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**SYSTEMS
FOR
NUCLEAR AUXILIARY POWER
ANNUAL REPORT
GOVERNMENT FISCAL YEAR 1976/TQ**

ERDA Research and Development Report

*Prepared for the United States
Energy Research and Development Administration,
Division of Nuclear Research and Applications
under Contract Number E(04-3)-701*

Rockwell International

Atomics International Division
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Canoga Park, California 91304

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I. SUMMARY

The overall objective of the Systems for Nuclear Auxiliary Power (SNAP) Program is to continue system and component engineering activities relating to the zirconium hydride (ZrH) reactor. The specific objectives for FY 1976/TQ were to: (1) study standardized ZrH reactor space power systems and components, (2) perform preconceptual analysis and design of ZrH reactor - organic Rankine power systems for subsea applications, (3) conduct fuel and hydrogen barrier investigations, (4) perform system studies in support of the Department of Defense and their contractors as directed by ERDA, (5) test components, and (6) provide for material disposal and facility surveillance.

A significant redirection of the zirconium hydride (ZrH) reactor space power system studies has resulted from the identification of several potential space missions which would profit from the availability of high power systems, in the 10- to 75-kWe range. These missions include, but are not limited to: space radar facilities, communication satellites capable of serving small, hand-held transceivers, and navigation satellites with increased transmitter power. The advantages of nuclear power systems, at these power levels, over solar cell arrays include: payload thermal conditioning capability, reduced spacecraft vulnerability, the capability to utilize spin stabilization in place of three-axis stabilization, and reduced interference with antenna view angles. A major preliminary systems study was completed during FY 1976/TQ which addressed the power requirements of these missions.

In the study, representative systems which utilize Brayton, Rankine, and Stirling cycle power conversion units as well as thermoelectric modules, are analyzed at power levels of 10, 25, 50, and 75 kWe. Waste heat rejection is accomplished by concentric, cylindrical space radiators which can be nested during launch for space shuttle integration. Subsequent studies, which supported this effort, were completed and provided useful information on system reliability and survivability. Taken in total, these analyses provide a solid basis for future system development plans at these power levels.

The increasing importance of the development of subsea oil recovery capabilities by U.S. oil and ocean engineering industries was recently highlighted by

the increase of oil imports to levels in excess of 40% of U.S. demand. Some of the most promising domestic oil reserves are located on the outer continental shelf, particularly offshore Alaska. Oil recovery from these reserves will involve operations in very severe environmental conditions. One of the major problems associated with the recovery of oil under these conditions is the requirement for substantial amounts of reliable electrical power.

Toward this requirement, engineering studies were continued on the ZrH reactor subsea power system. The concept includes a zirconium hydride nuclear reactor coupled to an organic Rankine power conversion system.

This report describes the activities conducted and the results obtained from the SNAP Program during FY 1976/TQ. Section II describes the work done in Systems and Applications Engineering, Section III covers Component Technology, and Section IV deals with Fuel Element Technology.

A. SYSTEMS AND APPLICATIONS ENGINEERING

Systems and Applications Engineering presents results of studies of: standardized space power systems, 10- to 75-kWe space power systems, laser effects on space radiators, meteoroid protection of space radiators, space system reliability, and subsea systems.

1. Space Systems

During FY 1976/TQ, characteristics of 5-, 10-, and 15-kWe thermoelectric (TE), Brayton, and organic Rankine space power systems were further refined from the designs established last year. Specific work included optimization of TE system mass with respect to the number of TE modules employed, and the establishing of standardized reactor fuel elements. The results included the selection of TE module configurations at 5-, 10-, and 15-kWe, and the designation of the reactor fuel element optimized for the 200-kWt reactor as the standardized fuel element for reactors whose power varied from 22 to 300 kWt.

One of the major objectives during FY 1976/TQ was the expansion of the concept of a ZrH reactor coupled to power conversion systems to include higher power levels. This objective was accomplished with the publishing of References I-1 and I-2 which describe the 10- to 75-kWe space systems studies. These studies resulted in the preconceptual designs of space power systems which

utilize Brayton, organic Rankine, and Stirling cycles and, to a limited extent, thermoelectric systems. Net electrical power levels of 10, 25, 50, and 75 kWe were considered for all but the thermoelectric systems. At 50 and 75 kWe, it was found that thermoelectric systems were excessively heavy.

The concept of concentric, cylindrical, telescoping radiators for waste heat rejection was utilized in these studies. The radiators are in a compact configuration on launch and are deployed when the power plant reaches station as shown in Figures I-1 and I-2.

The Brayton cycle is a closed gas-turbine design that uses an inert gas working fluid. The organic Rankine cycle is analogous to a steam turbine cycle except that the organic working fluid (Dowtherm A) has a vapor line in the temperature-entropy diagram that has a negative slope, thus improving turbine performance. The Stirling cycle has a closed gas arrangement with a reciprocating piston and a finely divided, solid-surface regenerator. It has the advantage of potentially high efficiency. While it is the oldest of these cycles in concept, it is the least well developed. The thermoelectric cycle depends on maintaining a radial temperature gradient across a stack of lead telluride washers that are alternately doped "N" and "P." Thermoelectric power conversion has the advantage that it is a passive device with no moving parts and, hence, is potentially highly reliable. Its disadvantage is that it has an inherently low efficiency.

The performance and masses of these power systems were calculated. Trade-off studies between system mass and radiator area were performed to determine the most favorable design points. The performance data generated included thermal powers, pumping powers, efficiencies, flowrates, temperatures, and pressure drops.

The power systems have a design life of 7 years. Shielding is provided to limit maximum radiation doses at the base of the power system of 10^{13} nvt fast neutrons and 10^7 rads in gamma rays.

The orbits about the earth were assumed to be geosynchronous. The launch vehicle was assumed to be the Space Shuttle with an upper stage to achieve a geosynchronous orbit.

Figure I-1. Zirconium Hydride Reactor Space Power System
Launch Configuration

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Figure I-2. Zirconium Hydride Reactor Space Power System
Deployed Configuration

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The Stirling cycle, as the data given in Table II-1 indicate, has the potential for high performance and low mass. This cycle, while not nearly so well developed as the other three, has considerable promise as a long-term approach. Of the near-term technology power systems, the Brayton system has the lowest mass, followed by the organic Rankine. It is also seen from the table that the specific mass decreases markedly as power is increased, then levels off or even increases. At low power levels, system efficiency tends to be low. At very high powers, the mass of the various coolants and piping tends to be high because of the system length. At the higher powers, the Brayton systems have a specific mass of about 75 lb/kWe, while the organic Rankine designs are roughly 100 lb/kWe. The Brayton system, however, requires a peak cycle temperature of 1270°F, while the organic Rankine temperature is only 700°F. On the other hand, the Brayton working fluid is a single-phase gas that is chemically inert while the organic Rankine fluid (Dowtherm A) undergoes a phase change and may exhibit some thermal decomposition. While the thermoelectric systems are the heaviest of all, they offer compensation in the passive and reliable nature of this power conversion approach.

In addition to the results of the parametric studies, progress was made in the analysis and design of the cylindrical, deployable radiators used in these power systems. Methods were developed to design minimum weight radiators in which the best values of tube size, tube spacing, armor thickness, fin thickness, heat transfer, and pressure drop were obtained. Advances were made in the engineering of radiator structure, deployment mechanisms, and radiator section alignment. Subsequent to the 10- to 75-kWe systems studies, three supplemental investigations were completed in support of space power systems.

The first of these studies considered the effects of lasers on space radiators. It was determined that low-power lasers posed the greatest hazard to radiators while high-power lasers would have insignificant effects due to their relatively short irradiance time capabilities. Consequently, the study was based on an arbitrary irradiance, $770 \text{ watt-sec-cm}^{-2}$, of the radiator by a CO₂ laser. A simplified heat transfer model was developed and estimates were made of the fraction of incident energy absorbed by the radiator. A radiator damage criterion was established as the temperature at which yield stress levels were generated

in the radiator fluid tubes due to the presence of saturated vapor. Finally, the irradiances required to cause damage in the radiators were calculated for each of the systems in the 10- to 75-kWe study. In all cases, the required irradiance was far in excess of the postulated base irradiance, indicating that the radiator employed in the 10- to 75-kWe systems will exhibit excellent survivability to laser interactions.

The second study was an investigation of radiator meteoroid protection structures. It was found that the armor for the radiator tubes, as calculated for the 10- to 75-kWe systems study, inadequately protected the tubes on the sides which faced the inside of the radiator cylinder. Consequently, a method was developed for determining the meteoroid shielding thickness needed by the sides of a D-shape tube attached to the inner surface of the radiator. The method uses a two-sheet bumper program modified to account for tube and cylinder geometry.

The third space system study considered, on a preliminary basis, systems reliability. Specifically, the reliability of the 50-kWe Brayton power system was examined. It was found that with no major components in parallel and no redundancy, the 7-year system reliability would be 0.716.

2. Subsea Systems

Due to the major emphasis on space systems, work on subsea systems during FY 1976/TQ was limited. However, several tasks were initiated, advanced or completed, primarily during the first quarter.

The subsea system concept was expanded to explore system power levels of 10 and 18.75 MWe, as well as 3 MWe, all operating at the same cycle state points. Preliminary estimates of performance and flow rates were supported by a computer program. The program was specifically written to analyze subsea systems and is expected to be utilized at a later date for system optimization.

The reactor decay heat transient analysis, initiated in FY 1975, was completed and documented.^(I-3) The analysis considered three cases:

- 1) Assuming heat rejection by unspecified means at an advantageous point, the reactor loop natural circulation parameters of ΔT , W , and

ΔP were determined. Based on 200 kWt of decay heat in the 3-MWe system, a final temperature of 929°F would occur 1000 sec after shutdown.

- 2) Assuming the natural circulation loop operative, heat is rejected to the surroundings by whatever passive heat transfer modes available. Transient calculations considered both water and air environments and indicated peak temperatures of less than 1000°F.
- 3) Assuming the reactor system is removed into the air, after a given period of time in water, the NaK coolant is accidentally lost from the system. Transient thermal calculations indicate extremely high temperatures attained in the core, on the order of 3000°F.

Due to the results of Case 3, recommendations were made that the thermal conductivity of the radiation shield be increased to avoid high temperatures resulting from such an accident.

A short study of the degradation of the organic in the power conversion system of the subsea system was made. It was found that the sum of pyrolytic decomposition resulted in 6 lb of degraded products. Methods were under study by which these products could be reduced or eliminated when the ZrH program priorities shifted to space systems.

A study was also initiated to determine more accurate methods of predicting the subsea reactor pressure drop. This study will be continued at a later date.

The subsea system transient analysis model was completed with the addition of the control system. A sample steady-state run resulted in satisfactory system response. Transient runs simulating the system's response to credible accidents will begin when work is resumed on subsea systems.

Finally, work was completed on a computer code which has the capability of designing natural convection heat exchangers for subsea applications. The program calculates surface area, finned tube configuration, the tube and fin weights, exchanger volume and tube pressure drops. This work was documented in Reference I-4.

B. COMPONENT TECHNOLOGY

Component technology activities during FY 1976/TQ included endurance testing of a reactor reflector control system actuator and ball screw, and pre-conceptual design of NaK pumps for systems operating at various power levels in both space and subsea applications. Subsequent to initial testing last year of the combined actuator and ball-screw assembly, it was noted that the actuator gearbox cover had cracked during assembly. The bearing was replaced and endurance testing initiated. During the thermal cycling test series, the ball screw became contaminated and jammed. Also, the nonmagnetic brake shim came loose. The ball screw was replaced, and a new brake shim was welded on using a modified welding technique. The endurance test was successfully repeated verifying ball-screw travel life and the suitability of the new brake shim welding. Post-test disassembly and inspection revealed that the quality of the parts was equivalent to those of a new machine. It was concluded that the design of the reflector drive was suitable for both normal control and scram shutdown without additional changes.

In ZrH nuclear power systems, circulation of the primary loop fluid, NaK, is provided by thermoelectric pumps. During FY 1976/TQ, preliminary designs of these pumps were generated for 15-kWe thermoelectric space systems as well as all of the 10- to 75-kWe space systems. In the case of the 10- to 75-kWe studies, both dc Faraday and ac helical pumps were considered. AC helical pump preconceptual designs were also prepared for the 10- and 18.75-MWe subsea systems. In the subsea system, parallel combinations of two and four pumps were also considered.

C. FUEL ELEMENT TECHNOLOGY

The fuel element technology task was divided into four activities during FY 1976/TQ: ceramic hydrogen barrier development, facilities modification, fuel casting, and NaK bonding experiments.

The three ceramic hydrogen barrier coatings, designated A, E, and J, identified during GFY 1975, were tested to further classify their physical properties. The tests performed included: hot indentation hardness tests, split-ring strain

tests, adherence testing for coating A, and flow coating tests. As a result of hot indentation hardness tests, coatings A, E, and J were found to soften at higher temperatures than SCB, the control coating. Split-ring strain testing revealed that the new coatings matched the expansivity of Hastelloy X better than SCB. The adherence of coating A to Hastelloy X was found to be inferior to that of SCB. However, methods were identified by which the adherence could be improved to acceptable levels. Finally, a new flow coating apparatus was tested and found to have acceptable performance. The apparatus was successfully utilized in the preparation of five slip-coated Hastelloy X tubes for permeation testing.

Facilities modification activities resulted in the addition of 1500 ft² of laboratories to the existing 500 ft² ceramics facility. The added capabilities include: permeation testing, hydriding, frit smelting, inert atmosphere fuel handling, and a radioactive material machine shop.

Due to unforeseen equipment inadequacies, attempts to melt fuel material in an existing induction heating furnace were unsuccessful. As a result, scheduled activities involving cast fuel were not completed during FY 1976/TQ. However, a laboratory furnace is being modified to accommodate the required temperatures and resumption of this activity is expected early in FY 1977. Equipment was received and activated and materials procured for the NaK bonding task.

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II. SYSTEMS AND APPLICATIONS ENGINEERING

Over the last 16 years, the development of zirconium hydride (ZrH) reactors and associated hardware has resulted in capabilities for the unattended production of power in space, or in remote terrestrial areas for extended periods of time. During FY 1976/TQ, a major effort was directed toward the expansion of space capabilities to include power systems at load levels from 10 to 75 kWe. The purpose of this study was to identify a variety of viable systems employing Brayton, organic Rankine, and Stirling cycle technologies as well as thermoelectric systems. The results of this study were published in an AI internal document.^(II-1) Additional investigations were made in support of this study and included: a study of the effects of lasers on space radiators, a meteoroid protection analysis, and a preliminary system reliability analysis.

Work was also continued on projects begun in FY 1975. Thermoelectric system optimization and reactor fuel element studies were performed in support of previous investigations of 3- to 15-kWe standardized space power systems. Various tasks were also advanced or completed in support of the subsea power system. Such power plants will provide power for the production of oil and natural gas from the ocean floor, unattended, for extended periods of time. In view of the continuing increase in U.S. demand for crude oil and dependence on foreign suppliers, this application remains timely and important.

A. STANDARDIZED SPACE SYSTEMS

1. Thermoelectric System Trade Study

A trade study was performed on nuclear thermoelectric (TE) space power systems at 5, 10, and 15 kWe. The objective of this study was to identify the power systems radiator operating temperature that resulted in near minimum mass. These power systems differed from those previously studied in that they have intermediate heat exchangers and deployable radiators.

The study was performed at each power level by varying the number of TE power modules. If a power system has relatively few modules, then considerable power is required from each one and the temperature difference between hot-clad and cold-clad modules is large. This causes the radiator to reject heat at

a. 5-kWe TE System

b. 10-kWe TE System

c. 15-kWe TE System

Figure II-1. Effect of Radiator Area, Number of Thermoelectric Modules, and Radiator Temperature on Thermoelectric System Mass

a low temperature, and results in a large and heavy radiator. Conversely, when a large number of power modules is used, the radiator is smaller and lighter. Most other components also vary in mass with the number of power modules. Estimates of these weight changes were made based on previous parametric studies. The reactor, however, remained at a constant mass for each power level. This was a consequence of the standardized reactor study discussed in Section II-A-2.

These TE power systems have deployable, organic-cooled radiators. The TE power modules are cooled by NaK, which, in turn, transfers its heat to the organic (Dowtherm A) radiator fluid in a tube-and-shell counterflow heat exchanger. There is a temperature drop in the heat exchanger, and also a drop between the bulk fluid in the radiator and the radiator fin root. Thus the temperature difference between the TE module cold cladding and the fin root may be 35 to 45°F on the average, compared with 5 to 10°F in all NaK systems with fixed radiators. This temperature penalty results in the radiators being somewhat larger in area, but because they are deployable and non-load bearing, they are lighter.

Other penalties include (1) the mass of the heat exchanger, (2) the mass of the organic coolant pump and piping, (3) the need for electrical power to drive the pump, and (4) the presence of unusable radiator area between deployed sections. Allowance for all these penalties was made in this study.

The results of this study are shown in Figure II-1 which shows how overall power system mass varies with the number of TE power modules for each power level. As has been the case in previous studies, there is a number of modules for which the power system mass is a minimum. This point occurs at radiator fin root temperatures that are somewhat lower than those found in all-NaK, fixed-radiator systems. The minimum mass point is usually not selected as the design point because other factors such as radiator area and system launch volume need to be considered. These preliminary results tend to show that deployed radiator systems, while they have inherent penalties, do lead to reasonable power system masses.

A comparison of a 5-kWe deployable radiator system with a 5-kWe fixed, NaK radiator system design is shown in Table II-1. The fixed radiator is heavier, although smaller because of the launch structural requirements. The

TABLE II-1
COMPARISON OF 5-kWe TE POWER SYSTEMS MASS
WITH FIXED NaK VS DEPLOYED ORGANIC RADIATOR

Parameter	Fixed NaK	Deployed Organic
Net Electrical Power (kWe)	5.0	5.0
Number of Thermoelectric Modules	16	16
Reactor Thermal Power (kWt)	95.7	95.7
Reactor BOL Outlet Temperature (°F)	1150	1150
TE Mean Hot Cladding Temperature (°F)	1093	1093
TE Mean Cold Cladding Temperature (°F)	527	527
Overall System Efficiency (%)	5.22	5.22
Radiator Mean Fin Root Temperature (°F)	522	492
Radiator Area (ft ²)	267	304
Mass (lb)		
Reactor	532	532
Shield	168	168
TE Modules	192	192
NaK Pump	79	73
Liquid Metal Components	191	175
Radiator	460	334
Heat Exchanger	---	77
Organic Pump and Loop	---	41
Deployment	---	37
Electrical	<u>40</u>	<u>40</u>
Total System Mass	1662	1669

deployed case has a smaller NaK heat rejection loop which accounts for the lighter NaK pump and liquid metal components. The total system masses are roughly the same.

2. Reactor Fuel Elements

During FY 1976, a two-part study was completed which showed the feasibility of the standard fuel element concept for Zirconium Hydride Reactor Systems over a power range of 5 to 15 kWe. This is equivalent to 22 to 300 kWt based on typical thermoelectric, organic Rankine, and Brayton power conversion systems efficiency.

The first part of the study consisted of a trade study in which reactor power, active core length, lattice pitch, number of fuel elements, prepoison loading, and reflector thickness establish minimum mass configurations over the entire thermal power range, which also meet the criteria listed in Table II-2. All calculations were performed using ZIP, a computer code written specifically to perform parametric nuclear and thermal-hydraulic calculations on shadow-shielded zirconium hydride reactors. The linear power density was limited to 1 kWt/ft.

In the second part of the study, various numbers of "standard elements," defined as those designed for the optimized 200-kWt reactor, were used at other power levels; the consequent mass penalties (i.e., minimum system mass with standard element minus optimized system mass at 1 kWt/ft) were calculated for each power level and are shown in Figure II-2. The only change allowed in the standard element design was the elimination of prepoison loading at the lower power levels, a step which did not dilute the standard element concept.

An element optimized for the middle of the power range (200 kWt) was chosen as the standard because: (1) it was discovered that although the optimized 100- to 300-kWt elements could each be used at 200 kWt with some degree of success, the mass penalties incurred by using the 100-kWt fuel element at 300 kWt or vice versa were a minimum of 200 lb, which was deemed unacceptable; and (2) such severe control problems were encountered using the 130-kWt and lower power optimized elements at 300 kWt that either (1) end of life reactivity and beginning of life shutdown requirements could not be met simultaneously, or (2) peak-average fuel temperatures were excessive.

TABLE II-2
STANDARDIZED FUEL ELEMENT STUDY CRITERIA

System Life (yr)	5			
Core Outlet Temperature (°F)	1200			
Core ΔT (°F)	100			
Fuel Temperature Limits (°F)	1400			
Fuel Phase	All 8			
Fuel Swelling Correlation	Bonser-Swenson			
EOL Fuel Element Gap (mils)	4			
Fuel Uranium Content (%)	10.5			
Reactor Thermal Power (kW)	22 to 300			
Linear Power Density (kWt/ft)	1 during mass optimization study; 0.5 to 1.7 during standard element study			
Number of Fuel Elements	19 to 421			
Fuel Length (in.)	10 to 24			
Prepoison	Natural gadolinium			
BOL Prepoison Loading (\$)	0 to 8.50			
Control Segment Stroke (in.)	3 to 8.50			
BOL Shutdown Margin (\$)	3 if power <200 kWt 2 if power >200 kWt			
EOL Reactivity Margin (\$)	10% of nonrecoverable losses			
Separation Distance to Dose Plane (ft)	Power Conversion System			
	Power Level (kWe)	Thermo-electric	Brayton	Organic Rankine
		Distance (ft)		
	5	25	38	26
	10	32	54	43
	15	42	73	53
Allowable Dose to Payload	10^{13} nvt/ 10^7 rads

Figure II-2. Reactor and Shield
Mass Penalties, 200-kWt
Reactor Element

When the 200-kWt standard fuel element was considered for higher powers, reactivity requirements were easy to satisfy, and the minimum reactor plus shield mass always corresponded to the minimum possible number of elements (i.e., maximum possible kWt/ft), lattice pitch, and reflector thickness such that conditions in Table II-2 were met. Also, the minimum reactor and shield mass for a given lattice size increased with the number of elements. On the other hand, when the standard element was considered for a lower power reactor (one which, when optimized, would have a thicker element) the thin standard element is less reactive for a given lattice size and minimum weight is a trade-off between the number of elements and reflector thickness, again at minimum pitch.

Since the derived mass penalties were largest for the 22-kWt reactor (127 lb or about 30%) and decreased monotonically to zero for powers greater than 200 kWt, it was reasoned that a shorter element, or one originally optimized for a lower power level at 1 kWt/ft, might improve the results. However,

TABLE II-3
REACTOR DESIGN SUMMARY, BRAYTON SYSTEMS

Parameter	Minimum Mass Trade Study at 1 kW/ft			Standard Element Study		
	Net Electrical Power					
	5 kWe	10 kWe	15 kWe	5 kWe	10 kWe	15 kWe
Thermal Power (kW)	22	47	64	22	45	68
Number of Fuel Elements	19	31	55	121	151	151
Active Fuel Length (in.)	14	18	14	16	16	16
Lattice Pitch (in.)	1.9568	1.4404	1.2501	0.8485	0.8485	0.8485
Gap Thickness (mils)	7.6	7.1	6.9	7.1	7.1	7.1
Fuel Diameter (in.)	1.8670	1.3513	1.1613	0.7593	0.7593	0.7593
Active Core Diameter (in.)	8.9567	8.4214	9.7354	9.8007	10.9485	10.9485
Active Core Volume (L)	14.455	16.430	17.078	19.780	24.684	24.684
Vessel OD (in.)	9.980	9.231	10.414	10.442	11.589	11.589
Reflector Mid-Plane Thickness (in.)	1.846	2.145	2.236	2.596	1.496	1.596
Reactor Mid-Plane Envelope (in.)	13.673	13.521	14.886	15.633	14.580	14.780
Segment Stroke (in.)	4.0	4.0	4.0	4.0	8.0	8.0
Fissile Mass (Kg U ²³⁵)	7.009	7.702	7.849	8.437	10.529	10.529
BOL H/Zr	1.648	1.639	1.634	1.611	1.611	1.611
EOL H/Zr	1.635	1.620	1.612	1.591	1.589	1.587
Peak-Average Fuel Temperature (°F)	1278	1294	1302	1244	1250	1259
Separation Distance (ft)	38	54	73	38	54	73
Reactor Mass (lb)	361	412.9	411.1	476.4	494.3	499.5
Shield Mass (lb)	63	63.9	62.2	76.2	69.5	65.1
Reactor plus Shield Mass (lb)	425	476.8	473.3	552.6	563.8	564.6
Mass Penalty (lb)	-	-	-	127.6	89.0	91.3

because of the problems associated with using the optimized elements from 130 kWt and lower at 300 kWt, the decision was made to try the elements designed for the minimum mass 140 and 170 kWt reactors, with an increased H/Zr ratio (maximum 1.65) to increase the available reactivity at very low powers. The trends described above were observed when using these elements at other than their design power levels and as expected, the lowest mass penalties were observed using the 140-kWt element with H/Zr increased from 1.62 to 1.65. These low relative masses persisted over the entire power range.

Table II-3 is an example of the type of data determined for each minimum mass configuration. The table also shows the characteristics of the fuel elements and reactors when the standard fuel element is used in the Brayton system reactors. Selection of the number of standard fuel elements was made considering the variation of reactor plus shield mass as shown for example, in Figure II-3. The maximum reactor plus shield mass penalty is 127.6 lb and occurs for the 5-kWe system.

Figure II-3. Combined Reactor and Shield Mass for Brayton System Standardized Fuel Element Reactors

TABLE II-4
SUMMARY OF STUDY RESULTS

Net Electrical Power (kWe)	10	25	50	75
<u>Brayton System</u>				
Total mass (lb)	1389	2394	3767	5570
Specific mass (lb/kWe)	138.9	95.8	75.3	74.3
Radiator area (ft ²)	435	1093	2000	3119
Deployed height (ft)	29	56.8	84.8	112.3
<u>Organic Rankine System</u>				
Total mass (lb)	1483	3028	4607	7702
Specific mass (lb/kWe)	150.5	118.5	89.8	102.7
Radiator area (ft ²)	540	1484	2500	4280
Deployed height (ft)	38.7	71	101.6	147.4
<u>Stirling System</u>				
Total mass (lb)	1067	1716	2596	4036
Specific mass (lb/kWe)	106.7	68.6	51.9	53.8
Radiator area (ft ²)	190	500	740	1250
Deployed height (ft)	16.8	33.2	44	56.9
<u>Thermoelectric System</u>				
Total mass (lb)	2321	5201	-	-
Specific mass (lb/kWe)	232.1	208.0	-	-
Radiator area (ft ²)	625	1612	-	-
Deployed height (ft)	41.5	68.5	-	-

B. 10- to 75-kWe SPACE SYSTEMS STUDIES

One of the major projects of the ZrH reactor program during GFY 1976/TQ was the generation of systems designs for space power applications in the 10- to 75-kWe range. These designs were documented in Reference II-1 and included Brayton, organic Rankine, Stirling, and thermoelectric systems. A brief summary of the results of these design efforts is shown in Table II-4. A more detailed account of the projects is contained in the following sections. In addition to the 10- to 75-kWe systems studies, several follow-on studies are also described which deal with various problems identified in the initial designs. All of the designs described are preconceptual and are subject to revision as improvements and new data become available.

1. Brayton Systems

During the year, a study of Brayton cycle nuclear space power systems was conducted at power levels of 10, 25, 50 and 75 kWe. The mass of these power systems ranged from 1389 lbm to 5570 lbm. Their radiator areas varied from 435 to 3119 ft². In the deployed mode, the height of these power systems ranged from 29 to 112.3 ft. Table II-4 summarizes the basic data of these four Brayton power systems. The details of this study are contained in Section II-B-1-a of this report.

In addition to the 10- to 75-kWe study, the sensitivity of the Brayton system mass to peak cycle and compressor inlet temperatures was examined. The purpose of this study was to determine the penalties that would be associated with reducing these temperatures in order to achieve lower fuel centerline temperatures and hydrogen leakage rates. This study is summarized in Section II-B-1-b.

Finally, an investigation was made of the 50-kWe Brayton system radiator manifolds in an effort to reduce the mass of the system. An optimization procedure was developed and the results of this effort are reported in Section II-B-1-c.

a. 10- to 75-kWe Systems Studies

In the Brayton systems, thermal power is generated in the zirconium hydride reactor and is transported by NaK in stainless-steel piping to a NaK-to-gas heat exchanger. The NaK is pumped by a dc electromagnetic pump powered by thermoelectric modules.

The Brayton cycle is a closed gas cycle using a gas turbine that drives an alternator and the compressor. The gas is a mixture of helium and xenon with a molecular weight of about 40. The gas receives heat in a NaK-gas heat exchanger. The heated gas travels to the gas turbine where it is expanded. The gas is still relatively hot after it leaves the turbine and transfers some of this heat to the recuperator. Then the gas is transported to the cooler heat exchanger where heat is transferred to the radiator loop. The cooled gas then enters the compressor. From the compressor, the gas returns to the regenerator where it receives heat from the turbine discharge flow. Thus preheated, the gas returns to the NaK-gas heat exchanger completing the cycle.

Heat rejected by the Brayton PCS enters the Dowtherm A coolant of the radiator loop. The radiator, as in all these power systems, consists of a truncated cone and one or more progressively larger cylindrical sections. Dowtherm A enters the cone section at the top and proceeds downward through all radiator sections which radiate the heat to space. There are flexible hoses connecting each section. The radiator is deployable, nested compactly in the launch configuration as shown in Figure II-4 and deployed in telescope fashion when in space.

Figure II-4. 50-kWe
Brayton System, 2000 ft²
Radiator Area

9095-5011

A radiation shield is located below the reactor to protect the power system and spacecraft from neutrons and from gamma rays. The NaK loop and the radiator loop both require volume accumulators to accommodate the changes in coolant density with temperature. Figure II-5 shows a schematic of a Brayton power system.

Figure II-6 shows a typical schematic of a Brayton power system. Shown is the conical radiator section which houses the major components — reactor, shield, power conversion unit, NaK-gas heat exchanger, coolers, pumps, and volume accumulators. A typical schematic of the overall power system in both the launch and deployed modes is shown with dimensions in Figure II-4. Shown is the 50-kWe Brayton power system. In the launch mode, it is 10.5 ft in diameter and 15.25 ft in length. In the deployed mode, the length extends to 84.8 ft, exposing the conical section and all five graduated cylindrical sections.

A parametric study was performed to determine the efficiency of Brayton power systems as a function of heat rejection temperature. Figure II-7 shows that the efficiency of this cycle declines with increasing heat rejection temperature. While this figure is for 50-kWe systems, similar results were obtained for the three other power levels.

A study was also made of the effect of heat rejection temperature on power system mass. At high heat rejection temperatures, the efficiency is low but the radiator is small in area and has a low mass. At low temperatures, the efficiency is much greater, but the radiator is required to be much larger and heavier. Figure II-8 shows that for 10-kWe systems, the overall system mass declines with radiator average temperature reaching a minimum between 200 and 300°F. This minimum depends, among other things, upon the orbit altitude and spacecraft attitude. To afford somewhat more operational flexibility, average radiator temperatures a little above those for minimum mass were selected. Later on in the study, radiator average temperatures were adjusted downward leading to somewhat lower system mass.

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Figure II-5. Reactor-Brayton System Schematic

Figure II-6. 50-kWe Brayton System Reactor Cone Assembly, Effective Radiator Area = 108.9 ft²

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Figure II-7. Overall System Efficiency

Figure II-8. Mass of 10-kWe Systems

TABLE II-5
BRAYTON SYSTEM PERFORMANCE CHARACTERISTICS

Net Electrical Power (kWe)	10	25	50	75
Electrical Power (kWe)				
Gross	10.5	26.25	52.0	78.75
Pumping	0.065	0.41	0.973	1.63
Transmission Line Loss	0.056	0.34	0.891	1.91
Net to Payload	10.38	25.50	50.14	75.21
Thermal Power (kWt)				
Reactor	57.2	128.0	255	366.6
TE Pump Modules	2.2	7.4	14.8	22.9
Heat Losses	5.0	6.0	8.0	10.0
Power Conversion Unit	50.0	114.6	232	333.7
Radiator	44.1	98.7	198	281.8
Efficiency (%)				
System	17.5	19.5	19.6	20.5
Power Conversion Unit	21.0	22.9	22.4	23.6
Temperatures (°F)				
Reactor Outlet	1300	1300	1300	1300
Reactor ΔT	100	100	100	100
Turbine Inlet	1270	1270	1270	1270
NaK-Gas Heat Exchanger Inlet	943	945	943	945
Cooler Gas Inlet	468	428	468	428
Compressor Inlet	220	220	220	220
Cooler Organic Inlet	192	192	192	192
Radiator Inlet	393	325	391	324
Radiator Outlet	192	192	192	192
Average Radiator	286	252	285	252
Flow Rates (lb/sec)				
Primary Loop	2.59	5.80	11.50	16.61
Gas Loop	1.22	2.09	5.41	6.05
Heat Rejection Loop	0.46	1.57	2.07	4.52
Pressure Drops (psi)				
Primary Loop	1.00	1.25	1.50	1.50
Heat Rejection Loop	25	40	80	55
Radiator (ft ²)				
Gross Area	467	1149	2118	3280
Effective Area	435	1093	2000	3119
Fin Effectiveness	0.766	0.80	0.746	0.80
Launch Height (ft)	11.3	15.2	15.3	17.4
Deployed Height (ft)	29	56.8	82.3	112.3
Diameter (ft)	6.7	8.2	10.6	12.3

Table II-5 lists the important data of the four Brayton power systems. The gross power delivered by the alternator is a few percent greater than the net electrical power to provide power to pump the radiator coolant and to allow for transmission line losses. The reactor thermal power includes not only that delivered to the PCS but also the thermal power needed to power the TE NaK pump and heat losses from the reactor, shield, and NaK piping. The thermal power rejected by the radiator includes that rejected by the PCS, but does not include that arriving from the sun and earth and then rejected.

All four cycles are based on a reactor outlet temperature of 1300°F and a NaK loop ΔT of 100°F. The temperature drop in the NaK-gas heat exchanger yields a turbine inlet temperature of 1270°F.

The pressure drop in the radiator coolant is very much larger than that in the NaK loop. This results from a combination of factors such as the relatively high efficiency of radiator coolant pumps, the need for high heat transfer in the radiator tubes, and the high L/D of these tubes.

The radiator sections overlap a few inches such that the effective area that rejects heat to space is about 5% less than the actual surface area of the sections. The launch configurations range from 11 to 17 ft in length but the deployed lengths vary from 29 to 112 ft.

Reactor mass varies only from 400 to 700 lb even though the net power ranges from 10 to 75 kWe. Much of the reactor mass is in structure and reflector outside the active core. Also, at the higher powers, fuel elements of small diameter are used allowing a higher power density. Shield mass is affected by both the power level and separation distance. Table II-6 shows that shield mass varies only moderately in these reference designs. Radiator armor mass is a strong function of power level. As power increases, the number and length of the armored tubes increases while the thickness also increases. At 10 kWe, 23 lb of armor is used but at 75 kWe, the armor mass is 832 lb.

Electrical transmission lines are selected on the basis of minimizing the total mass penalty. That is, the transmission line mass plus the additional power system mass needed to overcome the power loss in the line is minimized. Piping mass is selected on the same basis; i.e., the pipe, coolant, and pump mass plus the mass of additional power system needed to drive the pump are minimized.

TABLE II-6
BRAYTON MASS BREAKDOWN
(lb)

Power (kwe)	10	25	50	75
Reactor	412	517	587	712
Shield	108	138	165	148
Liquid Metal Components	127	239	406	542
Pumps	35	85	130	165
Piping	50	75	129	177
Volume Accumulator Units	14	27	72	80
NaK - Gas Heat Exchanger	28	52	75	120
Power Conversion Unit	348	509	906	1,175
HRL Components	45	74	239	270
Pump	10	15	20	25
Piping	14	31	129	145
Volume Accumulator Units	21	28	90	100
Radiator	319	868	1,374	2,621
Fins	116	246	453	702
Tubes (Wet)	12	80	86	158
Armor	23	147	238	832
Al-93	27	67	122	190
Structure	76	165	272	390
Manifolds	15	57	98	146
Alignment Hardware	23	70	58	142
Deployment Mechanism	27	36	47	61
Electrical	<u>30</u>	<u>49</u>	<u>90</u>	<u>102</u>
Transmission Line	5	24	65	78
Instrumentation and Control	25	25	25	25
Total	1,389	2,394	3,767	5,570

The specific power of the Brayton system improves from 139 lb/kWe at 10 kWe to 74 lb/kWe at 75 kWe. This mass saving is in the reactor, shield, and PCS.

b. Reduced Peak Temperature Study

Upon completion of the 10- to 75-kWe power system study, ^(II-1) more detailed analysis showed that fuel centerline temperatures and the resulting hydrogen leak rates were higher than desired. Therefore, the 10- and 50-kWe Brayton power systems were analyzed at the same and lower reactor outlet temperatures in an effort to mitigate these problems. The results of the revised analysis of the 10-kWe systems are shown in Table II-7. The 10-kWe reactor can deliver 1300°F NaK and still have a centerline temperature just below the limit of 1400°F. The resulting power system mass increases from 1389 lb in the reference report to 1450 lb — an increase of 4.4%. At lower reactor outlet temperatures, the fuel centerline temperatures dropped significantly while power system mass increased moderately. It was concluded that 10-kWe Brayton systems are satisfactory at reactor outlet temperatures in the range of 1250 to 1300°F.

TABLE II-7
10-kWe BRAYTON SYSTEMS AT VARIOUS
REACTOR OUTLET TEMPERATURES

Parameters	Reference 1	Revised		
Reactor Outlet Temperature (°F)	1300	1200	1250	1300
Reactor Thermal Power (kWt)	57.2	65.9	61.3	57.2
Fuel Centerline Temperature (°F)	1350	1307	1352	1399
Total System Mass (lb)	1389	1505	1469	1450
Mass Increase (%)	--	8.4	5.8	4.4

The results of a similar study for 50-kWe Brayton systems are shown in Table II-8. In this case, the more detailed analysis showed that a 1300°F outlet temperature led to fuel centerline temperatures in excess of 1400°F. In addition, the reactor shutdown margin was insufficient. At 1275°F, the reactor design

TABLE II-8
50-kWe BRAYTON SYSTEMS AT VARIOUS
REACTOR OUTLET TEMPERATURES

Parameters	Reference 1	Revised			
Reactor Outlet Temperature (°F)	1300	1200	1250	1275	1300
Reactor Thermal Power (kWt)	255	296	275	265	255
Fuel Centerline Temperature (°F)	≥1400	1351	1358	1354	>1400
Total System Mass (lb)	3767	4173	4058	4175	--
Mass Increase (%)	--	10.8	7.7	10.8	--

was feasible, but led to a power system mass of 4175 lb — about 10.8% higher than the reference value of 3767 lb. At 1250°F, a minimum system mass of 4058 lb was reached — still 7.7% above the reference case.

In an effort to reduce this mass penalty for 50-kWe systems, other compressor inlet temperatures in combination with lower reactor outlet temperatures were examined. The results of this study are shown in Table II-9. At a compressor inlet temperature of 160°F, the fuel centerline temperatures are satisfactory up to 1275°F while the minimum system mass of 3830 lb at 1275°F is only 1.7% above the reference value.

TABLE II-9
50-kWe BRAYTON SYSTEMS AT VARIOUS
REACTOR OUTLET TEMPERATURES
AND A COMPRESSOR INLET TEMPERATURE OF 160°F

Parameters	Reference 1	Revised		
Reactor Outlet Temperature (°F)	1200	1225	1250	1275
Reactor Thermal Power (kWt)	226	220	215	209
Fuel Centerline Temperature (°F)	1345	1355	1350	1374
Total System Weight (lb)	4079	3984	3935	3830
Mass Increase (%)	8.3	5.8	4.5	1.7

In summary, small reductions in both reactor outlet temperature and compressor inlet temperature in 50-kWe Brayton systems lead to more satisfactory fuel centerline temperatures and to lower hydrogen leakage rates with only a minor mass penalty. Consequently, the reference reactor outlet temperatures for the Brayton systems have been lowered to 1250°F.

c. Manifold Optimization

As part of the effort to refine and reduce mass in the 50-kWe Brayton system, a study was made to optimize the design of the radiator manifolds. Each of the six radiator sections has a toroidal manifold at both ends to accommodate the inlet and outlet flow of the radiator coolant. Typically, these manifolds are of thin-walled aluminum alloy tubing about 1 in. in diameter. The optimum tubing diameter is the one which results in the lowest overall mass. This mass consists of the tubing, coolant, pump, and that part of the overall power system that is needed to supply power to the pump.

Analysis indicates that each of the 12 manifolds will lose about 1.5 velocity heads based on the maximum flow velocity in the manifold. Thus, a pumping power can be associated with any manifold tube diameter. Considerations of the mass of pump plus power system to drive the pump indicate that this penalty is about 80 lb/kWe. The manifold mass and the pump system mass were calculated for several manifold tube diameters. These results are shown in Figure II-9. The minimum overall system mass occurs at a tube ID of 0.635 in. and is 87 lb of which the manifold mass is 61 lb and the pump system, 26 lb. The electrical power required at this point is 0.33 kWe. In an earlier study performed without optimization, it was concluded that the tube ID should be 0.85 in. Thus, by utilizing the optimization procedure, the manifold system weight can be reduced by about 20%. The results of this study are applicable to the manifolds of the other systems as well as Brayton systems.

2. Organic Rankine Cycle Systems

As part of the overall 10- to 75-kWe space systems study, ^(II-1) analyses of organic Rankine cycles at power levels of 10, 25, 50, and 75 kWe were performed. The mass range of these systems was 1,483 to 7,702 lbm. The radiator areas varied from 540 to 4,280 ft². In the deployed mode, exposing full

Figure II-9. Minimum Mass Manifolds,
50-kWe System

radiator area, the system lengths ranged from 38.7 to 147.4 ft. The basic data of all four organic Rankine cycle systems is shown in Table II-4. Details of the study are summarized in Section II-B-2-a of this report.

Subsequent to the original 10- to 75-kWe study, the mass of the organic Rankine cycle systems was deemed excessive relative to the mass of comparable Brayton and Stirling systems. Consequently, two separate studies were made in an effort to reduce the organic Rankine cycle system mass. The first involved the application of an indirect organic Rankine cycle system employing three loops in the system instead of two and is documented in Section II-B-2-b. The second alternative utilized parallel loops in the radiator and is summarized in Section II-B-2-c.

a. 10- to 75-kWe Systems Studies

The organic Rankine cycle system selected for analyses is represented schematically in Figure II-10. Heat is supplied by a ZrH reactor. Waste heat is rejected by telescoping cylindrical radiators. Primary loop NaK circulation is provided by a thermoelectric powered electromagnetic pump.

Figure II-10. Reactor - Organic Rankine System Schematic

The organic Rankine power conversion cycle is of the regenerative type to take advantage of the sensible heat of the superheated turbine exhaust. A jet condenser is employed to provide a single phase heat rejection loop. The organic working fluid, Dowtherm A, is vaporized in a NaK-to-organic counter-flow boiler. The fluid is then expanded in a single stage, supersonic, impulse turbine. Subsequent to expansion, a portion of the sensible heat energy of the vapor is transferred to the subcooled boiler feed stream in a plate and fin regenerative heat exchanger. The vapor is then condensed in a jet condenser by a spray of subcooled liquid supplied by the radiator.

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Figure II-11. 50-kWe Organic Rankine System Reactor Power Conversion Unit Assembly, Effective Radiator Area = 112 ft²

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Figure II-12. 50-kWe Organic Rankine System

After leaving the jet condenser, the combined flow is pumped by a centrifugal pump mounted on the turbine shaft. The flow from the pump splits into two streams.

The first stream cools the alternator as well as the pump and turbine shaft bearings before being preheated in the regenerator. Subsequent to preheating, the cycle is completed when the flow is vaporized in the boiler.

The second, and larger flow stream from the pump is subcooled in the radiator segments. A small portion of this flow also cools the cold junction of the NaK pump thermoelectric modules.

Electrical power is produced by the alternator located on the same shaft as the turbine and the pump. This power is distributed to transmission line losses and the load. The three dynamic components (turbine, alternator and pump) configured on the same shaft are designed as an integral unit known as a combined rotating unit or CRU. This concept allows an overall compact configuration as well as restricting the dynamic components to one small volume.

In addition to these components, several others are essential to the operation of this system. Volume accumulators are required in the primary and secondary loops to account for the changes in density of the loop fluids between ambient and operating temperatures. Flexible hoses are required for coolant transfer between radiator segments. Neutron shadow shielding is required to reduce the dose rate at the payload to the design limits. The radiator segments also require a deployment mechanism.

Figure II-11 shows a typical schematic of the organic Rankine power system. The schematic shows the conical radiator section which houses all of the major components. Also a schematic of the overall power system in both the launch and deployed modes is shown with typical overall dimensions in Figure II-12. The 50-kWe organic Rankine system is shown. In the launch configuration, it is 15.5 ft in length and 12.3 ft in diameter. In the deployed mode, it telescopes out to 101.6 ft in length.

As with the Brayton system, parametric studies of the organic Rankine system efficiency were performed. Figure II-7 shows the 50-kWe organic

TABLE II-10
ORGANIC RANKINE CYCLE
SYSTEM PERFORMANCE CHARACTERISTICS

Net Electrical Power (kWe)	10	25	50	75
Electrical Power (kWe)				
Gross	10.5	26	52	77
Transmission Line Loss	0.5	1	2	2
Net to Payload	10.0	25	50	75
Thermal Power (kWt)				
Reactor	105.7	234.1	438.8	639.7
TE Pump Modules	18.2	26.1	35	52.2
Heat Losses	1.4	1.7	3.8	4.2
Power Conversion Unit	86.1	206.3	400	583.3
Radiator	94.5	214.2	374.9	576.9
Efficiencies (%)				
System	9.5	10.7	11.4	11.7
Power Conversion Unit	12.2	12.6	13.0	13.2
Temperatures (°F)				
Reactor Outlet	800	800	800	800
Reactor ΔT	100	100	100	100
Turbine Inlet	700	700	700	700
Boiler Inlet (Dowtherm A)	570	570	570	570
Radiator Inlet	400	400	400	400
Radiator Outlet	300	300	300	300
Average Radiator	345	345	345	345
Flow Rates (lb/sec)				
Primary Loop	4.15	10.51	19.4	29.1
Power Conversion Unit Loop	0.46	1.11	2.14	3.2
Radiator	1.6	4.22	7.42	11.4
Pressure Drops (psid)				
Primary Loop	1.0	1.2	1.5	1.8
Radiator	24	50	75	90
Radiator (ft ²)				
Gross Area	564	1584	2643	4523
Net Area	538	1484	2497	4280
Fin Effectiveness	0.81	0.78	0.80	0.72

Rankine cycle power conversion system efficiency as a function of heat rejection temperature. As it is with all heat engines, the efficiency declines with increasing heat rejection temperature. The 50-kWe system curve is typical of the curves at the other power levels.

In determining the optimum organic Rankine cycle system, the total system mass was considered. Qualitatively the following component trends were noted: (1) the radiator mass decreased dramatically with increasing heat rejection temperature and, (2) the power conversion system and reactor masses increased with increasing heat rejection temperature due to decreasing efficiency. Since the radiator is, by far, the most massive component of the system, the overall result (as shown in Figure II-8) is a decreasing system mass with increasing heat rejection temperature. Theoretically, a system mass minimum should exist. However, the system is constrained by the use of all aluminum radiators to a maximum heat rejection temperature of 400°F. Consequently, this is the optimum operating point from a mass and size standpoint. While Figure II-8 is the mass curve for the 10-kWe system, similar results were obtained for the organic Rankine cycle system at other power levels.

The important organic Rankine power system performance parameters are listed in Table II-10. The organic system, unlike any of the other systems, has no third loop. The radiator fluid is driven by the same pump which circulates the cycle working fluid. Consequently, no pumping power losses are shown in the table. The relatively high transmission line losses shown are due to increased deployed system lengths compared to the Stirling and Brayton systems. The reactor thermal power shown includes PCU input, primary loop loss, and TE pump power requirements. The radiator power requirement includes PCU waste heat, primary loop losses, and TE pump waste heat, but not reradiated environmental heat input.

As with the other systems, the radiator pressure drop is much larger than in the primary loop. This is a result of the local optimization of the radiator and the utilization of high efficiency cycle pumps, small radiator tubes, and high radiator heat transfer coefficients. The effective and average radiator areas differ by 4.8 to 6.7% to account for radiator segment overlap.

TABLE II-11
ORGANIC RANKINE CYCLE MASS BREAKDOWN

Net Electrical Power (kWe)	10	25	50	75
Reactor	444.1	565.3	680	793.0
Shield	136.2	153.6	163	164.0
Liquid Metal Components	183.0	382.0	734	1,199.6
Pumps	46.0	74.0	156.0	306.0
Piping	55.0	131.0	224.0	355.5
Volume Accumulator Unit	21.0	59.0	140.0	228.4
Boiler	61.0	118.0	214.0	309.7
Power Conversion Unit	245.0	375.0	470	597.0
Heat Rejection Loop	43.7	223.0	398	888.0
Hoses	28.7	187.0	338.0	700.5
Volume Accumulator Unit	15.0	36.0	60.0	187.5
Radiator and Structure	396.8	1,268.7	2,064	3,853.4
Fins	120.8	334.1	564.0	967.3
Tubes	19.9	69.1	134.0	170.0
Armor	61.3	284.6	442.0	945.9
Manifolds	17.4	120.3	251.0	459.4
Structure	82.5	202.6	304.0	556.1
Alignment	21.2	74.2	63.0	212.8
Deployment	31.0	46.0	55.0	80.0
Coolant	10.1	51.9	100.0	214.3
Al-93	32.6	85.9	151.0	247.6
Electrical	<u>33.8</u>	<u>60.0</u>	<u>98</u>	<u>206.7</u>
Transmission Line	8.8	35.0	73.0	181.7
Instrumentation and Controls	25.0	25.0	25.0	25.0
Total	1,493.0	3,028.0	4,607	7,702

The organic Rankine cycle system mass breakdown is shown in Table II-11. The nominal variation of reactor mass with power is due to the mass intensive nature of nonpower-sensitive reactor components such as reflector and support structure. Very little variation in shield mass with power is encountered with ORC systems due to the relatively large separation distance. However, the low system efficiency also results in large increases in liquid metal component masses. These increases were significantly large enough to warrant the study of the additional mass reduction techniques discussed in Sections II-B-2-b and II-B-2-c.

b. Indirect Organic Rankine Cycle

An effort was undertaken to determine whether substantial mass savings could be realized in the organic Rankine cycle system by the addition of a third loop. This loop would remove the heat from the organic working fluid stream, by means of a cooler located downstream of the jet condenser. Additional piping and a centrifugal pump would circulate the third loop fluid, Dowtherm A, to a space radiator which would operate in a low flowrate, high temperature drop regime. The modified schematic of such a system is shown in Figure II-13.

It was thought that overall mass reduction would occur due to decreases in radiator tube, armor, and coolant mass. It was expected that this decrease in radiator mass would more than compensate for increases in system mass due to the added cooler, pump, piping, and the associated extra structure.

A wide range of possible operating conditions for such a system was considered. For the reference design radiator inlet temperature of 400°F, the heat exchanger cold stream and radiator temperature drops were varied between 110 and 290°F. Table II-12 shows the matrix of operating conditions considered. A trade developed between radiator and cooler mass. As the Δt_m of the cooler increased, the radiator weight increased while the cooler mass decreased. This was due to the depression of the average radiator temperature with increasing heat exchanger Δt_m .

A detailed study was performed on three of the operating conditions shown in Table II-12 at a system power level of 75 kWe. The three conditions selected for study included representative high, medium, and low cooler Δt_m . The cooler

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Figure II-13. Reactor - Organic Rankine System Schematic (Indirect)

TABLE II-12
INDIRECT ORGANIC RANKINE CYCLE
OPERATING CONDITIONS SUMMARY

a. Total Radiator Loop $\Delta P \leq 75$ psid

Cooler Data

	Hot Fluid	Cold Fluid
Type	Dowtherm A	Dowtherm A
T _{in} (°F)	400.0	100 to 190
T _{out} (°F)	362.0	300 to 390
Load (kwt)	576.9	576.9

b. Parametric Study Data

Cooler Δt_m

$T_{c*in} \backslash T_{c*out}$	100	150	175	190
300	168	149	139	133
350	128	112	104	99
375	101	87	81	79
390	77	66	60	58

*TC = Cold Fluid Temperature (°F)

area and pressure drop calculations were performed with the aid of ST4, a commercial shell and tube heat exchanger design code, documented in Reference II-2. The results of ST4 were used as a basis for hand calculations which determined the weights of the coolers. The radiators were designed by RADAN, a radiator design code, in the same manner as the direct Rankine cycle radiators (see Section II-B-5-b). The results of this study are tabulated in Table II-13.

It was found that even at optimum trade-off conditions, the total mass of the radiator and heat exchanger alone exceeded the mass allocated to these items in the direct cycle. The effort was terminated when it was found that in addition to excessive mass, the cooler area requirements were large.

TABLE II-13
75-kwe INDIRECT ORGANIC RANKINE CYCLE STUDY SUMMARY

Case Number	Base	1, 1	3, 2	4, 4
Cooler Δt_m (°F)	-	168.0	87.0	58.0
Cooler Area (ft ²)	-	313.0	722.0	1,445.0
Radiator Net Area (ft ²)	4,280.0	10,369.0	6,788.0	6,613.0
Radiator Gross Area (ft ²)	4,523.0	10,570.0	7,168.0	6,994.0
Weight Penalty/kWe Generated (lb)	105.2	105.2	105.2	105.2
Excess Electrical Power Generated (kWe)	0	1.81	2.02	2.59
Mass Summary				
Reactor, Shield, Liquid-Metal Components and Power Conversion Unit	2,753.6	2,753.6	2,753.6	2,753.6
Hoses	700.5	941.3	549.0	611.5
Volume Accumulator Unit	187.5	126.0	180.0	240.0
Cooler	0	527.7	1,095.4	2,103.0
Pump	0	30.0	30.0	30.0
Subtotal Heat Rejection Loop	888.0	1,625.0	1,854.0	2,985.0
Fins	967.3	2,300.6	1,533.1	1,495.8
Tubes	170.0	418.6	261.5	255.0
Armor	945.9	2,605.3	1,669.8	1,611.1
Manifolds	459.4	414.4	353.4	387.7
Structure	556.1	1,185.2	860.5	796.7
Alignment	212.8	487.8	337.5	328.8
Deployment	80.0	164.0	96.0	108.0
Coolant	214.3	549.9	250.4	244.6
AI-93	247.6	599.9	392.7	382.6
Subtotal Radiator and Structure	3,853.0	8,726.0	5,755.0	5,610.0
Transmission Line	181.7	325.7	202.4	231.4
Control	25.0	25.0	25.0	25.0
Subtotal Electrical	207.0	351.0	227.0	256.0
Excess kWe Penalty	0	190.4	212.5	272.5
Total	7,702.0	13,646.0	10,803.0	11,877.0

c. Parallel Loop Radiator Study

A study was also completed to determine whether significant organic Rankine system mass savings could be obtained by utilizing parallel hydraulic combinations of radiator segments in place of having all radiator segments in a series hydraulically. Under this new concept, one or more radiator cylinder segments would comprise a loop. Any number of parallel loops can be configured by this method up to the number of cylinder segments plus the cone. However, for purposes of this study, the maximum number of loops was limited to three in order to maintain comparable heat rejection loop pressure drops and pumping powers. Parallel loops were considered only at 25-, 50-, and 75-kWe organic Rankine cycle system power levels, since they had the highest masses at these levels when compared to Stirling and Brayton systems. However, the option of parallel loop radiators is applicable to any other similar system.

The relation which describes a space radiator heated by the flow of a fluid through an integral tube is simply a steady-state heat balance between the heat lost by the fluid and the heat lost by the radiator. For equivalent absolute temperature drops of the fluid, the heat rejected, radiator area, and fluid flow-rates are all directly proportional to one another. Consequently, fluid flow through each parallel loop was adjusted until its ratio to the total flow was the same as the ratio of the loop radiator area to total radiator area. In order to maintain comparable heat rejection loops, the fluid flow area of each loop was also adjusted such that fluid velocities equivalent to those in the series radiators were achieved.

Cases of one, two, and three loops were then analyzed for each power level by the computer code RADAN and the peripheral equipment configuration and masses were calculated by hand. In order to fairly evaluate the results, pumping power mass penalties were assigned to each case relative to the one loop (series) case.

Typical results of this study are shown in Table II-14 for the 50-kWe power level. Significant savings (up to 15.7%) in system mass were achieved in the heat rejection loop and radiator components.

TABLE II-14
SYSTEM MASS
50-kWe ORGANIC RANKINE CYCLE SPACE SYSTEM
RADIATOR PARALLEL LOOPS
(lb)

Number of Loops	1 Loop (Series)	2 Loops	3 Loops
Reactor	680	680	680
Shield	163	163	163
Liquid Metal Components	724	724	724
Pumps	156	156	156
Piping	224	224	224
Volume Accumulator Unit	140	140	140
Boiler	214	214	214
Power Conversion Unit	470	470	470
Heat Rejection Loop	398	245	256
Hoses	338	210	200
Volume Accumulator Unit	60	32	28
Pumping Penalty	0	3	28
Radiator and Structure	2,164	1,885	1,693
Fins	564	564	564
Tubes	134	81	54
Armor	442	378	274
Manifolds	251	139	94
Structure	304	304	304
Alignment	163	163	163
Deployment	55	55	55
Coolant	100	50	54
Al-93 Coating	151	151	151
Electrical	98	98	98
Transmission Line	73	73	73
Instrumentation	25	25	25
Total	4,707	4,275	4,094

As a direct consequence of this study, the reference designs, at 25, 50, and 75 kWe, of the organic Rankine cycle systems were revised to include the three-loop radiator concept. The new system performance characteristics and systems mass breakdowns are shown in Tables II-15 and II-16 respectively.

3. Stirling Cycle Systems

In conjunction with the 10- to 75-kWe space study, a study of Stirling cycle nuclear power systems was conducted at power levels of 10, 25, 50, and 75 kWe. The range of system masses is 1067 to 4036 lbm. The waste heat rejection radiator area varied from 190 to 1250 ft². The deployed system height ranged from 16.8 to 56.9 ft with the 10-kWe system requiring no deployment. A general summary of this study is shown in Table II-4. The details of this study are reported in Section II-B-3-a.

Close examination of the reactor outlet temperatures selected during the 10- to 75-kWe Stirling systems study revealed several potential problems associated with high fuel-centerline temperatures. In an investigation designed to mitigate these problems, the system mass sensitivity to reduced reactor outlet temperatures was examined. The results of this study are in Section II-B-3-b.

a. 10- to 75-kWe Systems Study

The thermal power needed to drive the Stirling cycle system is generated in the ZrH reactor. Heat is transferred to the cycle by NaK flowing in stainless-steel piping. Static head to drive the NaK is provided by a thermoelectric-powered, dc, electromagnetic pump. Heat is delivered to the cycle working fluid, helium, through a NaK-to-gas heat exchanger.

The Stirling engine is a closed cycle piston engine with cyclic recirculation of the working fluid.^(II-3) Power is produced by compression of the working gas at low temperature, heating the gas, expanding it at high temperature and as a result, cooling it again. The required heat is added to the working gas inside the engine through a heat exchanger wall. Since this wall has a high heat capacity, it is not possible to rapidly heat and cool the wall, and therefore, a second piston called the displacer is added to the machine to move the gas between two stationary chamber volumes; a hot chamber and a cold chamber (see Figures II-14 and II-15).

TABLE II-15
ORGANIC RANKINE CYCLE
SYSTEM PERFORMANCE CHARACTERISTICS*

Net Electrical Power (kWe)	10	25	50	75
Electrical Power (kWe)				
Gross	10.5	26	52	77
Transmission Line Loss	0.5	1	2	2
Net to Payload	10.0	25	50	75
Thermal Power (kWt)				
Reactor	105.7	234.1	438.8	639.7
TE Pump Modules	18.2	26.1	35	52.2
Heat Losses	1.4	1.7	3.8	4.2
Power Conversion Unit	86.1	206.3	400	583.3
Radiator	94.5	214.2	374.9	576.9
Efficiencies (%)				
System	9.5	10.7	11.4	11.7
Power Conversion Unit	12.2	12.6	13.0	13.2
Temperatures (°F)				
Reactor Outlet	800	800	800	800
Reactor ΔT	100	100	100	100
Turbine Inlet	700	700	700	700
Boiler Inlet (Dowtherm A)	570	570	570	570
Radiator Inlet	400	400	400	400
Radiator Outlet	300	300	300	300
Average Radiator	345	345	345	345
Flowrates (lb/sec)				
Primary Loop	4.15	10.51	19.4	29.1
Power Conversion Unit Loop	0.46	1.11	2.14	3.2
Radiator Loop 1	1.6	0.98	2.13	4.15
Loop 2	-	2.01	2.36	3.30
Loop 3	-	1.25	2.90	3.92
Pressure Drops (psid)				
Primary Loop	1.0	1.2	1.5	1.8
Radiator	24	60	90	105
Radiator (ft ²)				
Gross Area	564	1584	2643	4523
Net Area	538	1484	2497	4280
Mean Fin Effectiveness	0.81	0.78	0.81	0.72

*As of 9-1-76, 3 Parallel Loops

TABLE II-16
ORGANIC RANKINE CYCLE MASS BREAKDOWN*

Net Electrical Power (kWe)	10	25	50	75
Reactor	444.1	565.3	680	793.0
Shield	136.2	153.6	163	164.0
Liquid Metal Components	183.0	382.0	734	1,199.6
Pumps	46.0	74	156	306.0
Piping	55.0	131	224	355.5
Vacuum Accumulator Unit	21.0	59	140	228.4
Boiler	61.0	118	214	309.7
Power Conversion Unit	245.0	375.0	470	597.0
Heat Rejection Loop	43.7	144.0	256	621.0
Hoses	28.7	103	200	527.0
Vacuum Accumulator Unit	15.0	15	28	70.0
Pumping Penalty	0	26	28	24.0
Radiator and Structure	396.8	951.0	1,693	2,911.0
Fins	120.8	334	564	967.0
Tubes	19.9	30	54	89.0
Armor	61.3	120	274	464.0
Manifolds	17.4	39	94	222.0
Structure	82.5	203	304	556.0
Alignment	21.2	74	163	213.0
Deployment	31.0	46	55	80.0
Coolant	10.1	19	54	72.0
Al-93	32.6	86	151	248.0
Electrical	33.8	60.0	98	206.7
Transmission Line	8.8	35	73	181.7
Instrumentation and Controls	25.0	25	25	25.0
Total	1,483	2,630.0	4,094	6,493.0

* As of 9-1-76. 3 Parallel Loops

Figure II-14. Reactor - Stirling System Schematic

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Figure II-15. Typical Displacer Stirling Engine

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Since the alternating heating and cooling would inherently waste large quantities of heat, a matrix type regenerator is placed between the hot and cold sources. Heat is then stored in the regenerator as the gas moves toward the cold space (compression space) and then released as the working fluid returns from the cold space to the hot space (expansion space). The power piston moves in a phased relationship to the displacer piston, alternately compressing and expanding the gas, thereby providing output power.

Waste heat from the Stirling cycle power conversion unit enters the Dowtherm A fluid of the heat rejection loop through a gas-to-Dowtherm A heat exchanger. Both the NaK-to-gas and gas-to-Dowtherm A heat exchangers are integral components of the power conversion system.

Peripheral components required by the Stirling system, such as hoses, shielding, volume accumulators, and pumps, are similar to those listed in the Brayton system description in Section II-B-1-a.

Figures II-16 and II-17 show schematics of typical Stirling system at the 50-kWe power level. Power generation component configurations are shown as well as launch and deployed radiator configurations. Note that at 10 kWe, the radiator is small enough so that deployment is not required.

As shown graphically in Figure II-7, the efficiency of the Stirling system decreases linearly with temperature. This relation was developed as a result of a parametric analysis of Stirling system efficiency. It was also found that at other power levels, similar relationships exist. Note also that the Stirling system has the highest efficiency of any of the power conversion systems studies. This high efficiency is one of the reasons for the relatively flat system mass vs heat rejection temperature curve shown in Figure II-8. High system efficiency results in reduced overall radiator area and reactor power requirements. This makes the mass contribution of the PCS more significant. In a Stirling PCS, the heaviest component is the alternator and its mass is relatively insensitive to system efficiency. Consequently, the mass of the Stirling system decreases slowly with increasing heat rejection temperature. As with the other systems, the maximum available average heat rejection temperature of the Stirling system is restricted to 400°F. Since the theoretical minimum Stirling system weight occurs above 400°F, the optimum system was defined at 400°F.

Figure II-16. 50-kWe Stirling System
Reactor Power Conversion Unit
Assembly, Effective Radiator
Area = 116 ft²

Figure II-17. 50-kWe
Stirling System

The Stirling system performance characteristics are listed in Table II-17. The difference between the gross and net generated power is divided between transmission line losses, which are small due to the small system size, and the heat rejection loop pumping power. The indicated reactor thermal power is the sum of the power conversion system input power, the primary loop losses, and the TE pump power module input. The radiator load shown includes the PCS waste heat, primary loop losses, and TE pump power module waste heat, but excludes re-radiated solar input.

The Stirling cycle system mass breakdown is listed in Table II-18. As with the other systems, the reactor mass, which includes much structure and reflector, increases with power. The shield mass shows an increase with power up to 75 kWe where a unique combination of reactor power and relatively large separation distance result in a decrease in shield mass.

b. Reduced Peak Temperature Study

Decreasing the peak temperature of the Stirling space system (the reactor outlet temperature) would increase the system and reactor reliability and life-time due to a concurrent reduction of fuel centerline temperatures and hydrogen leakage rates. However, it was not clear what the mass penalty, if any, would be as a consequence of reducing this temperature. Therefore, a study was initiated to determine the system sensitivity to reduced reactor outlet temperature. The 10- and 50-kWe systems were selected as representative of the range of 10 to 75 kWe. The subject temperatures were reduced to 1250 and 1200°F, and detailed design configuration and mass breakdown data generated for each power level and temperature, respectively.

In order to evaluate system configuration and weight, performance characteristics were first determined. Some of the characteristics including electrical power levels, heat rejection loop temperatures, reactor ΔT , and pressure drops were assumed to be unchanged. The remainder were either calculated from first principles or determined by the study constraints.

From the information provided by the performance characteristics, the masses of the various system components were determined. The reactor and shield weight were calculated by the computer code ZIP. The details of these

TABLE II-17
STIRLING CYCLE
SYSTEM PERFORMANCE CHARACTERISTICS

Net Electrical Power (kWe)	10	25	50	75
Electrical Power Levels (kWe)				
Gross	10.5	26	52	77
Pumping	0.3	0.75	1.0	1.5
Transmission Line Losses	0.2	0.25	0.5	0.5
Net to Payload	10.0	25.0	50.5	75
Thermal Power (kWt)				
Reactor	45.3	93.8	188.6	259.2
TE Pump	8.7	12.0	17.4	23.0
Heat Losses	4.0	5.3	7.2	9.7
Power Conversion Unit	32.6	76.4	164	226.5
Radiator	33.3	82.5	135	210
Efficiencies (%)				
System	22.1	26.7	26.5	28.9
Power Conversion Unit	32.1	34.0	34.0	34.0
Temperatures (°F)				
Reactor Outlet	1300	1300	1300	1300
Reactor ΔT	100	100	100	100
Heater Inlet (NaK)	1300	1300	1300	1300
Cooler Inlet (Dowtherm A)	350	350	350	350
Cooler Outlet (Dowtherm A)	450	450	450	450
Radiator Inlet	450	450	450	450
Radiator Outlet	350	350	350	350
Average Radiator	395	395	395	395
Flow Rates (lb/sec)				
Primary Loop	2.04	4.23	8.49	11.68
Heat Rejection Loop	0.63	1.56	2.56	3.98
Pressure Drops (psid)				
Primary Loop	1.0	1.1	1.2	1.3
Heat Rejection Loop	10.0	50	75	90
Radiator (ft ²)				
Gross Area	190	554	776	1374
Effective Area	190	500	740	1248
Fin Effectiveness	0.82	0.71	0.77	0.71

TABLE II-18
STIRLING MASS BREAKDOWN
(lb)

Net Electrical Power (kWe)	10	25	50	75
Reactor	401.1	465	561.4	606
Shield	156.4	162	174.2	165
Liquid Metal Components	114	150	252	361.1
Pump	28	37	57	74.1
Piping	54	68.9	110	151.0
Volume Accumulator Unit	12	21.2	40	68.5
NaK - Gas NaK Heat Exchanger	20	22.4	44.7	67.5
Power Conversion Unit	286	440	880	1,320
Heat Rejection Loop	28	92	156	410.3
Gas - DTA Heat Exchanger DTA	10	28.4	56.7	85.0
Hoses	2.6	31.3	50.7	231.3
Volume Assumulator Unit	5.6	17	28.5	64.0
Pump	10	15	20	30.0
Radiator and Structure	104.1	369	520.5	1,091.0
Fins	37.0	118.6	165.9	293.8
Tubes	3.4	11.7	21.7	44.6
Armor	14.5	35.0	76.8	179.0
Manifolds	2.0	24.2	35.6	172.1
Structure	33.9	91.5	112.5	197.2
Alignment	0	25.0	21.2	59.9
Deployment	0	28.0	30.5	36.5
Coolant	3.3	6.4	11.4	35.7
AI-93	10.0	28.9	44.9	72.2
Electrical	<u>26.9</u>	<u>38</u>	<u>51.8</u>	<u>82.5</u>
Transmission Line	1.9	12.5	26.8	57.5
Instrumentation and Control	25.0	25.0	25.0	25.0
Total	1,067	1,716	2,596	4,036

calculations are reported in Section II-B-5-a. The remainder of the component masses were either calculated directly from the performance characteristics or components requirements, or the weights listed in the reference design were modified by appropriate normalized factors.

The system performance characteristics for the 50-kWe Stirling space system are tabulated for the three reactor outlet temperatures in Table II-19. The detailed mass breakdown of the system is shown in Table II-20. Figure II-18 shows, graphically, the total system as well as the reactor plus shield mass as a function of reactor outlet temperature. The system mass is essentially insensitive to changes in reactor outlet temperature over the range of temperatures studied.

Figure II-18. Effect of Reactor Outlet Temperature on Stirling - 50-kWe System Mass

TABLE II-19
SYSTEM PERFORMANCE CHARACTERISTICS
50-kWe STIRLING SPACE SYSTEM

Reactor Outlet NaK Temperature (°F)	1200	1250	1300
Electrical Power Levels (kWe)			
Gross	52.0	52.0	52.0
Pumping	1.0	1.0	1.0
Transmission Line Losses	0.5	0.5	0.5
Net to Payload	50.5	50.5	50.5
Thermal Power (kWt)			
Reactor	187.7	181.6	176.8
TE Pump	19.0	18.0	17.8
Heat Losses	7.7	7.4	7.2
Power Conversion Unit	161.0	156.4	151.8
Radiator	131.9	125.9	121.2
Efficiencies (%)			
System	26.64	27.53	28.28
Power Conversion Unit	32.29	33.30	34.26
Temperatures (°F)			
Reactor Outlet	1200	1250	1300
Reactor Δt	100	100	100
NaK Heater Inlet	1200	1250	1300
Dowtherm A Cooler Inlet	350	350	350
Dowtherm A Cooler Outlet	450	450	450
Radiator Outlet	350	350	350
Average Radiator	395	395	395
Flowrates (lb/sec)			
Primary Loop	8.46	8.18	7.97
Heat Rejection Loop	2.50	2.39	2.30
Pressure Drops (psid)			
Primary Loop	1.2	1.2	1.2
Heat Rejection Loop	75	75	75
Radiator Area (ft ²)			
Gross Area	759	726	701
Net Area	723	690	665
Fin Effectiveness (%)	77	77	77
Reactor Dimensions (in.)			
Fuel Element Length	16.0	18.0	18.0
Fuel Element Diameter	0.466	0.369	0.380
Number of Fuel Elements	151	187	199
Peak Fuel Temperature (°F)	1333	1353	1400

TABLE II-20
50-kWe STIRLING SYSTEM PEAK TEMPERATURE SENSITIVITY
MASS BREAKDOWN
(lb)

Reactor Outlet NaK Temperature (°F)		1200	1250	1300
Reactor	CALC*	559	615	654
Shield	CALC	168	169	175
Liquid Metal Components	CALC	294	280	271
Pump	CALC	78.2	76.8	76
Piping (wet)	CALC	117.5	113.1	110.2
Volume Accumulator Unit	CALC	50	44	40
NaK-Gas Heat Exchanger NaK	NP	48.0	46.4	44.7
Power Conversion		916	896	880
Heat Rejection Loop		153	147	142
Gas-DTA Heat Exchanger DTA	NP	55.4	52.9	50.9
Hoses	(NF)x(NH)	49.5	47.4	45.5
Volume Accumulator Unit	NF	27.8	26.6	25.6
Pump	-	20	20	20
Radiator and Structure		509	486	469
Fins	NA	162.1	154.8	149.0
Tubes	NF	21.2	20.3	19.5
Armor	NF	75.0	71.7	69.0
Manifolds	NF	34.8	33.3	32.0
Structure	NA	109.9	105.0	101.0
Alignment	NA	20.8	20.0	19.5
Deployment	NH	30.0	28.8	28.0
Coolant	NF	11.1	10.6	10.2
Al-93	NA	43.9	41.9	40.3
Electrical		51	50	50
Transmission Line	NH	26.3	25.3	24.6
Instrumentation and Control	-	25	25	25
Total		2,650	2,643	2,641

*CALC = Recalculated
NF = Normalized Flow
NA = Normalized Area
NH = Normalized Height
NP = Normalized Power

The variance of the reactor outlet temperature between 1200 and 1300°F in the 10- and 50-kWe Stirling space systems did not cause significant changes in the configuration or masses of these systems within the limits of accuracy of this study. As a result, the reference design reactor outlet temperature was lowered to 1250°F. New performance characteristics and system mass breakdowns were calculated at all power levels and are shown in Tables II-21 and II-22, respectively.

4. Thermoelectric Systems

In the thermoelectric (TE) power systems, the NaK heated by the reactor is pumped by a dc, dual-throat, EM pump through stainless-steel piping to an array of tubular thermoelectric modules. These modules generate the system electric power while separate TE modules of a different design power the NaK pump. The main power modules are grouped in circular arrays in the conical radiator section. Figure II-19 shows a schematic diagram of a TE system.

75-D4-103-3

Figure II-19. Reactor - Thermoelectric System Schematic

TABLE II-21
STIRLING CYCLE SYSTEM PERFORMANCE CHARACTERISTICS

Net Electrical Power (kWe)	10	25	50	75
Electrical Power Levels (kWe)				
Gross	10.5	26	52.0	77
Pumping	0.3	0.75	1.0	1.5
Transmission Line Losses	0.2	0.25	0.5	0.5
Net to Payload	10.0	25	50.5	75
Thermal Power (kWt)				
Reactor	43.9	96.4	181.6	264.4
TE Pump	9.7	13.1	18.0	24.2
Heat Losses	4.0	5.3	7.4	9.0
Power Conversion Unit	30.2	78.1	156.4	231.3
Radiator	31.4	67.8	125.9	182.9
Efficiencies (%)				
System	22.8	25.9	27.5	28.4
Power Conversion Unit	34.80	33.3	33.3	33.3
Temperatures (°F)				
Reactor Outlet	1250	1250	1250	1250
Reactor ΔT	100	100	100	100
Heater Inlet (NaK)	1250	1250	1250	1250
Cooler Inlet (Dowtherm A)	350	350	350	350
Cooler Outlet (Dowtherm A)	450	450	450	450
Radiator Inlet	450	450	450	450
Radiator Outlet	350	350	350	350
Average Radiator	395	395	395	395
Flowrates (lb/sec)				
Primary Loop	1.99	4.35	8.18	11.90
Heat Rejection Loop	0.60	1.29	2.39	3.47
Pressure Drops (psid)				
Primary Loop	1.0	1.1	1.2	1.3
Heat Rejection Loop	10.0	50	75	90.0
Radiator (ft ²)				
Gross Area	179	465	726	1213
Effective Area	179	411	690	1087
Fin Effectiveness	0.82	0.71	0.77	0.71

TABLE II-22
STIRLING MASS BREAKDOWN
(lb)

Net Electrical Power (kWe)	10	25	50	75
Reactor	412	493	615	703
Shield	170	156	169	196
Liquid Metal Components	100	158	280	415
Pump	26	44	77	102
Piping	45	69	113	174
Volume Accumulator	11	23	44	70
NaK - Gas NaK Heat Exchanger	19	23	46	69
Power Conversion Unit	288	448	896	1344
Heat Rejection Loop	27	75	147	340
Gas - DTA Heat Exchanger DTA	9	23	53	74
Hoses	2	23	47	180
Volume Accumulator Unit	5	14	27	56
Pump	10	15	20	30
Radiator and Structure	98	306	486	952
Fins	35	98	155	256
Tubes	3	10	20	39
Armor	14	29	72	156
Manifolds	2	20	33	150
Structure	32	75	105	172
Alignment	0	21	20	52
Deployment	0	25	29	33
Coolant	3	5	11	31
Al-93 Coating	9	24	42	63
Electrical	<u>27</u>	<u>35</u>	<u>50</u>	<u>76</u>
Transmission Line	2	10	25	51
Instrumentation and Control	25	25	25	25
Total	1122	1671	2643	4026

Figure II-20. 25-kWe Thermoelectric System Reactor Power Conversion Unit Assembly, Effective Radiator Area = 151.2 ft²

Figure II-21. 25-kWe Thermoelectric System, 1612 ft² Radiator Area

The power modules are cooled by a flow of NaK that is pumped by the other throat of the dual-throat EM pump. This NaK transfers its heat to the Dowtherm A in the radiator loop by means of a heat exchanger. The radiator, as in the other three power conversion systems, is deployable, consisting of a conical section at the top and several cylindrical sections below.

There is a radiation shield located just below the reactor. Volume accumulators are required for the primary and heat rejection NaK loops and for the radiator loop.

Only 10- and 25-kwe thermoelectric power systems were studied in depth. At the larger power levels, thermoelectric systems are less competitive from the standpoint of mass and size. Figure II-20 shows the conical radiator section of the 25-kwe thermoelectric system including the location of the major components. Figure II-21 shows the overall configuration in both the launch mode and the deployed mode. The thermoelectric systems have an end-of-life (EOL) reactor outlet temperature of 1200°F.

Thermoelectric systems are less efficient than the other candidate systems. One consequence of this is the greater reactor thermal power. Another is greater coolant flow rates, larger pipes, and larger pumps. The thermal losses are somewhat greater because the NaK pipes are larger in diameter (see Table II-23).

The reactor outlet temperature is taken to be 1200°F at end-of-life instead of 1300°F as in both the Brayton and the Stirling. There is very little performance gain when TE modules are operated hotter than about 1200°F, while degradation is known to be greater at the higher temperatures. To allow for TE module degradation, the reactor outlet temperature at beginning-of-life (BOL) is taken to be 1150°F. During the seven-year mission, the TE module power and efficiency will degrade somewhat. To maintain constant electrical power output, the reactor outlet temperature will be gradually increased until 1200°F is reached in about 7 years. The radiator temperature will also increase, since more heat must be rejected.

TE systems are inherently heavy. The 10-kwe TE system has about the same mass as the 25-kwe Brayton and the 25-kwe TE is just slightly lighter than the 50-kwe Brayton. Most of these differences are due to the mass of the radiator and liquid metal components (see Table II-24).

TABLE II-23
THERMOELECTRIC SYSTEM PERFORMANCE CHARACTERISTICS

Net Electrical Power (kWe)	10	25
Electrical Power (kWe)		
Gross	10.5	26.25
Pumping	0.25	0.60
Transmission Line Loss	0.25	0.60
Net to Payload	10.0	25.05
Thermal Power (kWt)		
Reactor	186.5	442
TE Pump Modules	14.7	26.5
Heat Losses	7.0	9.0
TE Power Modules	165.0	406
Radiator	175.4	410
Efficiency (%)		
System	5.36	5.66
TE Modules	6.37	6.47
Beginning-of-Life Temperatures (°F)		
Reactor Outlet	1150	1150
Reactor ΔT	115	200
Average Hot Cladding	1081	1044
Average Cold Cladding	514	488
HR Heat Exchanger NaK Inlet	584	558
HR Heat Exchanger NaK Outlet	434	408
HR Heat Exchanger Organic Inlet	414	428
HR Heat Exchanger Organic Outlet	564	508
Flow Rates (lb/sec)		
Primary NaK	7.32	10.4
HR NaK	4.47	12.7
HR Organic	2.07	9.34
Pressure Drops (psi)		
Primary NaK	1.25	1.50
HR NaK	1.25	1.00
HR Organic	18.0	12.0
Radiator (ft ²)		
Gross Area	660	1699
Effective Area	625	1612
Fin Effectiveness	0.80	0.79

TABLE II - 24
THERMOELECTRIC MASS BREAKDOWN
(lb)

Net Electrical Power (kWe)	10	25
Reactor	543	732
Shield	180	219
Liquid Metal Components	403	845
Pumps	104	224
Piping	133	206
Volume Accumulator Units	74	210
NaK - Organic Heat Exchanger	92	205
Power TE Modules	384	960
HRL Components	110	270
Pump	15	20
Piping	38	172
Volume Accumulator Units	57	78
Radiator	639	2,078
Fins	187	364
Tubes	79	304
Armor	56	691
AI-93	48	98
Structure	119	233
Manifolds	88	220
Alignment Hardware	30	93
Deployment Mechanism	34	75
Electrical	<u>62</u>	<u>97</u>
Transmission Line	37	72
Instrumentation and Control	25	25
Total	2,321	5,201

TABLE II-25
10 and 50-kWe Brayton Reactor

Reactor Outlet Temperature (°F)	1200	1250	1300
10 kWe			
Thermal Power (kW)	65.9	61.3	57.2
Peak-Average Temperature (°F)	1307	1352	1399
Number of Fuel Elements	55	55	55
Active Fuel Length (in.)	14	15	16
Lattice Pitch (in.)	1.2746	1.236	1.22
Fuel Diameter (in.)	1.185	1.1448	1.1265
Linear Power Density (kW/ft)	1.027	0.892	0.780
Reactor Mass (lb)	422	430	447
Separation Distance (ft)	31	29	28
Reactor and Shield Mass (lb)	556	563	581
50 kWe			
Thermal Power (kW)	226	215	209*
Peak-Average Temperature (°F)	1345	1350	1353
Number of Elements	187	253	349
Active Fuel Length (in.)	15	17	17
Lattice Pitch (in.)	0.8356	0.7165	0.6430
Fuel Diameter (in.)	0.7455	0.6275	0.5598
Linear Power Density (kW/ft)	0.967	0.600	0.423
Reactor Mass (lb)	586	653	725
Separation Distance (ft)	94	88	85
Reactor and Shield Mass (lb)	707	779	866

*Reactor outlet temperature = 1275° F

5. Components

In support of the 10- to 75-kWe systems study and other subsequent tasks, component engineering studies were performed. Various reactor and radiator analyses were accomplished and are summarized in Sections II-B-5-a and II-B-5-b, respectively.

a. Reactor

In conjunction with the reduced peak temperature studies previously reported, parametric studies were performed on the 10- and 50-kWe Brayton and Stirling reactor systems to determine the minimum mass reactor for each system at outlet temperatures of 1200, 1250, and 1300°F. Constraints were placed on the design: (1) that maximum peak-average fuel temperature be less than 1350°F, except for the 1300°F outlet design which could be no higher than 1400°F, and (2) that no beta-phase fuel occur during the 7-year design life. These restrictions were included to ensure against excessive hydrogen leakage and fuel swelling. Two important design changes were included in this study that differ from earlier studies of similar systems. Both changes occurred in the hydrogen leakage calculation and are the following:

- 1) The value of the hydrogen permeation coefficient through the glass barrier was increased to be more in accord with the present state of the art.
- 2) The glass defect constant was changed from 0.0015 to a more conservative value of 0.002, where experimental values ranged from 0.001 to 0.002.

These design changes resulted in more conservative reactor masses due to the increased initial reactivity requirements. The characteristics of each reactor design are summarized in Table II-25 and II-26.

A trade study was also performed on the 10-, 25-, 50-, and 75-kWe Brayton systems to determine the maximum permissible reactor outlet temperature as a function of linear power density, over a range of densities from 0.45 kW/ft to 1.15 kW/ft. Two constraints restricted the outlet temperature by requiring: (1) that maximum allowable peak-average fuel temperature be less than 1350°F, and (2) that no beta-phase fuel occur during the reactor life. As shown in

TABLE II-26
10 AND 50 kWe STIRLING REACTOR

	1200	1250	1300
10 kWe			
Thermal Power (kW)	45	43.9	43
Peak-Average Temperature (°F)	1314	1327	1382
Number of Fuel Elements	31	55	55
Active Fuel Length (in.)	14	14	14
Lattice Pitch (in.)	1.600	1.501	1.277
Fuel Diameter (in.)	1.509	1.408	1.185
Linear Power Density (kW/ft)	1.244	1.062	0.670
Reactor Mass (lb)	396	412	423
Separation Distance (ft)	16.5	16.1	15.7
Reactor and Shield Mass (lb)	561	583	598
50 kWe			
Thermal Power (kW)	188	182	177
Peak-Average Temperature (°F)	1333	1353	1400
Number of Fuel Elements	151	187	199
Active Fuel Length (in.)	16	18	18
Lattice Pitch (in.)	0.865	0.769	0.780
Fuel Diameter (in.)	0.775	0.680	0.689
Linear Power Density (kW/ft)	0.932	0.647	0.592
Reactor Mass (lb)	559	615	654
Separation Distance (ft)	43	43	43
Reactor and Shield Mass (lb)	727	784	830

Figure II-22, the maximum reactor outlet temperature is a negatively sloped linear function of the linear power density. In general, since the power conversion system efficiency increases with higher reactor outlet temperatures, a high-outlet-temperature, low-power-density reactor would be desirable. However, as indicated in Figure II-23, reactor mass increases with lower power densities. The effect is that an optimal reactor exists which represents the best choice between lower reactor mass penalties at higher linear power densities and higher PCS efficiency at higher outlet temperatures.

These calculations were performed using ZIP,^(II-4) a time-share program which calculates zirconium hydride reactor parameters quickly and at low cost. For nuclear calculations, ZIP uses a modified two-group model with constants fitted to a large number of APC2^(II-5) cases, and duplicates APC2 reactivity results within about 15%. For thermal-hydraulic calculations, ZIP is correlated with numerous TEMPRO^(II-6) peak-average fuel temperatures to within 5%. ZIP also calculates hydrogen loss, shadow shield thicknesses, and reactor and shield masses. In addition, at the user's option, ZIP will (1) search the lattice pitch to find a specified end-of-life reactivity, (2) calculate fuel swelling using the Bonser-Swenson correlation, (3) search for the beginning-of-life, fuel-to-cladding gap which yields the required end-of-life margin, and (4) continuously search the zirconium hydride phase diagram for occurrences of beta-phase fuel.

During FY 1976, a document^(II-7) was completed which reported results of two 50-kWe ZrH reactor designs, the Brayton and Rankine systems. These systems were analyzed in terms of several key parameters to determine if they could successfully complete the 7-year operating life. The key design parameters include hydrogen leakage rates, fuel swelling, fuel burnup, fission product inventory buildup, and control segment operation. The ZIP timeshare code was the primary computational tool.

An extensive data base, consisting of both fuel element experimentation and reactor operating experience, has provided support for the present design values of each system. The main sources of data are hydrogen permeation testing, barrier performance testing, SNAP fuel irradiation experiments, and S8ER and S8DR post-operational analysis.

Figure II-22. Maximum Reactor Outlet Temperatures

Figure II-23. Reactor Mass vs Reactor Outlet Temperature

Each parameter was evaluated for the maximum available reactivity life in its design value, as well as the fuel swelling limit and fuel phase limit. For example, the hydrogen permeation coefficients could be increased by approximately 30% in the Brayton system before zero reactivity was reached at a 7-year life. The difference between this value and the design value represented a reactivity life design margin for that parameter. It was predicted that the Brayton design had enough additional reactivity in its design parameters to operate for 8.1 years, while the organic Rankine design was estimated at 7.8 years. The design summary for each system is given in Table II-27.

TABLE II-27
REACTOR DESIGN SUMMARY

Parameter	Brayton	Organic Rankine
Thermal Power (kW)	215	409
Number of Fuel Elements	253	283
Active Fuel Length (in.)	17	16
Lattice Pitch (in.)	0.7165	0.7220
Gap Thickness (mil)	0.007	0.0063
Fuel Diameter (in.)	0.6275	0.6344
Active Core Diameter (in.)	11.9669	12.7542
Active Core Volume (L)	31.333	33.498
Core Vessel O.D. (in.)	12.585	13.455
Reflector Midplane Thickness (in.)	2.46	2.39
Segment Stroke (in.)	7.5	6.0
Fissile Mass (kg U ²³⁵)	12.804	13.777
Beginning-of-Life H/Zr	1.662	1.527
End-of-Life H/Zr	1.537	1.526
Peak-Average Fuel Temperatures (°F)	1350	964
Separation Distance (ft)	88	101.6
Reactor Mass (lb)	653	683
Shield Mass (lb)	128	150
Reactor plus Shield Mass (lb)	781	833

b. Radiator

(1) Structure Description

The deployable radiator typically consists of the frustrum of a cone housing the power conversion system plus a number of cylinders of equal length as shown in Figure II-4 . Waste cycle heat from the PCS is transported by the coolant to the radiator for radiation to space.

Cylindrical stiffener rings are mounted at ~ 2 -ft intervals and at the top and bottom of each radiator section. The radiator sections are provided with ring manifolds at each end for distribution and collection of the Dowtherm A coolant which flows through parallel, single-pass, straight tubes mounted on the inner radiator surface. The hot coolant feeds the radiator at the top of the conical frustrum, passes successively to the cylinders, and returns to the PCS from the last cylinder outlet. Flexible tubing is used to connect the flow of coolant between radiator sections and the PCS.

The radiator is constructed of Al 3003 alloy. A spectral coating (AI-93) is applied to the outer surface of the radiator.

In conjunction with the 10- to 75-kWe system studies, a number of radiator configurations were studied and analyzed to achieve minimum system masses subject to the constraints of thermal load, tube pressure drop, system reliability, and structural strength (Table II-28).

(2) Thermal Analysis

The object of the thermal analysis was to determine the optimum configuration capable of handling the heat load. This was accomplished by use of a radiator analysis code, RADAN. During the past year the digital computer program, RADAN, which operates on an IBM 360, was modified to handle the large surface area and heat rejection requirements by converting it from a limited fixed geometry (a conical frustrum and one cylinder) with fin sizing optimization code, to a more flexible geometry consisting of the conical frustrum and up to ten cylindrical sections with a coolant tube multipass option. The fin optimization routine was replaced by a geometry input option. Thus, for fixed input conditions of coolant heat rejection rate, flow rate, inlet temperature, and radiator geometry, case runs are made varying tube spacing, tube ID, and fin thickness.

Net Electrical Power (kWe)	Brayton				Rankine				Stirling				Thermoelectric	
	10	25	50	75	10	25	50	75	10	25	50	75	10	25
Radiator Properties														
Coolant Inlet Temperature (°F)	393	325	391	324	400	400	400	400	450	450	450	450	589	508
Coolant Flow Rate (lb/hr)	1628	5660	7380	16150	5724	15200	26600	40960	2273	5632	9215	14340	5616	33500
Coolant Temperature Drop (°F)	195.3	133	200	133	100	100	100	100	100	100	100	100	200	79
Heat Rejection (kWt)	44.1	97.6	198	281.8	94.5	214.2	374.9	576.9	33.3	82.5	135	210	175.4	410
Average Fin Root Temperature (°F)	286	252	286	252	345	343	346	345	389	391	395	392	481	461
Fin Effectiveness	0.766	0.799	0.746	0.799	0.808	0.775	0.798	0.719	0.82	0.71	0.763	0.706	0.8	0.792
Pressure Drop (psi)	12.2	27	80	38.7	24.2	50	75	90	10	50	75	90	14.4	6.6
Pumping Power (kWe)	0.033	0.245	0.973	1.01	0.236	1.29	3.39	6.26	0.05	0.492	1.21	2.25	0.149	0.403
Radiator Geometry														
Tube ID (in.)	0.0938	0.125	0.125	0.175	0.125	0.1875	0.1875	0.25	0.125	0.125	0.125	0.1875	0.125	0.3125
Tube Spacing (in.)	4.5	3.5	4.5	3.5	3.35	4.0	3.5	5.0	3.05	5.0	3.9	5.0	3.0	3.5
Radiator Effective Area (ft ²)	435	1093	2000	3119	538	1484	2497	4280	190	500	740	1248	625	1612
Armor Thickness (in.)	0.117	0.188	0.215	0.287	0.1548	0.225	0.277	0.315	0.109	0.133	0.165	0.201	0.175	0.283
Cone														
Small Diameter (in.)	28	30.2	34	37.7	27	30.3	36	42	24	30	30	38	34	38.5
Large Diameter (in.)	55	59.2	66	72.8	45	51.7	63	72	56	58	63	72	54	65.3
Height (in.)	90	96.7	108	119.3	65	76.3	95	106	105	99	112	131	65.4	90.4
Number of Tubes	29	40	35	49	34	32	44	37	41	28	37	35	46	47
Cylinders														
Total Number	2	3	5	6	2	4	6	7	1	2	2	4	2	4
Length	106.7	181.2	169	197	177	199	174	222	65.8	121.6	176.8	122.4	178.7	180
Diameter (in.), Number of tubes ()	67 (47)	71.2 (65)	78 (54)	84.8 (77)	57 (54)	63.7 (50)	75 (68)	86.3 (54)	56 (58)	70 (44)	75 (60)	84 (53)	66 (69)	77.3 (70)
	79 (55)	83.2 (75)	90 (63)	96.8 (88)	69 (65)	75.7 (59)	87 (78)	98.3 (62)		82 (52)	87 (70)	96 (60)	78 (82)	89.3 (80)
		95.2 (86)	102 (71)	108.8 (99)		87.7 (69)	99 (89)	110.3 (69)				108 (68)		101.3 (91)
			114 (80)	120.8 (110)		99.7 (78)	111 (100)	122.3 (77)				120 (75)		113.3 (102)
			126 (88)	132.8 (120)			123 (110)	134.3 (84)						
				144.8 (131)			135 (121)	146.3 (92)						
								158.3 (99)						
Radiator Mass (lb)														
Fin	116	246	453	702	120.8	334.1	564	967.3	37	118.6	165.9	293.8	187	364
Armor	23	147	238	832	61.3	284.6	442	945.9	14.5	35.0	76.8	179.0	56	691
Tubes	8	52	58	94	19.9	69.1	134	170	3.4	11.7	21.7	44.6	53	136
AI-93	27	67	123	190	32.6	85.9	151	247.6	10.0	28.9	44.9	72.2	48	98
Rings	76	165	272	390	82.5	202.6	304	556.1	33.9	91.5	112.5	197.2	119	233
Coolant	4	28	28	64	10.1	51.9	100	214.3	3.3	6.4	11.3	35.7	26	168
Total Mass	254	705	1172	2272	327.2	1028.2	1695	3101	102.1	292.1	433.1	822.5	489	1690

Table II-28. Computer Analysis Results

BLANK

The output of the program provided tube pressure drop, film temperature drop, coolant entrance-exit ΔT , fin surface effectiveness, heat rejection rate, and radiator weights. The latter included the sum of the masses of fin, armor, tubing, spectral coating, ring stiffeners, and coolant in the tubes.

From the tabulated data and cross plots of heat rejection vs radiator mass with fin thickness, tube ID, and tube spacing as parameters, it was possible to vary the input data in subsequent computer runs to achieve the desired heat rejection rate, pumping power, and minimum mass.

The program, described in Reference II-1, operates by dividing the entire radiator into a maximum of 25 segments, or nodes, in series. Starting with the first node, an estimate is made of its outlet temperature using the inlet flow conditions. The fluid properties evaluated at the average temperature are then used to compute the film heat transfer coefficient, film temperature drop, coolant pressure drop, heat transfer rate to the fin root, and the radiator heat rejection rate based on a computed radiator fin effectiveness. An iterative technique reduces the difference between the heat transferred by the coolant and the heat radiated by the fin to a prescribed error by adjusting the node exit temperature. The nodes are treated sequentially, and the total tube pressure drop and heat rejection rates are then summed for the entire radiator.

A detailed analysis was made of the tube fin temperature distribution and heat transfer rates in a tube fin cross section. The program DEAP was used to verify the magnitude of temperature drop between the tube centerline and the fin root.^(II-8) The results showed that the RADAN-computed liquid film temperature drop should be increased by a factor of 1.3 to account for the metal resistance and tube geometry. RADAN was adjusted accordingly.

(3) Structural Analysis

In addition to providing a heat transfer surface, the radiator must also serve as a load bearing member; and, because the cone supports the reactor, shield, and PCS, its structural requirements are greater than that of the cylinders. During the past year, progress was made in determining the preferred coolant tube geometry when used with a slender, thin-walled cone and cylinders in order to be capable of withstanding the load conditions imposed on the radiator

TABLE II-29
LAUNCH ACCELERATIONS
(g's)

Condition	Space Shuttle (Liftoff and Ascent)				Intermediate Upper Stage (Orbit Insertion)			
	Axial*		Transverse		Axial		Transverse	
	Limit	Ultimate §	Limit	Ultimate	Limit	Ultimate	Limit	Ultimate
Liftoff	-2.9	-3.625**	1.80†	2.25†	-5.5	-6.875	0.69†	0.86†
Maximum Q	-2.0	-2.5	0.78†	0.975†				
Boost	-0.3	-4.125	0.77†	0.963†				

*Parallel to the structural centerline.

§Ultimate loads are 1.25 times the limit. Limit loads represent maximum anticipated loads.

†RMS two directions.

**Minus sign indicates compressive loads are generated in structure.

during shuttle launch and orbit injections. Maximum accelerations are given in Table II-29 for a typical shuttle mission.^(II-9) The induced loads and structural compressive strength at key locations were determined as outlined below.

The system is assumed to be supported during launch and orbit injection as shown in Figure II-24. In the shuttle, the cylinders and cone are attached at the large diameter of the cone and the cone is laterally supported at the reactor. The suspension on the intermediate upper stage (IUS) is with both the cone and the radiator cylinders cantilevered from the spacecraft.

Figure II-25 shows the typical loads on the conical section resulting from the analysis of a selected configuration with the following dimensions and weights:

Reactor and shield	= 1000 lb
Length	= 204 in.
Surface specific weight	= 1.25 lb/ft ²
Large diameter	= 60 in.
Small diameter	= 24 in.

The curves show that the compressive load imposed by the shuttle in lb/in. of cone circumference decreases to a minimum at a distance of 150 in. from the top of the reactor and then increases. The loads generated by the IUS tend to increase with axial distance from the top of the reactor. Although the highest load generated is shown near the top of the reactor, the structural load-carrying capability at this location is considerably higher than at the base due to the tubes being spaced more closely at the top of the cone than at the bottom. A detailed analysis was made for each PCS at the 25- and 75-kWe power levels. The results (Table II-30) showed that the compressive loads developed in the cone were greater than in the cylinder, and that the 75-kWe Rankine system developed the largest compressive load (168 lb/in.).

Equations and parametric curves were developed for computing a tube-armor-fin radiator section compressive strength for each of the power conversion system geometries. The cross section analyzed is shown in Figure II-26.

The D-shaped tube structure was selected because it has a greater structural efficiency than that of a round tube-armor-fin combination.

Figure II-24. Structural Configuration

Figure II-25. Radiator Launch Loads Conical Section

TABLE II-30
RADIATOR LOADS
MAXIMUM COMPRESSION AT MOUNTING PLANE INTERFACE
(lb/in)

System	Shuttle Mounted				IUS Mounted			
	Limit Load		Ultimate Load		Limit Load		Ultimate Load	
	Cone	Cylinder	Cone	Cylinder	Cone	Cylinder	Cone	Cylinder
Brayton								
25 kWe	46	8	54	11	70	8	88	10
75 kWe	74	10	92	12	108	10	135	12
Organic Rankine								
25 kWe	52	9	65	12	81	9	102	11
75 kWe	98	15	123	19	134	14	168	18
Stirling								
25 kWe	41	5	51	6	65	6	81	7
75 kWe	60	12	75	15	83	13	104	16
Thermoelectric								
25 kWe	77	10	96	12	96	11	120	14

NOTES: Ultimate load = 1.25 times limit load.
Mounting assumptions are same as shown in Figure II-24.

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Figure II-26. Radiator Cross Section

The basic equations used are:

Total Panel Strength:

$$P_{cr} = \frac{(P_{ta} + P_p)}{b} \quad \dots (1)$$

where

P_{cr} = Compressive strength of the radiator (lb/in.)

P_{ta} = Compressive strength of the tube and armor (lb)

P_p = Compressive strength of the panel (lb)

b = Panel width (in.).

The Tube and Armor Strength (Long Column):

$$P_{ta} = \pi^2 EA (\rho^2 / L^2) \quad \dots (2)$$

where

P_{ta} = Tube and armor strength

E = Modulus of elasticity

A = Cross sectional area (in.²)

ρ = Radius of gyration (in.)

L = 24 in. (ring spacing).

Panel Buckling:

$$P_p = \frac{K_L T_f b \pi^2 E}{12 (1 - \mu^2)} \left(\frac{T_f}{b} \right)^2 \quad \dots (3)$$

where

P_p = Panel compressive strength

K_L = Panel buckling coefficient based on geometry

b = Panel width (in.)

T_f = Panel or fin thickness (in.)

μ = Poisson's ratio

The parametric input data are given in Table II-31.

Typical examples of the working curves are given in Figures II-27 and II-28. For a given equivalent diameter, the first plots D-tube width vs column strength for various armor thicknesses, and the second plots panel width vs panel strength for a range of panel (fin) thicknesses.

These curves are then used to check on the adequacy of the structural design based on the optimum geometry (tube size, spacing, fin thickness, armor thickness) resulting from the thermal analysis. The values of P_{ta} and P_p are summed in Equation 1, and the resulting compressive strength, P_{cr} was compared with the flight profile induced loads of Table II-30.

An analysis of the 75-kWe Organic Rankine and 25-kWe Stirling systems resulted in the former having a cone $P_{cr} = 77.5$ lb/in. vs a required 168 lb/in.,

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Figure II-27. D-Tube Width vs Radiator Compressive Strength and Armor Thickness

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Figure II-28. Curved Panel Strength (3003-TO Aluminum, 5000 psi Maximum)

TABLE II-31
PARAMETRIC INPUT DATA

Parameter	Identification	Values Used in This Study
Flow Area (diameter of equivalent circle) (in.)	D_e	$0.0625 < D < 0.4375$
Armor (in.)	T_a	$0.10 < T_a < 0.35$
Width of D-tube (in.)	W	$0.10 < W < 0.45$
Tube Wall Thickness (in.)	T_t	0.020
Ring Spacing (in.)	L	24
Modulus of Elasticity (psi)	E	10×10^6
Cutoff Strength (psi)	SIGMAX	5000
Fin Width (in.)	b	$1 < b < 8.0$
Fin Thickness (in.)	T_f	$0.015 < T_f < 0.045$

while the Stirling had a cone $P_{cr} = 17.9 \text{ lb/in.}$ vs 81 lb/in. , thus pointing up a need for changing the structure to increase the compressive strength while simultaneously satisfying the requirements for heat transfer and minimum mass.

(4) Deployment Analysis

A number of mechanical features of the telescoping, deployable, organic-cooled, heat rejection radiators were studied subsequent to the 10- to 75-kWe system studies. The 50-kWe Brayton power system which has one conical section and five cylindrical sections was used as the basis of additional design activity. Figure II-29 shows this power system with the radiator in the launch configuration.

The reference radiator is made of 0.015-in. aluminum alloy fins with 0.125-in ID tubes spaced about 4.0 in. apart. The radiator sections are supported by Z-shaped stiffener rings spaced axially about every 24 in. A toroidal manifold is at each end of each radiator section. Three alignment shafts and ball bushing assemblies are spaced 120° apart around each cylindrical section. These mechanisms guide the motion of the sections as the radiator is deployed. The mass of the reference radiator is 1612 lbm.

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Figure II-29. 50-kWe Brayton Power System with the Radiator in Launch Configuration

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(a) Deployment Mechanisms

Preliminary studies indicated that the Astro Research Corporation Bi-Stem mechanism was capable of deploying the system. It is a lightweight, twin-spool, cassette actuator with a stored boom. The package size is quite small, 12 in. by 12 in. by 18 in., and its mass is about 60 lb for the 50-kWe system.

Astro Masts were another candidate although somewhat larger and heavier. A comparable 50-kWe system retracted unit is 15 in. in diameter by 4 ft high, weighing 110 lb.

Rack and pinion deployment mechanisms were also briefly examined and were found to be feasible.

A bellows deployment system was studied during the last quarter. Its advantages are that it could serve two functions: (1) deploy the nested cylinders, and (2) provide feed and return lines for the coolant manifolds, thereby eliminating the flexible interconnect tubing.

One design approach uses two sets of bellows for coolant inlet and outlet systems, respectively, each fixed at 180° about the cylinder. The bellows are collapsed initially in the nested position and are pressurized to a few psia with the Dowtherm A coolant for deployment of the cylinders. The bellows expand the entire length of the cylinder (from 33 in. nested to 15 ft, 5 in. deployed). After deployment, the liquid system is fully pressurized for its cooling function. The liquid inlet system uses three passes per cylinder: the first is through the bellows from top to bottom of the cylinder; the second is via a fixed single return line where it connects to the top distribution manifold; and the third pass is down through the multiple coolant tubes whose outlets are connected to the bottom collector manifold which in turn feeds the inlet bellows of the next cylinder. The coolant return system (180° opposite) is connected so as to make a single pass through the bellows from bottom to top of each cylinder. The top of each bellows is connected to the bottom of the bellows of the next higher cylinder.

A nonoptimized system which used the properties of Table II-32 shows a structure weight of 1463 lb which is comparable to bi-stem actuator deployment systems.

TABLE II-32
HYDROFORM BELLOWS

Metal	Inconel 718
Thickness (in.)	0.005
Inside Diameter (in.)	1.0
Outside Diameter (in.)	1.48
Nested Length (in.)	33
Expanded Length (ft, in.)	15, 5
Convolutions	600
Weight/bellows, dry (lb)	1.75

Six candidate cases have been evaluated using either a bellows deployment mechanism or a bi-stem actuator but with inlet and return lines having either bellows, armored Viton hose, or flexible metal hose.

Table II-33 shows that the system weights were reasonably close to each other (within 60 lb). Other design factors such as reliability, meteoroid protection, and complexity will influence the final system selection.

(b) Alignment Mechanisms

Along with the deployment mechanism, the alignment hardware was examined. The preliminary layout used a track and wheel system. It consisted of attaching six sets of T-shaped tracks in line to the inside diameter of an outer cylinder and a similar T-shape attached to the outside diameter of an inner cylinder at 60° spacing. One wheel would be attached to the top of the outside cylinder and the other wheel to the bottom of the inside cylinder. As the cylinder deployed, the bottom wheel would come to a stop adjacent to the top wheel. It was found that for this method, a total of 247.5 lb was required for a 50-kWe Brayton system.

Another method was evaluated which had the potential for higher reliability and reduced weight. This method employs linear guide bearings with tubular shafts. Preliminary weight studies indicate this method of alignment for a 50-kWe Brayton system would weigh about 71 lb offering a net savings over the T-track and wheel system of 176.5 lb. This method was finally incorporated into the reference design.

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TABLE II-33
DEPLOYMENT SYSTEM COMPARISON

Case	Type of Deployment Mechanism	Type of Inlet Line	Type of Return Line	Total Structural Weight (lb)
1	Inlet and Return Line Bellows Actuator	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	1463
2	Bi-Stem Actuator	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	1513
3	Bi-Stem Actuator	System of 5 Interconnecting Armored Viton Hose (1.048 in. OD) (0.968 in. ID)	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	1474
4	Bi-Stem Actuator	System of 5 Interconnecting Armored Viton Hose (1.048 in. OD) (0.968 in. ID)	System of 1 Continuous Armored Viton Hose (1.048 in. OD) (0.968 in. ID)	1457
5	Bi-Stem Actuator	System of 5 Interconnecting Flexible Metal Hoses-Armored (1.125 in. OD) (0.750 in. ID)	System of 1 Continuous Flexible Metal Hose-Armored (1.125 in. OD) (0.750 in. ID)	1463
6	Bi-Stem Actuator	System of 5 Interconnecting Flexible Metal Hoses-Armored (1.125 in. OD) (0.750 in. ID)	System of 5 Interconnecting Bellows (1.480 in. OD) (1.000 in. ID)	1453

(c) Reduced Radial Clearance

In addition to the radiator deployment study, a study of the radiator mass sensitivity was made. In one example, reduction of the radial clearance from 6.0 to 4.25 in. reduced the radiator mass from 1612 to 1421 lb, a saving of 11.8%.

C. LASER EFFECTS ON RADIATORS

Zirconium hydride reactor space power systems reject waste heat to space by means of all-aluminum, finned radiators treated with a spectrum-tailored, high emissivity coating. The currently proposed conceptual radiator designs are described in detail in Section II-B-5-b. Among the man-made hazards which may cause degradation in radiator performance are lasers.

Previously, little consideration has been given to the effects of lasers on the radiator. Consequently, a study was made to quantify a typical laser pulse to which the radiator could be exposed and to assess the damage which would occur as a result of that pulse irradiating 1 ft² of the radiator.^(II-10)

The effects of high-power lasers have been examined in the open literature and are well documented in Reference II-11. They include local heating to the melting or vaporization point and removal of material to depths of about 0.01 in. Usually the beams of such lasers are tightly focused, resulting in a cratering effect and no significant structural damage. However, low-power lasers capable of continuous or pulsed operation of duration greater than the thermal time constant of the target can inflict much more significant damage. The thermal time constant of a specimen is defined by:

$$\tau = \frac{4\alpha}{D^2} \quad \dots (4)$$

where

τ = Thermal time constant

α = Thermal diffusivity of material

D = Effective thickness of material.

If transient heating is of greater duration than τ , the temperature of the material will be approximately uniform. It is possible under these conditions

to uniformly heat a target to the melting temperature. Fortunately, lasers capable of long pulse length operation are limited to relatively low power. However, it is not unreasonable to expect that a CO₂ gas laser could deliver an integrated irradiance* of 770 watt-sec-cm⁻² (110 watt-cm² for 7 sec) to 1 ft² of radiator at a wavelength of 10.6 μm. Consequently, this is the irradiance which was arbitrarily selected as a basis of initial study. Higher irradiances were considered, as necessary, up to 2500 watt-sec-cm⁻², to determine the damage thresholds in the final analyses. The radiators for three systems — Brayton, organic Rankine, and Stirling cycle — were modeled at the four system power levels of 10, 25, 50, and 75 kWe. Thermoelectric systems were not considered.

The flux density absorbed or the dose of heating actually deposited in a given target is a function of many variables lumped into a convenient term called a coupling coefficient. At low power, coupling coefficients are closely related to target absorptivity at the wavelength of the laser. The absorptivity of bare, unpolished aluminum at 10.6 μm is given by Reference II-12 as 0.022. Unfortunately, no data are available on coupling coefficients for the tailored-emmissivity coating, AI-93. Concern was expressed that since the coating is designed for high emissivity in the infrared region, the absorptivity at 10.6 μm, which is far infrared, might also be high. However, it was not expected that the overall coupling coefficient to the incident radiation would be greater than 0.20. Consequently, this value was selected as the upper limit of interest. Various values between 0.02 and 0.20 were also studied.

The models of the radiator system consisted of a control volume drawn around the irradiated portion of each radiator. The following heat balance was postulated:

$$M_R \Delta H_R = Q_{\text{sys}} + Q_{\text{sol}} + Q_L - Q_{\text{rad}} - Q_c \quad \dots (5)$$

* Irradiance or fluence are quantities which are independent of target material properties and only express the power delivered to the surface, not the absorbed power. The absorbed power is given by the flux density or dose.

where

M_R = Mass of irradiated radiator

ΔH_R = Enthalpy change of the irradiated radiator over the time increment of interest

Q_{sys} = Heat added to radiator by Dowtherm A (can be negative if Dowtherm A is heated by radiator)

Q_{sol} = Constant solar flux density = 0.0136 watt-cm^2

Q_L = Laser flux density

Q_{rad} = Heat lost by radiation to space or cooler segments of radiator

Q_c = Heat conducted away to nonirradiated radiator.

Equation 5 was analyzed using numerical methods and the digital computer code DEAP (Differential Equation Analyzer Program). It was found that $Q_{sys} + Q_{rad} + Q_c > Q_{sol}$, and that all were negligible compared to Q_L ; the radiator was uniformly heated by the laser; and the fin could be decoupled from the radiator with no resultant appreciable loss in accuracy. Consequently, Equation 5 was reduced to:

$$M_R \Delta H_R = Q_L \quad \dots (6)$$

where

$$H_R = C_{PR} \Delta T_R$$

and

C_{PR} = Heat capacity of radiator

ΔT_R = Temperature change of the radiator.

The final temperature of the irradiated radiator is then determined by the sum of the initial temperature and the temperature change.

The formation of hoop stress in the radiator tube due to laser heating was postulated to be the means by which yield stress levels could be accumulated in the radiator.

Simple hoop stress is described by Equation 7:

$$\sigma = \frac{PD}{2t} \quad \dots (7)$$

where

σ = Stress

P = Internal pressure

D = Tube ID

t = Tube wall thickness.

If, for each temperature, the yield stress is substituted into Equation 7 then, for each tube ID and wall thickness, a plot of the pressure needed to induce yield stress as a function of temperature can be generated. These plots are shown in Figure II-30. Superimposed on the figure is the vapor pressure curve of Dowtherm A, the radiator fluid. Due to the geometry of the situation and a number of other pressure suppression mechanisms, it was shown that the pressure in the tubes would not exceed the saturation pressure of the radiator fluid. The intersections of the vapor pressure curve of Dowtherm A and the yield stress pressure plots for each tube ID in Figure II-30 were then taken as the criteria for tube degradation.

The final temperatures of each system at each power level as functions of irradiation and coupling coefficient were calculated by substituting the appropriate radiator data into Equation 6. A graphic presentation of the results for a typical system (50 kWe Brayton) is shown in Figure II-31. Threshold of damage levels of irradiation and coupling coefficients are indicated where the final temperature exceeds the allowable limit, taken from Figure II-30.

As expected, the final temperature of a given system depended primarily on initial temperature and thermal inertia. Systems with high thermal inertia, due to large tubes and thick armor, heated to lower final temperatures. However, the advantages of high thermal inertia are partially negated by the lower allowable limits due to bigger tubes which yield at lower pressures and temperatures.

On the basis of the results of this study, the following conclusions were made.

Figure 30. Dowtherm A Saturation Pressure and Pressure to Induce Tube Yield Stress vs Temperature and Tube Inside Diameter

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Figure II-31. 50-kWe Brayton System Final Peak Temperature vs Fluence and Coupling Coefficients

- 1) For the postulated base fluence of $770 \text{ watt-sec-cm}^{-2}$, the radiator exhibits excellent survivability, even at the maximum expected coupling coefficient of 0.20.
- 2) If increased survivability is desirable, it is obtainable by increasing the allowable limit, decreasing the coupling coefficient, or decreasing the fluence.
- 3) It is imperative that the actual coupling coefficient of AI-93 be determined if greater accuracy is desired in the prediction of radiator damage.

Based on these conclusions and the assumption that increased survivability at higher fluence is desired, the following recommendations are made to implement Conclusion 2 above:

- 1) A reassessment should be made of the selection of Type 3003 aluminum as the radiator material. Several alloys with considerably higher strength are available with only slight penalties in thermal conductivity, mass, and fabricability.
- 2) The area of the fin immediately adjacent to and lying within the projected area of the tube and armor should not be coated. This will guarantee a low absorptivity and consequently a low coupling coefficient, while only slightly increasing the total radiator area.

D. METEOROID PROTECTION

During the past year, studies were directed toward reducing the mass of a meteoroid shield which provides 360° protection for the space radiator tubes. This was accomplished by utilizing a bumper concept for the portions of the tube facing inward to the cylinder wall and by varying tube cross sectional geometry to minimize the tube vulnerable area. Attempts to use the NASA recommended bumper formula (τ_1 = single sheet armor thickness, $0.1\tau_1$ = bumper, $0.4\tau_1$ = tube armor) resulted in an excessively heavy system. As a consequence, a Rockwell Space Division computer program^(II-13) which analyzes the fracture mechanics of a space projectile puncturing a metal sheet and the subsequent penetration of the fragments into a parallel second sheet, was utilized to predict

second sheet or tube armor thickness. This program is currently being used for similar calculations for the Space Shuttle Program.

A computational technique was formulated to account for oblique impacts from the debris fragments formed inside a cylinder radiator which impinge on the side and top of a D-shape tube, and is described in Reference II-14.

1. Theory

Reference II-13 utilizes a single sheet penetration equation which has been correlated with the material Brinell Hardness Number, ^(II-15) and defines the minimum thickness sheet able to resist perforation by the normal impact of a meteoroid or projectile.

$$t = \frac{8.15 \times 10^{-4} m_p \gamma \rho_p \left(\frac{1}{2} - \gamma\right) (V_p)^{2/3} f_f}{H^{\frac{1}{4}} \rho^{\frac{1}{6}}}$$

where

t = Penetration depth (cm)

m_p = Projectile mass (gm)

ρ_p = Projectile density (gm/cm³)

V_p = Projectile velocity (cm/sec)

f_f = Finite sheet penetration factor = 1.8

H = Sheet hardness, B.H.N.

ρ = Sheet density (gm/cm³)

γ = 0.367.

This equation differs from the NASA equation which correlates with the material acoustic velocity.

2. Cloud Model

The analysis defines the hole size torn out of the first sheet by the projectile. After puncture, the debris from both sheet and projectile are presumed to break into a finite number of debris particles N with mass ratio B .

$$N = n_s + n_p$$

... (9)

$$B = \frac{m_s}{m_p}$$

where

m_s = Mass of sheet material (gm)

m_p = Mass of projectile (gm)

n_s = Number of sheet particles

n_p = Number of projectile particles.

The particles are assumed to be uniformly distributed on the surface of a sphere formed immediately after sheet perforation which expands radially as its center moves toward the second sheet. The velocity in the x direction (normal to the surface) of the leading cloud particles is:

$$V_{1x} = V_p \frac{1 + \sqrt{B}}{1 + B} \frac{\text{cm}}{\text{sec}} \quad \dots (10)$$

The depth of penetration into the parallel second sheet by the leading debris particles of the projectile is:

$$p = R_p \left(\frac{m_p}{n_p} \right)^\gamma \left(V_{1x} \right)^{\frac{2}{3}} \text{ cm} \quad \dots (11)$$

where

$$R_p = \frac{8.15 \times 10^{-4} \rho_p^{\left(\frac{1}{2} - \gamma\right)}}{H_2^{\frac{1}{4}} \rho_2^{\frac{1}{6}}}$$

H_2, ρ_2 = second sheet hardness and density.

Figure II-32. Number of Cloud Fragments vs Mass Projectile

Figure II-33. Sheet 2 Penetration vs Projectile Mass

Similar expressions are given for the depth of penetration of the first sheet particles into the second sheet. Inputs to the computer program include first and second sheet properties and those of the projectile (meteoroid) including its velocity and mass. The program has been run for a range of masses and the results giving N and p vs m are plotted in Figure II-32, Curve A, and Figure II-33. For a specific tube and cylinder geometry the projectile mass is defined on the basis of probability of tube zero puncture (circuit reliability) and is given by the following equation.

$$m_p = \left[\frac{\alpha \tau A_r f N}{-\ln P(o)_t} \right]^{\frac{1}{B}} \quad \dots (12)$$

where

m_p = Projectile mass (gm)

α = Meteoroid flux constant, $10^{-14.37} \left(\frac{\text{gm}}{\text{m}^2 \cdot \text{sec}} \right)$

B = Meteoroid constant, 1.213

A_r = Cylindrical radiator area (m^2)

f = Fraction of debris particles striking a tube to total hitting inside cylinder

N = Number of debris particles formed per cylinder puncture

$P(o)_t$ = Tube reliability

τ = Mission duration (sec).

This equation has been cross plotted on Figure II-32 for two values of $P(o)_t = 0.995$ and 0.999 . The values of the other variables are taken from the reference Brayton cycle 50-kWe system radiator using the third cylinder (fin = 0.015 in.) and a D-shape tube height to width = 2.5 with equivalent diam = 0.25 in. as shown in Figure II-34. The mass corresponding to the intersection of $P(o)_t$ and Curve A was then used to read the penetration depth into the second sheet from Figure II-33.

For oblique impacts, the normal component of velocity causing the penetration into the second sheet was found to vary with the angular position of the tube surface relative to the puncture point.

Figure II-34. D-Tube and Cylinder Geometry

Thus for Side l_2 :

$$V_{n2} = \frac{V_p}{1+B} \left\{ \cos \theta + \sqrt{B - 1 + \cos^2 \theta} \right\} \sin \theta \quad \dots (13)$$

Side l_3 is assumed to be in a position directly opposite to the puncture and does not need a velocity component adjustment.

The meteoroid shield thickness was computed for the l_2 and l_3 sides of a D-shape tube with $P(o)_2 = P(o)_3 = 0.995$. The result, after adjusting for oblique fragment impact and a 1.8 finite sheet factor, was as follows:

$$t_2 = 0.047 \text{ in.}$$

$$t_3 = 0.0638 \text{ in.}$$

Resultant system mass calculations have indicated a reduction from the values obtained with the NASA bumper formula.

E. RELIABILITY ANALYSIS

A preliminary reliability analysis was made of the 50-kWe Brayton Power System and the results reported in Reference II-16. The reliability analysis was undertaken in order to:

- 1) Allocate reliability values to subsystem components.
- 2) Provide guidelines as to the necessity for parallel or redundant components.
- 3) Indicate those components requiring further development for improved reliability.

The system analyzed is shown in Figure II-35. It consists of three basic blocks: the reactor and primary NaK loop, the Brayton PCS, and the heat rejection loop. The Brayton PCS is shown in detail in Figure II-19.

The reliability of a single component can be expressed as

$$R = e^{-\lambda t} \quad \dots (14)$$

where

λ = failures/hour

t = mission time (hours).

The reliability of n components in series is equal to the product of their individual reliabilities. For two identical components connected in series:

$$R_s = R_1 \times R_2 = R^2 \quad \dots (15)$$

For two identical components connected in parallel:

$$R_p = 1 - (1 - R)^2 = 2R - R^2 \quad \dots (16)$$

$$R_s = 0.716$$

Figure II-35. Brayton System Reliability Model

9095-5033

Hence, $R_p > R$ and parallel connections are used when necessary to enhance circuit reliability. In the case of fluid circuits, valves are also provided to isolate failed components.

In the present analysis, no parallel major components were used, and the reliability of any subsystem was obtained by the series multiplication of the individual component reliabilities.

The mission duration for which the present space systems are being designed was taken to be 7 years or 6.132×10^4 hours. As a consequence, in order for the entire power system to have a reliability of, say 0.9, the combined failure rate of all components would (from Equation 14) have to be $\lambda_s = 1.718 \times 10^{-6}$ failures/hour. Many of the individual hardware components making up the system would thus have to have failure rates orders of magnitude smaller. Typical fluid loop welds have $\lambda = 10^{-9}$ to $\lambda = 10^{-10}$ failures/hour. However, other dynamic components such as fluid centrifugal pumps have a reported $\lambda = 10^{-6}$, thereby requiring two or more in parallel. Such parallel connections add to system weight and complexity.

The failure rate data for most components were taken from published vendor data and reports.^(II-17, -18, -19) For those components not listed, such as the actuator and ball screw combination for the reactor sliding reflector, the λ 's were estimated from test data. Table II-34 is a partial list of failure rates used. These rates were combined to obtain the block and system reliabilities given below:

System	Reliability
Reactor and Primary Loop	0.943
Brayton Power Conversion System	0.8213
Heat Rejection Loop	0.9244
System Reliability = $(0.943)(0.8213)(0.9244) \approx 0.716$	

This preliminary reliability estimate will be improved in future analyses by a more careful definition of fluid loop geometry and upgraded failure rate data.

TABLE II-34
COMPONENT FAILURE RATE

Component	λ (failures/hour)
Reactor	
Vessel	1.00×10^{-7}
Fuel	5.0×10^{-9}
Reflector and Motor	6.5×10^{-9}
DC Pump	8×10^{-9}
Accumulator	2.7×10^{-9}
Heat Exchanger	1.0×10^{-9}
Power Conversion Loop	
Brayton Rotating Unit	2×10^{-6}
Piping	5.4×10^{-8}
Heat Exchanger	6.9×10^{-7}
Cooler	3.6×10^{-7}
Recuperator	1.054×10^{-7}
Heat Rejection Loop	
Radiator	1×10^{-7}
Pump	1×10^{-6}
Cooler	1.8×10^{-7}
Accumulator	2.7×10^{-9}

F. SUBSEA SYSTEMS

In response to anticipated subsea oil and gas recovery power requirements, a ZrH reactor power source has been evaluated. The reactor is coupled to an organic Rankine cycle which converts heat energy to electricity and the system rejects waste heat to the surrounding ocean by means of a natural convection heat exchanger. In this concept, the power system consists of two modules. One module contains a ZrH reactor, shield, NaK loop, NaK pump, and heat exchanger. The second module contains the organic Rankine PCS (turbine, regenerator, condenser, hotwell, and organic pump), and the electrical generator. The waste heat rejection heat exchanger is mounted external to the pressure vessels. In GFY 1976/TQ, system and component analyses were performed which include performance at 3.0, 10.0, and 18.75 MWe, reactor thermal and pressure drop analyses, organic fluid degradation, system transient analysis, and seawater heat exchanger design. Summaries of these efforts are presented in the following sections.

1. Performance

Due to the major emphasis on the 10- to 75-kWe space system study, work on performance of subsea systems was confined to the first quarter. During that quarter, preconceptual subsea system configurations were generated at power levels of 10.0 and 18.75 MWe, which utilized the same cycle state points as the 3.0-MWe system. Only the powers, flow rates, and component dimensions were altered to accommodate the increased output. A comparison of the powers and flow rates is shown in Table II-35. A more detailed description of the common parameters associated with all system power levels is contained in Reference II-20. A time-share computer code was written to support this effort. The code is capable of parametrically analyzing the performance of any viable subsea system which utilizes the 3.0-MWe system configuration. It is expected that when work is resumed on subsea systems, this code will be used to refine the 10.0- and 18.75-MWe systems.

2. Thermal Analysis

The investigation, initiated during GFY 1975, of the thermal behavior of the subsea system reactor for the decay period following reactor shutdown has been

Figure II-36. Maximum System NaK Temperature

Figure II-37. Maximum Fuel Temperature

TABLE II-35
POWER SYSTEM PERFORMANCE DATA

Power (MW)			
Net Electrical	3.0	10.0	18.75
Reactor Thermal	10.54	35.1	65.8
Turbine Work	3.52	11.7	22.0
Regenerator Thermal	4.56	15.2	28.5
Heat Rejection	7.21	24.0	45.1
Flowrate (lb/sec)			
NaK	238	792	1488
Organic	36.4	121.2	277.5
Heat Rejection	278	926	1738

completed and documented.^(II-21) This study consisted of three cases. Case 1 assumed heat rejection by some unspecified means at an advantageous point in the NaK loop. Case 2 assumed that the mechanism of Case 1 was inoperative but that natural convection circulation of the NaK loop would still be available. Heat would be transferred to the surroundings by the passive heat transfer modes of conduction, convection, or radiation. Case 2 also considered external environments of air as well as water. Case 3 considered thermal behavior in the extremely unlikely event of a loss-of-coolant accident subsequent to removal of the reactor from water.

Case 1 was completed in GFY 1975 and the results, expressed as functions of NaK loop temperature drop, flow, and pressure drop were reported in Reference II-20, the Annual Report for that year. For Case 2, the effect of removing the reactor from water to an air environment of 100°F was investigated for three particular times of reactor removal: 7, 14, and 21 days after reactor shutdown. The results of the analysis provided maximum system NaK temperatures, maximum fuel temperatures, and maximum pressure vessel surface temperatures, and are shown in Figures II-36, II-37, II-38. Starting with the initial temperatures for the reactor in water, predicted by the Case 2 analysis, the Case 3 analysis resulted in peak temperatures for the system if all of the NaK is lost upon removal of the system to air at 7, 14, and 21 days after reactor shutdown.

Figure II-38. Maximum Outside Surface Temperature

Figure II-39. Transient Temperature Distribution in Reactor System (NaK Lost 14 Days After Reactor Shutdown)

The fuel assembly experienced a rapid response in this case and peak temperatures exceeded 3000°F as indicated in Figure II-39.

On the basis of the results of Case 3, it was recommended that shielding materials with larger thermal conductivities be investigated. An increase of only a factor of two in thermal conductivity would result in a significant decrease in the anticipated peak temperatures following a loss-of-coolant accident.

3. Reactor Pressure Drop Analysis

A hydraulic model of NaK flow through the core and plenums of the reactor at nominal operating conditions was initiated during the first quarter. Flow paths through the control rod assemblies, holes, and gaps between the core barrel and vessel, as well as through the fuel pin channels were considered. Two correlations previously developed in References II-22 and -23 were also incorporated into the model. Prior to obtaining results, this effort was postponed due to the shifting of emphasis from subsea to space systems.

4. Organic Fluid Degradation

Organic fluids at elevated temperatures are susceptible to thermal decomposition. The products of such decomposition may be polymerized material having a high boiling point or low molecular weight substances that are volatile. The former may change the thermodynamic properties of the fluid and/or collect on heat transfer surfaces, while the latter can collect in the condenser and hotwell increasing the turbine back pressure reducing cycle performance. For the compound toluene, which is the subsea power system working fluid, the thermal decomposition rate, R , established by tests is:

$$R = 3.20 \times 10^{-14} e^{0.034T} \quad (\%/h) \quad \dots (17)$$

where

T = the fluid temperature in °F.

The total weight of decomposed toluene in the system was determined, over the 4-year system life, to be 6.0 lb. The majority of this, 5.74 lb, would be generated in the pipe leading from the boiler to the turbine which, in the 3.0-MWe

TABLE II-36
SEAWATER HEAT EXCHANGER CHARACTERISTICS

Heat Load (MWt)	7.21
Tube Diameter (in.)	1.0
Tube Thickness (in.)	0.1
Organic Flowrate (lb/sec)	278
Log Mean ΔT ($^{\circ}F$)	27.3
Overall Transfer Coefficient (Btu/hr-ft ² - $^{\circ}F$)	110.8
Average Temperature ($^{\circ}F$)	54.3
Number of Tubes	200
Number of Fins Per Tube	40
Fin Height (in.)	0.8
Fin Thickness (in.)	0.040
Tube Bundle Volume (ft ³)	1273

design, has a toluene vapor inventory of 23.4 lb. The accumulation of the volatile products of decomposition is expected to significantly increase the turbine back pressure. Consequently, consideration was being given to shortening this pipe and providing for volatile material removal when this effort was postponed for reasons previously noted.

5. System Transient Analysis

The transient model of the subsea system, described in detail in Reference II-20, was completed with the addition of the control subsystem model. The integrated model was then tested with a steady-state run to verify its accuracy. The accuracy of the model was found to be acceptable. A revised set of input data which reflected changes in the reference design as well as a list of transients to be simulated was being prepared when this task was postponed due to the emphasis on space systems.

6. Seawater Heat Exchanger

The subsea system waste heat rejection is accomplished by a natural convection seawater heat exchanger. A computer program has been written in support of the design effort on this exchanger and is documented.^(II-24) The code is capable of calculating the exchanger surface area, tube pitch, tube and fin weights, total exchanger volume, and tube pressure drop. The input requirements include heat transfer fluid properties and inlet and outlet conditions, tube and fin properties, tube diameter, number of tube passes, and sea temperature.

The program employs the Dittus-Boelter, for turbulent flow, and the Seider and Tate correlations for laminar heat transfer film coefficients inside the tubes. Outside the tubes, the Larsen and Eckert equations are used to calculate the natural convection film coefficients. Pressure drops inside the tubes are calculated using the Fanning friction correlation.

Using the code and the heat rejection subsystem requirements for the 3.0-MWe system, a sample heat exchanger design was prepared and is presented in Table II-36.

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III. COMPONENT TECHNOLOGY

The Component Technology Activity, conducted as part of the Zirconium Hydride Reactor Technology Program, continued during FY 1976/TQ. This activity is devoted to development of reliable components capable of meeting the requirements of nuclear power systems for space and terrestrial applications. Tests were conducted on a reflector control segment actuator-ball nut system for space use. In addition, design studies were conducted on pumps for systems with various power levels, both for space and terrestrial applications.

A. SPACE SYSTEM COMPONENTS

This section describes the work performed on the actuator-ball nut system of reactor control and the analysis performed on the various components, mainly pumps, for space operations.

1. Actuator-Ball Nut Testing

Control of SNAP reactors is accomplished by varying the number of neutrons that are allowed to escape the reactor. An opening in the external reflector, the size of which can be changed, acts as a window for neutron escape. In the 5-kWe Reactor Thermoelectric System, as shown in Figure III-1, reflector control is accomplished by linearly displacing the reflector control sectors, one in each reflector half. A ball screw is utilized to obtain the required linear motion from an actuator rotary motion.

In the linear displacement, or sliding reflector design, the ball screw is fixed axially and is allowed to rotate while the ball nut, attached to the reflector control segment, is restrained from rotation. As the ball screw rotates, the reflector segment is raised or lowered, changing the window size. The ball screw, nut, and supporting bearings are referred to as the reflector drive train.

The actuator is a four-step sequence stepper motor with a built-in, spring-actuated, electrically released brake. To match the step size of the actuator to the required movement of the reflector, a single-stage gear reduction is employed and built into the actuator. The ball screw is directly attached to the

Figure III-1. 5-kWe Reactor Thermoelectric System

output of the reduction gear by means of a bellows coupling, selected to minimize backlash.

The reflector drive train and the actuator were each tested individually with the objective of determining performance over a variety of operating conditions. These two items were then combined into a single, combined test. The objective of the combined test was to demonstrate the performance of the actuator and ball-screw assembly.

The actuator was tested for output torque at various currents using an S8DS controller and a bench dynamometer. The following table shows the torque values established and tested for the stepper motor only:

	Design Load	Design Requirement	Test Results
Stepping Torque (oz-in.)	75	100	110
Holding Torque (oz-in.)	105	180	500

As can be seen, the stepper motor met the design requirements.

The ball-screw drive was tested at ambient conditions as well as at temperature in vacuum. The calculated mass of the reflector control sector under ground test conditions is 26 lb. The tests were performed at 1/2 reflector segment mass, at reflector segment mass (26 lb), and at 1-1/2 times the reflector segment mass. The highest torque was encountered when tested in vacuum at ambient temperature and appears to be independent of reflector mass. This value of torque, 6.8 in-lb, yields the following friction factors:

Load (lb)	Friction Factor
13	1.08
26	0.379
39	0.147

6531-40221A

Figure III-2. Disassembled Actuator for 5-kWe Reactor

6531-40222A

Figure III-3. Parts and Stator Assembly of Actuator for 5-kWe Reactor

AI-ERDA-13179

The range of friction factors considered in the design of the sliding reflector was 0.1 minimum, 0.3 expected, and 0.6 maximum. The friction factors obtained in tests for the 26-lb load and the 39-lb load fall within this range. The following is a description of each tested item.

a. Actuator

The actuator consists of three basic parts: the brake assembly, the stepper assembly, and the first stage of gearing. These basic parts all share a common housing for gear, brake, and bearing alignment. Actuator component parts are shown before assembly in Figure III-2, and partially assembled in Figure III-3. The assembled actuator is shown in Figure III-4. Figure III-5 is a sectional view.

The general material of construction is a 27% cobalt-iron alloy, chosen for its high temperature magnetic capability. The design is fully integrated, avoiding any duplication of functions.

The output shaft has both a groove and through-hole for interfacing with the reflector drive rotatable screw shaft. This shaft is integral with the first-stage reduction gear which is made of Hastelloy for strength and coefficient of expansion match with the cobalt-iron.

The pinion of the reduction gear is machined onto the end of a solid cobalt-iron rotor. This rotor has 50 teeth on its outer diameter and the tooth pitch matches the tooth pitch on the 8 poles. Opposing pairs of poles form a phase, and the teeth on the phases are displaced from adjacent phases by 1/4-tooth pitch.

Individual coils on the poles are connected into a four-phase winding with the phase coils in parallel. Successive pairs of poles are magnetized sequentially. Due to the displacement of the teeth on successive pole pairs, the rotor turns 1.8° for each successive pole energization. The stator windings utilize stainless-steel-clad copper conductor with "E" glass as turn-to-turn insulation on the coils.

A rotating brake disk is threaded on the end of the rotor shaft and is accurately positioned by means of a conical fit. Matching slots and teeth are machined in the outer diameter of the rotating brake and stationary brake ring,

6531-40228

Figure III-4. Actuator for 5-kWe Reactor

9095-4053A

Figure III-5. Actuator for 5-kWe Reactor -
Sectional View

AI-ERDA-13179

so the two rings, when engaged, will lock together preventing rotation. Both brake parts are made from cobalt-iron.

Brake reaction forces are carried through the bellows assembly, which also acts as a brake ring compression spring. This assembly is located and pinned so the slots and teeth in the brake disk and brake ring are aligned when Phases A or C are energized. Energization of the brake coil pulls the stationary brake ring, against the spring of the bellows, releasing the shaft to rotate to a new position.

The actuator bearings are carbon graphite, in which alumina journals rotate. Two sizes of bearings are employed. The output bearings are identical and are sized for the load, while the rotor bearings are oversized to allow the pinion, cut into the rotor, to pass through the bearing. Thrust washers are located on each side of the inboard bearing to accurately locate the shaft for both gear and brake operation.

b. Ball-Screw Drive

The ball-screw drive is a modified ball screw, the diameter of which is 3/4 in., with a 3/4-in. lead and a double pitch. Each series of balls makes approximately 1-1/2 turns. The ball screw is dry-film lubricated with Molycote, which is burnished within the grooves. An extension on each end of the ball screw is alumina coated to provide bearing journal surfaces for the carbon bearings. This ball screw is shown in Figure III-6 prior to testing. The short extension on the right side of the figure is for the combination radial and thrust bearing, while the long extension on the left side is for the radial bearing and the reflector guide bearing.

c. Combined Actuator and Ball-Screw Test History Prior to FY 1976/TQ

The actuator and ball screw were coupled together through a bellows connector and installed in a test fixture for combined testing. This test arrangement is shown in Figure III-7. The selected test load was 26 lb, the calculated mass of the reflector control sector. The offset between the center of gravity and the center of the ball screw approximates the side loading of the reflector during ground testing.

6531-40207

Figure III-6. Ball-Screw Drive for 5-kWe Reactor

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Figure III-7. Actuator and Drive Test Arrangement

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The test was instrumented by means of sheathed chromel-alumel thermocouples installed in two stages, the first to obtain temperature gradients and the second to obtain relative temperatures during operation. Operation of the actuator was verified during testing by observation of a shaft extension through a vacuum seal on the vacuum vessel and an attached indicator.

Power was supplied to the actuator by a test controller manufactured for S8DS actuators. This existing controller has a fixed step rate of 2-1/2 sec for a two-step sequence, which results in a rotational speed of the actuator of 0.24 rpm.

The test chamber was evacuated to a pressure of 1×10^{-6} torr or less, and the test item heated to the test temperature. Once at temperature, the test unit was operated in both directions and then allowed to dwell. After the prescribed dwell time, the unit was again operated. Operation consisted of energizing the actuator at various currents which were increased by 0.1-amp intervals until the actuator rotated. This value of current was associated with the starting or breakaway torque. Then the actuator was operated at a current of 0.1 amp above this breakaway current requirement.

The results of this testing showed an acceptable actuator and drive train combination. The maximum actuator breakaway current occurred during the first cold startup and was 5.3 amp. The rated installed operational current is 5.5 amp. Since the torque approximates a square function, this is equivalent to an 8% design margin on torque. The current and torque design margins for the combined tests are given in Table III-1.

TABLE III-1
COMBINED ACTUATOR AND BALL-SCREW TEST DESIGN MARGINS

Test temperatures (°F)	Ambient	500	800	1000
Minimum current (amp)	4.7	4.2	3.9	3.6
Average current (amp)	5.05	4.54	4.10	3.82
Maximum current (amp)	5.3	4.9	4.3	4.2
Minimum design margin (%)	8	26	64	71
Average design margin (%)	19	47	80	107
Maximum design margin (%)	40	71	99	133

The test showed no degradation or deviation from expected or initial performance. The total accumulated dwell and running times at various temperatures are given in Table III-2.

TABLE III-2
COMBINED ACTUATOR AND BALL-SCREW TEST
CUMULATIVE DWELL AND RUNNING TIMES

Dwell (hr)	Temperature (°F)	Temperature (°F)	Running Time (min)	Travel (in.)
—	Ambient	Ambient	145	28.54
32	500	500	58	3.78
32	800	800	58	3.78
1891	800	1000	263	16.44

After testing, the actuator and ball screw were disassembled and inspected. All parts except the gearbox cover bearing were in excellent condition and retained their original appearance. This gearbox cover bearing was cracked during initial assembly. At that time, it was decided not to replace the bearing until after the development performance mapping tests were completed.

The bearing in its cracked state had performed satisfactorily throughout all of the tests; however, the circumferential crack had increased in length. The bearing was replaced prior to extended life testing.

In order to maintain the proper bearing alignment, it is necessary to match-machine both bearings. It was, therefore, necessary to send the mating housing to the vendor with the housing containing the bearing to be replaced. This mating housing is also the actuator housing and contains one of the actuator rotor bearings. When the parts were returned, a hairline crack was discovered in the lower rotor bearing. The decision was made to reassemble the actuator for continued testing with the cracked lower rotor bearing for extended life testing.

d. Extended Life Testing FY 1976/TQ

The actuator was assembled, coupled to the ball nut and reassembled into the test arrangement (Figure III-7). For the extended life testing, the mass was reduced to 13 lb to duplicate the ground test load with helper springs, and the flight conditions with load biasing springs. A scram controller was added to the test setup.

An existing S8DR controller used for the normal step sequence has a stepping period of 1-1/4 sec for each step, or 48 steps/min. A scram controller provides a stepping rate of 1200 steps/min for the scram sequences. Each actuator step is 1.8 degrees of rotation. When this rotation is reflected through the output gear and ball screw, it results in a reflector motion of 0.0648 in./min during normal operation and 1.6 in./min during scram operation.

The actuator and drive train were thermally cycled and performance mapped at 200°F intervals from just above room temperature to 1100°F. (Endurance testing was performed with the screw at 1100°F.) Table III-3 lists the accumulated dwell and running times at each temperature during this test series. The actuator and drive train performed as expected during this series of tests until a sudden increase of torque occurred at the start of a scram run at ambient temperature, causing the drive to stall. Repeated attempts were made to free the drive by rotating the ball screw back half a turn, then forward, using the scram controller. This resulted in stoppage at the same location at each attempt. The actuator current was increased to the maximum allowable short time rating of 8 amperes and some additional stepping occurred with subsequent stalling. Since the normal minimum scram current, at this test condition, is 4.7 amp, the stalling at 8 amp indicates an increased torque to about 2.9 times normal (neglecting saturation of the magnetic iron). Turning the exposed actuator shaft extension by hand indicated a feel of locking, similar to a ball detent every 45° (16° of ball screw rotation).

The test was disassembled for detail inspection of the component parts for damage. Disassembly and measurements determined the ball screw had slight wear in the grooves in both the nut and the screw, as well as intermittent wear marks up to 0.0038 in. deep, on the edge of the spiral groove of the screw. The ball screw was taken to the supplier for evaluation of the measured wear. The

TABLE III-3
EXTENDED LIFE TESTS, CUMULATIVE DWELL AND RUNNING
TIMES AT VARIOUS TEMPERATURES

Screw Temperature (°F)	Actuator Temperature (°F)	Dwell Time (hr)	Stepping Time (hr)	Stepping Travel (in.)	Scram Time (hr)	Scram Travel (in.)
110	110	132	7.8	30	1.0	100
300	300	50	5.2	20	1.0	100
500	500	50	5.2	20	1.0	100
700	600	71	5.2	20	1.0	100
900	700	167	5.2	20	1.0	100
1000	800	1060	18.2	70	3.5	350

supplier determined the wear within the grooves of the screw and nut were normal but the intermittent wear marks were not. The supplier had observed such intermittent wear before, only on screws subjected to high radial or over-turning loads or on units run in a contaminated or dirty environment. Some flakes of dry film were located in the nut that the supplier felt could have caused or contributed to the intermittent wear. Subsequent calculation determined the relationship between the intermittent wear grooves and the balls leaving or entering the screw threads is 25.5 events/revolution, or every 14.1° which is close to the estimated 16° ball detent action.

The only other area of change from pre-test was in the actuator where the nonmagnetic brake shim, which had been spot-welded to the brake disc, had come loose, and was free to move. This shim could have interfered with the brake operation and caused the actuator to lock up, but it would not have caused an increase in torque.

The design stepping life of the reflector drive is a total travel of 400 in. During this last test a total travel of 1030 in., 258% of design life, had been logged. Total test travel on the screw in all the testing was 1713 in. or 428%. Total test travel with the actuator drive is 1082 in. or 271%.

It was proposed and approved to repeat the last test with a new ball screw and a new brake shim. The purpose of this retest is to verify the ball screw travel life and to determine the suitability of the change in brake shim welding.

The actuator and drive train was reassembled into its test configuration (Figure III-7) with another ball nut and modified brake shim welding for additional testing. The additional test history is shown in Table III-4. During this period of testing, all electrical readings, current, and voltage remained in relatively narrow range for each temperature tested, and showed a decrease in friction with an increase in temperature, which is normal for the type of equipment.

At the conclusion of testing (July 1, 1976), the test was returned to ambient conditions of temperature and pressure and post-test operation was compared to pre-test operation with insignificant variations. The actuator and drive train were disassembled for detailed inspection. The ball nut was removed from the test fixture and sent to the Inspection Department for post-test measurements. No significant changes in dimensions that would indicate wear or damage could be detected. The conclusion of no damage was supported by visual microscopic inspection. As shown in Figure III-8, load marks on the screw were visible although wear was not measurable. The spiral edge of the screw grooves are smooth and sharp without wear marks (which were up to 0.0038 in. deep on the previous screw). This observation supports the position that wear marks on the screw grooves could have been caused by excess dry film which was removed for this test.

The actuator was disassembled to check the actuator quality, wear, and out-of-tolerance parts or assemblies. All assembly tolerances were maintained. The only detectable change was on the end play of the output shaft, which had increased by 0.007 in. Disassembly revealed wear on the corner of the lower thrust bearing of approximately this amount. The appearance of the bearing wear suggests the bearing was not seated with the thrust surface normal to the final machined bore and the last assembly had provided a heavy coupling thrust loading on this surface, (this is the bearing that was replaced). This wear amount and appearance does not provide cause for concern. A post-test figure of this bearing is not included as the wear does not show the photograph.

TABLE III-4
EXTENDED LIFE VERIFICATION TEST RESULTS

Screw Temperature (°F)	Actuator Temperature (°F)	Dwell Time (hr)	Stepping Travel (in.)	Scram Travel (in.)
100	100	—	31	159
300	300	64	31	150
500	500	54	31	150
700	600	199	31	150
900	700	146	21	100
1000	800	3015	31	155

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Figure III-8. Ball Screw After Testing Showing Load Marks

9095-4096

Figure III-9. Nonmagnetic Brake Shim
Welded to Brake Ring, Post-Test
Examination

9095-40104

Figure III-10. Tooth Brake Ring

9095-40106

Figure III-11. Tooth Brake Disk

9095-40110

Figure III-12. Disassembled Actuator After Testing

The quality of all parts was excellent. All other bearing surfaces show no evidence of material removal or wear. All bearing surfaces show the expected load marks. The nonmagnetic brake shim, Figure III-9, is in good condition and remains welded to the stationary brake ring. Normally, the first parts to show wear and/or damage are the tooth brake surfaces, as shown in Figures III-10 and III-11. There is no detectable damage or wear under microscopic visual inspection. The disassembled actuator is shown in Figure III-12. In general, the quality of the parts is that of a new machine.

The design life requirements for the reflector drive are 5 years total time, and 50 scrams and 50 stepping-in cycles. This is equivalent to 43,000 hours and 400 in. of total travel. In this test, the actuator and ball screw travel was 1040 in., and the time at 300°F, and above was 3891 hours. In prior testing, the actuator had produced 1082 in. of travel and 5029 hours at 300°F and above, and, therefore, the following percentage of design life;

Test Duration Parameters	Percentage of Design Life
Ball Nut Travel	260
Actuator Produced Travel	530
Ball Nut Time at 300°F and Above	9
Actuator Time at 300°F and Above	23

From the above, the design is suitable for the operational travel of 400 in., and suitable for operation in the outer space environment of temperature and pressure. Although the 5-year design life has not been verified, other units of similar construction have been qualified for space. Experience with these qualified units has indicated any unit operating at high temperature for 1000 hours without damage is sufficiently stable that it probably would qualify for 5-year life. It is, therefore, concluded that the design of the reflector drive is suitable for this application without additional changes, and the use of the actuator for both normal control and scram shutdown is satisfactory.

2. Thermoelectric Pump

The dual throat thermoelectric pump assembly tested during GFY 1975 was removed from the test loop, cleaned by the alcohol process, sealed in a plastic bag, and placed in storage.

A test report (III-1) was prepared covering the fabrication, assembly, and testing. The pump responds to bootstrap operation as a 12°F temperature difference between the primary and secondary thermo-electric loops establishes flow in the primary pump loops. A number of recommendations to simplify fabrication, assembly, and improve performance are also presented. Computer code verifications with test data indicate few areas of corrections such as apparent contact resistance or current carrying area.

3. 15-kWe Thermoelectric System Pump Analysis

A layout drawing and preliminary performance calculations were made for a dual throat, dc conduction pump for a 15-kWe thermoelectric system. The results of these analyses are summarized in Table III-5.

4. 10- to 75-kWe Systems Pump Study

A study was performed to determine the size, mass, and performance of electromagnetic liquid metal pumps for selected space systems utilizing the Brayton cycle, organic Rankine cycle, Stirling cycle, and thermoelectric modules. Pumping requirements were based upon four power levels, (10, 25, 50, 75 kWe) and various system efficiencies for each space system. For the four cycles, two types of pumps were considered, the single-throat dc Faraday pump selected for its compatibility with thermoelectric power supplies, and the helical induction pump selected for its superiority in past studies, over the other types of electromagnetic pumps; i. e., linear or annular in the size range.

A series of standardized helical pump designs was produced to cover the various ratings which was then expanded to include characteristic curves showing the effect of changes in diameter, length, magnetic gap, and frequency. This information was made available for system layout.

The thermoelectric systems considered the single- and dual-throat Faraday pumps due to the compatibility with thermoelectric power supplies. All Faraday

TABLE III-5
PUMP PERFORMANCE FOR DUAL-THROAT DC
FARADAY ELECTROMAGNETIC PUMP

System Rating (kWe)	15	10
Primary Flow (lb/sec)	14.0	9.43
Primary Pressure (psi)	0.95	0.95
Primary Temperature	1200	1200
Secondary Flow (lb/sec)	8.65	5.80
Secondary Pressure (psi)	1.51	1.51
Secondary Temperature (°F)	610	610
Overall Length (in.)	4.3	3.5
Overall Width (in.)	13.2	10.8
Overall Height (in.)	5.8	4.7
Nozzle Length	TBD*	TBD*
Circulating Current (amp)	3600	2753
Pump Operating Voltage (V)	0.200	0.200
Number of Thermoelectric Elements	6	

*TBD = To be determined.

pumps were electrically matched to be driven by thermoelectric modules, as supplied for thermoelectric pumps tested last Fiscal Year, and the number of pump modules determined. This information was made available for system layout.

Table III-6 is a typical summary table listing the standardized helical pump design and single-throat dc Faraday pump selected for the flow ratings of the Rankine cycle's 25- and 75-kWe systems.

5. JPL System Cost Review

JPL subcontracted to General Electric for a study, conducted during 1971 and 1972, which was documented in a report, "Nuclear Electric Propulsion, Mission Engineering Study, Development Program and Cost Estimates." (III-2) A brief review of this report was conducted to modify the cost associated with the heat rejection system to reflect current changes in the system and power level, as well as the impact of inflation. An attempt is being made to proportion this modified cost and equivalent manhours to the labor classifications

TABLE III-6
RANKINE CYCLE NaK PUMP PERFORMANCE
(700 °F NaK)

System Power (kWe)	25	25	25	75	75	75
Flowrate (lb/sec)	6.71	7.48	10.51	18.35	20.58	29.14
Head Rise (psi)	1.2	1.2	1.2	1.8	1.8	1.8
<u>Helical Pump</u>						
Overall Diameter (in.)	10	11	11	12	12	13
Overall Length (in.)	23	25	25	27	27	29.5
Mass (lb)	230	350	350	460	460	570
Power Required (kWe)	1.6	1.5	1.8	3.2	4.2	7.1
<u>Thermoelectric Pump</u>						
Height (in.)	3.5	3.5	4.0	7.0	7.2	8.0
Width (in.)	7.0	7.4	8.0	11.0	11.2	12.5
Length (in.)	2.2	1.9	2.5	3.7	4.0	5.2
Mass (lb)	24	25	26	105	115	210
Power Required (kWt)	21.8	21.8	26.1	39.2	43.5	52.2

within the various program phases. Because this allocation is being made without the benefit of design drawings or program definition, the cost estimates are extremely preliminary.

B. TERRESTRIAL SYSTEM COMPONENTS

Study of components for terrestrial systems was limited to pump analysis for 10- and 18.75-Mwe subsea systems. The subsea system uses NaK as the reactor coolant fluid which is circulated by an electromagnetic pump. The pump selected for this application was the ac helical pump, selected for its superiority in post-test studies, over the other types of electromagnetic pumps, i.e., linear or annular. For each basic flowrate, pumps were sized for one pump, two pumps in parallel, and four pumps in parallel. Preliminary size and performance characteristics were calculated and the results are shown in Tables III-7 and III-8.

TABLE III-7
10-MWe SUBSEA SYSTEM PRELIMINARY
PUMP CHARACTERISTICS

Pumps in Parallel	1	2	4
Flowrate (lb/sec/pump)	794	397	198
Fluid Temperature (°F)	600	600	600
Head Rise (psi)	10	10	10
Overall Diameter (in.)	27	24	24
Overall Length (in.)	75	61	55
Power Required (kWe)	201	87	43
Power Factor (%)	18.5	21.9	27.4
Efficiency (%)	15.5	18.0	18.4
Current at 440 Volts (amp)	1422	520	205
Current at 2300 Volts (amp)	272	99	39

TABLE III-8
18.75-MWe SUBSEA SYSTEM PRELIMINARY
PUMP CHARACTERISTICS

Pumps in Parallel	1	2	4
Flowrate (lb/sec/pump)	1488	744	372
Fluid Temperature (°F)	600	600	600
Head Rise (psi)	10	10	10
Overall Diameter (in.)	35	27	24
Overall Length (in.)	85	75	66.7
Power Required (kWe)	387	153	68.3
Power Factor (%)	14.2	17.4	22.1
Efficiency (%)	15.1	19.1	21.4
Current at 440 Volts (amp)	3583	1158	406
Current at 2300 Volts (amp)	686	222	78

REFERENCE, SECTION III

- III-1. L. Donelan, "Test Results of Performance Test of NaK, Electromagnetic Pump TE Assembly," TR-652-330-003, September 11, 1975
- III-2. "Nuclear Electric Propulsion, Mission Engineering Study, Development Program and Cost Estimates," GESP-7074, 1972

IV. FUEL ELEMENT TECHNOLOGY

The fuel element technology effort was composed of four subtasks during FY 1976/TQ: (1) hydrogen barrier evaluations, (2) facility modifications, (3) fuel casting, and (4) NaK bonding studies.

The hydrogen barrier evaluations were a continuation of the work performed during FY 1975. Based on coupon tests, three candidate coating compositions were selected for additional testing. During FY 1976/TQ, tube specimens were prepared for permeation testing and further characterization of the properties of the new coatings was accomplished.

The FY 1976/TQ workscope required the activation of an induction melting furnace and the reestablishment of capabilities for radioactive machining, frit smelting, NaK loading and bonding, hydriding, and permeation testing.

The objective of the fuel casting task was to demonstrate the technical feasibility of producing fuel rods by induction melting and direct casting of segments, 4- to 6-in. long. This approach is a significant departure from the previous practice of arc melting and extrusion for fuel rod fabrication.

Installation of equipment required for the NaK bonding studies was completed. All materials required for compatibility testing were procured or fabricated with the exception of cast fuel rods.

A. CERAMIC HYDROGEN BARRIER

Coatings A, E, and J, the selected candidates for hydrogen barrier application, along with SCB, the control coating, were applied to squares and tubes of Hastelloy-X for hot indentation hardness, and split-ring tests. Coating A was also tested along with SCB for adherence. Hastelloy X tubes were pre-oxidized and coated to test the performance of a new flow coating apparatus and to obtain data for the development of flow coating procedures and permeation testing.

1. Hot Indentation Hardness Testing

Twenty-five squares of Hastelloy X, 0.39 in. by 0.30 in. by 0.026 in., were coated with compositions SCB, A, E, and J and tested for hot indentation hardness. The hardness tests utilized a 10-min load of 244 gm on a 3/16-in. -diameter stainless-steel ball at temperatures ranging from 1400 to 1750°F. This was

Figure IV-1. Schematic View of Hot Indentation Hardness Tester

Figure IV-2. Hot Indentation Hardness Test, Coatings SCB, A, E, and J on Hastelloy X

done to determine the softening, T_s , and flow, T_f , temperatures of the new coatings compared to SCB (see Figure IV-1 for apparatus used).

Based on the areas of imprints produced by indentation, coatings SCB, A, E, and J were found to soften with increase in temperature — gradually at first (up to 1510°F), then at a faster rate with further increase in temperature (see Figure IV-2). The softening and incipient flow temperatures (T_s and T_f) for the various coatings were found to vary as shown in Table IV-1. (T_s is the temperature required to produce an area of imprint of 1×10^{-4} in.² and T_f is the temperature required to produce an area of 5×10^{-4} in.²)

TABLE IV-1
RESULTS OF HOT INDENTATION HARDNESS TESTS

Coating	Substrate	Deformation Temperature (°F)		Flow Rate R_f (°F)
		Softening, T_s	Incipient Flow, T_f	
AI E	Hastelloy X	1560	1672	58
AI A	Hastelloy X	1520	1615	20
AI J	Hastelloy X	1514	1611	21
AI SCB	Hastelloy X	1508	1608	21

Rates of flow (R_f) in terms of °F to produce an increase in area of imprint of 2×10^{-4} in.² at the flow point were calculated to obtain additional data regarding the effects of temperature on deformation. These values were found to range from 20 to 58°F as indicated. Coatings J, A, and E were found to soften as well as flow at higher temperatures than SCB with coating E being the most refractory or "hardest."

2. Split-Ring Test

Coatings A, E, and J, the selected candidates for hydrogen barrier application, were tested along with coating SCB for their comparative degrees of stress-strain development on Hastelloy X using the split-ring method of test.

Ten 2-in. -long tubes of Hastelloy X (0.7086-in. OD by 0.029-in. wall) were grit blasted and pre-oxidized for 15 min at 1950°F in a mixture of 55% Ar — 45% O₂ flowing at a rate of 575 cm³/min. The tubes were flow coated with the four compositions, fired in air, and sectioned into 3/4-in. lengths for test.

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Figure IV-3. Composite Strains (S_c) for Coatings on Hastelloy X
Extrapolation of Data to 3 MILS

Enamel and metal thicknesses were determined from thickness measurements taken at eight places around each ring before and after stripping. The residual or induced strain (S_r) produced in the system during processing (in terms of gap width) was calculated on the basis of OD measurements taken before splitting (d_1) and after stripping and splitting (d_5) using the following formula:

$$\text{Residual Strain } (S_r) = (d_5 - 2t) \sin \left[180 \left(1 - \frac{d_1 - t}{d_5 - t} \right) \right]$$

where

d_5 = Outside diameter of tube after stripping and splitting (in.)

t = Thickness of the metal (in.)

d_1 = Outside diameter of tube after coating (in.).

The strain in the glass-to-metal composite (S_c) and yield of the metal (Y_m), if any, which might have been caused by the compressive forces of the coating, were calculated on the basis of OD measurements taken off the ring before splitting (d_1), after splitting (d_2), and again after stripping (d_3). The gap widths, X_c and X_m , were calculated on the basis of the measurements taken and used to compute composite strain and yield of metal as follows:

$$\text{Composite Strain } (S_c) = X_c - X_m$$

$$\text{Yield of Metal } (Y_m) = X_m - S_r.$$

The strains in a glass-to-metal composite increase in value with increase in coating thickness. This being the case, all values for S_c had to be normalized to the same thickness of coating, 3 mils, for comparative purposes. This was done by plotting S_c values against coating thickness and extrapolating the data to 3 mils (see Figure IV-3). The coating-to-Hastelloy X composites were found to exhibit strains (S_c) which varied from a high of 40.1 mils (gap width) for SCB to a low of 28.0 mils for Composite E. The metal was not stressed beyond its yield point by any of the coatings. The measured values of Y_m are all less than the experimental error limits of about 2.5 mils (Table IV-2).

TABLE IV-2
SUMMARY OF SPLIT-RING TEST DATA
FOR COATINGS ON HASTELLOY X

Coating	Composite Strain (S_c)		Yield of Metal (Y_m)	
	Values Normalized to Coating Thickness of 3 mils	% Compared to SCB	Y_m Coating Thickness, t (mils)	Coating Thickness, t (mils)
SCB	40.1	100.0	+2.22	2.15
J	39.1	97.5	+1.40	2.17
A	36.0	89.8	+0.03	2.63
E	28.0	69.8	+0.36	2.98

Coatings A and E, selected candidates for hydrogen barrier application, along with Coating J were found to be better than SCB from the standpoint of matching the expansivity of Hastelloy X with the values for strain ranging from 2.5 to 30.2% less than SCB. The Hastelloy X was not stressed beyond its yield point by any of the coatings.

3. Adherence

Six samples of Hastelloy X, 2 in. by 3/4 in. by 0.048 in., three grit-blasted and three pre-oxidized for 15 min. at 1950°F, were coated with compositions A and SCB, and fired in air or helium for adherence testing. The objectives of the study were to check the sensitivity of the flat plate method of test and to obtain data showing the effects of metal preparation and firing conditions on the adherence of Coatings A and SCB to Hastelloy X.

Each sample was bent to an angle of 90° around a 1/2-in. -diameter hardened steel rod using a hand bend fixture as shown in the following sketch:

The bent samples were examined and graded for adherence based on the amounts and appearances of the glass retained after flexure.

The adherence of the A coating over Hastelloy X was found to vary from very poor (over blasted areas fired in helium) to good or acceptable over oxidized areas fired in air. The SCB coating was found to show excellent adherence over blasted as well as oxidized areas of Hastelloy X (see Table IV-3).

TABLE IV-3
ADHERENCE TEST DATA FOR COATINGS A AND SCB ON HASTELLOY X

Coating	Metal Preparation	Firing Atmosphere	Appearance of Substrate		Adherence Rating
			Color	Glass Retention	
A	Blasted	Helium	Shiny	No glass slivers	Very poor
A	Blasted	Air	Shiny	Few slivers of glass	Poor
A	Oxidized	Helium	Medium Gray	Few-to-many glass slivers	Poor to fair
A	Oxidized	Air	Dark Gray	Metal covered by slivers of glass	Good
SCB	Blasted	Air	Dark Gray	80.3% of as-fired glass	Excellent
SCB	Oxidized	Air	Dark Gray	69.8% of as-fired glass	Excellent

According to these data, the adherence of Coating A to Hastelloy X appeared to be significantly affected by metal preparation and firing condition – perhaps more so than SCB. The adherence of Coating A did not appear to be quite as good as that of SCB (good compared to excellent) which might cause one to be concerned; however, it is believed that the composition or oxidation parameters can be modified very slightly to produce the desired degree of adherence.

Some fundamental studies were initiated to learn how increased adherence could be accomplished. Substrates were examined by x-ray diffraction before and after coating to determine the characteristics of the compounds which may have been formed at the interface.

According to the results recorded in Table IV-4, the adherence of Coating A was found to improve from "very poor" to "good" with increase in the content of Cr_2O_3 and decrease in the content of $\text{Fe}_3\text{Mo}_3\text{C}$ found at the glass-to-metal interface after coating.

TABLE IV-4
X-RAY DIFFRACTION DATA ON HASTELLOY X SUBSTRATES
AFTER COATING WITH A
(Sums of peak heights between $2\theta = 8$ and 49.4°)

Compounds	Blasted		Oxidized	
	Fired in		Fired in	
	Helium	Air	Helium	Air
FeNi	353	373	373	370
Cr_2O_3			73	106
$\text{Fe}_3\text{Mo}_3\text{C}$	21	7		
Adherence	Very poor	Poor	Poor to fair	Good

These data suggest that the adherence of Coating A can be improved by: (1) pre-oxidation for longer times at 1950°F or at higher temperatures for shorter times, or, (2) modifying the Hastelloy X composition to include mill additions of $\sim 1\%$ Cr_2O_3 which should stabilize the reaction of the glass with the Cr_2O_3 formed at the interface during firing.

4. Coating of Tubes

Ten Hastelloy X tubes, 12 in. by 0.95 in. ID by 0.02 in. wall, were coated during the latter part of FY 1976/TQ, four to test the performance of a new flow coating apparatus and six to obtain preliminary data for the development of flow coating procedures and permeation testing.

The tubes were pre-oxidized for 15 min. at 1950°F with a mixture of 55% Ar and 45% O₂ flowing at a rate of 575 cm³/min. The tubes were not grit-blasted prior to oxidation due to the lack of required apparatus. One tube was coated with composition E, one with A, four with SCB, and four with composition J.

Coatings were applied by means of a newly developed coating apparatus shown in Figures IV-4 through IV-7. Figure IV-4 shows the entire assembly with the tube positioned in an upright position for pouring. Figure IV-5 shows the tube being filled with the slip which is poured until it comes out of the overflow at the top right. After the cap is placed over the overflow, the assembly is rotated 180° and the tube drained at a prescribed rate as shown in Figure IV-6. Figure IV-7 shows the apparatus after the slip-coated tube has been removed. The apparatus was found to operate successfully.

The first two tubes (1 and 2) coated with Compositions A and E were found to be unacceptable due to spallation, drainage marks, and pitting (see Table IV-5). Tubes 3 and 4 coated with Compositions SCB and J were found to be better from the standpoint of continuity of coverage. Drainage marks were eliminated by the utilization of "dryer" slips.

Tubes 5 through 10 were evacuated prior to application of slips of SCB and J. This procedure was found to improve the coating from the standpoint of appearance as well as coverage. Five of the last seven tubes which were slip-coated were found to be acceptable for permeation test.

B. FACILITY MODIFICATIONS

Five new laboratory areas, a total of about 1500 ft², were established in support of the ZrH Program. This was in addition to the 500 ft² ceramics laboratory already in existence. The added capability included: (1) permeation

Figure IV-4. Flow Coating Apparatus
with Tube in Place for Slip Coating

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9095-4083

Figure IV-5. Flow Coating Apparatus, Tube
Being Filled with Slip

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9095-4082

Figure IV-6. Flow Coating Apparatus
Tube Being Filled with Slip

9095-4085

Figure IV-7. Actuator and Drive Test Arrangement

AI-ERDA-13179

TABLE IV-5
COATING OF TUBES
DATA ON PROCESSING, APPLICATION, AND RESULTS

Tube Number	Coating	Firing Time (min)	Dry Weights Application (gm)				Appearance After Final Coat	Disposition	
			1st	2nd	3rd	Total		Grade	Permeation
1	E	5	1.484	1.708		3.192	Coating spalled. Bottom half best. Many drain marks.	Reject	
2	A	5	2.203	3.095	3.643	8.941	Two bad drain marks. Coating rough.	Reject	
3	SCB	5	1.288	2.496		3.784	Very badly pitted. Bottom partially spalled.	Reject	
4	J	5	2.269	2.755		5.024	Good coating. Slightly rough.	Acceptable	Test
5	SCB	4	1.728	1.913		3.641	Few healed pits throughout.	Acceptable	Test
6	SCB	4	1.923	1.873		3.796	Few healed pits throughout.	Acceptable	Test
7	SCB	4	2.709	1.750		4.459	Badly blistered over localized area 4-1/2-in. down.	Reject	Test*
8	J	5	2.250	3.003		5.253	Few healed pits throughout.	Acceptable	Test
9	J	5	2.333	1.716		4.049	Two small blistered areas 4-1/2-in. down.	Acceptable	Test
10	J	5	1.663	1.802		3.465	Few healed pits throughout. One spalled area on B.E.	Reject	Test*

* Should be tested to determine effects of defects on H₂ release.

testing, (2) hydriding, (3) frit smelting, (4) inert atmosphere glovebox, and (5) a radioactive material machine shop. All of the new facilities were fully operational at the conclusion of the reporting period.

The permeation testing facility, Figure IV-8, consists of four furnaces with a diffusion pump vacuum system, controllers, recorders, and mass spectrometers tuned to measure hydrogen leakage. Calibrated standard leaks are an integral part of the equipment.

A new programmable welding power supply was procured for use in conjunction with the controlled atmosphere glovebox (Figure IV-9). This equipment will be utilized for NaK loading, bonding, and final closure welding of test fuel elements.

The radioactive machine shop is shown in Figures IV-10 and IV-11. Capabilities include a lathe, grinders, a precision balance, and an exhaust hood. The hood is used for melt charge preparation and disassembly of as-cast rods from the molds.

C. FUEL CASTING

Originally, the induction melting and casting development were to have been conducted in an existing, company-owned furnace. To that end, a Test Plan was prepared and approved. A User Authorization, which defined safe and accountable procedures for handling radioactive materials, was approved by the Isotope Committee. Graphite molds and crucibles were designed and fabricated. Zirconium bar stock was purchased and depleted uranium plate material was found to be available in stock. Prior to melting, considerable effort was expended to upgrade the vacuum integrity of the furnace chamber.

Three attempts to make a melt were unsuccessful. On the first run, a water leak in the coil forced a premature termination of the cycle. During each of the next two runs, it was not possible to achieve the required melting temperature of about 1800°C. A review of the equipment performed by a factory representative determined that significant modifications to the coil design and insulation would be required to exceed the present upper temperature of about 1600°C. In addition, the output of the induction power supply was suspect.

Figure IV-8. Permeation Test Laboratory

Figure IV-9. Inert Atmosphere Glovebox and
Welding Power Supply

Figure IV-10. Hot Machine Shop,
Grinders and Fume Hood

Figure IV-11. Hot Machine Shop,
Grinders, Analytical Balance, and Lathe

It was decided to transfer the casting effort to a laboratory-scale induction melting furnace. Components required to adapt the furnace chamber and vacuum system for this work were designed and ordered. The last of the system components is scheduled for delivery by October 4, 1976.

D. NaK BONDING

No actual test work was performed on this subtask due to the lack of cast fuel slugs. The glovebox for the NaK loading and final closure welding was checked out and is operational. The welding power supply was received, checked out, and is also operational. Cladding tubes, end caps, and beryllium reflectors for the test specimens were fabricated and are available for future use.