MAXIMUM IRIIDIUM TEMPERATURE ($\tau = 150$ SEC.)

Figure 4.2.2-53. Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-54. Reference Ceramic/Iridium Helipak Heat Source Temperature Profile

Figure 4.2.2-55. Reentry Thermal Response of Ceramic/Iridium Heat Source

The reentry comparison is another matter. Comparing the results from a temperature history viewpoint (Figure 4.2.2-50 and Figure 4.2.2-55):

	<u>3D</u>	<u>2D</u>
Maximum POCO Surface/Time	5065/105	5113 ⁰ F/107.5 sec.
Maximum Iridium/Time	3960/180	4364/155
Maximum Outer Fuel Node/Time	3930/205	3875/280

From the results it is obvious that:

1. The 2D model is conservative with respect to the maximum iridium and maximum POCO temperatures.
2. This is due to better definition of heat flow paths in 3D.
3. The maximum local fuel temperature - same as maximum iridium - is conservative in 2D.

A comparison at impact can also be made using Figure 4.2.2-49 and Figure 4.2.2-54.

The fuel ball located on the stagnation line has the following temperatures:

	<u>3D</u>	<u>2D</u>
Pyrocarb - Maximum	2653	2741
Minimum	2251	2174
Iridium - Maximum	2679	≈2755
Minimum	2349	2363
Fuel - Maximum	2919	3075
Minimum	≈2380	2400

The difference in the impact temperatures is 14 to 76⁰F higher for the 2D iridium and 20⁰F to 160⁰F higher for the 2D fuel.

The following conclusions can be made:

1. The differences incurred by using 2D approximate models are significant but not substantial enough to warrant the time and expense of using 3D detailed models for design development.
2. The 2D models are slightly optimistic in steady state analyses but conservative in reentry responses.
3. It is felt that future analyses can be performed using 2D models until the design is finalized at which time a 3D detailed model will be constructed. The 2D analyses would therefore be conservative.

4.2.2.4 Analysis of Revised MHW Helipak Heat Source

The revised design Helipak is shown in Figure 4.2.2-56. This design was analyzed in depth for thermal performance. The scope of the analyses included: operational steady state; earth orbital decay reentry response; superorbital (particularly $\gamma_{\min}$) reentry response; multiple-skip reentry behavior; hypervelocity ($V > 36,333$ fps) reentry response and impact velocity determination.

This section presents a description of the reference Helipak, the results of the analyses, and conclusions reached from the results.

The aerothermal evaluation of the Reference MHW Helipak Heat Source considered:

1. Steady State Operational temperature profile
 - 1000°C Hot Junction
 - 1100°C Hot Junction
2. Earth Orbital Decay Reentry Thermal Response
3. Superorbital ($v_o = 36,333$ fps) Reentry Thermal Response
 - $\gamma_{\min}$
 - $\gamma = -6^{\circ}$ to -90° (trajectory heating and ablation analysis only)

4. Hypervelocity ($v_o > 36,333$ fps) Reentry Thermal Response

- γ min @ $v_o = 40,000$ fps; $44,000$ fps

5. Impact Velocity and Temperatures

The design analyzed was basically as shown in Figures 4.2.2-56 and 4.2.2-56a.

The resultant Heat Source dimensions and weight are:

1. Weight = 43.01# (with outer clad)
= 39.37# (without outer clad)
2. Length = 17.141" (with clad)
= 17.091" (without clad)
3. Diameter = 6.734" (with clad)
= 6.684" (no clad)

A typical cross section through the centerline of a FSA plane is shown in Figure 4.2.2-57.

The ground rules for the analyses were:

1. The analyses would assume a 1000°C thermoelectric hot junction, but would evaluate the effects of a 1100°C Hot Junction.
2. The requirements for insulation would be dependent on keeping the maximum iridium temperature during superorbital γ min reentry less than $\sim 3800^{\circ}\text{F}$.
3. The aeroshell requirements would be assessed and the final thickness dependent on:
 - a. Greater than 150% of Superorbital γ min recession
 - b. Sufficient for multiple-skip reentry protection

The thermal model of the Reference Helipak Heat Source was developed using previously described methods. Particular attention was paid to the three dimensional/two dimensional analyses in establishing the thermal model so that the resultant model shown in Figure 4.2.2-58 would approximate closely the actual 3D situation.

Figure 4.2.2-56. Revised Helipak Heat Source (PPO Fuel) - 2200 Watts (t)

ROOM TEMPERATURE (2500°F)

FUEL LOADING = 122 W_{TH}

Figure 4.2.2-56a. Fuel Sphere Assembly (FSA)
For Revised MHW Helipak Heat Source

Figure 4.2.2-57. Typical Cross Section of MHW "Revised" Helipak Heat Source Design

Figure 4.2.2-58. THT-D Thermal Nodal Model of MHW "Revised" Helipak Heat Source

The Revised Helipak has a continuum ballistic coefficient (β_0) of 41.4 PSF (no clad), and trajectories were calculated for the proper weight and reference area (L X D) using the established drag coefficient (C_D) behavior for a side-on stable cylinder (including subsonic C_D vs M_∞ behavior). The trajectories calculated included earth orbital decay, superorbital and hypervelocity reentries. As a result of comments at NASA-Ames during discussions on heat source testing, an attempt was made to explore the γ min angle further. It was found that previously γ min was determined to within 0.05° , and that when determined to within 0.002° , the angle changed and the reentry time changed dramatically. However, the aerothermal results did not change as will be shown. In particular, previously γ min for this Heat Source was found to be -5.3° , and when reexamined, the γ min was found to be -5.262° and the respective reentry times were 1429 vs 639 sec. Figures 4.2.2-59 and 4.2.2-60 show the altitude/velocity-time relationships for these reentries. Figures 4.2.2-61 to 4.2.2-66 show the relationships for other reentries considered.

Aerodynamic heating and recession analyses were performed using the previously described trajectories and the ABTON computer code. Figures 4.2.2-67 through 4.2.2-74 show the behavior of selected parameters during the representative reentries. Table 4.2.2-3 summarizes the significant results of these analyses. Of interest is the recession behavior in the γ region near γ min. Note that the peak recession is $0.193''$ at $\gamma = 5.27^\circ$ but at γ min (-5.262°) it is $0.189''$ and $\gamma = -5.3^\circ$ (former γ min) it is $0.186''$. Thus, γ min determined to 0.05 as in the past is sufficiently accurate. These analyses assume the most conservative situation during subsonic flight, that is, recession continuing to impact at diffusion controlled rates, yielding maximum aeroshell ablation results. The surface heat balance considers only convective cooling and neglects the possible exothermic reaction contribution. However, the internal component (i.e. iridium) impact temperatures are not sensitive to this assumption ($\Delta T < 50^\circ\text{F}$). The complete evaluation of the subsonic behavior is complex and the approach taken here appears to be reasonable. Comments concerning CO_2 or CO at the wall are felt to be premature since the TRW Transit arc tests are felt to be inconclusive (agreed to by TRW). The subsonic behavior will not be known until the subsonic tests planned to be conducted at NASA-Ames are completed. (See Section 4.2.2.4)

Figure 4.2.2-59. MHW Helipak Heat Source Minimum Gamma Re-entry

Figure 4.2.2-60. MHW Helipak Heat Source Minimum Gamma Re-entry

Figure 4.2.2-61. MHW Helipak Heat Source Minimum Gamma Re-entry

Figure 4.2.2-62. MHW Helipak Heat Source Steep Angle Re-entry

Figure 4.2.2-63. MHW Helipak Heat Source Steep Angle Re-entry

Figure 4.2.2-64. MHW Helipak Heat Source Earth Orbital Decay Re-entry

Figure 4.2.2-65. MHW Heat Source Hypervelocity Re-entry

Figure 4.2.2-66. MHW Heat Source Hypervelocity Re-entry

Figure 4.2.2-67. MHW Helipak Heat Source Minimum Gamma Reentry

Figure 4.2.2-68. MHW Helipak Heat Source Minimum Gamma Reentry

Figure 4.2.2-69. MHW Helipak Heat Source Minimum Gamma Reentry

Figure 4.2.2-70. MHW Helipak Heat Source Steep Angle Reentry

Figure 4.2.2-71. MHW Helipak Heat Source Steep Angle Reentry

Figure 4.2.2-72. MHW Helipak Heat Source Reentry Earth Orbital Decay

Figure 4.2.2-73. MHW Heat Source Hypervelocity Reentry

Figure 4.2.2-74. MHW Heat Source Hypervelocity Reentry

The multiple-skip behavior was determined by using ratios to obtain "rough" values from the basic skip reentry analysis described in detail in Section 4.2.2.5. The cumulative recessions in Section 4.2.2.5 can be reduced to 82.2% of those values. The outer clad surface temperatures shown in 4.2.2.5 are reduced less because the temperature is not as sensitive (less than 75^oF difference). Thus, the recession values for a $\gamma_o = -4^o$ (super orbital velocity) is 0.372", and for $\gamma_o = 4.5^o$ is 0.272", with corresponding outer clad temperatures of 3075^oF or 3375^oF ($\epsilon = 0.5$) for $\gamma_o = -4^o$ and 3925^oF ($\epsilon = 0.8$) or 4300^oF ($\epsilon = 0.5$) for $\gamma_o = 4.5^o$. A summary of the results of heating and recession analysis are shown in Table 4.2.2 -3.

Thermal analyses were conducted using the model shown in Figure 4.2.2-58 along with heating rates generated by the ABTON computer code. Some analyses were performed for a modified heat source, where the aeroshell was composed of 450 mils of POCO backed by 50 mils of Pyrolytic Graphite (P. G.). This required alterations in the model and four nodes (radially) were used in the POCO (vs. five originally) with one P. G. node.

Steady state operational analyses were performed for the following conditions:

1. Hot Junction = 1100^oC, 0.5" POCO aeroshell
2. Hot Junction = 1000^oC, 0.5" POCO aeroshell
3. Hot Junction = 1000^oC, 0.45 POCO/0.05" P. G. aeroshell

The resultant temperature profiles are shown in Figure 4.2.2-75 to 4.2.2-77.

The reentry thermal response calculations were performed using the predicted steady state temperatures as initial conditions, but the model assumes that the outer clad is removed at reentry initiation (400,000 ft altitude). The emphasis of the analyses was to determine if an insulator would be required, and analyses were for γ min superorbital conditions at initial temperatures corresponding to a 0.5" POCO aeroshell and both 1100^oC and 1000^oC hot junction temperatures. These cases predicted maximum iridium fuel liner temperatures of 4145 and 4095 F respectively, and thus indicated the probable need for a P. G. insulator in order to establish a reasonable margin on Iridium melt. The temperature goal for

Figure 4.2.2-75. MHW Revised Helipak Temperature Profile

Figure 4.2.2-77. MHW Revised Helipak Temperature Profile

iridium in reentry is 3800°F . The temperature history plots of these cases are shown in Figure 4.2.2-78 and 4.2.2-79. An analysis was performed for a 0.45" POCO/0.05" P. G. aeroshell (1000°C H. J.) which yielded maximum iridium temperatures of 3827°F indicating that a 0.05" P. G. insulator was sufficient. The temperature response curve is shown in Figure 4.2.2-80.

Two other analyses were run for a 0.5" POCO aeroshell (1000°C hot junction) for Earth Orbital Decay where the heat source is a free body at the onset of significant aerodynamic heating, and for a refined γ min superorbital (-5.27° vs -5.3° for previous γ min) having a longer total trajectory time. The temperature vs time results are shown in Figure 4.2.2-81 and 4.2.2-82.

Since the inclusion of a 0.05" P. G. insulator appeared necessary the effort was extended to the hypervelocity reentry situations, and analyses were performed for γ min conditions at initial inertial velocities of 40,000 fps and 44,000 fps using 0.45" POCO/0.05" P. G. aeroshells (1000°C hot junction). The analyses results are plotted in Figure 4.2.2-83 and 4.2.2-84. These and the preceding analyses are summarized in Table 4.2.2-4.

For these analyses the computer code utilized an aeroshell surface emissivity of 0.65. A more realistic value is 0.8. An analysis of the critical case, $\gamma = -5.3^{\circ}$ superorbital, with a value of 0.80 results in a maximum iridium temperature of 3667°F which provides additional margin. The thermal response results from this analysis are shown in Figure 4.2.2-85 and Table 4.2.2-4. Similar reduction in reentry and impact temperatures can be expected for the other cases in Table 4.2.2-4.

Impact velocities were calculated for several conditions:

1. Heat Source including clad
2. Heat Source no clad, no ablation
3. Heat Source no clad, γ min ablation
4. Heat Source no clad, Multiple Skip Ablation

Figure 4.2.2-78. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-79. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-80. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-81. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-82. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-83. MHW Helipak Heat Source Reentry Thermal Response

Figure 4.2.2-84. MHW Helipak Heat Source Reentry Thermal Response

Table 4.2.2-4. Results of Revised Helipak Aero-Thermal Analysis

		④								
Initial Path Angle (γ_o)		Deg.	-5.3	-5.3	-5.27	-5.3	-5.85	-6.31	0.0	-5.3
Initial Inertial Vel. (V_o)		FPS	36,333	36,333	36,333	36,333	40,000	44,000	25,708	36,333
POCO Thickness		In.	0.5	0.5	0.5	0.45	0.45	0.45	0.5	0.45
P. G. Thickness		In.	0.0	0.0	0.0	0.05	0.05	0.05	0.0	0.05
Stdy St. Sink Temp.		°F	1850	2030	1850	1850	1850	1850	1850	1850
Steady State Temperature (Initial Re-entry)	Outer Clad (#1) ①	°F	1944	2109	1944	1940	1940	1940	1944	1940
	POCO Aero. (#2)	↓	1968	2133	1968	1963	1963	1963	1968	1963
	Iridium-Outer (#9)		2117	2278	2117	2170	2170	2170	2117	2170
	Fuel Center (#12)		2556	2712	2556	2606	2606	2606	2556	2606
	Iridium-Inner (#14)		2460	2616	2460	2509	2509	2509	2460	2509
	FSA-Inner (#15)		2415	2572	2415	2464	2464	2464	2415	2464
	Fuel-Average		2455	2615	2455	2510	2510	2510	2455	2510
	Iridium-Average	↓	2298	2458	2298	2350	2350	2350	2298	2350
Re-entry Maximum Temperatures	POCO Aero. (#2)	°F	5092	5105	5067	5160	5634	6010	3741 ②	4887
	Iridium-Outer (#9)	↓	4095	4145	4097	3827	3979	4118	3378	3667
	Fuel-Outer (#10)		3838	3902	3833	3660	3765	3860	3281	3523
	Fuel-Center (#12)		3560	3640	3410	3494	3450	3465	2990	3306
	Iridium-Inner (#14)		3110	3195	3195	3010	3170	3260	2685	2917
	FSA-Inner (#15)	↓	3038	3140	3150	2950	3120	3200	2630	2839
Time of Impact		Sec.	639	639	954	639	891	963	3813	639
Impact Temp. (Approx.)	POCO Aeroshell (#2)	°F	1870	1880	1870	1767	1915	1740	1868	1611
	Iridium-Outer (#9)	↓	2420	2430	2425	2523 ③	2425	2500	2300	2336
	Fuel-Center (#12)		3270	3370	3175	3178	3200	3170	2880	3048
	Iridium-Inner (#14)		2820	2920	2850	2770	2825	2850	2670	2721
	FSA-Inner (#15)	↓	2700	2840	2725	2647	2675	2720	2550	2619

① Refers to node number in THT-D model

② Initial re-entry at 3200 sec. (onset of heating)

③ Average (stag. ball) iridium = 2651°F

④ Emissivity Aeroshell = 0.8, other cases = 0.65

Figure 4.2.2-85. Reentry Thermal Response of Helipak Heat Source

The calculations utilized the correlations of drop test data reported in the Phase I final report. However, it is now recognized (from recent arc test) the cylinder ends do not round as much as previously calculated, but maintain relatively sharp corner. The resultant impact velocities for the 4 cases listed above are:

1. 282 fps
2. 271 fps
3. 300 fps ($Rc/D = 0.05$)
4. 304 fps ($Rc/D = 0.075$)

From the preceding analyses, the following conclusions can be reached:

1. The use of a P. G. insulator is required particularly since there is data to show that the iridium melt temperature reduces to 4175°F (from 4450°F) in the presence of carbon. (See Section 4.2.1.3.1.2)
2. The Revised Helipak with P. G. has 44,000 fps reentry capability, but with smaller margin as limited by reduced melt temperature of iridium.
3. The use of a 0.400" POCO aeroshell would show substantial margin in every case
4. The outer clad multiple skip melt temperature requirements can be as low as 3400°F and an $\epsilon = 0.5$
5. The iridium shell local maximum impact temperature is 2721°F and the maximum iridium shell (stagnation line sphere) average temperature is 2530°F , when there is a 50 mil P. G. layer, and the aeroshell $\epsilon = 0.8$.

Future analyses will evaluate the following:

1. Reentry heating uncertainties
2. $\gamma_o = -6^{\circ}, -10^{\circ}, -20^{\circ}, -90^{\circ}$ thermal response
3. Thermal stress in the aeroshell in steep superorbital reentries
4. Thermal shock in aeroshell
5. Effect of vacuum to air inside heat source on maximum reentry temperatures, (and effect of helium to air).

4.2.2.5 Multiple Skip Reentry

One of the most difficult design conditions associated with an RTG Heat Source is the Multiple Skip reentry. This occurs when the initial reentry velocity and path angle do not result in immediate reentry, but rather the body leaves the atmosphere to reenter on a subsequent reentry. Since this design condition was discovered, during the SNAP-27 GLFC Safety analysis, it has not been pursued in depth because of the time, expense and lack of proper tools. However the MHW Heat Source design must reflect consideration of this condition, and, therefore, a study was undertaken to evaluate the problem. This section presents the methods, assumption and results of that study.

STATEMENT OF THE PROBLEM

The MHW RTG is being considered as a power supply for a variety of missions including low earth orbit, synchronous earth orbit, lunar, and interplanetary. Therefore, it must be designed to survive potential reentries which would result from mission aborts related to these applications. Of the low earth orbit, synchronous earth orbit and lunar missions the most severe is the lunar mission which can result in reentries of up to earth escape velocity ($\sim 36,333$ fps - inertial) and initial path angles from 0° to -90° . Therefore, these three applications can be examined simultaneously and conservatively if the lunar mission conditions are used for analysis and these are the MHW design conditions.

Figure 4.2.2-86 shows the general conditions when the Heat Source intercepts the earth's atmosphere following a mission abort. There are three possible results:

- (a) Capture, or prompt reentry, resulting in earth impact.
- (b) Non-capture, or skip-out, where the heat source leaves the earth's atmosphere at a velocity less than earth escape. The heat source will go into an elliptical earth orbit with multiple skip-outs and thus be a multiple skip reentry.
- (c) Non-return, where the heat source leaves the earth's atmosphere at a velocity in excess of earth escape and, therefore, leaves the earth's influence.

The results of concern are the capture and skip out reentries. For a given velocity and heat source there is a path angle where the heat source trajectory decreases in altitude,

NOT TO SCALE

Figure 4.2.2-86. Typical Trajectory Behavior for Heat Source Reentry

and then shows an increase, but the heat source does not emerge from the atmosphere. Subsequently the altitude decreases again finally resulting in earth impact. This angle is referred to as the minimum gamma, or $\gamma_{\min}$. It is significant as both the divider between capture and skip out and as the most severe thermal response reentry for a given initial velocity.

As γ approaches zero from $\gamma_{\min}$, the skip out condition is characterized by an increasing number of elliptical orbit revolutions before final reentry and earth impact, hence, the term "multiple skip reentry". Each orbit, of course, passes (skips) through the earth's atmosphere and subjects the heat source to aerodynamic heating. Each pass also results in a velocity loss, and when the velocity has been reduced and the path angle altered sufficiently, the heat source will reenter and result in earth impact. The multiple skip reentry is significant because the cumulative aeroshell ablation could be in excess of the thickness required for protection during the "capture" reentry conditions.

ANALYSES

The MHW Heat Source Design used for the analyses is the iridium/ceramic helipak used in Section 4.2.1.2.1. The analyses make several assumptions:

1. The heat source is a free, unencumbered body at reentry initiation.
2. The heat source attains and maintains a side-on, stable reentry orientation (longitudinal axis perpendicular to the flow and no rotation).
3. The heat source has the same stagnation line for every skip in a multiple skip reentry.

The initial reentry configuration is determined by the spacecraft configuration, the mission and the abort cause, however, for any mission a free heat source initial reentry configuration is possible. Thus, an initial configuration other than a free heat source would rely on a specific mission application. In general, based upon previous studies, the free heat source is the most severe initial configuration for reentry behavior evaluation, and therefore, this initial configuration is conservative.

A symmetrical, cylindrical body tends to stabilize in a side-on, stable reentry even if significant initial motions are applied to the body prior to reentry. Although other modes of reentry are possible, (i.e., end-on, stable; side-on, spinning; and random tumbling) the most probable, and the most "severe" is the side-on, stable mode assumed for these analyses.

The use of the same stagnation line during each skip is a conservation assumption, since it accumulates the recession during each skip at one location. In order to insure a "worst case" analysis, this assumption was employed resulting in the "maximum" cumulative recession values for the heat source aeroshell.

These are "conservative" assumptions, but the philosophy is "If the heat source is safe with these conditions, the actual situation should result in increased margin".

The trajectory analyses utilized a GE computer code, DYNMASS, which employs an oblate, rotating earth model. The aerodynamic drag coefficient behavior utilized is shown in Figure 4.2.2-87. In the analysis, the C_D was expressed as a function of altitude for altitudes above 100,000 feet and as a function of Mach Number, M_∞ , at lower altitudes. The MHW Heat Source hypersonic continuum ballistic coefficient (β_0) is 50.6 psf (assumes outer clad inplace).

The initial analysis identified the three reentry regimes; capture, multiple skip, and non-return on a velocity vs. path angle map, and the results are shown in Figure 4.2.2-88.

In order to calculate the multiple skip trajectories with reasonable computer costs, the DYNMASS code was modified to assume that the trajectory conditions at earth atmosphere entry (400,000 feet) are the same as at the previous exit (except γ changes from + to -) after traversing the long elliptical orbit outside the earth's atmosphere. This assumption in effect assumes a two body problem of the earth and heat source, while the actual situation could involve the moon and/or the sun. However, it is felt the analysis with this assumption is still valid. The elliptic orbit outside the earth's atmosphere is of interest from the

Figure 4.2.2-87. Cylindrical Drag Coefficient Behavior

Figure 4.2.2-88. MHW Heat Source Reentry Regimes for Potential initial Reentry Conditions

standpoint of the traverse time necessary and the maximum distance from the earth during the orbit. A computer program was written to calculate these values from fundamental equations.

A series of initial conditions were evaluated ranging from the minimum gamma ($\gamma_{\min}$) to -3.5° at earth escape initial velocity. Figure 4.2.2-89 shows the behavior of the number of skips as a function of the initial γ value. Note that the γ 's from -5.1° to -5.3° all have one skip. This is true for all number of skips, and actually the curve should be a series of short horizontal and vertical lines since only integer number of skips is possible. γ 's less than -3.5° were not evaluated because the number of skips was approaching ∞ and the plan was to first evaluate this range of γ 's.

Figure 4.2.2-90 shows three important parameters of the first skip at any γ : the velocity loss, the time in the atmosphere and the minimum altitude. When the $\gamma_{\min}$ value is approached, the ΔV and $\Delta \tau$ values increase rapidly while the $h_{\min}$ value has a more gradual dependence. The $\Delta \tau$ and $h_{\min}$ are valuable in assessing the amount of clad oxidation which might take place, as will be discussed later.

Another part of the analysis is to determine:

1. The total amount of time in the atmosphere (including and excluding the final entry)
2. The initial conditions of the final entry
3. The amount of time spent outside the atmosphere.

The first two items will be discussed later. Figures 4.2.2-91, and 4.2.2-92 show the results of the analyses to determine 3 above.

The aerodynamic heating and heat shield recession analyses utilized ABTON program. The inputs are the altitude, velocity (relative) and time values as computed by the DYNMASS. Representative skips were taken as computed by DYNMASS and the heating and recession values were calculated. Of course, the initial skip is always of interest and the initial skip

Figure 4.2.2-89. Number of Skips as a Function of Initial Reentry Path Angle

Figure 4.2.2-90. Initial Skip Behavior as a Function of Initial Reentry Path Angle

Figure 4.2.2-91. MHW Heat Source Time Outside the Atmosphere Between Successive Skips

Figure 4.2.2-92. MHW Heat Source Time Outside the Atmosphere During Multiple Skip Reentries

results are shown in Figure 4.2.2-93 as a function of initial path angle. The one foot cold wall sphere stagnation point heating rates, are shown, along with the hot wall rates (accounting for temperature of surface) for the actual heat source clad based on a heat source diameter of 5.58". Note that as γ decreases the $\dot{q}$ values also decrease and begin to approach zero as γ approaches zero. The upper curve in the figure shows the amount of heat shield (POCO AXF-5Q) recession which would take place at the stagnation point if no clad were present. Note that the recession approaches zero as γ approaches zero.

In order to get an idea of the impact of these heating rates, the equilibrium reradiation temperature was calculated corresponding to the peak heating rate during the initial skip. These calculations neglect the conduction and the thermal capacitance effects which when lumped can be referred to as thermal inertia. This indicated that we could consider using a coated ($\epsilon > 0.5$) clad at γ 's in the -4° to -4.5° range.

The next step was to evaluate the cumulative recession during all the skips associated with an initial γ and also the final entry recession. This was done by evaluating representative skips. The results are shown in Figure 4.2.2-94 where the recession per skip is shown as a function of the skip number. The peak recessions (at the final skip) for each γ value do not form a smooth curve. This is due to the fact that discrete solutions are involved where the final skip exit velocity is not a constant and that the final skip ΔV is also not constant. Therefore, the final skip recession, which varies as a function of ΔV and is related to the final exit velocity, also varies - again not smoothly.

The next step is to integrate the recession during the skips which is done by summing the recession occurring during each discrete skip. This is plotted in Figure 4.2.2-95 (lower curve) as a function of γ . A mean curve is drawn through the points and extrapolated to γ values of -2.5° . However this extrapolation would have to be verified since the recession may actually approach an asymptote or even show a decrease. Several points are above the curve due to the before mentioned discrete solution effect.

Figure 4.2.2-93. MHW Heat Source Initial Skip Recession and Peak Heating Rate as a Function of Initial Inertial Reentry Angle

Figure 4.2.2-94. MHW Heat Source Stagnation Point Recession as a Function of Skip Number

Figure 4.2.2-95. MHW Heat Source Stagnation Point Recession as a Function of Initial Reentry Angle

The other curve in Figure 4.2.2-95 shows the recession versus γ including the final entry. This is a very smooth relationship because the total energy dissipation is involved. That is, the fact that the final entry velocity varies in an unrelated manner, is compensated for by the final entry behavior. From Figure 4.2.2-95 we can see that a 0.5" POCO Aeroshell will have to use a clad to protect the aeroshell below -4° to -4.3° depending on the margin of safety desired.

Table 4.2.2-5 shows the significant results of the preceding analyses in tabular form.

The recession analysis shows that reradiation protection by the outer clad is desirable at γ 's = -4.0° or -4.3° and lower. THT-D analyses were used to establish the actual thermal behavior, and compare it to the approximate values previously calculated. The analyses were performed using the THT-D thermal model of the Ceramic/Iridium Helipak Heat Source discussed in Section 4.2.1.2 (Case 1 model in Table 4.2.2-2).

Figure 4.2.2-96 shows the peak clad temperatures during the first skip as a function of γ and for two values of emissivity. The analyses assumed that the heat source was in the generator until reentry was initiated (giving maximum clad temperatures). A reduction from the approximate value of almost 700°F occurs at $\gamma = -4^{\circ}$ and $\epsilon = 0.5$, while it is 470°F for $\epsilon = 0.8$, establishing that the thermal inertia effect is quite significant. Figure 4.2.2-96 thus establishes the clad requirements with respect to melting point and emissivities.

Another matter of concern is the potential oxidation of the clad during a skip. To evaluate this, the oxygen environment, and the temperature behavior, as a function of time must be known. Figure 4.2.2-97 shows the oxygen partial pressure (free stream) as a function of time (as well as the one foot sphere reference heating rate, the altitude and the relative velocity) for the initial skip when $\gamma = -4^{\circ}$.

The corresponding clad temperature response, for $\epsilon = 0.8$, is shown in Figure 4.2.2-98, along with the approximate equilibrium reradiation temperatures ($\alpha \dot{q}$). The thermal inertia causes the actual maximum clad value to be lower and to occur later.

Table 4.2.2-5. Multiple Skip Analysis Summary

Initial Inertial Path Angle (γ_0) 400,000 ft. 36,333 fps	$\gamma = -3.5$	$\gamma = -3.75$	$\gamma = -4.0$	$\gamma = -4.25$	$\gamma = -4.5$	$\gamma = -4.75$	$\gamma = -5.0$	$\gamma = -5.1$	$\gamma = -5.2$	$\gamma = -5.25$	$\gamma = -5.3$	$\gamma = -5.35$
Number of skips	128	71	37	18	9	4	2	1	1	1	1	0
Time in Atmosphere (1st Skip)	142.7 sec	153	163.5	174.6	187.1	203.2	228.9	246.5	276.5	303.5	360.2	-
ΔV (Relative) Loss (1st Skip)	55 fps	98	188	381	805	1732	3635	4908	6700	7900	9475	-
Minimum Alt. (Skip #1)	320.8 K ft	309.3	296.6	283.9	270.0	253.9	237.9	230.9	223.5	219.8	215.8	211.7*
Peak H. W. $\dot{q}_{stag}$ 2.79" R, 1st Skip	41.0 B/sec - ft ²	66.1	102.4	155.1	220.6	312.6	395	-	-	-	-	-
Peak C. W. $\dot{q}_{stag}$ 1 ft R. Sphere (1st Skip)	28.6 B/sec - ft ²	46.3	72.1	108.4	155.3	230.4	293.6	319.1	340.0	354.2	-	379.9
Stag. Recession R = 2.75" (1st Skip)	.0023 in.	.0036	.0057	.0092	.0147	.0248	.0440	.0495	.0604	.0677	-	-
Cumulative Rec. All Skips R = 2.75"	.422 in.	.375	.2911	.2162	.1837	.1133	.0907	.0495	.0604	.0677	.0773	
Final Entry Recession, R = 2.75"	.160 in.	.154	.165	.174	.145	.156	.147	.182	.156	.148	.140	.232
Total Recession R = 2.75"	.582 in.	.529	.4571	.3902	.3287	.2693	.2377	.2315	.2164	.2157	.2173	.232
Final Entry Initial Vel. (Rel.)	24.9 Kilo ft/sec	24.8	25.3	26.0	24.8	26.6	26.7	30.3	28.5	27.3	25.7	-
Final Entry Time	1041 sec	928	843.8	888.2	692.8	582.6	515.6	563.8	472.3	464.8	488.3	-
Cumulative Time in Atmosphere at Impact	20998 sec	17418	9750	5330	3276	1666	1093	810	749	768	849	819

*No Skip

Figure 4.2.2-96. Comparison of Actual MHW Heat Source Outer Clad Peak Temperatures with Equilibrium Reradiation Values for Various Initial Path Angles

Figure 4.2.2-97. Multiple Skip Trajectory Behavior of MHW Heat Source

Figure 4.2.2-98. MHW Heat Source Outer Clad Temperature Response During a Skip Out Reentry

If the clad survives the initial skip it will cool during the time outside the atmosphere ($T_{\text{sink}} = 100^{\circ}\text{F}$) and, therefore, the initial temperatures for the second skip will be substantially lower because no generator ($T_{\text{sink}} = 1100^{\circ}\text{C}$) will be present. The effect of this is shown in Figure 4.2.2-99, for $\alpha = 0.8$, and we can see that the difference between the approximate and actual peak temperatures is 715°F (vs. 470°F for initial skip, or a reduction of 245°F in the maximum clad temperature during a skip).

Of further significance is the behavior of this peak clad temperature during the rest of the skips. To determine this the skip before final entry (#37 for $\gamma = -4^{\circ}$) was evaluated. The results are also shown in Figure 4.2.2-99 where the delta between approximate and actual is 340°F which indicates that as the skips get longer in duration, and the peak $\dot{q}$ decreases, the approximate and actual values approach each other.

The remaining significant thermal response is during the final entry. For this, it was assumed that the clad disappears early in the reentry. Figure 4.2.2-100 shows the temperature histories of the maximum aeroshell surface node, the maximum iridium fuel liner node, and the maximum fuel node. In order to place the results in perspective, Figure 4.2.2-100 can be compared to Figure 4.2.2-36 in Section 4.2.1.2.1 which shows the response of the same nodes during a minimum gamma reentry. The following compares the two analyses:

	<u>Aeroshell</u>	<u>Iridium</u>	<u>Fuel</u>
$\gamma = -4.0^{\circ}$, final entry	3760°F	3480°F	3180°F
MINIMUM GAMMA	5120°F	4440°F	3760°F

As is obvious, the γ_{min} reentry has a much more severe thermal response.

It is clear that there is a solution to the multiple skip reentry problem which consists of using a clad material which has a high melt temperature and an $\epsilon > 0.5$, and does not oxidize away during the skips.

Figure 4.2.2-99. Comparison of Actual MHW Heat Source Clad Peak Temperatures with Equilibrium Reradiation Values as a Function of Skip Numbers

Figure 4.2.2-100. MHW Heat Source Thermal Response During Final Entry

This will provide a margin of safety, with respect to computed ablation values. The heat source is protected from $\gamma_{\min}$ to $\approx -4.0^\circ$ by ablation, and from $\approx -4.0^\circ$ to 0° by cladding. This conservatively assumes that the heat source will:

1. Be side-on stable with slight rotation to maintain constant stagnation point
2. Have same stagnation point each successive skip.

The real problem was then examined. First, the idea that the heat source will rotate at the prescribed rate to maintain the same stagnation point is very remote ⁽¹⁾. However, even if this does happen, the idea of the same stagnation line each skip becomes more remote as the $|\gamma|$ decreases, which means less recession per skip. For instance, at $\gamma = -4^\circ$, the recession is 5.7 mils, or about 0.1% of the diameter. This does not appear to be sufficient to prejudice the heat source to that stagnation line on successive skips. Of course, as the $|\gamma|$ decreases this possibility becomes still more remote.

(1) Sandia internal letter, R. D. Klett to S. McAllees, "Stability of Axisymmetric Ablating Bodies during Reentry", February 4, 1971.

4.2.2.6 Post-Impact FSA Thermal Behavior

The most critical aspect of the FSA thermal response after impact is the PICS iridium temperature. If it stabilizes below 815-870[°]C (1500-1600[°]F) the iridium will not oxidize and provide fuel containment (a detailed discussion is contained in Section 4.2.1.3.1.3). Several analyses were performed to evaluate this thermal behavior; they are described in the following sections.

4.2.2.6.1 Post Impact Temperatures of PISA. The PISA Post Impact Shell Assembly (fuel and iridium only - no graphitic cover) was analyzed for a variety of post impact conditions, including effects of partial burial. The PISA design evaluated is for 122 watts as shown in Figure 4.2.1-101 which is the same as that in the "Revised" Helipak (Section 4.2.2.4). A total of 30 nodes was used in the THT-D model employed in the analysis.

The material thermophysical properties used in the thermal model were:

1. Fuel (Table 4.2.2-6) @ 76.5% T.D., 3.48 w/cc (lowest density PPO fuel assumed)
2. Magnesia (100% T.D.) from Thermophysical Properties Research Center Handbook
3. Iridium from T. P. R. C. volumes except emissivity.

The iridium emissivity behavior is a parameter in the study. The initial analyses used the emissivity data presented in Section 4.2.1.3.1.4 for unoxidized iridium. The unoxidized iridium emissivity was initially used in this analysis because:

- a. The iridium initially at impact is probably unoxidized.
- b. The unoxidized emissivity is less than the oxidized, therefore the analysis is overly conservative.
- c. The oxidized iridium emissivity behavior is not fully defined.

Figure 4.2.2-101. MHW FSA Thermal Model

Table 4.2.2-6. PPO Thermo Physical Properties

- 76.5% Theoretical Density
- $\ddot{q}'' = 3.48 \text{ w/cc (70}^\circ\text{F)}$
- $\rho = 546 \text{ lb/ft}^3$

Temp. ($^\circ\text{F}$)	"k" Btu/hr-ft- $^\circ\text{F}$	Cp Btu/lb- $^\circ\text{F}$	ϵ
400	2.52	0.074	0.35*
600	2.26	0.078	
800	1.94	0.080	
1000	1.68	0.081	
1200	1.47	0.0815	
1400	1.31	0.082	
1600	1.19	0.0825	
1800	1.11	0.0828	
2000	1.04	0.0830	
2200	0.995	0.0830	
2400	0.96	0.0830	
2600	0.94	0.0830	
2800	0.92	0.0830	
3200	0.89	0.0830	
3600	0.88	0.830	
4400	0.88	0.830	

*Assumed

The thermal model assumes that STP air is present in the iridium/magnesia and magnesia/fuel gaps, since this is the most conservative situation, and also the most likely. The initial condition evaluated was the case of a PISA lying on a adiabatic surface at 100°F . This surface is assumed to have an emissivity of 0.5. The PISA was assumed to be in 70°F air.

Convective cooling coefficients (h_c) were calculated for a sphere suspended in air, and it was assumed that the bottom 30° node lying near the ground had no convection cooling. The h_c values were calculated as a function of surface temperature and found to vary little ($\pm 10\%$) for the temperature ranges 500°F to 2500°F . Table 4.2.2-7 shows the h_c values used in the analyses.

Table 4.2.2-7.. Convective Coefficients (h_c) for PISA Post Impact

θ^*	h_c
$0^\circ - 30^\circ$	0^{**}
$30^\circ - 60^\circ$	$1.96 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$
$60^\circ - 90^\circ$	$1.83 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$
$90^\circ - 120^\circ$	$1.64 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$
$120^\circ - 150^\circ$	$1.54 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$
$150^\circ - 180^\circ$	$0.955 \text{ Btu/hr-ft}^2 - ^\circ\text{F}$

*See Figure 4.2.2-101

**Assumed zero due to ground interference

Ref. Kreith, "Principles of Heat Transfer" pg. 312

The initial analysis assumed a 2500°F isothermal PISA and calculated the temperature history until steady state was achieved @ ≈ 60 minutes as shown in Figure 4.2.2-102. Figure 4.2.2-103 shows the FSA steady state temperature profile. Since the PISA most probably will be at least partially buried, a series of steady state analyses were performed for varying amounts of burial. The profiles for these cases are shown in Figures 4.2.2-104 through 4.2.2-108, and the results summarized in Table 4.2.2-8. These analyses are conservative since they assume no heat flow to the burial medium (i.e., sand, soil). However, they allow one to scope the extent of the problem. Of course, these analyses are also conservative since they use unoxidized iridium emissivities while the actual PISA shell would certainly oxidize to some extent, thereby changing (raising) the emissivity.

A subsequent analyses was performed to evaluate the effects of oxidation. Test data (Section 4.2.1.3.1.4.1) indicates that oxidized iridium has an emissivity of >0.5 , and that oxidation occurs at temperatures above 815°C (1500°F). The emissivity behavior assumed in the analysis was:

$$\epsilon = 0.18 \text{ to } T \leq 1470^\circ\text{F}$$

$$\epsilon = 0.50 \text{ at } T \geq 1500^\circ\text{F}$$

NOTE: All curves initiate
at 2500°F

Figure 4.2.2-102. Post Impact Thermal Response of MHW PISA

Figure 4.2.2-103. MHW PISA Post Impact Temperature Profile

Figure 4.2.2.104. MHW PISA Post Impact Temperature Profile

Figure 4.2.2-105. MHW PISA Post Impact Temperature Profile

Figure 4.2.2-106. MHW PISA Post Impact Temperature Profile

Figure 4.2.2-107. MHW PISA Post Impact Temperature Profile

Figure 4.2.2-108. MHW PISA Post Impact Temperature Profile

with linear interpolation in between. This assumption is conservative since iridium probably oxidizes to form a higher than unoxidized surface ϵ at temperatures greater than 1100°F . Using this behavior, the thermal response for the non-burial and 50% burial conditions were calculated and the results are shown in Figures 4.2.2-109 and 4.2.2-110.

Table 4.2.2-8. FSA Post Impact Analyses Results
(Unoxidized Iridium)

θ Burial*	PISA Area Buried	Min. Ir. Temp $^{\circ}\text{F}$	Max. Exposed Ir. Temp $^{\circ}\text{F}$	Max. Ir Temp $^{\circ}\text{F}$	Min. Fuel Temp $^{\circ}\text{F}$	Max. Fuel Temp $^{\circ}\text{F}$
0°	0	1469	1504	1504	1590	1776
30°	6.2%	1485	1523	1542	1585	1801
45°	15.9%	1537	1600	1627	1638	1876
60°	25%	1605	1684	1740	1707	1976
75°	37.5%	1715	1811	1895	1825	2128
90°	50%	1859	1956	2109	1978	2334

*See Figure

The thermal response for an oxidized iridium PISA with $\epsilon = 0.5$ for all temperatures above 1100°F was calculated, and the maximum iridium temperature for 50% burial is 1500°F with an average of 1380°F . Similar figures for the 0% burial yield maximum values of 1074°F and average of 1039°F .

The PISA was subsequently changed to a ≈ 115 watt design as compared to 122 for the previous calculations. The analytical results above were ratioed giving the following results for the 114 watt sphere:

	T_{max} iridium	T_{avg} Iridium	% Burial
$\epsilon = 0.18 @ T \leq 1470^{\circ}\text{F}$	1511	1483	0
$\epsilon = 0.50 @ T \geq 1500^{\circ}\text{F}$	1730	1591	50
$\epsilon = 0.50 @ \text{all } T\text{'s}$	1075	1061	0
	1525	1405	50

- PISA OD = 1.789
- PISA SURFACE
- 0% BURIED
- $\epsilon = 0.18$ @ $T \leq 1470^\circ\text{F}$
- $\epsilon = 0.50$ @ $T \geq 1500^\circ\text{F}$
- AIR IN GAPS

SINK = 70°F

$\epsilon = 1.0$

Figure 4.2.2-109. MHW PISA Post Impact Temperature Profile

- PISA OD = 1.789
- $\epsilon = .18$ @ $T \leq 1470^\circ\text{F}$
- $\epsilon = 0.50$ @ $T \geq 1500^\circ\text{F}$
- AIR IN GAPS
- PISA SURFACE
- 50 % BURIED

SINK = 70°F

$\epsilon = 1.0$

Figure 4.2.2-110. MHW PISA Post Impact Temperature Profile

4.2.2.6.2 FSA Post Impact. Behavior, Including Graphitic Cover Oxidation.

An analysis was undertaken to evaluate the behavior of the complete FSA after impact assuming the graphitic cover remains intact. The analysis used the FSA from the Ceramic/Iridium Helipak (Section 4.2.2.1).

The graphite recession and the temperature history including convection cooling and exothermic heating were calculated. The sphere O.D. was 2.5 inches (of which 0.25 inches is impact shell) and with initial temperatures: Fuel = 2720°F Liner = 2530°F, Impact Shell = 2475°F.

The extremes of fast and slow graphite reaction rates have been considered, but the wound graphite impact shell is probably described best by the slow rate.

The convection coefficients used are those for a sphere suspended in air (Kreith)* with free convection cooling. The coefficients near the top and along the sides should be very accurate, those at the bottom should be lower than given by Kreith due to the difference in flow fields. Kreith's equation gives maximum heating at the bottom which is a stagnation point, but in this case the earth interferes with air flow over the bottom of the sphere. The maximum convection coefficient (at the bottom) varies from 2.07 to 2.66 Btu/ft²-hr-°R under the conditions of this analysis with a distribution given as a function of circumferential position. The multiplication factors used were for positions at the midpoint of the nodes indicated on the thermal model sketch.

Recession was calculated including reaction rate dependence for both slow and fast reactions. Flow conditions are constant so diffusion controlled recession rate is also constant. The reaction rate dependence causes recession rate to be a function of temperature and, therefore, a function of time. Recession rate calculations were based on Scala's equations and rate constants.

*Kreith, "Principles of Heat Transfer"

Calculation of exothermic heating was based on the equation given in the Phase I Final Report. The largest effect occurs with the fast reaction rate and the calculation for this case assumed 100% CO₂ at the wall in order to produce the maximum exothermic heating.

The thermal model used was two dimensional - radial and circumferential. The model consists of one slice of a fuel sphere taken from the detailed 3D Helipak model and shown in the sketch.

<u>Node</u>	<u>Factor</u>
213	.36
209	.59
209	.70
313	.75

GAPS WITH RADIATION ONLY
NO CONDUCTION
NO CONTACT COEFFICIENT

FUEL ϵ - 0.5

IRIDIUM ϵ - 0.33

A THT-D analysis was made including convection cooling, exothermic heating and radiation by defining a combined radiation term

$$q_{\text{net}} = q_{\text{conv}} + q_{\text{exotherm.}} - q_{\text{rad}} = -\epsilon_{\text{comb}} \sigma T_w^4$$

This definition was used to calculate ϵ_{comb} as a function of wall temperature since each of the terms q_{conv} , $q_{\text{exothermic}}$, q_{rad} can be calculated at various wall temperatures. The THT-D tables which result for the fast reactions are:

<u>Temp.</u> <u>°F</u>	ϵ_{comb}			
	<u>Node 213</u>	<u>Node 209</u>	<u>Node 309</u>	<u>Node 313</u>
500	.912	1.040	1.105	1.140
1000	.700	.667	.657	.653
1050	.607	.515	.482	.469
1100	.577	.447	.394	.371
1150	.577	.427	.362	.333
1200	.589	.437	.368	.337
1500	.697	.605	.560	.540
2000	.763	.724	.706	.698
2500	.784	.764	.755	.751
3000	.793	.783	.778	.776

Surface emissivity was assumed to be 0.8 so a value of ϵ_{comb} near 0.8 indicates that radiation is the major term in the heat balance. Values of ϵ_{comb} less than 0.8 indicate the influence of exothermic heating which is important around 1100 to 1200°F when the recession rate is high. Below 1000°F, recession rate decreases rapidly due to reaction rate dependence and exothermic heating decreases. In this mid-range of temperature, the magnitude of convection cooling is about 1/3 the magnitude of exothermic heating. Values of ϵ_{comb} greater than 0.8 indicate a cooling contribution due to convection. Note that ϵ_{comb} is positive throughout, i.e. the net surface energy balance indicates cooling throughout.

Exothermic heating and reaction rate decrease below about 2000°F when the graphite reaction rate is slow. Since radiation dominates in this range, exothermic heating and convection

cooling have been neglected in the analysis of recession for the case of slow reaction rate. At low temperatures (less than 1000^oF), convection cooling can affect temperature response but the recession rate of slow graphite becomes zero before this point is reached.

Figure 4.2.2-111 shows the temperature history calculated with the tabulated ϵ_{comb} data, while Figure 4.2.2-112 gives the recession rate based on these temperatures and the fast reaction rate. Figure 4.2.2-113 shows the temperature history with radiation cooling only and Figure 4.2.2-114 gives the recession rate obtained with this temperature and the slow reaction rate.

Assuming the fast reaction rate, recession would continue for about 800 seconds with the total varying from 26.2 to 13.8 mils. However, the recession would stop after about 55 seconds if the slow reaction rate is assumed, with a total recession which varies from 0.86 to 1.55 mils.

A similar analysis was conducted for the FSA in the revised Helipak, and the temperature response is shown in Figure 4.2.2-115 while the recession behavior of the graphitic cover (fast reaction rate) is shown in Figure 4.2.2-116.

These results were adjusted to predict the steady state results for the 115 watt fuel sphere, with resultant temperatures of:

graphite	(maximum)	=	625 ^o F
iridium	(maximum)	=	720 ^o F
fuel	(maximum)	=	975 ^o F

4.2.2.7 Heat Source Surface Temperature in Air

The surface temperature of the Heat Source during normal handling in air has been determined. the heat source used was for the design defined in Phase I. This design is defined in Section 4.2.1.2.5.

Figure 4.2.2-111. Radiation, Convection, Exothermic Oxidation Fast Reaction Rate

Figure 4.2.2-112. Fast Reaction Rate Temperatures with Radiation, Convection, Exothermic Oxidation

Figure 4.2.2-113. Temperature History of FSA, Radiation Only

Figure 4.2.2-114. Slow Reaction Rate Temperature with Radiation Only

Figure 4.2.2-115. MHW Fuel Sphere Post-Impact Thermal Response

Figure 4.2.2-116. MHW Fuel Sphere Graphitic Cover Post-Impact Recession

Diameter	5.6 inches
Length	12.4 inches
Thermal Load	2200 watts

The surface temperature of the Heat Source during normal handling in air has been determined. The heat source used was for the design defined in Phase I. This design is defined in Figure Section 4.2.1.2.5. Figure 4.2.2-117 represents the results of a cursory study which makes the following assumptions:

- a. Isothermal lateral surface
- b. Insulated end surfaces
- c. 70°F air boundary condition

Two conditions were evaluated:

- a. Radiation only - this is the highest temperature condition
- b. Radiation and convection from a vertical cylinder position - this is the lowest temperature condition

Figure 4.2.2-117 shows the results as a function of the heat source surface emissivity.

Figure 4.2.2-117. MHW Reference Ceramic/Iridium Helipak Heat Source Surface Temperature in Normal Air Environment

4.2.2.8 Graphite Noble Metal (GNOME) Heat Source

A potential backup design to Helipak is combining the use of fuel pucks with the graphite crush-up impact protection of Helipak. A typical configuration for 2200 watts using standard SSC 40 watt fuel pucks 2.15 inches in diameter and 0.210 inches thick is shown in Figure 4.2.2-118. The liner is iridium, the impact protection is Pyrocarb 406, (typically) the aeroshell is POCO and the outer can is any usable metal can such as iridium, or as previously thought usable, WC3015.

4.2.2.8.1 GNOME Heat Source with SSC Fuel.

The Pyrocarb/Iridium MHW heat source design presents no thermal problems and allows considerable latitude for "tuning" the design to accommodate either operational or reentry temperature limits.

This design, when 2200 watts are contained in one cylindrical heat source, weighs 48.5 pounds including a 30 mil WC3015 outer clad.

Some preliminary ground rules were established for use in the design of GNOME heat source. These are:

1. Minimize operational temperatures, keeping the fuel/fuel liner interface as close to 2250°F as possible.
2. Use standard SSC fuel pucks - 2.15" dia, 0.21" thick, and 40 watts/puck.

An initial effort evaluated the operational temperature profile as a function of the Pyrocarb "406" impact protection layer. These results are shown in Figure 4.2.2-119. This analysis assumed two concentric liners in the event a structural clad was added, but to correct to the case with only the Iridium fuel liner a ΔT of 30°F can be subtracted from the fuel/liner interface and fuel $\bar{q}_w$ temperatures.

Figure 4.2.2-120C shows the variation of the outer clad temperature with the heat source radius and clad emissivity. The results in Figure 4.2.2-119 used a 0.8 emissivity and the heat source radius resulting from the material stack-up shown in Figure 4.2.2-118.

Figure 4.2.2-118. 2200 Watt Pyrocarb Iridium MHW Heat Source Design

Figure 4.2.2-119. GNOME Heat Source Temperatures

Figure 4.2.2-120. GNOME Heat Source Component Temperatures

Figure 4.2.2-120C illustrates the sensitivity of the internal operational temperature to the clad emissivity, since any increase in clad temperature due to emissivity can essentially be added directly to the internal temperatures.

Figure 4.2.2-120B shows the temperature difference across the Pyrocarb "406" layer in this application as a function of the "406" thickness.

Some missions could result in the use of a downgraded generator, with hot shoe temperatures of as low as 1400°F. Figure 4.2.2-120A shows the effect of reduced hot shoe temperature on clad temperature. This reduction could be applied to internal temperatures as well.

Preliminary estimates of the required graphite impact layer thickness for the sides and ends have been made and Figure 4.2.2-118 shows the resultant configuration for a 2200 watt heat source. In order to evaluate the reentry thermal behavior, a γ min reentry analysis was performed. The analysis method and techniques are similar to those used in Section 4.2.2.1 and the Phase I final report. The analysis assumed a side-on stable reentry and used a 2D (radial and circumferential) model. The continuum ballistic coefficient, β , of the heat source is 47 PSF (without the WC3015 can which should burn off early in the trajectory) which corresponds to a γ min of -5.35° . The ABTON and THT-D analysis were performed and the results are shown in Figure 4.2.2-121. As can be seen, the maximum Iridium temperature is 3075°F, and the impact temperature is about 2600°F (extrapolated). Therefore, this design thermally is well within the Iridium temperature regime ($T_{\text{melt}} = 4429^\circ\text{F}$) and performs well below the fuel melt temperature of 4352°F. The corresponding operational temperature at the fuel liner/fuel interface is 2443°F, with a fuel centerline of 2623°F.

The ablation calculations for this design indicate that about 0.154 inch of recession at the stagnation point will occur prior to the subsonic portion of the flight. Subsequent to this as much as 100 mils additional may be ablated, but this still leaves a 50 percent safety margin.

- IMPACT VELOCITY ≈ 330 fps (SUBSONIC AND γ_{MIN} RECESSION)
- ≈ 345 fps (MULTIPLE SKIP REENTRY)

Figure 4.2.2-121. MHW Heat Source Reentry Thermal Response

The calculated impact velocity using the drop test data correlations reported in the Phase I final report is 302 fps for the unablated configuration (no WC3015 can), 320 fps for the heat source after the γ min reentry and 328 fps for the worst Multiple Skip case.

Depending upon further tests and calculations, the required graphite impact shell thickness may be changed. The impact on operational temperatures can be obtained from Figure 4.2.2-119. The reentry temperature response results are shown in Figures 4.2.2-122 and 4.2.2-123 for Pyrocarb thickness of 0.71 and 1.2 respectively.

If thicknesses greater than 1.5 inches are considered the maximum reentry temperature will be below 3075⁰F, but will not be reduced substantially since the operational temperature is increasing as well, and the available ΔT for heat absorption becomes smaller.

Figure 4.2.2-122. MHW Heat Source Reentry Thermal Response

Figure 4.2.2-123. MHW Heat Source Reentry Thermal Response

4.2.2.8.2 GNOME Heat Sources using PPO and PMC Fuel. The present MHW Helipak Heat Source designs are based upon use of PPO (or ceramic) fuel. Of interest is the effect of this fuel or PMC upon operational and reentry temperatures of a GNOME (Graphite Noble Metal) Heat Source using fuel discs. This section presents an evaluation of both fuels performed by evaluating PPO and PMC GNOME designs and then comparing performance.

The previous section discussed the results obtained for the SSC GNOME design sketched below:

Analyses were also performed for 0.71" and 1.2" Pyrocarb thicknesses.

To provide a comparative analysis, where only the fuel was different, a 1.2" Pyrocarb impact shell thickness case was chosen as a base point.

Table 4.2.2-9 summarizes the significant factors needed to compare the two heat sources. During normal operation, the fuel temperatures are 475° higher for the PPO versus the PMC due to the higher $\ddot{q}$ and lower "k". Both the reentry and impact temperatures are also higher due to the higher initial temperatures as well as the higher $\ddot{q}$, and the lower "k".

With both fuels, it appears that the iridium temperatures in the operational condition are no worse than for Helipak, about 2400°F to 2500°F, the reentry temperatures are lower than Helipak by about 400°F to 600°F and the impact temperatures are the same as for Helipak,

about 2500°F to 2600°F. The success of this design depends upon impact evaluation to determine the thickness requirements of the Pyrocarb 406 (or other carbon/carbon composite) and the survival of the iridium which will determine the weight of the system. Thermal performance appears not to be a problem.

Table 4.2.2-9. Comparison of GNOME Heat Source Designs

	PMC Fuel	PPO Fuel
Fuel heat generation rate, $\ddot{q}$, W/cc	3.2	4.0
Fuel thermal conductivity, k, B/Hr-ft-°F	3.5	1.6
Heat generation/axial length, W/in.	192	239
Operational Temperature, °F		
Sink temperature	2012	2012
Iridium fuel liner	2379	2529
Fuel $\bar{C}$	2600	3080
Fuel ΔT	178	486
Reentry peak temperatures, °F ($\beta = 45$, $\gamma = 5.35^\circ$)		
Iridium	3220	3394
(Time ~ sec)	(270)	(270)
Fuel	3220	3483
(Time ~ sec)	(270)	(525)
Impact temperatures, °F		
Iridium (Maximum)	2570	2650
Fuel (Maximum)	2900	3370

4.2.2.9 Evaluation of Individually Protected Fuel Pucks

The use of individually protected fuel pucks as the heat source had been proposed. GE/IPSO investigated the applicability of this approach to the MHW and the fundamental merits of this heat source concept. This section presents the results of the aerothermal study including a comparison of this approach with the other MHW heat source concepts, Helipak and GNOME.

The initial effort was to determine the behavior of an individually protected fuel puck, where the fuel puck was the "standard" 40 watt SSC puck, and the reentry body was also in the shape of a puck. Figure 4.2.2-124 shows the initial configuration evaluated. The following areas were investigated:

1. Reentry heating environment
2. Possible orientations
3. Thermal response during reentry
4. Thermal response as a function of initial path angle for superorbital reentry conditions
5. Recession behavior
6. Insulative requirements

The configuration in Figure 4.2.2-124 utilizes Pyrocarb 406 as a typical reentry protection because it will provide some insulative effect during reentry and also provide impact energy absorption. The fuel liner weight is calculated based on Pt-40 Rh ($T_{\text{MELT}} = 3510^{\circ}\text{F}$) but Iridium ($T_{\text{MELT}} = 4430^{\circ}\text{F}$) of about 75% of the thickness can be substituted with no weight penalty and no ballistic coefficient change.

SNAP-29 studies have shown that flat plates with thickness to diameter (or side length) ratios of 1/15 to 1/30 can reenter in edge-on spinning, flat-on stable, and flat-on tumbling flight modes. However, the configuration here has a thickness to diameter ratio of 1/4, and is therefore, approaching a cylinder which has a side-on (or edge-on) stable reentry mode. This configuration might also be stable edge-on, non-spinning but for this study it was not evaluated. Of the other possible modes, the behavioral limits should be established by the edge-on spinning and the flat-on stable orientations. Further, the best reentry behavior should be in the flat-on, stable mode and therefore, that was investigated first since if this was unacceptable the remainder of the study would be academic.

• STANDARD 40 WATT SSC FUEL PUCK

Figure 4.2.2-124. Individually Protected Fuel Puck Design

Calculations using SNAP-29 data and other sources, indicate that the drag coefficient (hypersonic continuum) of a circular disc flat-on is 1.68. Assuming that the outer can will be removed early in reentry, the weight of the unclad body can be used in determining the β which then is 7.37 psf (hypersonic continuum). In order to calculate the trajectory it was assumed that the puck remained in the generator to 300,000 feet, and became free and unencumbered at that point. The generator β was taken as 40 psf. Several trajectories were also calculated assuming free pucks at 400,000 feet and virtually no difference in altitude and velocity versus time is observed. The implications of generator break-up altitude will be discussed later; this is an important parameter due to the change in β from 40 to 7.37. The generator break-up altitude is also important in determining the minimum gamma, $\gamma_{\min}$, angle. For a free puck this is about -4.6 to -4.8° , while the generator has a $\gamma_{\min} = -5.3^\circ$. The generator/fuel puck configuration history would have a $\gamma_{\min}$ dependent on generator break-up altitude, but between the above two values.

Using the generator $\gamma_{\min}$, (-5.3°) as a starting point, a trajectory was calculated using $\beta = 40$ to 300,000 feet and $\beta = 7.37$ to $M_\infty = 1.0$ where the values of C_D from the LASL drop tests were used ($C_D \approx 0.85$) until impact. These LASL drop tests were performed using similar bodies, except they were square plates ($t = 1''$, $L = 2.75''$) instead of discs. The resultant trajectories, due to the approximations above, are only indicative of the expected behavior but are sufficient for this study. Figure 4.2.2-125(a) shows the behavior of Mach Number (M_∞), dynamic pressure (q^*) and reference cold wall convective heating ($\dot{q} \sqrt{R'}$). It is important that the peak dynamic pressure, when translated into peak stagnation pressure is about 210 psf (0.1 atm), which is capable of being simulated simultaneously with the peak $\dot{q} \sqrt{R'}$ value of 126 BTU/sec-ft^{3/2} in both the Sandia and Houston arc facilities. Also the body diameter (3'') is amenable to this type testing. Thus an advantage of this concept is the capability of performing superorbital ablation/thermal response testing. For comparison the peak $\dot{q} \sqrt{R'}$ value for a $\beta = 40$ psf (Helipak, GNOME typical β 's) would be about 300 BTU/sec-ft^{3/2}, therefore, another advantage - reduced reentry thermal severity is demonstrated. Figure 4.2.2-125(b) shows the altitude and velocity versus time behavior. The time from 100,000 feet is about 450 sec. and the time from $M_\infty = 1$ is about 400 seconds. This long time period accentuates any subsonic graphite recession and/or exothermic

Figure 4.2.2-125. Re-entry Behavior of Individually Protected Fuel Puck

surface reaction situation so that the impact temperature is difficult to predict as well as the total recession. Figure 4.2.2-125(b) shows the stagnation point temperature response assuming no exothermic reaction at the graphite surface, and if a graphite diffusion threshold of 1800°R (1340°F) is assumed, the total recession is 0.085" is observed. But if there were surface exothermic reactions sufficient to maintain the surface above 1800°R , the total recession could be 127 mils. Therefore, the sensitivity of recession to this time period after the end of convective heating is demonstrated. For comparison the equivalent time period for the other MHW heat sources is ≈ 250 seconds.

The heating distribution and level in comparison to stagnation point heating on a one foot radius sphere is shown in Figure 4.2.2-126 for the puck in the flat-on orientation. The heating distribution over the flat surface of the puck is that for a flat faced cylinder, and therefore, increases radially from the centerline to a value at the corner which is 160% of the stagnation point heating⁽¹⁾. Heating rate on the back surface is 10% of the stagnation point value (SNAP-29 data) while heating rate on the edge is 30% of the stagnation point value⁽²⁾. The stagnation point level is 1.58 times that to a one foot sphere determined as follows:

$$q_{\text{stag flat cylinder}} = 0.56 \left(\begin{array}{c} q_{\text{stag sphere}} \\ R_N = 1.495'' \end{array} \right) = 0.56 \sqrt{\frac{12}{1.495}} \left(\begin{array}{c} q_{\text{stag sphere}} \\ R_N = 12'' \end{array} \right)$$

After establishing the heating distribution and the heating behavior during the trajectory, a thermal response analysis was performed. A two dimensional (radial and axial) heat transfer model was used since the behavior is not a circumferential function. The initial temperatures assumed a good radiative heat transfer coupling between the puck and generator thermoelectrics and thus a liner temperature of 2175°O was used. The THT-D heat transfer

¹ Klett, R. D., "Drag Coefficients and Heating Ratios for Right Circular Cylinders in Free-Molecular and Continuum Flow from Mach 10 to 30". Sandia Laboratory SC-RR-64-2141.

² Kemp, N. H., Rose, P. H., and Detra, R. W., "Laminar Heat Transfer Around Blunt Bodies in Dissociated Air". JAS, July 1959.

computer code was utilized for the thermal response calculations. Figure 4.2.2-127 shows the model temperature distribution at the time when the maximum aeroshell surface temperatures are reached. The nodal pattern is also displayed in this figure, but only the significant temperatures are shown. As can be seen a maximum value of $\cong 4330^{\circ}\text{F}$ is reached which can be compared to $\cong 5500^{\circ}\text{F}$ for other MHW heat sources.

Figure 4.2.2-126. Heating Rate Distribution Standard Puck - Face On

Figure 4.2.2-127. Temperature Distribution of Individually Protected Fuel Puck

Figure 4.2.2-128 shows the temperature distribution for the same case at the time of maximum fuel liner temperatures, and a maximum temperature of 3860°F is observed. This temperature is probably acceptable if an Iridium fuel liner is employed, but not for Platinum-Rhodium.

Initially it was expected that as the initial path angle (γ) increased the maximum liner temperature would also increase since the reentry body was a low thermal capacitance, uninsulated system. Therefore, a series of trajectory and heating calculations for γ 's = -6° , -10° and -20° were made, and the thermal response analyses performed. Figure 4.2.2-129 shows the stagnation point reentry heating behavior (versus time) for the various initial path angles. Also shown are the integrated heating values which clearly show that the highest integrated heating value (and thus recession) occurs for the smallest path angle, -5.3° .

Figure 4.2.2-130 shows the maximum liner temperature as a function of γ , and the expected behavior did not occur, thus the smallest path angle resulted in the maximum temperature. The γ min for this body, as discussed previously, is dependent on generator break-up altitude and, therefore, the maximum fuel liner temperature may be higher than the value for -5.3° . However, the behavior of maximum liner temperature with initial path angle in the range γ min to -7° would have to be better defined in order to make a conclusion.

Figure 4.2.2-130 also shows the effects of including a 50 mil disc of Pyrolytic Graphite (P.G.) between the Pyrocarb 406 and the fuel liner. Approximately 75 mils of P.G. would be required to maintain the maximum liner temperature below 3250°F (leaving 270°F margin), however, as pointed out previously more definition in the region γ min to -7° would be required to fix this number.

Heating rate and recession rate histories during flight were calculated assuming the body shape to remain unchanged (heating and pressure distributions used were for sharp cornered shapes). Figure 4.2.2-131 shows the calculated ablated shape for a flat-on reentry at $\gamma = -5.3^{\circ}$. Figure 4.2.2-131 also shows, in tabular form, the recession values for the other γ 's. Recession was assumed to end when THT-D calculated surface temperatures of 1800°F

Figure 4.2.2-128. Temperature Distribution in Individually Protected Fuel Puck

SUPERORBITAL RE-ENTRY HEATING ENVIRONMENT OF INDIVIDUALLY PROTECTED FUEL PUCKS

Figure 4.2. 2-129. Superorbital Re-entry Heating Environment of Individually Protected Fuel Pucks

Figure 4.2.2-130. Individually Protected Fuel Puck Re-entry Behavior

GAMMA ANGLE γ	RECESSION MILS	
	"C"	"E"
-5.3°	67.5	137.0
-6.0°	61.9	124.0
-10.0°	51.8	102.0
-20.0°	40.7	79.2
EARTH ORBITAL	57.0	104.0

SCALE: 4 x FULL SIZE
 $\gamma = -5.3^\circ$ CASE SHOWN

Figure 4.2.2-131. Recession Profile Standard Puck - Face On Re-entry

were reached. Since these THT-D calculations did not account for exothermic graphite oxidation, recession ended early in the subsonic region, and as has been shown previously the total recession values could be considerably larger.

Having established the fundamental performance of this puck in a face (or flat) on, stable reentry mode, the next effort was to evaluate the severity of the edge-on, spinning reentry mode. The hypersonic drag coefficient of this shape, when referenced to its frontal area (πR^2) is 0.38, and thus the β was found to be 32.5 psf for this puck. Figure 4.2.2-132 shows the behavior of C_D as a function of angle of attack (α) for discs of this type.

The heating distribution and level for the puck in the edge-on spinning orientation are shown in Figure 4.2.2-133. The heating across the edge varies from the stagnation point value at the center to values 160% larger at the corners. Heating at the corner of the flat faces is 30% of the stagnation value and decreases toward the spin axis with a flat plate distribution - inversely proportional to square root of distance. The stagnation point heating level has been calculated with rough assumptions as 1.09 times that to a one foot radius sphere. For this purpose the width of the edge determines the heating. A flat strip of width .75" and infinite length would have a stagnation heating rate

$$\dot{q}_{\text{flat strip}} = .565 \times \frac{1}{\sqrt{2}} \left(\begin{array}{l} \dot{q}_{\text{stag sphere}} \\ R_N = .375'' \end{array} \right)$$

while a strip of finite length would have increased heating due to three dimensional flow effects. Curvature around the spin axis also increases heating due to three dimensional flow. The value

$$\dot{q}_{\text{stag edge}} = .60 \left(\begin{array}{l} \dot{q}_{\text{stag sphere}} \\ R_N = .375'' \end{array} \right)$$

was used. The effect of spinning is to reduce the average heating to 32% (Ref. 2) of the

Figure 4.2.2-132. Fuel Puck Drag Coefficient vs Angle of Attack

Figure 4.2.2-133. Heating Rate Distribution -
Standard Puck - Edge On Spinning

stagnation value. Therefore, the stagnation point level of 1.09 times the heating to a one foot sphere was determined as follows:

$$\dot{q}_{\text{stag edge spinning}} = .60 \times .32 \times \sqrt{\frac{12}{.375}} \left(\begin{array}{l} \dot{q}_{\text{stag sphere}} \\ R_N = 12'' \end{array} \right)$$

A two dimensional thermal model, radially and axially was constructed taking advantage of the axial symmetry and the circumferential uniformity. Figure 4.2.2-134 shows the temperature distribution at the time of maximum fuel liner temperatures. Note that the maximum liner temperature of 3830⁰F compares favorably with the 3860⁰F value found in the flat-on stable reentry analysis. This indicates that the edge-on spinning and flat-on stable modes of reentry have comparable thermal severities.

Figure 4.2.2-135 shows the recession profile for this edge-on spinning reentry condition. The same qualifications mentioned previously regarding the subsonic recession are applicable.

Preliminary integration studies indicated that the use of standard fuel pucks would not be easy and also that the operational temperatures would not be minimized since the only practical heat source configurations had to utilize the puck with heat transfer coupling from the puck circumferential edge area. Therefore, the use of higher wattage fuel pucks was pursued. Using the present generator thermopile dimensions shown in Figure 4.2.2-136 as restraining, it was found that 24-92 watt pucks in a hexagonal array could be mounted with the heat transfer coupling being from the flat surface. Figure 4.2.2-137 shows an approach to using this size fuel puck. This design employs a 50 mil P.G. insulation disc, and also a 50 mil circumferential layer of fibrous graphite, such as Union Carbide's UCAR. These insulation thicknesses were estimated based on the previously discussed analysis and in light of the fact that the ballistic coefficient, β , of this configuration come out to 13.2 (which is 80% greater than the standard puck concept indicating a thermally more severe reentry environment). A steady state analysis of this concept is shown in

Figure 4.2.2-134. Temperature Distribution at 115 Seconds
Fuel Pock Edge-On $\gamma = -5.3^{\circ}$

Figure 4.2.2-135. Recession Profile
Standard Puck - Edge on Re-entry

Figure 4.2.2-136. MHW Reference RTG Dimensions
For Heat Source Integration

**92 WATT FUEL PUCK
(D = 2.15")**

**W = 1.154 LB (NO OUTER CAN)
= 1.463 LB (.030" WC3015)**

Figure 4.2. 2-137. Individually Protected Fuel Puck Concept

Figure 4.2. 2-138 and the fuel/fuel liner maximum is seen to be 2500°F when helium is present in the gaps and 3200°F when the gaps are evacuated. A reentry thermal response analysis of the design assuming initial temperatures using helium filled gaps predicts maximum liner reentry temperatures of 3720°F in the face on reentry which is acceptable for Iridium but a 100 mil P.G. disc would be required to enable use of the Platinum Rhodium fuel liner with its lower melt temperature.

Figure 4.2. 2-139 shows the reference heating rates for this design, assuming face-on, stable reentry and for various γ 's.

The use of this type design does not affect the reentry performance substantially but does allow better thermal integration, although the structural integration problems are greater. Figures 4.2. 2-140 and 4.2. 2-141 show potential integration concepts using the 40 watt and the 92 watt individually protected fuel puck.

The reentry performance of these designs has not been established but it should be similar to those presented above.

Another effect of the integration is thermal shock, which was touched on previously. The generator has a ballistic coefficient of 35-50 depending on the particular design while the pucks (face-on) have β 's of from 7.37 to 13.2 psf. It is this low β which gives the low reentry thermal severity and, therefore, makes the individual puck concept feasible. However, if during reentry the generator does not disassemble until late in the heating pulse, the individual puck will be exposed to a potentially very severe thermal shock situation.

Another possibility is that the generator could remain an entity but with large holes in the leading panels ( ) thus exposing the fuel puck flat surface to a heating environment generated by a β of 35-50, which is 3-7 times greater than the fuel puck β . This more severe heating environment could result in liner overtemperature and possibly aeroshell burn through.

WC3015 ϵ = .8 (INSIDE AND OUTSIDE)	SINK TEMPERATURE = 1130 °C (2066 °F)
LINER ϵ = .8 (OUTSIDE)	SINK EMISSIVITY = .6
ϵ = .16 (INSIDE)	
FUEL ϵ = .16	

Figure 4.2.2-138. Operational Temperature Profile of
92 Watt Individually Protected Pucks

Figure 4.2.2-139. Superorbital Re-entry Heating Environment of Individually Protected Fuel Pucks

Figure 4.2.2-140. "Pallet" Concept Heat Source

Figure 4.2.2-141. "Domino" Concept Heat Source

A third potential situation which could occur, due to the structural integration requirements that dictate the heat source assembly must be capable of surviving the vibration, etc. is that the heat source assembly with a β of 35-50 would remain an entity thus generating the same problems as the second situation above.

The Helipak and GNOME designs are not sensitive to these situations since their β 's are 40-50 and the heat source is designed to that thermal severity.

Table 4.2.2-10 shows the comparison of these individually protected fuel puck concepts with the Helipak and GNOME designs. Table 4.2.2-11 summarizes the advantages and disadvantages of the individually protected fuel puck concept.

After a review of the analysis and results, the conclusions are that at best the individually protected puck systems only equal the performance of the Helipak heat source except when post impact temperatures are considered. Further, considerable testing and analysis would be required to develop performance predictions of equal confidence to those existing for the Helipak and GNOME design. Finally, the results of the testing and analysis may indicate that the performance is substantially inferior to the other systems.

4.2.2.10 Reference Helipak Design

The reference Helipak design as updated based on previous analyses and design test data is shown in Figure 4.2.2-142. The reference design has been analyzed using details of the analysis as presented in previous sections. Figure 4.2.2-143 presents a summary of the results. Some of the values in this table represent adjusted numbers from previous analyses to account for minor differences between previously analyzed configurations and the reference Helipak.

Table 4.2.2-10. MHW Heat Source Design Concept Comparison

	Helipak	* GNOME	40 Watt Puck	92 Watt Puck
Fuel/Fuel Liner Operational Temperature, °F	2350 (IR) 2460 (Pt-Rh)	2360 (IR) 2380 (Pt-Rh)	2286 (IR) 2420 (Pt-Rh)	2370 (IR) 2500 (Pt-Rh)
Internal Gas	He	He	He	He
Maximum Fuel/Fuel Liner Temperature during Re-entry, °F	3250 (Pt-Rh)	3590 (IR) 3220 (Pt-Rh)	3850 (IR) 3250 (Pt-Rh)	4100 (IR) 3720 (IR with 50 mil P.G.) 3500 (Pt-Rh) 3250 (Pt-Rh with 100 mil P.G.)
Impact Velocity, Ft/Sec	340 (IR) 340 (Pt-Rh)	330 (IR) 335 (Pt-Rh)	125 (IR) 130 (Pt-Rh)	140 (IR) 145 (Pt-Rh)
Impact Temperature, °F	2200-2500	2200-2500	500-1500	500-1500
Fuel Liner Post-Impact Temperature, °F				
Radiation Only $\epsilon = .8$	1000	1230	845	1025
(60° Lower With Convection) $\epsilon = .5$	1200	1500	1005	1170
(60° Lower With Convection) $\epsilon = .2$	1630	2000	1400	1635
Subsonic % of Hypersonic	≤ 80%	≤ 80%	?	?
Recession Time Period	≈ 175 sec	≈ 175 sec	≈ 400 sec	≈ 400 sec
System Weight 2200 Watts (No Mgt. Weight)	36.8# (IR)	40.6# (IR) 46.2# (Pt-Rh)	46.9 (IR)	35.4 (IR)

Note: .075" P.G. with Pt-Rh
for 40 and 92 watt pucks

* Graphite noble metal

Table 4.2.2-11. Advantages and Disadvantages of Individually Protected Fuel Pucks

Advantages	Disadvantages
<ul style="list-style-type: none"> • Less severe re-entry heating environment in flat-on, flat-on tumbling and edge-on spinning modes. • Significantly reduced impact velocity. • Flexibility and Modularity • Lends self to testing in existing facilities. (Including superorbital arc testing.) • Potential to be light weight heat source. • Post impact temperatures are reduced. 	<ul style="list-style-type: none"> • Subsonic behavior (temperature and recession) requires considerable investigation. • Subsonic flight time and impact point are very difficult to predict (latter means large possible area of impact). • Potential thermal stress failures due to gradients in two directions. • Aerodynamic behavior must be established with emphasis on possibility of edge-on stable re-entry. • Internal gas management is difficult and one PRD per puck would be required if helium internal was required. • Substantial amount of testing required. • Integration is difficult. • Positive release required. • Low β into high β.

Figure 4.2. 2-142. MHW Reference Design
2400 Watts

* Graphite $\epsilon = 0.65$

Figure 4.2.2-143. MHW Heat Source Performance Summary