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**10-75-KWE-REACTOR-POWERED
ORGANIC RANKINE-CYCLE
ELECTRIC POWER SYSTEMS
(ORCEPS) STUDY**

MASTER

FINAL TECHNICAL REPORT

30 MARCH 1977

Prepared For

U. S. ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION
NUCLEAR RESEARCH AND APPLICATIONS DIVISION
WASHINGTON, D.C. 20545

(CONTRACT NO. EY-76-C-03-1336)

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The Principle Investigator for this study was John E. Boretz. He was assisted by Eugene Nezgoda and Arnold Halpern who conducted the computer programming and established the parametric data for the various power conversion system working fluids, cycle analyses, and heat exchanger characteristics. In addition, much of the parametric data on the reactor loop components was supplied by Dr. Albert Martin of the Atomics International Division of Rockwell International Inc.

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1. INTRODUCTION

1.1 GENERAL

This study was conducted to evaluate the performance characteristics of a U-ZrH reactor powered "ORCEPS" (Organic Rankine Cycle Electric Power System) in the 10 to 75 KW_E power range. The main emphases in this study was to determine the practicality of utilizing such a system for current and future space applications. This involved the investigation of various organic working fluids, cycle conditions and system conceptual designs, the preparation of parametric data and the conducting of system performance trade-offs in order to arrive at a Reference Design Configuration. In addition, the potential for commonality and standardization was investigated in order to provide ERDA/NRA management with sufficient data to establish the system effectiveness of using a reactor powered ORCEPS for various space applications. The Reference Design Configuration was defined in sufficient detail to permit the preparation of preliminary Development Plans. This included time-phased programmatic milestones and costs associated with the development, flight qualification and orbital start-up of a reactor powered ORCEPS.

1.2 GUIDELINES AND CONSTRAINTS

This study was based upon the use of existing technology associated with U₂₃₅-ZrH nuclear reactors. The reactor core outlet temperature was limited to a range of from 850°F to 1250°F (depending upon the thermal output power level) to be compatible with achieving an operating life of seven years.

A net electrical power level range of 10 to 75 KW_e was assumed. This encompasses the mission requirements for most of the space applications envisioned for the 1982 to 1992 time period. The power conversion unit (PCU) was assumed to have an operating life of seven years without the use of redundant, standby PCU's. Pyrolytic, radiolytic and synergistic interaction degradation rates have been factored into the parametric

data to achieve these operating life goals.

The technology levels considered were to be consistent with a development, qualification and flight readiness program capable of meeting a mid-1980's launch date. In addition, the geometry of the reactor-ORCEPS in the stowed, launch arrangement was to be compatible with the Space Shuttle payload bay dimensions. The launch configuration was also required to be compatible with the Space Shuttle launch and Intermediate Upper Stage (IUS) orbit injection environments. Start-up in orbit and operation in a zero-g environment were also assumed.

1.3 STUDY OBJECTIVE

The overall objective of this study was to investigate various reactor-ORCEPS using various organic working fluids and thermodynamic cycle conditions in order to arrive at a preferred Reference Design Configuration (RDC). The RDC was to be configured to be compatible with Space Shuttle/IUS launch and orbit injection environments and be based upon technology levels consistent with achieving a flight readiness capability by the mid-1980's. The following specific objectives were to be accomplished:

- Determine the feasibility and system effectiveness of utilizing a U-ZrH reactor power ORCEPS for various current and future space applications.
- Select various candidate organic working fluids and cycle conditions to establish an optimum set of reactor-ORCEPS performance characteristics.
- Prepare parametric data and conduct system trade-offs in order to select a Reference Design Configuration in the 10 to 75 KW_E power level range.
- Determine the feasibility and cost benefits associated with the use of commonality, modularization, and standardization of the Reference Design Configuration and assess level of growth potential.
- Evaluate the performance, design, development and operational implications associated with utilization of the Reference Design reactor-ORCEPS space power system.

2. SUMMARY AND CONCLUSIONS

2.1 SUMMARY

This 10-75KW_e Reactor-ORCEPS Study was concerned with the evaluation of several organic Rankine cycle energy conversion systems which utilized a U₂₃₅-ZrH reactor as a heat source. A liquid metal (NaK) loop employing a thermoelectric converter powered electromagnetic pump was used to transfer the thermal energy from the reactor to the organic working fluid.

Initially, fourteen organic fluids were selected from over seventy-five potentially attractive fluids (Table 3.5). These fourteen fluids were subjected to a preliminary screening process based upon their fluid properties. Such factors as molecular weight, melting point, vapor pressure, and pyrolytic and radiolytic decomposition characteristics were considered. From this initial screening, nine organic fluids were selected for further evaluation. Various cycle conditions were defined for use in this evaluation. This included a peak cycle temperature of 750°F (399°C) and condensing temperatures in the range from 200 to 240°F (90 to 116°C). Using TRW Systems "PORCAP" computer program, cycle analyses were conducted for these nine organic fluids. The power conversion unit (PCU) cycle efficiencies were determined as a function of peak cycle temperature and turbine isentropic efficiencies. These results were summarized in Figure 3.51. In addition, normalized comparisons of vapor pressure (at a nominal condensing temperature) and working fluid flow rates (as a function of net electrical power output) were prepared. Based upon these data, four candidate working fluids were selected for further evaluation and tradeoff. These were CP-25 (toluene), CP-28 (hexafluorobenzene), FL-M (Fluorozene M), and RC-1 (40/60 mixture of hexafluorobenzene and pentafluorobenzene).

To aid in the selection of candidate reactor-ORCEPS power system, parametric data of the reactor heat source, shielding, and NaK loop components were prepared. In addition to providing data on performance, weight, and volume, it also contained information on reactor operating life as a function of the reactor NaK outlet temperature. This data indicated that at a thermal power level of 600KW_T, to achieve an operating life of 7 years, the outlet temperature should not exceed 850°F (455°C). At lower thermal power levels, (i.e., 100KW_T) temperatures up to 1250°F (677°C) were permissible. To be

consistent with an ORCEPS peak cycle temperature of 750°F, a reactor NaK outlet temperature of 850°F was chosen. This not only provided a high factor of safety on reactor operating life, but also minimized development risks and enhanced system standardization and commonality considerations.

Shielding weights were based upon the use of lithium hydride (LiH) and a standard 12° cone angle was selected. The dose plane separation distances were varied from 30 feet (10KW_e) to 100 feet (75KW_e), to meet the total seven year cumulative radiation requirements and minimize shielding weights. Additional parametric data was prepared to characterize the performance, weight and volume of the various components comprising the ORCEPS-PCU. These included the turbine, pump, regenerator, boiler, condenser-radiator, and generator.

At this point in the study, three conceptual candidate reactor-ORCEPS power system designs were synthesized (Figures 3.69, 3.70, and 3.71). The first candidate design used the typical baseline reactor and NaK loop components consisting of the reactor, T/E converter, E/M pump and volume accumulator. The ORCEPS-PCU was a conventional subsystem consisting of the NaK-to-working fluid boiler, turbine, generator, pump, regenerator and radiator, but with the condensing portion of the cycle occurring in a jet condenser. The second candidate power system was similar to the first, except that the separate NaK loop T/E converter was eliminated and a larger T/E converter was integrated with the NaK boiler to operate in a topping cycle manner with respect to the organic Rankine cycle. With this arrangement, the heat rejected from the T/E converter is used to provide the heat input to the organic Rankine cycle. This increases the overall cycle efficiency and reduces the radiator area requirements. This integrated T/E converter not only provides the power for the NaK loop E/M pump, but it also furnishes a portion of the net electrical output power to the spacecraft payload. In order to use this concept, it is necessary to operate the reactor with a NaK outlet temperature of 1300°F and an ORCEPS peak cycle temperature of 700°F, in order to achieve an acceptable level of performance ($\eta_{T/E} = 3.5\%$) in the integrated T/E converter. The third conceptual design is the same as the first candidate system, except the jet condenser is eliminated. An optional jet pump is utilized instead, but only in those few instances when it is required to meet the cycle pump net positive suction head requirements.

An evaluation of these three conceptual configurations led to the selection of the third design as the Reference reactor-ORCEPS configuration. Using this design, reactor-ORCEPS parametric data depicting the overall performance and design characteristics for each of the candidate working fluids. These data are summarized in Tables 3.13, 3.14, 3.16, and 3.17). In addition, reactor-ORCEPS system mass summary data was prepared to facilitate the conducting of the four Reference systems trade-offs. The major results of these analyses and trade-offs are summarized in Table 2.1 for a nominal 50KW_e power output level.

Table 2.1. Comparison of Reference Reactor-ORCEPS Systems
(50KW_e Power Level)

Working Fluid	Overall System Efficiency (%)	Weight (lb)	Radiator Area (ft ²)	Number of Radiator Sections	Deployed Height (ft)	Specific System Mass (lb/KW _e)
CP-25	19.6	2972	1762	4	68.0	59.4
FL-M	18.6	3192	2000	4	68.0	63.8
CP-28	18.4	3418	2165	5	81.5	68.4
RC-1	16.0	3464	2258	5	81.5	69.3

Based upon these data, the reactor-ORCEPS power system utilizing CP-25 (toluene), was selected as the optimum Reference Design System for the 10 - 75KW_e power level range. However, the system using FL-M (Fluorozene-M), had performance, weight, and volume design characteristics very nearly equal to the CP-25 system. In addition, because its potential turbine performance (velocity ratios) and pump net positive suction head characteristics (suction specific speeds) were somewhat superior to the CP-25 system, it was selected as an alternate Reference Design System which should also be considered during the preliminary design phase of any future specific applications.

An evaluation was made of the potential for standardization of the Reference reactor-ORCEPS configuration. A 50KW_e power level system was selected. The weight and cost penalties associated with downrating this standard system to the 25KW_e and 10KW_e levels and uprating it to the 75KW_e power level

were determined. The weight penalties were summarized in Table 3.29 and Figure 3.91 and the recurring cost penalties shown in Figure 3.95. In general, both the cost and weight penalties were of a sufficiently small magnitude, so that standardization of the reactor-ORCEPS power system at the 50KW_e level is an attractive system effective option for future space applications.

A preliminary Development Plan and Schedule was prepared for the Reference reactor-ORCEPS system. The plan consisted of three phases. Phase I was essentially a prototype development effort with the reactor-NaK loop components and the ORCEPS components designed, fabricated and tested to confirm performance capability of these subsystems. Phase II included integration and ground qualification testing of the overall reactor-ORCEPS power system. Finally, Phase III was a flight qualification program. The reactor-ORCEPS would be installed, checked out, and launched using the Space Shuttle and the Intermediate Upper Stage launch vehicles. The flight qualification program would include confirmation of the reactor-ORCEPS with the launch and orbit injection requirements, orbital start-up, and performance and operating life tests to confirm system reliability. To make the Phase III flight test program more cost effective, it could be combined with any one of several proposed Space Shuttle Experiment Payload programs. A 6 year development program was envisioned with Phase I and II requiring 2-1/2 years each and Phase III being conducted over a one year period. Total RDT&E costs of this program were estimated to be 57 million dollars which would be expended at an average rate of approximately 10 million dollars/year.

2.2 CONCLUSIONS

The results of the 10-75KW_e reactor ORCEPS Power System Study have provided an insight into the achievable performance and overall weight characteristics for these systems. It has shown that while operating at moderate peak cycle temperatures (i.e., 750°F), PCU efficiencies of up to 25% and overall reactor-ORCEPS efficiencies of 20% can be obtained. In addition, by combining this low temperature operation with the use of process fluid lubricated bearings in the turbopump-generator of the PCU, the required operating life of seven years should be readily achievable at minimum development risk.

The CP-25 (toluene) working fluid cycle was found to provide the highest performance levels at the lowest system weights. The range of weights established was 1073 pounds at the 10KW_e power level to 4069 pounds at the 75KW_e level. The Space Shuttle has a low earth orbit (220 n. mi. at 28.5° inclination) payload capability of 65,000 pounds. Hence, the weights determined for the reactor-ORCEPS Reference Design System are only 2 to 6% of this payload capability. This makes the reactor-ORCEPS system an attractive candidate for various future Experiment Payload Missions.

The specific weights currently achievable with the reactor-ORCEPS system varies from approximately 100 lb/KW_e to 50 lb/KW_e over the power level range from 10 to 75KW_e . This is equivalent to 10-20 watts per pound. Since current (1977) solar array/battery systems are only 3.75 watts per pound and are only projected to achieve 7.0 watts per pound by 1980, the reactor powered ORCEPS should remain a very weight competitive system for the next decade, (1980-1990).

The reactor-ORCEPS power system can be standardized at the 50KW_e power level and downrated to the 25KW_e or 10KW_e levels and uprated to the 75KW_e level, without excessive weight or cost penalties. The total weight of the nominal 50KW_e system is approximately 3000 pounds. The downrated 25KW_e system would weigh approximately 2500 pounds (a 600 lb penalty) and the downrated 10KW_e system would weigh 2000 pounds (a 1000 lb penalty). Similarly, the uprated 75KW_e system would weigh 5375 pounds (a 1300 lb penalty). However, by standardizing these systems, approximately \$50M to \$60M of RDT&E costs would be saved by eliminating this non-recurring cost from all but the first system. In addition, the recurring cost penalties would be minimal. At the 10KW_e and 25KW_e levels this would amount to approximately \$500K out of \$4M and at the 75KW_e level the cost penalty would be only \$2M out of \$10M. These costs would not only be offset by the savings in the non-recurring cost; but the time to manufacture and flight qualify a reactor-ORCEPS system in the 10- 75KW_e power range could be reduced from 6 years to 1-1/2 to 2 years. Considerable cost savings would be experienced for any future application by this reduction in the development period.

As a result of this study, the following specific conclusions can be drawn.

1. The reactor-ORCEPS power system using CP-25 as a working fluid and operating at a 750°F peak cycle temperature, is capable of pro-

viding high power ($10\text{--}75\text{KW}_e$) for long durations (7 years) at a modest non-recurring development cost (approx. \$60M over 6 year period) and low development risks.

2. The achievable specific weights of 10 to 20 watts per pound (i.e., 50 to 100 lb/ KW_e) should prove to be an attractive weight range for current and future space applications.
3. By utilizing only 2 to 6% of the Space Shuttle payload capability, the reactor-ORCEPS can furnish high power levels (10 to 75KW_e) with minimum impact on the net allowable payload weights for future DoD and NASA missions.
4. At an estimated recurring cost of only \$80 to \$100 per watt, in the 50 to 75KW_e power level range, the reactor-ORCEPS is a very cost effective power system and should be given serious consideration by future mission planners.
5. The potential for increased hardness and decrease vulnerability of the reactor-ORCEPS systems, can have considerable benefits for power system survivability in view of the increasing threats associated with "killer satellites" using explosive or laser type weaponry.
6. There appears to be additional possibilities for further extension of the standardization of reactor-ORCEPS systems by simply changing the molecular weight of the organic working fluid. With a potential molecular weight range of from 44 (RC-2) to 416 (FC-75) for the various organic fluids evaluated during this study, this approach for achieving commonality is worthy of future consideration.
7. There are many potential missions for the reactor-ORCEPS power system, including sub-sea, remote-site, earth-orbit, and interplanetary applications, so that early consideration should be given to a standardized configuration capable of encompassing a 10 to 75KW_e power range.

3. TECHNICAL DISCUSSION

3.1 RATIONALE FOR SELECTION OF REACTOR POWERED ORCEPS

Nuclear reactor power systems have been proposed for many manned and unmanned space applications (References 1 to 8) during the last two decades. For systems in the 0.5 KW_E to 50 KW_E power range, the uranium (U_{235})-zirconium hydride (ZrH) reactor utilizing a lithium hydride (LiH) shadow shield has been the configuration that has been under active development since 1957. This reactor was the heat source utilized during the early AEC SNAP development programs (i.e., SNAP 2, 8, and 10A). In addition, preliminary design investigations were conducted for the NASA manned Space Station and Lunar base programs and the Air Force MORL and Hard Rock Silo programs. In order to meet the operating life goals (i.e., 5 to 10 years) of these programs, the maximum reactor core temperature was limited to 1300°F . Initial development programs were limited to the use of the mercury Rankine cycle and silicon germanium thermoelectrics as energy conversion systems. Since most unmanned space applications during the 1965 to 1975 time period were in the lower power range (i.e., 0.5 to 3 KW_E), these nuclear reactor power systems were considerably heavier (i.e., 300 to 500 lb/KW_E) than existing solar array-battery systems (i.e., 150 to 250 lb/KW_E). In addition, budgetary limits during this same time period, resulted in postponement of the higher power (i.e., 20 to 100 KW_E) manned space applications (Space Station, Lunar Base). However, with the advent of the Space Shuttle program, renewed interest in manned space applications, (i.e., manufacturing, surgical operations, satellite recovery and repair) and higher powered unmanned applications (i.e., space based radar), a need for power systems in the 10 to 75 KW_E range has emerged. The moderate reactor core temperature (1300°F) U-ZrH reactor when coupled with the continuing development programs which have resulted in substantial improvements in both static and dynamic power conversion units (PCU) (i.e., Brayton, organic Rankine, Stirling, and thermoelectric) emerges as a very attractive candidate electric power system for future manned and unmanned space applications.

There are various power conversion systems that can be considered for use with the U-ZrH reactor heat source to provide 10-75 KW_E of output power. These include thermoelectric, Brayton, Stirling, and organic Rankine cycles (Reference 7). Each system has advantages and disadvantages which are discussed below.

The silicon-germanium thermoelectric system is a static energy conversion system which enhances its potential for high reliability on long life applications. However, even in the lower 10 to 25 KW_E power range, low conversion efficiencies (5 to 6%) result in very heavy systems and large radiator areas. Hence, they are not too attractive in the 10 to 75 KW_E power range.

The recuperated closed Brayton cycle in the 10 to 75 KW_E power level range can achieve high conversion efficiencies (21 to 24%). This results in lighter weight systems and reduced radiator areas. However, these higher efficiencies are only achieved by operating at comparatively high (1300°F) peak cycle temperatures. This results in reduced reactor life (see Figure 3.1) and increased reactor and power conversion system material and fabrication costs. In addition, both the recuperator and radiator are both gas heat transfer coefficient and pressure drop limited (whether working fluid is helium, mixture of helium and xenon, or argon) and hence result in large flow area requirements and large volumes. The compressor (which must be very efficient since it uses up to 60% of the turbine work output) employed in this cycle, is very sensitive to both inlet temperature and pressure and extreme care must be taken to avoid compressor surge and fatigue. Helium diffusion through metals (in those systems using this gas) to the outer space vacuum can also be a problem unless a make-up helium storage accumulator is employed. This adds to the weight and complexity of the system. Finally, the NaK to He/Xe heater is also gas heat transfer coefficient limited and large area, volume, and weight penalties result.

The Stirling power conversion system (particularly in the 10 to 75 KW_E power range) has not been sufficiently developed for space applications. It is unlikely that this system could achieve a technology readiness level before 1987. In addition, it is also a high peak cycle

Figure 3.1. Operating Life versus Reactor NaK Outlet Temperature

temperature (1300°F) system and has all the shortcomings that were discussed above for the Brayton cycle. In addition, it is a reciprocating machine (work piston and displacer piston) using either a complex rhombic drive or a multi-piston swashplate (Rinia arrangement) to convert to a rotary mechanical output. Severe lubricating, heat transfer, and seal leakage problems exist and a heavy, large volume, regenerator is required. The system operates at low speed (4000 rpm) resulting in a very heavy generator weight. If a linear alternator and modify Beale type Stirling engine is employed to eliminate some of the above problems, a low generator efficiency results giving rise to a high weight, large volume energy conversion unit.

The ORCEPS is capable of achieving comparatively high power conversion unit efficiencies (20 to 28%) when operating with fairly low peak cycle temperatures (800°F). This considerably extends the operating life of the U-ZrH reactor (see Figure 3.1) and permits the use of lower cost materials and fabrication techniques. The use of process-fluid bearings insures no-wear, and long operating life (up to 10 years). In addition, the pump work in an organic Rankine cycle is less than 3% of the turbine shaft output power and hence its efficiency is not as critical as the Brayton cycle compressor. Boiling and condensing takes place at constant temperature. This greatly simplifies the design of these heat exchangers and avoids mechanical or brazed-joint failures due to excessive temperature gradients. The pump discharge pressures are of sufficient magnitude (i.e., 150 to 500 psia) that the ORCEPS is not sensitive to moderate pressure drops. Finally, by judicious selection of the cycle working fluid, radiator temperatures up to 250°F can be utilized without any significant effect on overall cycle efficiency. Hence, the required radiator area associated with this lower temperature can be minimized. Therefore, because of its great potential for long operating life and low cost at moderate system weights and radiator areas, the ORCEPS becomes the most attractive candidate system for future near term (1980-1985) space applications.

3.2 REACTOR CHARACTERISTICS

This study was based upon the use of a U_{235} -ZrH nuclear reactor as a heat source. The reactor core maximum operating temperature was limited to 1000°F in order to meet the seven year operating life goal (see Figure 3.1). A typical reactor powered ORCEPS is shown in Figure 3.2. The reactor heat source loop utilizes a sodium-potassium eutectic (NaK) as a heat transfer fluid to transport the thermal energy from the reactor core to the PCU boiler. An electromagnetic pump, powered by a thermoelectric converter is used to circulate the NaK in the liquid metal components loop. An accumulator is provided to allow for the thermal expansion of the NaK from its initial temperature before start-up to its ultimate steady-state operating temperature. A description of the reactor loop components and parametric performance, weight, and volume data for them follows.

3.2.1 Reactor Design

The reactor design is essentially an adaptation of the existing and test-proven SNAP space reactor. A layout of a typical reactor in approximately the 200 KW_T range is shown in Figure 3.3. At this thermal power level, the reactor core would contain 85 fuel elements, each 0.875 inches in diameter and 20.875 inches in length. Each assembled fuel element consists of an outer cladding tube (Hastelloy) which has been chromized, straightened, diffusion treated and coated and which is swagged over an internal fuel rod. The fuel rod is a uranium-zirconium mixture, hydrided to a hydrogen content of 6.3×10^{22} atoms of hydrogen/cc. The fuel elements are held in place between two grid plates within the core vessel of the reactor. The NaK heat transfer and coolant fluid flows interstitially between the fuel elements. The complete core assembly is supported by six tie rods mounted to the lower head of the reactor vessel.

The reflector consists of six semicircular movable beryllium control drums and six tricuspid-shaped, fixed beryllium filler pieces. The reactivity control system regulates the neutron leakage. This is accomplished by rotating the movable drums which have a 4.5 inch radius, are 5.125 inches thick and 22 inches long. Void filled pieces are located

Figure 3.2. Typical Reactor-Organic Rankine Cycle Electric Power System (ORCEPS) Schematic

on the back side of the drums to prevent criticality in the event of an accidental water-immersion incident occurrence. The six beryllium control drums are attached to shafts that are rotated by electromagnetic actuators. The rotary position of a control drum is sensed by the output of a potentiometer which is connected to the main control shaft. The actuator and the position-sensing potentiometer are located above the containment vessel top head for access during ground test.

3.2.2 Reactor Parametric Data

The reactor thermal output power requirements will vary with the net electrical power output of the system. For an estimated overall reactor-ORCEPS efficiency range of 15 to 20%, and an output power range of 10 to 75 KW_E , the thermal output power requirements range becomes 50 to 500 KW_T . Hence, the parametric reactor weight and volume data was prepared to cover this range of values. These data are shown on Figure 3.4, together with typical reactor geometries (i.e., vessel height and midplane diameter). It should be noted that at 50 KW_T the reactor weight is 400 pounds. At 400 KW_T , the reactor weight increases to only 700 pounds despite an eight fold increase in thermal output power. This is

Figure 3.3. Typical Reactor Assembly

Figure 3.4. Reactor Weight and Reactor Volume versus Reactor Power

an important factor regarding standardization of the reactor-ORCEPS since for an assumed overall efficiency of 20%, less than a 300 pound reactor weight penalty is incurred by using a reactor sized for 75 KW_E at the 10 KW_E power level.

3.2.3 Electromagnetic (E/M) Pump

The (E/M) pump is a non-moving, static induction pump. The pump has a permanent magnet to provide the magnetic flux, and integral thermoelectric elements to produce the cross electrical current (i.e., $\mathbf{J} \times \mathbf{b}$ force) that results in a net pump power. Extensive experience with this type pump has been demonstrated in the SNAP 10A and 2 programs. The past experience with this NaK (E/M) pump is summarized in Table 3.1. The longest endurance run for a SNAP 10A type pump was 20,000 hours. This pump proved to be quite reliable.

Table 3.1. NaK (E/M) Pump Test Experience

<u>Category</u>	<u>SNAP 10A Pump</u>	<u>SNAP 2 Pump</u>
No. of pumps fabricated	41	16
Total test hours	194,000 hrs	18,000 hrs
Longest endurance run	20,000 hrs	6100 hrs
Operating NaK temperature	1010°F	1200°F

The required NaK pump flow rate for a required reactor thermal power is shown on Figure 3.5 for a nominal PCU efficiency of 20.5%. For different PCU efficiencies, the flow rate was determined by ratioing the actual PCU efficiency to this nominal value. Additional NaK pump parametric data are provided in Figures 3.6 to 3.10 as a function of reactor thermal power. These data were utilized to provide input information to Tasks 5 (Parametric Data) and 6 (Candidate Power Systems) of this study.

3.2.4 Thermoelectric Converter

Two state-of-the-art PbTe thermoelectric converter designs were considered during the study. They are shown in Figures 3.11 and 3.12. The cylindrical module is manufactured by Minnesota Mining and

Figure 3.5. Reactor Loop NaK Pump and Piping Flow versus Reactor Thermal Power

Figure 3.6. NaK Pump Pressure Rise versus Reactor Thermal Power

Figure 3.7. NaK Pump Efficiency versus Reactor Thermal Power

Figure 3.8. Reactor Loop NaK Pump Input Power versus Reactor Thermal Power

Figure 3.9. Reactor Loop Components Mass versus Reactor Thermal Power

Figure 3.10. Reactor Loop NaK Pump Volume versus Reactor Thermal Power

Figure 3.11. Cylindrical Thermoelectric Module Design

Figure 3.12. Tubular Thermoelectric Module Design

Manufacturing Company with couples arranged in series. Multiple couples are used in several rows to provide the required output power. This also enhances the design reliability due to redundancy. Each thermoelectric element has an aluminum follower bar compressively loaded directly against the copper conduction shoe to provide good thermal contact. An aluminum oxide hardcoat on the aluminum follower bar forms the cold-side electrical insulation. A stainless-steel transition ring is vacuum nickel-brazed into each end of the copper conduction shoe. These transition rings contain the electrical output terminal and the fill gas pinch-off tube.

The alternate design considered is a tubular module, manufactured by the Westinghouse Astronuclear Laboratory, which uses series-connected couples in the form of washers. The p- and n-type thermoelectric material washers are separated by thin washers of natural mica electrical insulation. Hot- and cold-side tungsten and molybdenum conduction rings span adjacent lead telluride washers to form a separate current flow path. The current is electrically isolated from the structural claddings by thin sleeves of boron nitride, which also provide good thermal contact between the rings and cladding. Approximately 10% redundancy of the converter modules is provided as a failure contingency.

The thermoelectric module uses a portion of the NaK flow being circulated by the (E/M) pump in the reactor thermal transfer loop. The required flow rate varies depending upon the input power required by the (E/M) pump. This is a function of the reactor thermal power required and is shown in Figure 3.13. Additional thermoelectric module data is provided in Figures 3.14 to 3.17.

3.2.5 Volume Accumulator and NaK Piping

The NaK loop includes a volume accumulator and interconnecting piping. These are made of stainless steel to be compatible with the high temperature NaK fluid. The piping consists of the main NaK supply and return lines, upper and lower NaK headers, and individual lines connecting the thermoelectric modules. Differential thermal expansion between the headers and the thermoelectric converter modules is taken

Figure 3.13. T.E. Module NaK Flow versus Reactor Thermal Power

Figure 3.14. T.E. Module Efficiency versus Reactor Thermal Power

Figure 3.15. Reactor Loop T.E. Module Input Power versus Reactor Thermal Power

Figure 3.16. Reactor Loop T.E. Module Output Power versus Reactor Thermal Power

Figure 3.17. Reactor Loop T.E. Module Volume versus Reactor Thermal Power

up in expansion loops. The volume accumulator maintains the system operating pressure within safe design limits. It also provides the space to accommodate changes in the NaK volume for initial start-up to steady-state operating temperatures. The reactor loop volume accumulator weight and volume as a function of reactor thermal power is provided in Figures 3.18 and 3.19. The weight of the reactor loop NaK piping (including headers) is provided in Figure 3.20. The estimated reactor loop pressure drop in the NaK piping and boiler are shown in Figure 3.21. Actual pressure drops would be maintained within these values during detail design of the NaK loop flow passages.

3.2.6 Shielding

The reactor heat source subsystem contains integral shielding. This is located between the reactor core and the NaK boiler. The purpose of the shielding is to reduce the neutron flux to the required cumulative radiation level at the dose plane. These levels are 10^{13} nvt of neutrons and 10^7 rads of gamma rays at the end of a seven year period. The shield material selected is lithium hydride (LiH) which has a hydrogen density of 5.9×10^{22} atoms/cc. The other properties of LiH are given in Table 3.2.

Table 3.2. Properties of Lithium Hydride	
Molecular weight	7.948
Density	0.775 gm/cc
Hydrogen density	5.9×10^{22} atoms/cc
Specific heat	0.96 Btu/lb- °F at 68°F 1.67 Btu/lb- °F at 1000°F
Dissociation pressure	27 mm Hg at 1270°F
Coefficient of thermal expansion	$\sim 23 \times 10^{-6}$ °F ⁻¹
Thermal conductivity	2.5 Btu/(hr)-(ft ²)(°F/ft)

The dose plan diameter is a function of the shielding cone half angle, θ , and the separation distance, S. This is shown schematically in Figure 3.22. To provide the largest dose plane diameter, in order to accommodate as many potential space applications as possible, the 12° cone angle was selected for this study (Figure 3.23). While this results in a small

Figure 3.18. Reactor Loop Volume Accumulator Mass versus Reactor Thermal Power

Figure 3.19. Reactor Loop Volume Accumulator Volume versus Reactor Thermal Power

Figure 3.20. Reactor Loop NaK Piping Mass
versus Reactor Thermal Power Output

Figure 3.21. Reactor Loop Boiler and NaK Piping Pressure Drop versus Reactor Thermal Power

$$\tan \theta = \frac{D_3 - D}{2S + L}$$

$$D_1 = D + 2L \tan \theta$$

$$D_2 = D_1 + 2T \tan \theta \quad V_{\text{SHIELD}} = \frac{\pi}{24} \left[D_2^3 - D_1^3 \right] \frac{1}{\tan \theta}$$

Figure 3.22. Space Reactor - Payload Shadow Shield Configuration

Figure 3.23. Dose Plane Diameter versus Separation Distance

weight penalty for the reactor-ORCEPS power systems, the potential cost benefits due to standardization more than offset it from an overall system effectiveness viewpoint. The separation distance effects both dose plane diameter and system weight. The shield thickness is determined by the separation distance and reactor thermal power (Figure 3.24). The values selected for this study are shown in Table 3.3. Should larger dose plane diameters be required, these can be achieved by increasing the separation distance. While a weight penalty of approximately 2 lb/ft is incurred, this is offset to a degree by a reduction in the shield thickness requirement.

Table 3.3. Separation Distance and Dose Plane Diameters For Various Power Levels (12° Cone Half Angle)

<u>Net Electrical Power, (KW_E)</u>	<u>Separation Distance, (ft)</u>	<u>Dose Plane Diameter, (ft)</u>
10	30	15
25	50	22
50	60	28
75	100	44

Parameter values of shadow shield weight and volume were determined during this study. The results are shown on Figures 3.25 to 3.32. Figures 3.28 and 3.32 depicting values for a 12° cone half angle were used to determine the weight and volume design characteristics for the candidate power systems.

3.2.7 Operating Life

The operating life of the reactor is a function of the reactor thermal power and NaK outlet temperature. This was depicted previously in Figure 3.1. During this study, the maximum reactor outlet temperature was limited to 850°F (455°C). Hence, for any reactor thermal power of 600 KW_T or less, a total operating life of 7 years (based on initial fuel-clad contact) should be readily achievable. This conservative value also permits growth potential up to 100 KW_E ($\eta_0 = 0.17$) for higher power space applications.

Figure 3.24. Shield Thickness versus Separation Distance

Figure 3.25. Shadow Shield Weight versus Separation Distance 6° Cone Half Angle

Figure 3.26. Shadow Shield Weight versus Separation Distance
 8° Cone Half Angle

Figure 3.27. Shadow Shield Weight versus Separation Distance 10° Cone Half Angle

Figure 3.28. Shadow Shield Weight versus Separation Distance 12° Cone Half Angle

Figure 3.29. Shadow Shield Volume versus Separation Distance 6° Cone Half Angle

Figure 3.30. Shadow Shield Volume versus Separation Distance 8° Cone Half Angle

Figure 3.31. Shadow Shield Volume versus Separation Distance 10° Cone Half Angle

Figure 3.32. Shadow Shield Volume versus Separation Distance 12° Cone Half Angle

3.3 ORCEPS CHARACTERISTICS

The ORCEPS systems are characterized by the use of high molecular weight (HMW) organic working fluids. These fluids are ideally suited for use with space reactor powered systems in the 10 KW_E to 75 KW_E range. Because of the positive slope of the saturated vapor line on a temperature-entropy diagram, expansion through the turbine results in increasing superheat. This not only avoids the possibility of turbine blade erosion experienced in steam cycles (due to moisture in turbine exhaust vapors), but also permits the use of regeneration. This results in being able to achieve higher efficiencies than steam at lower peak cycle temperatures when using a single turbine stage. In addition, because of the smaller amount of energy per pound of working fluid as compared to steam, a higher cycle flow rate is required for a given amount of work output. This higher flow rate when combined with the lower isentropic head, results in a higher turbine specific speed. In addition, the lower peak cycle temperatures associated with organic fluids permit operation at higher rotative speeds. This also increases the turbine specific speed. Finally, because of the lower nozzle efflux velocities resulting from the reduced isentropic heads, a higher velocity ratio is obtainable. All these factors result in a higher turbine efficiency which in turn permits the achieving of a higher overall cycle efficiency.

There are several other factors that often are also pertinent in the use of ORCEPS for space applications. Typical of these are:

- a) The use of higher peak cycle temperatures to obtain higher Carnot and hence overall efficiencies
- b) The impact of zero "g" operation on the system
- c) The effects of pyrolytic and radiolytic decomposition on system operation
- d) The availability of test data confirming the operating life of multifoil, compliant, process fluid lubricated hydrodynamic bearings utilized
- e) The potential for standardization and commonality at various output power levels

Each of the above factors are discussed below to establish their effects on the use of ORCEPS type electric power systems.

3.3.1 Peak Cycle Temperatures

The Carnot efficiency of a heat engine cycle is given by the expression,

$$\eta_c = \frac{T_1 - T_2}{T_1}$$

where T_1 is the upper temperature limit at which energy conversion occurs and T_2 is the lower temperature limit at which heat is being rejected.

For an ideal Rankine cycle the thermal efficiency (η_i) is defined as the heat (w_i) transformed into work per pound of working fluid (w_i) divided by the heat furnished by the boiler per pound of working fluid (Q_1). This is designated as,

$$\eta_i = \frac{\text{Ideal work output}}{\text{Net Heat Input}} = \frac{W_i}{Q_1}$$

However, defining the efficiency in this manner is misleading since it does not take into account the effects of regeneration and superheating. Hence, it is customary for cycles using engines or turbines to define their efficiency based on the net available heat utilized. This is the so-called engine efficiency (η_e) and is designated as,

$$\eta_e = \frac{\text{Ideal work output}}{\text{Net Heat Input}} = \frac{W_i}{Q_a - Q_r}$$

where Q_a is the available heat (including superheat requirements) and Q_r is the turbine exhaust waste heat available for regeneration. Thus, it can be seen that by increasing the superheat in an ideal Rankine cycle (for given boiling and condensing pressures) the ideal work output is increased but only at the expense of adding additional heat. Hence, the overall efficiency of the heat input components (i.e., heat source, boiler, superheater, and insulation) must be greater than the turbine efficiency, if any increase in overall engine efficiency is to result. Since Rankine cycles employing high temperatures (i.e., 1000 to 1600°F) and a high degree of superheat may experience increased thermal losses, the net gain in overall engine efficiency may be marginal. Hence, despite the increase in the Carnot efficiency of the cycle, the resulting increase in the Rankine cycle efficiency may be negligible. The Q_r term, however, in the above equation can have a significant impact on the

overall engine efficiency. By utilizing the turbine exhaust waste heat, the overall heat input to the boiler-superheater can be reduced. However, in steam Rankine cycles, even with large amounts of superheat introduced to the heat input side of the cycle, little or no superheat at the turbine exhaust is available for regeneration. However, most HMW organic working fluids provide a high degree of superheat at the turbine exhaust which is then available for regeneration. In addition, this efficiency improving cycle operation can be achieved with relatively small amounts of superheat from the boiler. Thus, the increase in the Q_a term (heat input) in the engine efficiency equation, to achieve a significantly large value of Q_r (regeneration), is relatively small. The overall impact of utilizing HMW organic working fluids in a regenerative Rankine cycle, is that relatively high engine efficiencies can be achieved at moderate peak cycle temperatures.

The actual Rankine cycle efficiency ($\eta_{e \text{ actual}}$) is defined similar to the ideal Rankine cycle efficiency except that a term, W_a (actual work output) is substituted for W_i (ideal work output), so that

$$\eta_{e \text{ actual}} = \frac{\text{Actual Work Output}}{\text{Net Heat Input}} = \frac{W_a}{Q_a - Q_r}$$

The actual work output is equal to the product of the ideal work output and the turbine efficiency minus the pump work and other energy conversion system losses (i.e., bearings, seals, windage). Thus, the W_a term becomes,

$$W_a = (W_i \times \eta_t) - (W_p + W_B + W_s + W_w)$$

where η_t is the turbine efficiency. The pump work (W_p) is a small percentage of the ideal work (2 to 5%) and the bearing, seal and windage losses are also relatively small. Hence, the turbine efficiency is the most sensitive parameter in establishing the actual work output.

If a generator is employed to provide an electrical rather than a mechanical output, the W_a term must be further reduced by the efficiency of the generator, so that

$$W_{a \text{ electrical}} = W_a \times \eta_g$$

where η_g is the generator efficiency. The generator efficiency includes electromagnetic losses, I^2R losses, windage losses and rectification losses (if D.C. power is desired).

Hence, the overall efficiency of an ORCEPS system can be defined as,

$$\eta_o = \frac{\text{Net Electrical Power Output}}{\text{Net Heat Input}} = \frac{W_{\text{electrical}}}{Q_a - Q_r}$$

In determining the value of Q_a above, all the thermal losses in the heat source, boiler, and superheater, must be taken into consideration. In a well insulated system, all these losses can be kept to less than 10% of the total available heat input.

The peak cycle temperature range available for ORCEPS systems is 500° to 800°F. The upper value is limited primarily by constraints imposed by pyrolytic decomposition and the desired operating life of the system. The Carnot cycle efficiencies achievable over this peak cycle temperature range is a function of the heat rejection (i.e., condensing temperature). These are shown in Figure 3.33 for a range of heat rejection temperatures (100° to 400°F). The parametric values for percent of Carnot efficiencies achievable are based upon typical ORCEPS cycle and component data summarized in Table 3.4.

Table 3.4. Typical ORCEPS Cycle and Component Data

Working Fluid	Toluene ($C_6H_5-CH_3$)
Peak Cycle Temperature	700°F
Condensing Pressure	12 psia
Condensing Temperature	225°F
Radiator Emissivity	0.90
Regenerator Effectiveness	0.95
Turbine Efficiency	0.80
Pump Efficiency	0.65
Generator Efficiency	0.85
Cycle Efficiency	0.205
Specific Radiator Area	10 ft ² /KW _T

Figure 3.33. Carnot Cycle Efficiency versus Condensing Temperature

3.3.2 Zero-G Operation

The ORCEPS system using organic working fluids operates in a closed cycle fashion with turbine back pressures in the 4 to 25 psia range. Under these conditions the system, which includes a two-phase flow condenser, is capable of satisfactory operation in a zero-gravity environment, in an acceleration field of varying magnitude and vectorial orientation, or within a constant gravitation field of varying orientation. The pressure drop needed to establish stable two-phase flow within the condenser is adequately met by this turbine back pressure range.

Various tests and analytical procedures have been carried out (Refs. 9 & 10) to confirm the adequacy of the ORCEPS systems to operate under zero-"g" conditions. The design techniques and experience gained during these previous programs assure that the ORCEPS systems will operate satisfactorily under any of the gravitational modes imposed upon it.

3.3.3 Working Fluid Selection

In a Rankine cycle power conversion system, the working fluid plays an important role in determining the performance of the system. The thermodynamic, chemical and physical properties of the fluid have a strong influence on the turbopump system efficiency, materials of construction, component weight and size, toxicity hazards, and to some degree the total system cost.

Approximately 100 working fluids were initially chosen for screening and initially reduced to 14. Table 3.5 summarizes the various organic Rankine cycle working fluid properties. From these a total of nine working fluids were then selected for more critical screening and cycle analyses trade-offs. In addition to their desirable fluid properties, these fluids were selected either on the basis of their use in current organic Rankine cycle development programs or because they were among the most promising that have been considered on previous development programs. The nine candidate fluids selected were CP-25, CP-28, CP-34, RC-1, PP-2, FL-M, FC-75 and Dowtherm A. Detailed analyses of these fluids and cycle conditions were then initiated to provide programmatic alternatives and to assure achievement of performance objectives. To accomplish this, specific guidelines were established for the working fluid screening process. These guidelines are set forth in a range of parametric values as shown in Table 3.6. Figures 3.34 and 3.35 were utilized to evaluate the various fluid condensing pressures as a function of condensing temperature to determine which stayed within the condenser pressure guidelines.

An ideal working fluid for a Rankine cycle engine should have the following characteristics:

- Thermal and radiolytic stability
- Non-corrosive with materials of construction
- Vapor pressure of condensate should not be too low so as to result in pump cavitation problems or reduced turbine efficiency.
- The saturated vapor line on the temperature-entropy (T-S) diagram should be either vertical or have a positive dt/ds slope so that isentropic expansion results in superheat available for regeneration.
- Non-toxic
- Low cost and availability

Table 3.5. Organic Rankine Cycle Working Fluid Properties

No.	Fluid Name	Chemical Structure	Molecular Weight	Critical Temperature °F	Critical Pressure psia	Boiling Temperature °F	Melting Point °F
1.	Toluene (CP-25)	$C_6H_5-CH_3$	92.13	609.4	592.2	231	-140
2.	Hexafluorobenzene (CP-28)	C_6F_6	186	470	474	177	42
3.	Thiophene (CP-34)	C_4H_4S	84.13	584.6	826	184	-37
4.	PP-1	Perfluoro(-n-hexane) C_6F_{14}	338	351	266	135	-125
5.	PP-2	Perfluoro(methyl-cyclohexane) C_7F_{14}	350	415	293	169	-36
6.	PP-3	Perfluoro(1, 3 - dimethylcyclohexane) C_8F_{16}	400	465	272	216	-67
7.	Fluorinol-85	85 mole % CF_3CH_2OH 15 mole % H_2O	87.74	452	800.14	166	-82
8.	Fluorozene M	Hexafluorometaxylene $C_6H_4-(CF_3)_2$	214	523.3	366	240	-52
9.	FC-75	Perfluoro(2-butyl tetrahydrofuran) $C_8F_{16}O$	416	440	231	210-225	-120
10.	RC-1	60 mole % C_6F_5H 40 mole % C_6F_6	175.3	440	356.9	172	-44
11.	RC-2	65 mole % H_2O 35 mole % $2CH_3Py$	44.3	560	1516	200	-40
12.	2-Methylpyridine	$2CH_3Py$	93.1	658	667.4	264	-87
13.	Monochlorobenzene	C_6H_5Cl	112.50	678.6	655.6	270	-50
14.	Dowtherm A	73.5 wt. % $C_{12}H_{10}O$ 26.5 wt. % $C_{12}H_{10}$	166	927	454.7	495	53.6
15.	Steam	H_2O	18	705.7	3207.5	212	32

Py = Pyridine - N:CHCH:CHCH:CH

Table 3.6. Range of ORCEPS Parametric Values

Net Electrical Power	10 to 75 KW _e
Peak Cycle Temperature	600 to 750°F
Turbine Efficiency	65 to 85%
Pump Efficiency	65 to 75 %
Boiler Efficiency	95%
Regenerator Efficiency	95%
Generator Efficiency	87%
Turbine Pressure Ratio	< 50 to 1
Condenser Temperature	160 to 250°F

The most commonly used fluid in the Rankine cycle is steam. However, for low power level systems (i.e., 10-75 KW_e) steam is not a very attractive fluid, particularly for use with small single-stage turbines. Its very high nozzle efflux velocity, for a moderate temperature drop, results in impractically high rotor speeds in order to achieve a reasonable level of efficiency. An additional shortcoming of steam is that unless a high degree of superheat is employed, the expansion process can result in entering the wet region of the working fluid thermodynamic properties which could cause turbine blade erosion.

The fluids most capable of meeting the most desirable combination of cycle characteristics are broadly characterized as high molecular weight organic working fluids. These fluids provide a low nozzle efflux velocity and a comparatively large mass flow rate through the nozzle. This enables a satisfactory matching between the nozzle gas exit velocity and the rotor tip speed at moderate turbine rotative speeds and results in higher single-disc vapor turbine efficiency.

Figure 3.34. Condensing Pressure as a Function of Condensing Temperature for Candidate ORCEPS Working Fluids

Figure 3.35. Normalized Vapor Pressures (CP-25 = 1.0) versus Vapor Pressure for Candidate Working Fluids

High molecular weight of the working fluid is desirable primarily to reduce the required blade speed of a turbine. It is possible to show that the nozzle velocity for a given temperature drop in one stage is approximately inversely proportional to the square root of the molecular weight of the fluid. The optimum blade speed is about one-half the nozzle velocity. Whereas a steam turbine usually requires several stages to keep the enthalpy drop per stage low, to keep the turbine rotor speed within the bounds of acceptable centrifugal force stresses, a turbine using a high molecular weight fluid would need only one stage. Since nozzle velocities are lower for high molecular-weight fluids, and since the enthalpy drop per pound is also approximately inversely proportional to the molecular weight, the mass flow rate circulated must be greater for a given output.

Another attractive feature of these fluids is that the slope of the saturated vapor line in the temperature-entropy (T-S) diagram is either vertical or has a positive dt/ds slope. This not only insures that isentropic expansion does not result in "wet" vapor, but in an actual cycle, permits the use of a significant degree of "cycle efficiency-improving" regeneration without the need for excessive superheating in the boiler.

3.3.3.1 Regenerative Rankine Cycle

The closed Rankine cycle conversion system and its cycle processes superimposed on a temperature-entropy diagram are shown in Figure 3.36. The fluid shown has a positive sloping saturated vapor line which is typical of most of the organic working fluids. This is a significant factor since it results in a superheated vapor leaving the turbine. A negative saturated vapor slope increases the moisture content in the fluid leaving the turbine. An increase in vapor "wetness" in the low-pressure stages of the turbine decreases turbine efficiency and also causes erosion of the turbine blades which would be a problem for long life operation. A description of the cycle is as follows.

Starting at state point 1, the fluid enters the turbine as a superheated vapor. The superheated vapor expands through the turbine, producing mechanical energy which is converted into shaft power, and exits at point 2 conditions. If the turbine exhaust at point 2 is superheated (as in the case of a fluid having a positive slope saturated vapor line) it enters the vapor side of a regenerator, where some of this superheat energy is transferred to the boiler

Figure 3.36. Flow and T-S Diagram of a Regenerative Rankine Cycle Engine

feed liquid, before entering the condenser at point 3 where the system waste heat is rejected by desuperheating and condensing to a saturated liquid at point 5. At the condenser outlet, the condensate is subcooled. The fluid enters the impeller pump at point 6 and exits at point 7 as a high pressure, subcooled liquid and then enters the liquid side of the regenerator. The liquid exits the regenerator at point 8 and enters the boiler. Heat is added in the boiler and the fluid becomes a saturated liquid at point 9, a two-phase fluid between points 9 and 10, a saturated vapor at point 10, and finally a superheated vapor at point 1, completing the cycle.

3.3.3.2 Parametric Organic Rankine Cycle Analysis Program

TRW's Parametric Organic Rankine Cycle Analysis Program (PORCAP) was developed to perform analytical tradeoff studies associated with the design of power systems for biological, terrestrial, undersea, airborne, and space applications.

Input data required to run the program includes:

- Fluid Properties
- Turbine Inlet Pressure (psia)
- Turbine Outlet Pressure (psia)
- Condenser Temperature (°F)
- Subcooling Temperature (°F)
- Regenerator Effectiveness
- Turbine Inlet Temperature (°F)
- Turbine Efficiency
- Power Output (watts)

The program was written so that pertinent performance parameters could be varied to determine their sensitivity and impact on resultant system trade-offs. Program outputs include:

- Thermal Input Required (watts)
- Efficiency
- Turbine Work (watts)
- Pump Work (watts)
- Working Fluid Flowrate (lbs/hr)
- Turbine Speed (rpm)

- Turbine Diameter (in.)
- Turbine Specific Speed
- Turbine Specific Diameter
- Turbine Outlet Temperature (°F)
- Regenerator Hot Side Outlet Temperature (°F)

The basic program operation and calculation sequence are as follows:

- (1) Read fluid properties and program constants
- (2) Calculate turbine ΔH
- (3) Calculate pump ΔP
- (4) Calculate system flowrate
- (5) Determine turbine specific speed and rotative speed
- (6) Determine turbine specific diameter and outside diameter
- (7) Calculate turbine tip speed and spouting velocity
- (8) Calculate turbine outlet properties (enthalpy, temperature, entropy)
- (9) Calculate regenerator hot side outlet fluid conditions (temperature, enthalpy, entropy)
- (10) Calculate enthalpy at pump inlet
- (11) Calculate enthalpy at boiler inlet
- (12) Calculate thermal power requirements, turbine work, pump work, generator work
- (13) Calculate overall cycle efficiency
- (14) Output results

The nine selected working fluid candidates, CP-25, CP-28, CP-34, RC-1, PP-2, FL-85, FL-M, FC-75 and Dowtherm A, were then analyzed in greater detail, utilizing TRW's PORCAP program. In order to investigate the characteristics of the various fluids, and to evaluate each, standard cycle analyses were performed. Physical property data and thermodynamic tables were utilized for each of the candidate fluids. Use was also made of the various pressure-enthalpy (P-H) and enthalpy-entropy (H-S) diagrams for these candidate fluids. In addition to the range of parametric values listed in Table 3.5, the following guidelines were established for the working fluid process:

- Boiler inlet temperature (T_1) - The boiler inlet temperature was set at a maximum of 800°F unless thermal stability data suggested a lower peak cycle temperature. This temperature is considered practical for

long term thermal stability operation for most of the candidate working fluids. However, a 1000°F boiler inlet maximum temperature was set for CP-28 to reflect recent favorable thermal testing of this fluid by Imperial Smelting Corp. of England. (Reference 11). The choice of a nominal turbine inlet temperature of 750°F coupled with a nominal condenser temperature of 200°F results in a Carnot efficiency of:

$$\eta_c = \frac{T_h - T_c}{T_h} = 0.455$$

- Turbine Pressure Ratio (P_T Ratio) - The turbine pressure ratio of 50 to 1 was chosen as a maximum value so as to remain within the desirable range for achieving high efficiencies with single stage turbines.
- Critical Pressure and Temperature (P_c , T_c) - State points for all of the fluids were maintained below the critical temperatures and pressures of each fluid because of limited thermodynamic data above these values.

Figures 3.37 through 3.46 reflect results of TRW's PORCAP computer runs using CP-25, CP-28, CP-34, RC-1, PP-2, FL-85, FL-M, FC-75 and Dowtherm A; respectively, as the working fluids. The cycle and turbine efficiency data assumes an efficiency of 65%, 95% and 95% for the pump, regenerator and boiler components respectively. In addition, a minimum of 5°F subcooling was assumed for the condenser outlet to assure avoidance of any problems concerned with pump cavitation.

3.3.3.3 Pyrolytic Decomposition

A U-ZrH reactor powered ORCEPS system is unique in that the maximum turbine inlet temperature is not restricted by core temperature limitations. In fact the 650°F to 800°F turbine inlet temperature range selected for ORCEPS systems is very compatible with the core upper temperature limitations. Turbine inlet temperatures are established by the pyrolytic (thermal) decomposition rate of the organic working fluid. Test data for a wide variety of organic working fluids are shown in Figure 3.47. These data, which are based upon the bulk thermal stability tests where the fluid is maintained continuously at the peak cycle temperature is conservative for predicting actual

WORKING FLUID-CP25

Figure 3.37. Cycle Efficiency versus Turbine Efficiency (CP-25)

WORKING FLUID-CP28

Figure 3.38. Cycle Efficiency versus Turbine Efficiency (CP-28)

WORKING FLUID-CP34

Figure 3.39. Cycle Efficiency versus Turbine Efficiency (CP-34)

WORKING FLUID FL-M

Figure 3.40. Cycle Efficiency versus Turbine Efficiency (FL-M)

WORKING FLUID- RC1

Figure 3.41. Cycle Efficiency versus Turbine Efficiency (RC-1)

WORKING FLUID-FL85

Figure 3.42. Cycle Efficiency versus Turbine Efficiency (FL-85)

WORKING FLUID-DT A

Figure 3.43. Cycle Efficiency versus Turbine Efficiency (DT A)

WORKING FLUID-DTAK

Figure 3.44. Cycle Efficiency versus Turbine Efficiency (DTA-KIPS)

WORKING FLUID- PP2

Figure 3.45. Cycle Efficiency versus Turbine Efficiency (PP-2)

WORKING FLUID-FC75

Figure 3.46. Cycle Efficiency versus Turbine Efficiency (FC-75)

Figure 3.47. Thermal Decomposition of Typical High Molecular Weight Working Fluids

cycle decomposition rates. For example, in an actual cycle, the working fluid is circulating and is at the peak cycle temperature only a small percentage of the system operating time. Thus, for 100,000 hours of operation, the total decomposition over the ORCEPS peak cycle temperature range of 650°F to 800°F would vary from 0.005% to 0.8% using the upper range of values from Figure 3.47. Hence, it can be seen that by selecting a low peak cycle temperature, the potential problem associated with thermal decomposition would be negligible. For a 750°F temperature, the maximum fluid decomposition anticipated over a 7 year period ($\sim 60,000$ hours) would be approximately 0.06%. This would have no significant effect on overall system performance.

Considerable attention has been given to the problem of selecting working fluids for a variety of Rankine cycle power applications in recent years. Assessment of thermal stability has become a major undertaking in these studies, resulting in a large quantity of thermal decomposition data for several hundred organic compounds.

The ability of organic materials to withstand high temperature depends, first of all, on the chemical structure of the organic material. In practice, however, secondary factors, such as the presence or absence of impurities or the types of metals and non-metals present, sometimes strongly affect the ability of the organic fluid to withstand the thermal environment. In this regard it is useful to consider the term, thermochemical stability, which is not synonymous with thermal stability. The latter is largely independent of the system in which the property is measured. Thermochemical stability, on the other hand, is used to denote the stability of a fluid in its operating environment. This environment generally includes the presence of metals, lubricants, and contaminants. Thermal stability can be estimated, calculated, or measured with a reasonable degree of accuracy, whereas thermochemical stability generally requires experimental determination under conditions simulating the actual operating system.

Numerous methods have been employed in the past to measure thermal stability of fluid products. The most widely used today involve some adaptation of the isoteniscope vapor pressure method. To measure thermal decomposition temperatures, advantage is taken of the fact that the most general change in properties attending thermal decomposition of organic molecules is an increase in pressure. Using this approach, decomposition rates have been determined and documented.

3.3.3.4 Radiolytic Decomposition

Radiolytic degradation rates for organic materials are generally expressed in terms of "G" values; molecules degraded/100 ev of energy absorbed in the material by gamma or neutron interactions. The "G" values for different materials vary greatly depending on the type of material and reaction taking place. The "G" values for a given material are also generally dependent on the type of radiation, energy of the radiation, and, at very high dose rates, on the dose rate.

The most stable organic materials to high energy radiation are the aromatics; those containing a benzene ring. The aromatic molecules generally provide increased thermal stability as well as radiation stability. For example, the fluid hexafluorobenzene (CP-28) is a fluorinated aromatic and therefore would be expected to show excellent stability with respect to gamma and fast neutron radiation. Some radiation stability measurements have been made by the Sperry Gyroscope Company and they determined the following "G" values for CP-28 exposed to gamma irradiation:

$$\begin{aligned} G (\longrightarrow \text{Polymer}) &= 2.01 \frac{\text{molecules}}{100 \text{ ev}} \\ G (\longrightarrow \text{Gas}) &= 0.001 \frac{\text{molecules}}{100 \text{ ev}} \end{aligned}$$

The irradiation results in cross-linking to form higher molecular weight (higher boiling) materials than the hexafluorobenzene. The gas formation rate is very low, probably due to the back-reaction of any fluorine produced forming saturated halocyclics. G values for aromatic hydrocarbons such as biphenyl and the terphenyls are in the range of 0.15 - 0.20 molecules degraded/100 ev absorbed when irradiated at a temperature of 600°F with mixed neutron/gamma irradiation.

It is instructive to consider the relationship between the fraction of original molecules affected over a specified period and the "G" value of the material. The number of molecules affected can be written as:

$$\frac{\text{Affected Molecules}}{\text{gm}} = \left(\frac{F}{100} \right) \left(\frac{6.025 \times 10^{23}}{M} \right) = \left(\frac{G}{100} \right) (D) \left(\frac{1}{1.60 \times 10^{-12}} \right)$$

Where

F = percentage of original molecules affected by radiolytic change

M = molecular weight of material

G = molecules degraded/100 ev

D = total absorbed irradiation dose, ergs/gram

The required dose for a given F can thus be expressed as:

$$D = \left(\frac{F}{100} \right) \left(\frac{6.025 \times 10^{23}}{M} \right) \left(\frac{(1.60 \times 10^{-12})(100)}{G} \right) = 9.65 \times 10^{11} \frac{F}{MG} \text{ erg/gm}$$

For CP-28, M = 186

$$D = \frac{5.2 \times 10^9 F}{G} \text{ erg/gm}$$

Assuming an F of 2% is acceptable and that the G value for the working fluid is in the range of 0.5 - 2.0 molecules/100 ev, the maximum dose for material radiated constantly is:

$$D = 0.45 \times 10^{10} \longrightarrow 1.8 \times 10^{10} \text{ erg/gm}$$

In a Rankine cycle system with a NaK heated boiler, only a small fraction of the total working fluid inventory will be in the boiler where the irradiation of the fluid occurs. This dilution factor is estimated to be in the range of 50 - 100 and will increase the maximum acceptable dose for the material in the boiler to:

$$(D)_{\text{max}} = 2.2 \times 10^9 \longrightarrow 1.8 \times 10^{10} \text{ rads}$$

On the other hand, the total radiation dose to the organic fluid located in the NaK heated boiler is predicted to be 3.4×10^8 rads and 2.8×10^8 rads for the 10KW_e and 75KW_e systems respectively (see Appendix A). This gives a conservatively determined factor of safety of one to two orders of magnitude so that radiolytic decomposition should pose no problem in the reactor-ORCEPS systems.

3.3.3.5 Effect of Combined Thermal and Radiation Environments

The organic fluid in a Rankine cycle reaches its highest temperature in the superheated region of the boiler. These temperatures, in the proposed cycles, reach a maximum of 750°F. In addition, the fluids in the boiler are subjected to radiolytic degradation. Therefore, an important question relates to a possible synergistic effect between the radiolytic and thermal degradation in which the combined effect resulting from high temperature irradiation is greater than the sum of the degradation predicted from irradiation at lower temperatures where thermal degradation was negligible (radiolysis alone) and from thermal degradation measurements without irradiation (thermal degradation alone). No specific data are available for the CP-28, CP-25, FL-M and RC-1

fluids. However, the behavior of hydrocarbons would be expected to be similar to aromatic hydrocarbons, on which very extensive measurements have been made for evaluation of their use as organic moderator-coolants for nuclear reactors, (Reference 12). The results are summarized below.

The effective "G" values obtained on meta rich mixture of o-, m-, and p- terphenyl when irradiated at various temperatures is illustrated in Figure 3.48. The measurements cover a wide range of dose rates and irradiation types. At fluid temperatures below approximately 750°F (399°C), the measured G value is almost independent of the fluid temperature indicating that thermal degradation and any effect of temperature on the radiolytic degradation rate is negligible at these temperatures. Above 750°F (399°C), the measured G values increase somewhat primarily because of increased thermal degradation. For measurements with a low dose rate, thermal degradation controls at high temperature and the measured G value increases rapidly with increasing temperature. For high dose rate measurements, such as the AECL 5 watt/gm dose rate, the radiolytic degradation is much greater than that due to thermal degradation, even at fluid temperatures as high as 825°F (441°C). This effect of dose rate on the influence of fluid temperature on the measured G value, provides a qualitative indication that any synergism between the radiolytic and thermal degradation in high temperature irradiations should be small, that is, that thermal degradation and radiolytic degradation can be treated as independent and additive for meta-rich terphenyls.

In the MIT work, a model was developed which adequately described the results of the test results from MIT as well as other investigators. It was assumed that the effect of radiolysis and radiopyrolysis were independent and linearly additive; radiopyrolysis refers to thermal decomposition of the radiated material which, at high concentration of degradation products, was much higher than that of the unirradiated material. Irradiations of the terphenyls were carried out to degradation product concentrations >50% in many experiments. With this assumption, a general rate equation expressing the total terphenyl degradation rate can be written in units of grams/watt-hours as:

$$\left[- \frac{d C_{OMP}}{d \tau} \right] = \left[k_{R, OMP, N} C_{OMP}^N + \frac{k_{P, OMP, M} C_{OMP}^M}{\bar{\gamma}} \right] = \frac{G(-OMP)}{11.65}$$

Figure 3.48. Effect of Temperature on Terphenyl Initial Degradation Rates, Meta-Rich Terphenyls

At temperatures above about 662°F (350°C), thermal decomposition (radiopyrolysis) of the coolant becomes significant in low average dose rate experiments. To separate the total measured degradation rate into radiolysis and radiopyrolysis, the radiolysis degradation rate measured at low temperature (applying a small activation energy of radiolysis; $\Delta E_R = 1 \frac{\text{k-cal}}{\text{mole}}$) was subtracted from the total measured degradation rate:

$$k_{P, OMP, M} = \left[- \frac{d C_{OMP} / d \tau}{C_{OMP}^M} - \frac{k_{R, OMP, N} C_{OMP}^N}{C_{OMP}^M} \right] \bar{\tau}$$

The following results were obtained for meta-rich terphenyl coolants using this procedure:

Reaction Order for Radiolysis

$$N = 1.7 \pm 0.02$$

Initial Degradation Rate From Gamma Irradiation at 320°C

$$G_{\gamma}^{\circ} = 0.19 \pm 0.02$$

Fast Neutron Effect

$$\frac{G_N}{G_{\gamma}} = 3.9 \pm 0.4$$

Reaction Order for Radiopyrolysis

First Order Assumed, $M = 1.0$

Radiopyrolysis Rate Constant

$k_{P, OMP, 1}$ is given in Figure 3.49 as function of fluid temperature and terphenyl concentration.

The above discussion applied to meta-rich terphenyls only. A review of high dose rate irradiations where the thermal decomposition rate was negligible compared to the radiolysis rate was made for pure ortho- and pure meta-terphenyl with the results given in Figure 3.50.

From the foregoing it appears that the question of possible synergistic effects of irradiation at high temperatures should be experimentally verified for the proposed working fluids. However, if their behavior is similar to that of the terphenyls, as is expected, the energy of activation for the radiolytic

Figure 3.49. Effect of Temperature on the Pyrolysis of Irradiated and Unirradiated Terphenyl Coolant
Class I Terphenyls: Meta, Santowax WR, Santowax OMP, OM-2

Figure 3.50. Effect of Temperature on the Radiolysis Rate of Ortho and Meta Terphenyl Mixed Reactor Radiation

decomposition rate is very close to zero up to a fluid temperature at which appreciable thermal degradation occurs. Since these temperatures (over 750°F) are above those of interest for this study, it is concluded that radiolytic degradation and thermal degradation are independent and only linearly additive.

3.3.3.6 Toxicity

A full assessment of toxic hazards is beyond the scope of this study. However, a literature search indicates that none of the four fluid candidates (CP-28, CP-25, FL-M and RC-1) present any appreciable toxicological hazard.

Some investigations have been conducted (Reference 13) concerning the anesthetic and analgesic action of highly fluorinated benzenes. These investigations indicated that the vapor toxicity of pentafluorobenzene and hexafluorobenzene, and their mixtures, is quite low. A Russian investigator (Reference 14) reported the median 2 hour lethal air concentration (LC_{50}) of hexafluorobenzene with white mice is 95 mg/liter (12,500 ppm) as compared to 37 mg/liter (13,700 ppm) for benzene itself. Additional mice inhalation work sponsored by Imperial Smelting has been reported (Reference 15). The principal results of 30 minute exposure tests are summarized in Table 3.7.

Table 3.7. Organic Fluid Exposure Test Results

<u>FLUID</u>	<u>LC_{50}, ppm (vol.)</u>
Pentafluorobenzene	30,000
Hexafluorobenzene	65,000
50:50 mixture of Hexa-fluorobenzene and Penta-fluorobenzene	50,000

Fluorozone - M is believed to have low toxicity but test reports are not as yet available. Handling experience of this material and compounds of similar chemical structure indicates that no dangerous toxicity hazard exists either by skin contact or inhalation (Reference 16).

Some appreciation for the inhalation hazards of these materials can be had by comparing their animal LC_{50} 's with concentrations of more familiar materials which have resulted in human death. Gasoline is reported (Reference 17) to have caused a human death after 5 minutes exposure in a concentration estimated between 5000 and 16,000 ppm. Benzene is reported (Reference 18) to

be lethal in 5 to 10 minutes at 19,000 to 20,000 ppm, and hazardous to life in 30 to 60 minutes at 7500 ppm. Both gasoline and benzene are handled in very large quantities every day with only the rarest reports of vapor intoxication. The toxicity of Toluene has been reported to fall in the range between that of benzene and gasoline. Therefore, it can be concluded that if similar precautions are taken with the candidate organic fluids that are normally taken with such conventional fluids as gasoline and benzene, no significant toxicity hazards will exist.

3.3.3.7 Selection of Fluids for Detailed Evaluation

In selecting working fluids for more detailed evaluation, the following factors were considered:

- (1) Overall System Efficiency
- (2) Regenerator Requirement (Throughput)
- (3) Prior Experience with Fluid and Reliability and Availability of Property Data (Physical, Thermodynamic, Heat Transfer)
- (4) Turbine Characteristics (Speed, Specific Speed, Pressure Ratio, Velocity Ratios)
- (5) Radiator/Condenser Requirement (Area, Pressure Drop)

Figure 3.51 summarizes the cycle efficiency versus turbine isentropic efficiency and condenser temperature for the nine candidate working fluids. The condenser temperatures were maintained at approximately 200°F. The turbine inlet temperatures reflect the recommended maximum temperature for each working fluid. It should be noted that the maximum turbine inlet temperature of 1000°F (538°C) for CP-28 reflects recent data released by Imperial Smelting Company. These data are based upon the fact that at any increment of time only a small portion of the total working fluid is in the region at the maximum operating temperature of the superheated section of the boiler. This results in extending the actual operating period of the working fluid. On this basis, Imperial Smelting reports that 1,022°F (550°C) is an acceptable maximum working temperature for hexafluorobenzene (CP-28), especially since the major decomposition product at this temperature is octafluorotoluene. In addition, 600°F (316°C) was their recommended upper temperature limit for the use of Thiophene (CP-34).

Figure 3.51. Cycle Efficiency versus Turbine Isentropic Efficiency for Candidate Working Fluids

The four fluids retained for further detailed parametric analyses were CP-28, CP-25, FL-M and RC-1. These fluids were found to have the best combination of physical, thermodynamic and heat transfer characteristics to permit attainment of high cycle efficiencies (Figure 3.51), acceptable condensing pressures (Figures 3.34 and 3.35 of Section 3.3.3) and regenerator weight requirements (Section 3.3.5).

3.3.4 Boiler

Vaporization and superheating of the working fluid take place in the boiler. The boiler consists of three regions: the liquid region, where the liquid is heated to its saturation temperature; the two-phase region, where evaporation occurs; and the vapor region where the superheating occurs. A one-pass type boiler, which delivers superheated vapor to the turbine through a continuous tube, was chosen as a baseline design. A swirl wire is utilized in the two-phase and superheater regions of the tube to induce a gravitational field within the boiler tube for zero-g operation. Titanium standoffs of varying height are used to attach the boiler tube to the heat source wall. These standoffs are required to maintain a relatively small temperature difference between the tube wall and the fluid in the two-phase region in order to eliminate the potential of film boiling, which adversely affects boiler performance.

3.3.4.1 Design Analyses

Several boiler design concepts were investigated during this study. In a one-pass boiler the inlet subcooled liquid is discharged as a heated vapor with only one pass through a continuous tube. The heat flux must be kept low in the two-phase portion of the boiler to prevent film boiling, which requires a relatively long boiler tube. A recirculation type boiler would deliver a wet vapor-liquid mixture to a separator where the saturated vapor and liquid are separated. The saturated vapor is then sent to the superheater, and the liquid is returned to the boiler inlet by a pump. Since a continuous liquid film (with a high heat flux) can be maintained on the heat transfer surface of a recirculating boiler, the heat transfer area requirement is less than for the once-through type. However, the extra complexity of the zero gravity separator and pumping system in the recirculation type is apparent. Furthermore, the boiler may be a single-tube or multiple-tube type. These tubes may in turn be either conductively or radiatively coupled to the heat source.

3.3.4.2 Convection Heat Transfer

As mentioned above, the working fluid entering the boiler tube is successively heated to its saturation temperature, evaporated, and superheated. The amount of heat transferred in each of the zones is determined from the properties of the fluid and its flow rate.

The flow in the preheater and superheater zones is single phase (i.e., liquid and vapor, respectively) and is turbulent. As in the case of the condenser, heat transfer coefficients in these regions are calculated from the Dittus-Boelter correlation:

$$Nu = 0.023 Re^{0.8} Pr^{0.4}$$

where

Nu = Nusselt number

Re = Reynolds number

Pr = Prandtl number

Knowing the heat transfer coefficient, the length of boiler tube required in the preheater and superheater sections can be determined from the following relationship:

$$Q = hA_s \Delta T_m$$

where

Q = heat transferred

h = heat transfer coefficient

A_s = tube inside surface area

ΔT_m = mean temperature difference between tube and fluid

Many correlations exist for boiling heat transfer, all based on experimental data obtained for many organic fluids and water. Different flow regimes exist throughout the two phase region of the boiler depending on the fluid properties, flow rate, surface properties, heat flux, temperature and pressure, and of course, vapor quality. Typical flow regimes are: nucleate boiling, slug flow, annular flow, mist flow, film boiling, or any combinations of these. The heat transfer coefficient will depend on the specific local conditions in the tube. Therefore, average coefficients for the complete two-phase region cannot be defined and an incremental approach is necessary.

In the low quality portion of the boiler tube, the correlation developed by Chen agrees well with data obtained for many fluids (Reference 19). The correlation assumes an additive macro- and micro-convection mechanism in boiling heat transfer with net vapor generation. Two dimensionless functions, S and F , account for the suppression of bubble growth due to flow and for the increase in convection turbulence due to the vapor. Assuming the macroconvective contribution is still described by the Dittus Boelter equation and the micro-convective contribution by Forster and Zuber's formulation, the following correlation was obtained by Chen:

$$h = h_{\text{mac}} + h_{\text{mic}}$$

where

$$h_{\text{mac}} = 0.023 (\text{Re})^{0.8} (\text{Pr})^{0.4} (k/D) F$$

$$h_{\text{mic}} = 0.00122 \frac{k_{\ell}^{0.79} c_{p\ell}^{0.45} \rho_{\ell}^{0.49} g_c^{0.25}}{\sigma^{0.5} \mu_{\ell}^{0.29} \lambda^{0.24} \rho_v^{0.24}} (\Delta T)^{0.24} (\Delta P)^{0.75} S$$

and

h = heat transfer coefficient, Btu/hr ft² °F

k = thermal conductivity, Btu/hr ft °F

D = diameter, ft

c_p = specific heat, Btu/lb °F

ρ = density, lb/ft³

σ = surface tension, lb/ft

μ = viscosity, lb/hr ft

λ = latent heat of vaporization, Btu/lb

ΔT = superheat ($T_{\text{wall}} - T_{\text{sat}}$), °F

ΔP = difference in vapor pressure corresponding to ΔT , lb/ft²

F is the ratio of the two-phase Reynolds number to the liquid Reynolds number and is assumed to be a function of the Martinelli parameter x_{tt} . S is a suppression factor based on the liquid Reynolds number and F .

In the high quality region of the boiler tube, evaporation occurs as liquid impinges on the tube wall. Assuming that all the droplets impinging on the wall evaporate, i.e., the wall acts as an infinite sink, the heat transfer coefficient can be calculated from the analogy between heat transfer and mass transfer (Reference 20). Based on this analogy, the mass of liquid transported to the wall is

$$\dot{m} = 0.023 \frac{(1-x) G_o}{Re_v^{0.2} Sc^{0.6}}$$

where

$\dot{m}$ = liquid transport to tube wall per unit area

x = vapor quality

G_o = total mass velocity

Re_v = vapor Reynolds number

Sc = Schmidt number

The heat transfer coefficient is in turn given by

$$h = \frac{\dot{m}\lambda}{\Delta T}$$

where

λ = latent heat of vaporization

ΔT = wall to fluid temperature difference

The heat transfer coefficient in the low quality region of the boiler increases with quality; the coefficient in the high quality region decreases with quality. In the neighborhood of 50 percent quality, the coefficients derived by the two techniques are equal. The lower of the two coefficients is always used throughout the two-phase zone.

The above relationships determine the heat transfer coefficient along the boiler tube. Heat flux along the tube is determined by the product of the heat transfer coefficient and the wall-to-fluid temperature difference. To avoid large excursions in tube wall temperature, which may lead to hot spots

on the tube wall, this heat flux must be below the critical heat flux of the particular working fluid. Many relationships are presented in the literature for the critical heat flux of organic fluids. Typically, the value of the critical heat flux for CP-28 (a typical organic fluid), based on the Cichelli-Bonilla correlation (Reference 21) is 160,000 Btu/hr ft².

Heat transfer in the nucleate boiling regime does not change significantly at reduced gravity but the critical heat flux is reduced by the 1/4 power of acceleration (Reference 22). To guard against such a reduction in the critical heat flux, it is necessary to induce an effective gravitational field in the boiler tube, as described in Section 3.3.4.3.

In summary, the preheater and superheater film coefficients are based on the Dittus-Boelter equation using liquid and vapor properties, respectively; the nucleate boiling zone heat transfer design is based on Chen's equation; the dry wall boiling zone heat transfer design is based on mass transfer considerations; and the transition point between the nucleate and film boiling is determined by equating the two relationships. Relatively low heat transfer coefficients are obtained resulting in a conservative design.

3.3.4.3 Zero Gravity Operation

To promote heat transfer and prevent instabilities in the zero gravity environment a swirl wire is utilized in the boiler tube for the entire length of the boiling and superheating sections. This swirl wire provides a vortex flow, thus inducing a "g field" which helps to maintain a liquid film on the tube wall and maintains high heat transfer coefficients. Furthermore, entrainment of liquid in the vapor flowing to the turbine is thus eliminated. The contribution of the swirl wire to the boiling coefficient is neglected, to increase the design margin, but the effect on the pressure drop must be considered. Twisted tapes can also produce vortex flow in tubes, but have been observed under certain conditions to concentrate the liquid at the tube axis rather than at the tube wall.

If vortex flow exists in the tube, the induced gravitational field will force the liquid against the tube wall. Annular flow can thus be assumed. For turbulent flow in both the liquid and vapor regions, and assuming a 1/7 power velocity distribution in the liquid boundary layer, the thickness of the boundary layer is,

$$\delta = \frac{D}{4 \left(1 + \frac{7}{8} \frac{\rho_\ell}{\rho_v} \frac{x}{1-x} \right)}$$

where

δ = thickness of liquid annulus, inch

D = tube diameter, inch

ρ = density, lb/ft³

x = vapor quality

As expected, the thickness of the annulus decreases as the quality increases. A swirl wire having a diameter near the thickness of the liquid boundary layer will force the liquid into vortex flow.

The required swirl wire pitch can be related to the desired gravitational force as follows:

$$g_{sw} = \frac{U_T^2}{D/2}$$

$$\tan \alpha = \frac{U_T}{U_V} = \frac{\pi D}{\ell}$$

$$\ell^2 = 0.05 \frac{\dot{m}^2 (1-x)^2}{D \rho_v \rho_\ell \delta^2 g_c \left(\frac{g_{sw}}{g_c} \right)}$$

where

g_c = Earth's gravitation constant, 32.2 ft/sec²

g_{sw} = gravitational force induced by swirl wire, ft/sec²

U_T = tangential velocity (axial flow velocity), ft/sec

α = swirl wire pitch angle

ℓ = swirl wire pitch, inch

$\dot{m}$ = mass flow rate, lb/hr

For a given pitch, the gravitational field increases with the quality. A typical value of g_{sw}/g_c is 5 at a quality of 20%, which is a very conservative value.

3.3.4.4 Boiler/Heat Source Integration

As discussed above, a relatively small temperature difference ($\sim 40^\circ\text{F}$) is desired between the tube wall and the working fluid in the two-phase region of the boiler in order to eliminate the occurrence of film boiling in the low quality region of the boiler. Since the vapor outlet temperature from the boiler is appreciably higher than the saturation temperature and since the NaK heat source fluid has a high thermal conductivity, direct attachment of the boiler tube to the NaK heat source tubes would result in very high tube wall temperatures. A thermal resistance between the heat source and the two phase region of the boiler is therefore required to maintain low tube wall temperatures in this region and still allow the required superheat to be attained.

A computer model of the boiler divides the boiler tube into small nodal elements. A variable thermal resistance is then used to connect each node to the NaK heat source, and the value of this resistance is then determined for the required tube temperature and heat flux. Based on this resistance network, variable height titanium standoffs can be designed to connect the boiler tube to the NaK heat source base plate.

3.3.4. Parametric Analyses

A boiler computer model, incorporating all the above thermal and pressure drop relationships, has been developed at TRW to enable tradeoff analyses to be performed. Typical boiler tube lengths and pressure drops (for CP-28) are shown in Figure 3.52 as a function of tube inside diameter and working fluid flow rate.

3.3.5 Regenerator

The purpose of the regenerator is to recover heat from the superheated vapor leaving the turbine and transfer it to the pump discharge high pressure liquid prior to its entering the boiler. As a result of this energy transfer, less heat must be supplied to the boiler resulting in increased cycle efficiency and appreciable weight and volume reductions.

Figure 3.52. Typical CP-28 Boiler Requirements

3.3.5.1 Design Analyses

Three basic considerations are involved in the design of a heat exchanger for space use: heat transfer rates between the two working fluids, pressure drop in each fluid, and overall weight of the system. The heat transfer rate per unit surface area is approximately proportional to the fluid velocity. The frictional pressure drop also increases with velocity. However, the frictional pressure drop increases approximately with the square of the velocity. The heat transfer rates and frictional pressure drops must therefore be matched to the specific requirements in the design of each heat exchanger.

In the liquid flow regions, the pressure drops are usually low and heat transfer rates dominate the design. With gas or vapor flow, however, the allowable pressure drops may be the controlling influence on the design. To reduce pressure drops, the velocity is reduced by increasing the flow channel size or increasing the number of flow channels. These lower velocities, coupled with the low thermal conductivities of the candidate working fluids, result in relatively low heat transfer coefficients. Hence, additional surface area must be provided by either lengthening the channels or adding fins. To minimize the total volume and weight of the exchanger, compact heat exchangers, using finned surfaces are the preferred configuration.

3.3.5.2 Heat Transfer

The basic heat transfer relationship for any heat exchanger is,

$$dQ = U(T_h - T_c) dA$$

where

Q = heat transferred

U = overall heat transfer coefficient

T = fluid temperature

A = area

subscript: h = hot

c = cold

To integrate this equation, the variations of U , T_h and T_c with location, or area, must be known. The overall heat transfer coefficient is related to the liquid and vapor heat transfer coefficients, the wall conductivity, and

fouling factors. Assuming the latter are negligible,

$$\frac{1}{U_h A_h} = \frac{1}{h_h A_h \eta_h} + \frac{\Delta x}{k A_w} + \frac{1}{h_c A_c \eta_c}$$

where

h = local heat transfer coefficient

η = fin effectiveness

x = wall thickness

k = wall conductivity

A_w = mean wall surface area

Although the local heat transfer coefficients vary along the heat exchanger due to temperature variations, average values are usually assumed. The above equations are then combined with the heat balance equation to determine the required heat exchanger area. Assuming negligible heat losses from the exchanger, the heat lost by the hot fluid must equal the heat gained by the cold fluid, or

$$Q = \dot{m}_h c_{p_h} (T_{h_{in}} - T_{h_{out}}) = \dot{m}_c c_{p_c} (T_{c_{out}} - T_{c_{in}})$$

where

$\dot{m}$ = flow rate

c_p = fluid heat capacity

Two quantities are very useful in the solution of the above heat transfer rate equation. The first is the heat exchanger effectiveness, defined as:

$$\epsilon = \frac{(\dot{m}c_p)_h (T_{h_{in}} - T_{h_{out}})}{(\dot{m}c_p)_{\min} (T_{h_{in}} - T_{c_{in}})} = \frac{(\dot{m}c_p)_c (T_{c_{out}} - T_{c_{in}})}{(\dot{m}c_p)_{\min} (T_{h_{in}} - T_{c_{in}})}$$

This quantity compares the actual heat transfer rate to the maximum possible heat transfer rate. The other is the exchanger heat transfer units defined as:

$$N_{tu} = \frac{A U_{ave}}{(\dot{m}c_p)_{\min}}$$

This parameter expresses the "size" ($A U_{ave}$) of the exchanger.

For a pure parallel or counterflow exchanger or any exchanger where one of the fluid temperatures remains constant, the above equations can be solved to give:

$$Q = UA \Delta T_m$$

where

$$\Delta T_m = \frac{\Delta T_{out} - \Delta T_{in}}{\ln \frac{\Delta T_{out}}{\Delta T_{in}}}$$

ΔT = temperature difference between the two fluids.

Furthermore, for a counterflow exchanger (Reference 23)

$$\epsilon = \frac{1 - e^{-N_{tu} \left[1 - \frac{(\dot{m} c_p)_{min}}{(\dot{m} c_p)_{max}} \right]}}{1 - \frac{(\dot{m} c_p)_{min}}{(\dot{m} c_p)_{max}} e^{-N_{tu} \left[1 - \frac{(\dot{m} c_p)_{min}}{(\dot{m} c_p)_{max}} \right]}}$$

For cross-flow, multipass, and tube-and-shell exchangers, graphical solutions to the above heat transfer rate equation exist to allow a determination of the required exchanger size for a specific application.

3.3.5.3 Pressure Drop

As mentioned above, pressure drop in a liquid-to-liquid heat exchanger is small and relatively unimportant. For gas or vapor flow, however, the pressure drop may be appreciable and careful analyses must be performed. In general;

$$\Delta P_{\text{fric}} = \Delta P_{\text{fric}} + \Delta P_{\text{entrance}} + \Delta P_{\text{exit}} + \Delta P_{\text{acc}}$$

$$\Delta P_{\text{fric}} = f_D \frac{L}{D} \frac{v_m G^2}{2g}$$

$$\Delta P_{\text{ent}} = (1 - \sigma^2 + k_c) \frac{v_1 G^2}{2g}$$

$$\Delta P_{\text{exit}} = (\sigma^2 + k_e - 1) \frac{v_2 G^2}{2g}$$

$$\Delta P_{\text{acc}} = \left(\frac{v_2}{v_1} - 1 \right) \frac{v_1 G^2}{2g}$$

where

ΔP = pressure drop

f_D = Darcy friction factor

L = length

D = effective diameter

G = mass velocity

v_1 = entrance specific volume

v_2 = exit specific volume

v_m = mean specific volume

σ = ratio of free-flow to frontal area

k_c = entrance loss coefficient

k_e = exit loss coefficient

3.3.5.4 Parametric Analyses

Three general types of heat exchangers were analyzed: plate-fin, coaxial tube fin, and finned tube. A general schematic of each type of exchanger is shown in Figure 3.53. The plate-fin regenerator was found to be considerably smaller and lower in weight than the other types. Furthermore, due to its smaller size, the weight penalty for meteoroid protection is also reduced. By using design charts in "Kays and London" (Reference 23), and selecting a one-pass plate fin surface for the vapor and a six-pass louvred plate fin surface

FINNED TUBE
REGENERATOR

PLATE-FIN
REGENERATOR

CO-AXIAL TUBE
REGENERATOR

Figure 3.53. Regenerator Designs

for the liquid, and varying liquid and vapor lengths, a parametric analyses was conducted. The basic regenerator modular design is shown in Figure 3.54.

While UA_k is not a direct measure of regenerator bulk, it is certainly well correlated with bulk through its sensitivity to heat exchanger surface area, and is therefore useful as a size criterion. The regenerator characteristics for the four final working fluid candidates are shown in Table 3.8. The cycle conditions assumed a turbine inlet temperature of 750°F (399°C) and component efficiencies for the generator, turbine, pump, boiler and regenerator of 0.87, 0.85, 0.65, 0.95 and 0.95 respectively.

Tradeoff analyses were performed to arrive at a regenerator configuration and geometry which would have a minimum weight for a given flow rate, effectiveness and allowable pressure drop. The large frontal surface area of the basic plate-fin design resulted in acceptable pressure drops, for both the liquid and vapor, as noted in Figure 3.55. The weight of a regenerator, for a given design, also depends upon the choice of material. The relatively high temperature and pressure difference across the regenerator core precludes the use of aluminum. Some magnesium alloys have a temperature capability somewhat higher than that of aluminum and the use of magnesium for the regenerator is a possible weight savings design option. Magnesium can be easily fabricated, welded and brazed, though the cost of a magnesium regenerator would be higher than that of an aluminum one. Other candidate materials are beryllium, stainless steel and copper. The choice of copper and stainless steel would impose an appreciable weight penalty over aluminum, magnesium and beryllium. During this study, to minimize system weight, beryllium was selected as the regenerator material.

Figure 3.54. Regenerator Basic Module Design

Table 3.8. Typical Regenerator Characteristics
(50 KW_E Power Level)

Fluid:	CP-25	CP-28	FL-M	RC-1
Duty: (Btu/hr)	480,700	902,200	998,000	837,000
Flow Rate: (lb/hr)	3,279	8,345	7,610	9,198
Vapor Pressure: (psia)	10	22	7.2	25
Liquid Pressure: (psia)	500	399	300	300
Inlet Temperature: (°F)				
Vapor	570	631	640	634
Liquid	205	200	195	210
Outlet Temperature: (°F)				
Vapor	221	217	217	230
Liquid	445	450	500	422

Figure 3.55. Regenerator Liquid and Vapor Side Pressure Drops versus Net Electrical Power

3.3.6 Radiator - Condenser

A typical zirconium hydride (ZrH) reactor power system launch configuration is shown in Figure 3.56. During launch, the deployable radiators are telescoped into a compact configuration, with the power conversion unit (PCU) housed in the conical portion of the radiator. The post-launch deployed configuration is shown in Figure 3.57. Because the cylindrical radiator sections are non-load bearing, they can be very lightweight. The relatively low specific weight of conventional radiators is also a very important factor in view of the comparatively large surface areas required.

Cycle heat rejection from the ORCEPS system occurs in the condenser - radiator assembly. A very important parameter for establishing ORCEPS weight and volume is radiator area. This is a function of the heat rejection (at average condensing temperature, T_2) from the ORCEPS PCU (q_r), and the radiator emissivity, ϵ . The recommended ORCEPS range for condensing temperatures for this application are 200°F to 240°F. This is a compromise between achievable PCU efficiency and minimal radiator area. For space applications, an emissivity of 0.9 is readily achievable by the use of selective coatings (i.e., Z-93 paint) and/or second-surface mirrors. For an assumed radiator sink temperature of -410°F (-246°C), the required radiator specific area (A/q_r ; ft²/KW_T) as a function of condensing temperature is shown in Figure 3.58. At a PCU average condensing temperature of 225°F, the specific radiator area becomes approximately 10.0 ft²/KW_T.

3.3.6.1 Design Analyses

The condenser-radiator is designed to reject the waste heat from the system to space. In contrast to the boiler (described in Section 3.3.6), whose size is dictated by heat transfer considerations and to a lesser degree by the allowable pressure drop, the design of the condenser is dictated primarily by the allowable pressure drop. Slightly superheated vapor from the regenerator enters the condenser. This fluid is then condensed and exits the condenser as a subcooled liquid (5°F subcooling). The condenser thus consists of three regions: the desuperheating region where the entering vapor is cooled to its saturated vapor temperature; the two-phase region where condensation occurs; and the subcooling region where the liquid is cooled below its saturated liquid temperature.

Figure 3.56. Zirconium Hydride Reactor Organic Rankine Space Power System in Launch Configuration

Figure 3.57. Zirconium Hydride Reactor Organic Rankine Space Power System in Deployed Configuration

Figure 3.58. Radiator Specific Area versus Condensing Temperature

Due to the low absolute pressures and the relatively low density of the working fluid in the condenser, a multiple-tube rather than a single-tube, condenser should be used if low pressure drops are to be achieved. Use of circumferential tubes or multiple-pass tubes along the radiator length would result in satisfactory operation of the condenser under zero-gravity conditions. To completely avoid the possibility of slug flow, it is necessary to flow with gravity throughout the condensing tubes. Thus, by entering the condensing tubes at the top of the condenser and spiralling the tubes around and down the radiator as many times as necessary to accommodate the required length, this form of instability can be avoided.

3.3.6.2 Pressure Gradients

The pressure drop across the condenser tubes consists of two basic components: the frictional pressure drops of the vapor, two-phase mixture, and liquid; and the pressure drop due to momentum or acceleration change:

$$\left[\Delta P = \Delta P_v + \Delta P_{TP} + \Delta P_\ell \right]_{\text{frictional}} + \Delta P_{\text{acc}}$$

The frictional pressure drops of the vapor and liquid are obtained from the standard equation (Reference 21).

$$\Delta P = f_D \frac{L \rho V^2}{D 2g_c}$$

where

f_D = Darcy friction factor

L = tube length

D = tube I.D.

ρ = fluid density

V = fluid velocity

The most widely accepted basis for correlating two-phase frictional pressure drop is that used by Lockhart and Martinelli (Reference 24) and Nelson and Martinelli (Reference 25) for two-phase, two-component flow in pipes. The basic assumptions upon which Lockhart and Martinelli based their analysis are: static pressure drop for the liquid phase must equal the static pressure drop for the gaseous phase regardless of the flow pattern,

as long as no appreciable radial static pressure difference exists; and the volume occupied by the gas at any instant must equal the total volume of the pipe. These postulates infer that annular or spray annular flow exists and that slug, plug, bubble and stratified flows are excluded from the correlation. Since in stable operation of space radiator-condensers, annular or spray annular flow is the flow regime occurring in a major portion of the condensing section, the correlation is applicable. The various possible two-phase flow patterns were separated into four mechanisms. The mechanisms are somewhat empirical since they do not correspond to any particular flow regime but are based on whether the individual phase flowing alone in the pipe would be viscous or turbulent. The quantity O^2 was defined as the ratio of the two-phase pressure drop to the pressure drop of either component flowing alone at its mass-flow rate in the pipe.

$$\frac{dP}{dL}_{TPF} = \frac{dP}{dL}_{liquid} O_{\ell}^2$$

For this study, both vapor and liquid flows are assumed turbulent over the major portion of the condenser and

$$O_{\ell} = O_{\ell tt} = f(x_{tt})$$

where

$$x_{tt}^2 = \frac{(dP/dL)_{liq}}{(dP/dL)_{vap}} = \left(\frac{\rho_v}{\rho_{\ell}}\right) \left(\frac{\mu_{\ell}}{\mu_v}\right)^{0.2} \left(\frac{1-x}{x}\right)^{1.8}$$

and

$$\left[\frac{dP}{dL}\right]_{liq} = \text{local pressure gradient assuming only liquid phase flowing}$$

$$\left[\frac{dP}{dL}\right]_{vap} = \text{local pressure gradient assuming only vapor phase flowing}$$

ρ = density

μ = viscosity

x = quality

subscripts: ℓ = liquid

v = vapor

The fact that the properties and quality are continually changing along the length of the tube necessitates an incremental approach to the solution of the overall two-phase pressure drop equations.

As mentioned above, the total pressure drop measured under two-phase flow includes any pressure changes resulting from changes of phase and velocity. The phase velocities of the liquid and vapor are generally not equal. Therefore the fraction of the fluid vaporized, or condensed, undergoes a momentum change and leads to a pressure change along the flow channel. From the impulse-momentum principle

$$\Delta F = \dot{m} (V_2 - V_1)$$

$$\Delta P = G (V_2 - V_1)$$

and the acceleration pressure drop is given by

$$\Delta P_{acc} = \frac{G^2}{g_c} \left[\frac{1}{\rho_l} - \frac{1}{\rho_v} \right]$$

where

F = force

$\dot{m}$ = flow rate

G = mass velocity

Since in a condenser the density of the exiting fluid is higher than that of the entering vapor, the velocity of the working fluid is reduced appreciably and the acceleration pressure drop is negative; that is, a pressure gain is obtained. The net pressure drop in the condenser is thus the frictional pressure drop minus the acceleration pressure gain.

In a multi-tube condenser-radiator, additional static pressure changes also occur as turning losses from the inlet vapor lines to the vapor header, as frictional losses in the vapor header, and as turning and entrance losses from the header to the tubes. Liquid exit header and liquid line turning pressure losses are negligible for low liquid velocities (less than ~ 4 feet per second).

3.3.6.3 Condenser Instabilities

Condensers with multiple liquid/vapor interfaces are subject to two types of instabilities: single tube and multiple tube. Single tube instabilities, as the name implies, are those instabilities which are the result of the flow patterns and liquid/vapor behavior in a single tube alone and do not depend on the contribution of neighboring tubes. The greatest danger of single tube instabilities is that, if uncorrected, they usually result in multiple tube instabilities whose results are much more serious. Multiple tube instabilities require the presence of neighboring tubes to absorb or supply inventory. Their effects, if unchecked, are progressive, beginning with the filling or emptying of a tube or tubes of condensate, admission of vapor to the outlet header and/or liquid to the inlet header (usually accompanied by a rise in condenser pressure), and finally flow stoppage due to inadequate pump suction head.

Single tube instabilities can be separated into runback, slugging and meniscus instabilities:

- Runback instability is caused by a low drag force exerted by the flowing vapor on the condensate. In zero-g operation, this drag must move the condensate along the tube wall at a rate fast enough to prevent bridging of the film, a cause of instability. If this drag force is sufficient, the condensate flow rate in the tube will oscillate and eventually the tube may fill with condensate and multiple tube instability may result.
- One of the results of runback is the formation of liquid slugs. These slugs are formed when liquid bridges the condenser tube, thus trapping vapor pockets. As the vapor in these pockets condenses, the slug slams into the interface. Determination of the slugging frequency and resulting pressure rises are important in condenser design.
- Meniscus instability can occur when the surface energy forces are not able to control the vapor/liquid interface. The behavior of the meniscus in a condenser tube is a strong function of the Bond number.

$$Bo = \frac{\text{gravity forces}}{\text{surface tension forces}} = \frac{\rho_l a D^2}{\sigma g_c}$$

where

- a = acceleration
- D = tube diameter
- σ = surface tension

The critical Bond number has been found to be independent of the gravitational level -- it is only a function of the system configuration, type of liquid, and direction of the acceleration with respect to the liquid/vapor interface. Below the critical Bond number, a stable interface is maintained.

Multiple tube instability is initially characterized by a filling or an emptying (of condensate) of a single tube in a parallel tube array. The primary criterion for multiple tube stability requires that for stable operation

$$\frac{d(\Delta P)}{dG} > 0.$$

This requirement can be expressed as

$$\Delta P_F > 1.18 \Delta P_{acc}$$

where

ΔP = total pressure drop

G = mass velocity

ΔP_F = frictional pressure drop

ΔP_{acc} = acceleration pressure gain

Setting a value for the maximum allowable vapor/liquid interface movement establishes another stability criterion. Without proper design, the location of this interface may be susceptible to all forms of acceleration including launch acceleration, mechanical vibrations, fluid disturbances, and local gravitational fields. One of the other type of the above criteria may dominate and establish the required tube diameters.

Tube diameters can be selected to maintain a relatively high vapor velocity throughout the condensing section to eliminate runback and slugging instabilities and maintain the Bond number below the critical value to prevent meniscus instabilities. Though it is felt that tapered condenser tubes are not required in this application, such tubes can be used to increase the vapor velocity in the downstream portion of the condenser. Furthermore, to ensure stable operation under negative accelerations, the tube diameter in the region of the vapor-liquid interface should be below the critical diameter given by Reference 26:

$$D_s = 1.835 \left\{ \frac{\sigma}{(\rho_l - \rho_v)n} \right\}^{1/2}$$

where

D_s = critical diameter

σ = surface tension

ρ = density

$n = \frac{a}{g_c}$

a = local acceleration

If the tube diameter exceeds the critical diameter, an orifice can be installed in the tube in the region of the interface.

3.3.6.4 Parametric Analyses

Parametric analyses were conducted to determine the required condenser size and resulting pressure gradients for CP-28, CP-25, FL-M, and RC-1. The number of condenser flow tubes, tube diameter, and working fluid flow rates were varied for the 10, 25, 50 and 75 KW_e systems. Table 3.9 reflects the amount of waste heat rejected by the condenser, the working fluid mass flow rate, required radiator-condenser area and the resulting weight. These data were used in the overall system weight determinations.

3.3.7 Turbine Design

There are various factors that influence turbine design and achievable turbine efficiency. It is beyond the scope of this study to consider these in great detail. However, there are several parameters which can be considered in a preliminary design manner which permits initial estimates to be made of turbine efficiency, size, and weight. These parameters are turbine flow rate, pressure ratio, rotative speed, isentropic head, specific speed, specific diameter, pitch diameter and velocity ratio. A brief discussion of the significance and impact each of these parameters has on turbine performance and weight is provided in the following.

Table 3.9. Radiator-Condenser Parametric Data

FLUID	KW _E (net)	KW _T (rejected)	FLUID FLOW RATE		AREA (ft)	WEIGHT (lbs)
			(lb/hr)	lb/sec		
CP-25	10.0	32.0	655	0.182	352	229
CP-25	25.0	80.1	1,639	0.455	881	573
CP-25	50.0	160.2	3,279	0.911	1,762	1,145
CP-25	75.0	240.3	4,918	1.366	2,643	1,718
CP-28	10.0	37.6	1,666	0.463	433	281
CP-28	25.0	94.1	4,169	1.158	1,082	703
CP-28	50.0	188.3	8,343	2.317	2,165	1,407
CP-28	75.0	282.4	12,512	3.476	3,248	2,111
RC-1	10.0	43.0	1,840	0.511	451	293
RC-1	25.0	107.5	4,599	1.277	1,129	734
RC-1	50.0	215.0	9,198	2.555	2,258	1,468
RC-1	75.0	322.5	13,797	3.832	3,387	2,202
FL-M	10.0	34.8	1,523	0.423	400	260
FL-M	25.0	86.9	3,803	1.056	1,000	650
FL-M	50.0	173.9	7,610	2.114	2,000	1,300
FL-M	75.0	260.8	11,413	3.170	3,000	1,950

3.3.7.1 Turbine Preliminary Design Factors

The required turbine flow rate is a function of the net electrical power requirement (i.e., load), the thermodynamic properties of the working fluid, the cycle conditions (i.e., isentropic head) and the PCU component efficiencies (i.e., turbine, generator, pump, regenerator). Because the turbine flow rate is dependent upon the turbine efficiency and conversely this efficiency is a function of flow rate (i.e., specific speed) they must be established in an iterative manner. Using the cycle conditions previously summarized in Figure 3.51 and assuming a turbine efficiency of 0.85, the turbine flow rates for the various working fluids are shown in Figure 3.59.

Figure 3.59. Cycle Working Fluid Flow Rate versus Net Electrical Power

The turbine efficiency is also related to the velocity ratio, U/C_o , where

U = turbine peripheral velocity, ft/sec.

C_o = nozzle spouting velocity, ft/sec.

However,

$$U = \frac{\pi D_p N}{720}$$

and

$$C_o = \psi_N \sqrt{2g H_{isen}} = \psi_N \sqrt{2g \frac{k}{k-1} RT_1 \left\{ 1 - \left(\frac{P_2}{P_1} \right)^{\frac{k-1}{k}} \right\}}$$

where

D_p = turbine pitch diameter, inches

N = turbine rotative speed, rpm

ψ_N = nozzle flow coefficient

H_{isen} = turbine isentropic head, ft.

k = ratio of specific heats of working fluid, C_p/C_v

T_1 = turbine inlet temperature, °R

R = Universal Gas Constant, $\frac{1545}{M}$

M = molecular weight

P_2 = turbine exit pressure, psia

P_1 = turbine inlet pressure, psia

To achieve high turbine efficiencies in a single stage turbine, it is usually desirable to limit the turbine pressure ratio (P_1/P_2) to a value of 50 or less. This results in moderate values of the nozzle spouting velocity, and hence higher values of the velocity ratio, U/C_o (0.5 to 0.9) are achievable at reasonable rotative speeds (50,000 to 100,000 rpm).

To achieve high turbine efficiency with a single stage rotor, it is also necessary to operate in a specific speed (N_s) range of approximately

20 to 200, where N_s is defined as,

$$N_s = \frac{N\sqrt{Q}}{(H_{isen})^{0.75}}$$

where

Q = turbine exit volumetric flow rate, ft^3/sec

With the value of N_s established, it is then possible to determine the turbine specific diameter, D_s , from turbine design charts (Reference 27). The turbine pitch diameter is then calculated as follows;

$$D_p = \frac{12D_s\sqrt{Q}}{(H_{isen})^{0.25}}$$

The selection of the rotative speed requires a matching of the turbine and pump characteristics to achieve maximum turbopump performance. The selected values are usually a compromise between achieving maximum turbine efficiency and minimum weight (high speed), and avoiding pump cavitation and excessive turbine disk/blade stresses (lower speed).

During this study, the preliminary design approach outlined above was utilized in determining the ORCEPS-PCU turbine characteristics. In a detailed design, many other factors would have to be considered. These include nozzle angle, number of nozzles, percent admission, blade height, blade angles, number of blades, solidity, angle of attack, blade velocity/length/turn coefficients, percent reaction, windage losses, leakage and spillage losses, and heat transfer losses. Finally, the turbine type (axial flow, radial inward flow, radial outward flow) is another important design consideration. This will not only effect the achievable turbine performance and weight, but can also have a significant impact on cost.

3.3.8 Pump Design

The major factors to be considered in the preliminary design of the ORCEPS-PCU pump are pump flow rate, pressure rise (head), rotative speed, specific speed, specific diameter, net positive suction head, suction specific speed, specific diameter, and impeller outside diameter. A brief discussion of the significance and impact each of these parameters has on pump performance and weight is provided below.

3.3.8.1 Pump Preliminary Design Factors

The required pump flow rate is usually the same as the turbine flow rate in a closed organic Rankine cycle system. However, in the U_{235} -ZrH reactor powered ORCEPS, additional flow requirements are imposed on the pump. These include cooling flow to the NaK loop thermoelectric converter, and to a jet condenser (if utilized) or to a jet pump (if required). These increased flow requirements increase the pump horsepower input which is a function of flow rate as follows,

$$W_p = \frac{0.000583 \times \Delta P \times Q}{\eta_p}$$

where

W_p = pump work input, hp

ΔP = pump pressure rise, psid

Q = pump flow rate, gpm

The impact of this increased flow rate is to lower the overall ORCEPS efficiency, since the increased turbine output requires additional heat input to the boiler, which does not result in an increase in net electrical power output. Hence, it is obvious, that it is desirable to minimize the magnitude of these increased pump flow requirements. One approach would be to increase the efficiency of the thermoelectric converter and E/M pump. This would result in decreased cooling requirements, but was not considered during this study. Another method would be to eliminate the necessity for the use of either the jet condenser or jet pump. During this study, by the judicious selection of working fluids and cycle conditions, the need for a jet condenser has been avoided since turbine exit pressures have been maintained within a range of 7.5 to 25 psia. If Dowtherm A had been selected as a working fluid, this would result in turbine exit pressures in the very low 0.1 to 1.0 psia range and make use of a jet condenser mandatory. The jet condenser imposes approximately a ten-fold increase in pump flow rate and despite a reduced pump pressure rise in a Dowtherm A cycle (approximately 50 psi), the net result is an increase in the pump horsepower requirements. As an alternative to the jet condenser, an optional jet pump was considered during this study. Its use was considered only in those instances when an increased pressure was required to meet the net positive suction head of the cycle pump. The design

characteristics of the jet pump are discussed in Section 3.3.10.

The pump pressure rise is established by the cycle conditions selected for each working fluid and the pressure drop between the pump discharge and the turbine inlet. Since the pump is directly driven by the turbine, its rotative speed is the same. With these parameters established it is now possible to determine the ORCEPS-PCU pump design characteristics. The pump performance is a function of its specific speed, N_s , which is calculated as follows;

$$N_s = \frac{N\sqrt{Q}}{(H)^{0.75}}$$

where

N = rotative speed, rpm

Q = pump flow rate, ft^3/sec or gpm

H = pump pressure rise, ft

For good pump performance, values of N_s in the 500 to 2500 range (gpm basis) or 15 to 125 (cfs basis) is desired. With the specific speed established, the pump specific diameter, D_s , can be obtained for a pump design chart (Reference 27). Having obtained this value, the pump impeller outside diameter, D_o , is determined by,

$$D_o = \frac{12 D_s \sqrt{Q_{\text{cfs}}}}{(H_{SV})^{0.25}}$$

where

H_{SV} = net positive suction head

The required pump net positive suction head (H_{SV}) is that value above the vapor pressure of the working fluid at the pump inlet that must be maintained to prevent the occurrence of cavitation. With proper hydrodynamic pump inlet design and the use of an axial inducer, pump suction specific speeds, S , of 15,000 to 30,000 (on gpm basis) are readily achievable. Using values within this range it is now possible to determine the required values of H_{SV} as follows;

$$S = \frac{N\sqrt{Q_{\text{cfs}}}}{(H_{SV})^{0.25}}$$

Depending upon the pump rotative speed and flow rate (total), the required values of H_{sv} could vary from 2.5 to 20 psi to stay within the prescribed achievable suction specific speed value range.

In addition, there are many other detail design parameters that will determine overall pump (includes axial inducer, centrifugal impeller, diffuser, discharge scroll) performance and weight. However, sufficient information is obtained from the preliminary design factors discussed above, to fairly accurately predict the efficiency, horsepower input and weight of the overall pump.

3.3.9 Generator Design

There are various types of generators that can be used for the ORCEPS-PRU. These include brushless homopolar inductor and permanent magnet types. However, in the power range from 10 to 75 KW_e , the homopolar inductor generator provides the highest efficiencies at the lowest weight. The weight and speed characteristics for this type of generator are shown on Figure 3.60. These data were used to provide the parametric data for Tasks 5 and 7 of this study.

The advantages of this type of alternator are:

- eliminates brushes and the problems associated with brush operation
- eliminates rotating windings and the need for rotor cooling
- eliminates rotating rectifiers and the problems associated with their operation
- stator can be easily cooled by gas, vapor, or liquid
- capable of high speed (up to 100,000 rpm) operation which permits direct turbine drive, eliminates need for a gear box, and results in light weight (0.5 to 1.0 lb/ KW_e)
- high efficiencies achievable (0.85 to 0.90) depending upon load factor

The achievement of high performance and light weight for this type generator is a function of many detail design parameters. These include magnetic and non-magnetic material properties, rotor and stator length and diameter, stator windings, number of turns, number of poles, and rotor to stator radial "air" gap. Typical losses encountered are I^2R , eddy current, and windage.

Figure 3.60. Weight and Speed Characteristics for Homopolar Inductor Generators

A detailed design is beyond the scope of this study but a brief general description of the generator is provided below.

3.3.9.1 Generator General Description

A cross section of a typical, brushless, homopolar, inductor generator is shown in Figure 3.61. A conventional polyphase stator (1) is supported by a non-magnetic housing (2). A D.C. excitation coil (3) is wound over the non-magnetic housing. A magnetic housing (4) and (5) supports the stator-excitation coil combination. The rotor is constructed from two identical magnetic pole sections (7) and (8) and a non-magnetic spacer (6) as shown in Figures 3.61 and 3.62. Sections (7) establish poles of one polarity and sections (8) establish poles of opposite polarity. The magnetic flux produced by the excitation coil travels axially in section (4) through the end bells (5) and enters the rotor through the radial parasitic gap at (9). The flux then travels axially into the poles of section (7) and enters the stator through the radial air gap (11). The flux continues circumferentially around the back of the stator iron and returns radially across the gap (11) to the poles of section (8). The magnetic circuit is completed through the parasitic gap (10) returning the flux to the housing end bell. Since the flux distribution in the gap at (11) is established by the shape of the rotor poles, the flux travels with the rotor and generates the voltage in the stator winding. It is important to note that the magnetic section of the stator is 100 per cent effective in producing power as in a conventional salient pole generator. The generated voltage is controlled by adjusting the power delivered to the excitation winding.

The low specific weight of the generator is realized primarily by the rotor design and the resulting high speed capability. The pole pieces are tapered in three directions to establish minimum rotor sections required for the rating since the flux density decreases linearly to zero from the root to the tip of each pole. This limits the leakage flux between poles and reduces the mass of the pole pieces improving the mechanical design. The solid rotor separator serves as an effective damper winding for transient and unbalanced load operation. Since the size of any generator for a given rating is a direct function of speed, the high strength welded rotor construction produces a weight advantage through its ability to operate at high peripheral velocities. Earlier generators operated with rotor surface speeds of

Figure 3.61. Brushless Homopolar Inductor Generator

Figure 3.62. Generator Rotor Construction Schematic

525 ft/sec. Present rotor design improvements have resulted in successful rotor spin tests at surface speeds up to 875 ft/sec. This makes this type of generator ideally suited for being direct driven by high rotative speed turbines.

3.3.10 Jet Pump

A jet pump would be utilized instead of a jet condenser in those ORCEP-PCU systems only where a low net positive suction head might result in pump cavitation. Of the four final candidate working fluids selected, only FL-M required a jet pump to raise the nominal 7.24 psia condenser pressure to 10, 15 and 20 psia (to meet suction specific speed requirements) for the 25, 50 and 75 KW_e systems respectively. The jet pump (shown schematically in Figure 3.63 mixes the high pressure turbopump discharge flow (liquid) with the low pressure condenser exit flow (liquid). This increases the pump inlet pressure sufficiently so that pump cavitation is avoided. A cross sectional drawing of a jet pump assembly is shown in Figure 3.64.

The jet pump consists of an inlet nozzle section, a mixing section and a diffuser section. The diffuser section converts some of the kinetic energy into pressure and provides the required inlet pressure to the pump axial-flow inducer. The high pressure flow enters the venturi throat at high velocity creating a low static pressure at the nozzle tip of the injector. The combined flow is then gradually expanded in the venturi throat and out into the pump inlet housing thus recovering most of the velocity head. This results in a low overall loss in pressure and an acceptable jet pump efficiency.

Tests conducted at TRW on other programs, using water and Freon 113 are presented in Figure 3.65 together with an ideal performance curve. The figure depicts the total pressure and the component static and dynamic pressures at the four critical locations within the jet pump. As may be noted, the major loss is due to the lack of recovery of the dynamic pressure between the venturi throat and the pump inlet. During these tests, the centrifugal pump inducer was not utilized, thus the exact geometry of this transition section was not duplicated. The overall performance would be significantly improved in actual operation with the pump impeller in place. The contoured pintle extending out from the pump inducer enhances the diffuser action at the outlet of the venturi throat thus increasing the static pressure recovery.

Figure 3.63. Reactor-Organic Rankine Cycle Electric Power System (ORCEPS) Schematic

Figure 3.64. Cross-Sectional Drawing of Jet Pump

Figure 3.65. Jet Pump Pressure Variations as Function of Location

Additional jet pump performance data was performed to characterize the performance at various nozzle positions (Figure 3.66). The ratio of the flow of the high pressure liquid to the low pressure liquid is only 2:1 (compared to 10:1 in a jet condenser). Hence, the performance penalty on overall system efficiency is minimal.

3.4 OPERATING CHARACTERISTICS

There are several potentially attractive applications for the U_{235} -ZrH reactor powered ORCEPS. Some of these are listed in Table 3.10. This study was oriented towards meeting the electrical load requirements of the long-term Mission No. 1 (Table 3.10). However, the technology and parametric data provided in this study could be used as a basis to extrapolate to the power levels for the other missions.

The required operating characteristics of the long-term space-based radar surveillance system (designated System "B" in Reference 29) are summarized in Table 3.11.

3.4.1 Electrical Loads

The required power level for the antenna of System B is 40.2 KW_e . Additional spacecraft subsystem loads (i.e., communication, command and control, attitude control, data processing, etc.) were estimated to be 4.8 KW_e . Finally, power conditioning and transmission losses were conservatively estimated at 0.5 KW_e and 4.5 KW_e . These values are summarized in Table 3.12.

In order to evaluate the potential degree of standardization and commonality afforded by the reactor-ORCEPS, a power level range of 10-75 KW_e was selected for parametric evaluation and system trade-off during this study. The proposed standard system would be $50 \text{ KW}_e + 25 \text{ KW}_e$ in order to encompass this range. The 50 KW_e System "B" power level would be assumed as the baseline power system when evaluating standardization.

3.4.2 Zero-G Operation

The reactor-ORCEPS system which utilizes NaK in the reactor heat source loop and an organic working fluid (i.e., CP-25, CP-28, FL-M, RC-1) in the PCU loop is a closed system. Because of this, the entire system is capable of satisfactory space start-up and continuous operation in a zero-gravity

Figure 3.66. Jet Pump Performance Data with Freon 113

Table 3.10. Potential Applications For U_{235} -ZrH Reactor - ORCEPS Power System

APPLICATION	POWER LEVEL; KW _e -
1. SPACE BASED RADAR SURVEILLANCE SYSTEMS	
• Near-Term (1985) Missions - Hardened Power System	20 - 25
• Long-Term (1995) Missions - Hardened Power System	50 - 75
2. SUB-SEA NUCLEAR POWER GENERATING STATION	
• Artic/Alaska Off Shore Oil Operations	3000 - 15000
3. REMOTE SITE UNATTENDED NUCLEAR POWER SYSTEM	
• Canadian Dew Line Replacement	2 - 10
• Downed Aircraft/Lost Hunters Communication Relay Station	2 - 10
4. AIR FORCE (CIVIL ENGRG. CTR. - FLORIDA) POWER GENERATING STATION	
• Remote Cold Regions	1000
5. LONG DURATION (10 YR) HARDENED MISSILE SILO POWER SYSTEM	
• Hard Rock Minuteman Missile Silos or Other Hardened Underground Facilities	12 - 20
6. EXPLORATION OF OUTER PLANETS (SATURN, JUPITER, PLUTO, URANUS)	
• Side Looking Radar Planetary Mapping	25 - 40
7. MANNED SPACE STATION	
• Manufacturing	25 - 100
• Hospital	25 - 100
• Scientific Research	25 - 100

Table 3.11. Space Based Radar Surveillance
System Operating Characteristics (Ref. 29)

● POWER LEVEL	50KW _e
● OPERATING LIFE	7 Years
● TOTAL INTEGRATED RADIATION LEVELS (At Base Of Shield)	
- NEUTRONS	10 ¹³ NVT
- GAMMA RAYS	10 ⁷ RADS
● ORBIT	Geosynchronous - Equatorial Or 5500 N.MI. Circular
● LAUNCH CONFIGURATION	Space Shuttle Plus Intermediate Upper Stage (IUS)
● POWER SYSTEM RELIABILITY	0.97 - 0.995

Table 3.12. SBRSS System "B" Electrical Power Requirements

<u>System Subassembly</u>	<u>Power Level at 28VDC (KW_e)</u>
Antenna Prime Sensor	40.2
Spacecraft Subsystems	4.8
Power Conditioning Losses	2.5
Transmission Line Losses	2.5
Total	50.0

environment. It will also operate successfully in an acceleration field of varying magnitude and vectorial orientation, or within a constant gravitational field of varying orientation.

The turbine back pressure range for the selected four candidate working fluids is 7.24 to 25 psia. The pressure drop needed to establish stable two-phase flow within the condenser-radiator is adequately met by this turbine back pressure range. In addition, swirl wires are incorporated into the tubes of the NaK/organic fluid boiler. This creates a continuous artificial-g field of sufficient magnitude to assure good heat transfer in all sections of the boiler (i.e., liquid, two-phase, superheat).

Various tests and analytical models have been carried out (References 9 and 10) to confirm the adequacy of the reactor-ORCEPS systems to operate under zero-g conditions. The design techniques utilized and the experience gained during these previous programs assure that the reactor-ORCEPS systems will operate satisfactorily under any of the currently anticipated gravitational modes that would be imposed upon it.

3.5 CANDIDATE POWER SYSTEM CONCEPTS

Nuclear reactor power systems have been proposed for many manned and unmanned space applications during the last two decades. During this period, the U_{235} -ZrH reactor was under active development as part of the overall SNAP program (i.e., SNAP 2, 8 and 10A). Approximately 10,000 hours of ground operation and over 1000 hours of space operation have been demonstrated with the SNAP 10A, 43KW_T reactor. Consideration was also given to these type reactor power systems for the NASA Manned Space Station and the DoD Hard Rock Silo development programs. When coupled with the low peak cycle temperature (750°F) ORCEPS, this nuclear reactor power system becomes a very attractive candidate for many, long duration (7 years) high power (10-75KW_e) space applications. Hence, the U_{235} -ZrH reactor was the baseline heat source considered during this study.

In transferring the thermal energy from the reactor core to the PCU working fluid, there are two configurations that can be considered (i.e., a two-loop or a three-loop system). These are shown schematically in Figure 3.67. In the three-loop system NaK is circulated in the primary reactor loop and an intermediate NaK heat transfer loop. Reactor thermal energy is transferred to the intermediate loop through a NaK to NaK heat exchanger. This

Figure 3.67. Two-Loop and Three-Loop Reactor-ORCEPS Space System Schematics

heat exchanger is shielded from the highly radioactive reactor core. In addition, a second shield is placed between this heat exchanger and the PCU loop containing the working fluid. The pressure of the NaK in the intermediate loop is maintained at a higher pressure than that in the reactor loop. Because of this, if a small leak should develop in the NaK to NaK heat exchanger, the leakage would be towards the reactor loop. The use of isolation valves and/or check valves in this configuration can also assure that the radioactive NaK will not come in contact with the lower level radioactive or non-radioactive organic working fluid loop.

For space applications, the two-loop system is preferred since it results in a lighter, small volume system. In addition, by eliminating the parasitic losses associated with the pump and heat exchanger of the intermediate loop, a higher overall system efficiency results. By operating at low peak cycle temperatures, selection of the proper materials, and the use of adequate quality control techniques during the manufacturing and quality control phases of a development program, the reliability of the two-loop system can equal or even exceed the three-loop system.

From a safety standpoint, the flight qualified system is not in a critical (i.e., radioactive) state during launch and orbit injection operations. The reactivity control, (i.e., regulating neutron leakage) is provided by the rotating beryllium reflector drums which are external to the core. These drums are locked in place during launch operations with a doubly redundant locking mechanism which precludes any accidental start-up during ground checkout and launch operations. In addition, the zirconium hydride reactor also exhibits an inherent safety mechanism at over-temperature or over-power conditions in which positive reactor shutdown is obtained by hydrogen evolution from the core. In view of the foregoing, the two-loop system was selected as the baseline candidate power system configuration.

3.5.1 Power System Requirements

The net electrical power output range of 10 to 75KW_e was selected to encompass the initial requirements for the Space Based Radar Surveillance System (SBRSS) (Reference 28). In addition, there are several other potential Air Force, DoD, ERDA, or NASA applications (See Section 3.4.3) over a wider range of power levels, whose designs could be extrapolated from this baseline reactor-ORCEPS system.

Other important factors in establishing the power system requirements are the transmission line and power conditioning losses associated with providing the net electrical power to the spacecraft load bus. These are a function of the spacecraft/payload system design configuration and the load characteristics. For the SBRSS, a 28 VDC output is required. Other applications may consider higher voltages or a combination of A.C. and D.C. power. For this study the power conditioning losses were assumed not to exceed 5 percent. This value was used (i.e., $\eta_{pcu} = 0.95$) in establishing the performance, weight, and volume of the candidate systems. The electrical transmission losses are not only a function of the spacecraft/payload design but also of the detail design of the power transmission cables. Since large diameter cabling or higher voltage transmission lines could reduce these losses considerably, a detailed trade-off analysis would be required for each application in order to arrive at an optimum configuration. By mutual agreement with ERDA/NRA, this was beyond the scope of this study. Hence, these losses and the weights associated with the power transmission cabling weight and associated deployment mechanism and environmental protection components were not included in the candidate power system performance and weight summaries. In an earlier study (Reference 7), these losses were assumed to be approximately 5 percent. The magnitude of these losses on this basis (see Figure 3.68) varied from $0.5KW_e$ at the $10KW_e$ level to $3.75KW_e$ at the $75KW_e$ level.

3.5.2 Power System Configurations

The reactor-ORCEPS configuration can be separated into heat source, power conversion, and heat rejection components. The heat source consists of the U_{235} -ZrH reactor, shield, and liquid metal components (E/M pump, T/E converter, accumulator, NaK heat exchanger, and NaK heat transfer fluid). The power conversion components include the organic fluid boiler, turbine, pump, regenerator, alternator, power conditioning unit, bearings and seals. The heat rejection components consist of the condenser-radiator and depending on cycle conditions (i.e., turbine exhaust pressure) a jet condenser or jet pump.

The first candidate system considered during this study (Candidate System No. 1) is shown in Figure 3.69. It is based upon the use of the SNAP program liquid metal loop technology and the power conversion loop configuration of the "KIPS" power system (Reference 29). As may be noted, the thermoelectric

Figure 3.68. Gross Electrical Power and Electrical Transmission Losses versus Net Electrical Power

Figure 3.69. Candidate System No. 1 - Reactor-Organic Rankine Cycle Electric Power System (ORCEPS) Schematic

(T/E) converter is used to supply the electrical power to the E/M pump. The T/E converter uses lead-telluride (Pb-Te) couples which provide low voltage (approx. 3 volts), high amperage (100 to 500 amps) output power. The converter also utilizes a portion of the NaK flow being circulated in the reactor liquid metal loop to maintain the hot junction temperature of the couples at approximately 800°F (427°C). A portion of the flow from the organic loop pump discharge is circulated through the cooling coils of the T/E converter to maintain the Pb-Te modules cold junction temperature at approximately 225°F (107°C). This results in an overall T/E converter efficiency of 2.5% (See Figure 3.14). The input power to the E/M pump is a function of the pump flow rate, pressure rise, and efficiency (See Figures 3.5, 3.6 and 3.7). The required E/M pump input power also varies with the net electrical output power of ORCEPS and for the 10-75KW_e range is approximately 0.4 to 1.0KW_e. Since the T/E converter efficiency is 2.5%, a thermal input power of from 16 to 40KW_T is required from the reactor heat source (see Figure 3.15).

The NaK-to-organic working fluid heat exchanger-boiler which would use min-K or superinsulation to minimize heat losses, would have an efficiency of 0.95%. The NaK loop volume-accumulator volume would vary from 0.2 to 2.0 cu. ft. for the 10 to 75KW_e power range.

The PCU turbine efficiency would be 0.85. This performance level is achievable in the 10 to 75KW_e range since the ORCEPS high flow rates and turbine speeds combined with comparatively low isentropic heads results in high specific speeds and hence high efficiencies. Since high velocity ratios (0.5 to 0.9) are also obtainable with the ORCEPS cycle conditions, a single-stage turbine can be utilized. This results in both minimum weight and volume. Similarly, the organic fluid centrifugal pump using a radial impeller, diffuser, and discharge scroll and an axial inducer can readily achieve an overall pump efficiency of 0.65 and suction specific speeds (rpm/gpm basis) in the 15,000 to 30,000 range. The brushless, homopolar induction alternator would achieve an overall efficiency of 0.87. However, to match the typical load characteristics, (Section 3.4), a power conditioning unit would be required. With recent advances in solid state components, a power conditioning unit efficiency of 0.95 would be achievable.

With proper design and without incurring excessive weight and volume penalties, a regenerator effectiveness of 0.95 can be achieved. This is an

important parameter for obtaining high PCU efficiencies since the regenerator transfers most of the turbine exhaust heat to the working fluid before entering the boiler. This considerably reduces the thermal input required by the PCU boiler, which results in higher efficiencies.

The condenser-radiator weight and size is a function of the PCU heat rejection temperature, its emissivity, and the PCU efficiency which determines the quantity of heat that must be rejected. In addition, a heat sink temperature must be assumed. For this study, this value was taken as 50°R. The PCU heat rejection temperatures (200°F-215°F) and efficiencies (19.7%-24.7%) varied with the candidate working fluids and cycle conditions selected (see Sections 3.3.2 and 3.3.3). The emissivity was assumed to be 0.9, since this is a realistically attainable value utilizing existing space radiator technology (i.e., second surface mirrors, Z-93 paint, etc).

Candidate System No. 1 also includes an optional jet condenser. This component is only required when the turbine exhaust pressure is too low (i.e., KIPS, Dowtherm A cycle; $P_2 = 0.1$ psia) to overcome the pressure drop in the condenser-radiator and/or to provide adequate net positive suction head to the cycle pump inlet. For the KIPS-Dowtherm A cycle conditions, a flow ratio (high pressure pump discharge subcooled liquid to cycle working fluid flow rate) of 9:1 is required in order to meet cycle conditions (Reference 30). The use of a jet condenser in an organic Rankine cycle increases the required pump work and hence reduces the overall PCU efficiency. This occurs despite the potential increase in turbine efficiency due to a reduced back pressure.

Candidate System No. 2 is shown on Figure 3.70. The essential difference between Candidate Systems 1 and 2 is that System 2 replaces the NaK loop thermoelectric converter with a larger thermoelectric converter which operates in a topping cycle manner with respect to the ORCEPS. This T/E converter replaces the PbTe modules with silicon germanium (SiGe) modules operating at a hot junction temperature of 1270°F, and a cold junction temperature of 700°F. This results in a T/E converter efficiency of approximately 3.5% compared to 2.5% for the PbTe converter. A portion of this T/E converter electrical output is used to power the E/M pump. The remaining output power is combined with the output from the alternator by the use of an appropriate power conditioning unit. By utilizing the heat rejected from

Figure 3.70. Candidate System No. 2 - Reactor Hybrid-Organic Rankine Cycle Electric Power System (ORCEPS) Schematic

the T/E converter portion of the PCU, higher overall cycle efficiencies can be achieved and smaller radiator areas are required. The peak cycle temperature to the turbine must be reduced from 750°F to 650°F and the reactor core temperature must be increased to 1300°F. Hence, some of the advantages of this configuration are offset by these changes in system temperature requirements.

A third configuration was also considered during this study. This was designated Candidate System No. 3 and is shown on Figure 3.71. This system is similar to System No. 1 except that the jet condenser has been eliminated. An optional jet-pump is included for use only with any of the candidate working fluids that might require it to meet cycle pump net positive suction head requirements. The advantage of the jet pump over the jet condenser, to accomplish this function, is that its required flow ratio is only 2:1 as compared to 9:1 in Reference 29 and 16:1 in Reference 30. This concept also includes an optional preheater (which would become part of the regenerator) which rejects the T/E converter waste heat to the cycle working fluid thus enhancing the overall system efficiency and reducing radiator area requirements. If the jet pump is not utilized, the T/E converter waste heat is still rejected to the cycle working fluid in the regenerator.

3.5.3 Reference Design Configuration

From the foregoing discussion, it was concluded that Candidate System No. 3 should be selected as the Reference Reactor-ORCEPS Design. The rationale for this choice was the desire to eliminate the need for a jet condenser. In addition to the detrimental impact this component has on PCU efficiency and weight, it also reduces the flexibility of the system for achieving standardization. This is due to the fact that the jet condenser must be uniquely sized to the cycle flow rates, pressures, and enthalpies associated with a specific power level.

The use of the T/E converter in a topping mode with respect to the organic Rankine cycle loop has merit in that it enhances PCU efficiency and reduces overall system weight (i.e., eliminates separate NaK loop T/E converter, results in smaller condenser-radiator and regenerator). However, to achieve this level of performance, it is necessary to increase the reactor core NaK outlet temperature from 850°F (455°C) to 1300°F (705°C). Operation of the ZrH reactor at this temperature level will significantly reduce its

Figure 3.71. Candidate System No. 3 - Reactor-Organic Rankine Cycle Electric Power System (ORCEPS) Schematic

operating life (see Figure 3.1), particularly in the higher reactor thermal power range (100 to 300KW_T).

Finally, Candidate System No. 3 represents the simplest configuration. By judicious selection of working fluids and cycle conditions, except in a few instances, there is no need to utilize the optional jet pump in this configuration. In addition, since the jet pump is such a simple device (single phase liquids only), it has considerable potential for enhancing standardization and commonality for a range of output power levels with an insignificant impact on PCU performance and weight.

3.6 REACTOR-ORCEPS PARAMETRIC DATA

The four candidate working fluids selected for this study were CP-25, CP-28, FL-M and RC-1. The rationale for their selection and cycle conditions was provided in Section 3.3.3. The major parameters affecting the reactor-ORCEPS weight and volume are power level, efficiency (overall and PCU), and condensing temperature. The reactor-ORCEPS overall performance and design characteristics are summarized on Tables 3.13 and 3.14. The gross electrical power output includes the power conditioning losses, but not the transmission line losses between the reactor-ORCEPS and the spacecraft load bus. The PCU efficiency is given by;

$$\eta_{pcu} = \frac{\text{Gross Electrical Power Output}}{\text{Boiler Thermal Input}}$$

and the overall system efficiency is defined as;

$$\eta_{oA} = \frac{\text{Net Electrical Power Output}}{\text{Reactor Thermal Output}}$$

The ORCEPS power conversion unit efficiencies are a function of working fluid thermodynamic properties, cycle conditions, and achievable PCU component efficiencies. The overall system efficiencies take into account the reactor-NaK loop component efficiencies, thermal losses and the PCU generator power conditioning losses. The resulting efficiencies obtained are summarized in Table 3.15.

Table 3.13. Reactor - ORCEPS Parametric Data - Overall Performance/Design Characteristics

	Cycle Working Fluid							
	Toluene (CP-25)				Hexafluorobenzene (CP-28)			
<u>POWER, KW</u>								
Gross Electrical Power Output	10.5	26.3	52.5	78.8	10.5	26.3	52.5	78.0
Net Electrical Output	10.0	25.0	50.0	75.0	10.0	25.0	50.0	75.0
Electromagnetic Pump Power Input	0.40	0.50	0.72	0.95	0.45	0.55	0.80	1.0
Thermoelectric Converter Thermal Input	16.0	19.0	30.0	37.0	16.2	22.0	31.0	39.0
Reactor Thermal Output	61.2	132.1	255.4	374.7	67.0	149.6	285.0	419.8
Boiler Thermal Input	42.5	106.5	212.6	319.0	47.9	120.1	239.7	359.8
Radiator Heat Rejection	32.0	80.2	160.1	240.2	37.4	93.8	187.2	281.0
<u>EFFICIENCY, %</u>								
Overall System	16.4	19.0	19.6	20.0	15.0	17.6	18.4	18.8
Power Conversion Unit	24.7	24.7	24.7	24.7	21.9	21.9	21.9	21.9
Thermodynamic Cycle	28.4	28.4	28.4	28.4	25.2	25.2	25.2	25.2
Carnot & % of Carnot	45.0/52.2	45.0/52.2	45.0/52.2	45.0/52.2	45.5/45.8	45.5/45.8	45.5/45.8	45.5/45.8
Components								
• Turbine	85.0	85.0	85.0	85.0	85.0	85.0	85.0	85.0
• Generator (Overall)	87.0	87.0	87.0	87.0	87.0	87.0	87.0	87.0
•• Electromagnetic	95.5	95.5	95.5	95.5	95.5	95.5	95.5	95.5
•• Mechanical (Bearings & Windage)	96.0	96.0	96.0	96.0	96.0	96.0	96.0	96.0
•• Power Conditioning Unit	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
• Pump (Overall)	65.0	65.0	65.0	65.0	65.0	65.0	65.0	65.0
•• Hydraulic	80.0	80.0	80.0	80.0	80.0	80.0	80.0	80.0
•• Volumetric	90.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0
•• Mechanical (Seals)	90.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0
• Boiler	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
• Regenerator	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
<u>TEMPERATURES °F (°C)</u>								
Reactor Outlet	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)
Boiler Outlet	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)
Turbine Inlet	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)
Condenser-Radiator (Average)	208 (98)	208 (98)	208 (98)	208 (98)	200 (93.5)	200 (93.5)	200 (93.5)	200 (93.5)
<u>PRESSURES, psia (Kg/cm²)</u>								
Boiler Outlet	505 (35.45)	505 (35.45)	505 (35.45)	505 (35.45)	405 (28.43)	405 (28.43)	405 (28.43)	405 (28.43)
Turbine Inlet	500 (35.1)	500 (35.1)	500 (35.1)	500 (35.1)	399 (28.01)	399 (28.01)	399 (28.01)	399 (28.01)
Turbine Outlet	12.0(0.843)	12.0(0.843)	12.0(0.843)	12.0(0.843)	25.0(1.76)	25.0(1.76)	25.0(1.76)	25.0(1.76)
Pump Inlet	10.0(0.702)	10.0(0.702)	10.0(0.702)	10.0(0.702)	22.0(1.54)	22.0(1.54)	22.0(1.54)	22.0(1.54)
Pump Outlet	525 (36.86)	525 (36.86)	525 (36.86)	525 (36.86)	425 (29.84)	425 (29.84)	425 (29.84)	425 (29.84)

Table 3.14. Reactor - ORCEPS Parametric Data - Overall Performance/Design Characteristics

	Cycle Working Fluid							
	Fluorozone M (FL-M)				40/60 Hexafluorobenzene-Pentafluorobenzene (RC-1)			
<u>POWER, KW</u>								
Gross Electrical Power Output	10.5	26.3	52.5	78.8	10.5	26.3	52.5	78.8
Net Electrical Output	10.0	25.0	50.0	75.0	10.0	25.0	50.0	75.0
Electromagnetic Pump Power Input	0.40	0.50	0.72	0.95	0.45	0.55	0.80	1.0
Thermoelectric Converter Thermal Input	16.0	19.0	30.0	37.0	16.2	21.5	30.5	38.5
Reactor Thermal Output	64.2	139.0	268.9	395.8	72.7	163.2	312.6	461.6
Boiler Thermal Input	45.1	112.9	225.5	338.2	53.3	133.5	266.5	400.0
Radiator Heat Rejection	34.6	86.6	173.0	259.4	42.8	107.2	214.0	321.2
<u>EFFICIENCY, %</u>								
Overall System	16.0	18.0	18.6	19.0	14.0	15.3	16.0	16.3
Power Conversion Unit	23.3	23.3	23.3	23.3	19.7	19.7	19.7	19.7
Thermodynamic Cycle	26.8	26.8	26.8	26.8	22.6	22.6	22.6	22.6
Carnot & % of Carnot	45.5/56.0	45.5/56.0	45.5/56.0	45.5/56.0	44.3/48.7	44.3/48.7	44.3/48.7	44.3/48.7
Components								
• Turbine	85.0	85.0	85.0	85.0	85.0	85.0	85.0	85.0
• Generator (Overall)	87.0	87.0	87.0	87.0	87.0	87.0	87.0	87.0
•• Electromagnetic	95.5	95.5	95.5	95.5	95.5	95.5	95.5	95.5
•• Mechanical (Bearings & Windage)	96.0	96.0	96.0	96.0	96.0	96.0	96.0	96.0
•• Power Conditioning Unit	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
• Pump (Overall)	65.0	65.0	65.0	65.0	65.0	65.0	65.0	65.0
•• Hydraulic	80.0	80.0	80.0	80.0	80.0	80.0	80.0	80.0
•• Volumetric	90.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0
•• Mechanical (Seals)	90.0	90.0	90.0	90.0	90.0	90.0	90.0	90.0
• Boiler	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
• Regenerator	95.0	95.0	95.0	95.0	95.0	95.0	95.0	95.0
<u>TEMPERATURES °F</u>								
Reactor Outlet	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)	850 (455)
Boiler Outlet	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)	775 (413)
Turbine Inlet	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)	750 (399)
Condenser-Radiator (Average)	200 (98)	200 (98)	200 (98)	200 (98)	214 (101.2)	214 (101.2)	214 (101.2)	214 (101.2)
<u>PRESSURES, psia (Kg/cm²)</u>								
Boiler Outlet	305 (21.41)	305 (21.41)	305 (21.41)	305 (21.41)	305 (21.41)	305 (21.41)	305 (21.41)	305 (21.41)
Turbine Inlet	300 (21.06)	300 (21.06)	300 (21.06)	300 (21.06)	300 (21.06)	300 (21.06)	300 (21.06)	300 (21.06)
Turbine Outlet	9.0(0.632)	9.0(0.632)	9.0(0.632)	9.0(0.632)	27.0(1.895)	27.0(1.895)	27.0(1.895)	27.0(1.895)
Pump Inlet	7.24(0.51)	7.24(0.51)	7.24(0.51)	7.24(0.51)	25.0(1.755)	25.0(1.755)	25.0(1.755)	25.0(1.755)
Pump Outlet	325 (22.82)	325 (22.82)	325 (22.82)	325 (22.82)	325 (22.82)	325 (22.82)	325 (22.82)	325 (22.82)

Table 3.15. Reactor-ORCEPS Performance Summary

Net Electrical Power Level	PCU Efficiency				Overall System Efficiency			
	10	25	50	75	10	25	50	75
<u>Working Fluid</u>								
Toluene (CP-25)	24.7	24.7	24.7	24.7	16.4	19.0	19.6	20.0
Hexafluorobenzene (CP-28)	21.9	21.9	21.9	21.9	15.0	17.6	18.4	18.8
Fluorozene M (FL-M)	23.3	23.3	23.3	23.3	16.0	18.0	18.6	19.0
40/60 Hexafluorobenzene/ Pentafluorobenzene (RC-1)	19.7	19.7	19.7	19.7	14.0	15.3	16.0	16.3

As may be noted, the PCU efficiencies varied from 19.7% to 24.7%. The overall system efficiencies varied from 14.0% to 20.0%. At a nominal 50KW_e net electrical power level, where the impact of the low efficiency (i.e., 2.5%) NaK loop T/E converter is not as great as in the lower power levels, the range of overall system efficiencies was 16.0% to 19.6%.

Another important parameter for establishing the reactor-ORCEPS weight and volume is the radiator area. This is a function of the system heat rejection requirements (at average condensing temperature, T_1) from the ORCEPS PCU and NaK loop T/E converter (q_r) and the radiator emissivity, (ϵ). The ORCEPS range for the condensing temperatures was 200°F to 214°F. These values are a compromise between maximizing achievable PCU efficiency and minimizing radiator area. For space applications, an emissivity of 0.9 is readily achievable through the use of selective coatings (i.e., Z-93 paint) or second surface mirrors. For an assumed average heat sink temperature of 50°R, the required radiator specific area (A/q_r ; ft²/KW_T) as a function of condensing temperature is shown on Figure 3.72. For the selected range of condensing temperatures, the specific radiator areas varied from 10.5 to 11.5 ft²/KW_T. Using these values and the radiator heat rejection requirements indicated in Tables 3.13 and 3.14, the total radiator areas were determined for the four working fluids and summarized in Tables 3.16 and 3.17.

Figure 3.72. Radiator Specific Area versus Condensing Temperature

Table 3.16. Reactor - ORCEPS Parametric Data - Overall Performance/Design Characteristics

Net Electrical Power Level KW_e	TOLUENE (CP-25)				HEXAFLUOROBENZENE (CP-28)			
	10	25	50	75	10	25	50	75
<u>FLOW RATE, lb/sec (Kg/sec)</u>								
Reactor Loop-NaK Pump	2.5 (1.135)	7.0 (3.18)	13.0 (5.90)	19.0 (8.63)	2.8 (1.27)	8.0 (3.63)	14.7 (6.67)	21.5 (9.77)
Reactor Loop- T/E Converter NaK Flow	0.15 (0.068)	1.12 (0.784)	1.40 (0.980)	1.66 (1.163)	1.00 (0.700)	1.17 (0.820)	1.44 (1.010)	1.74 (1.22)
Cycle Working Fluid Loop	0.182 (0.083)	0.455 (0.206)	0.911 (0.413)	1.366 (0.620)	0.463 (0.210)	1.158 (0.525)	2.317 (1.051)	3.476 (1.577)
Jet Pump Loop	N/A N/A	N/A N/A	N/A N/A	N/A N/A	N/A N/A	N/A N/A	N/A N/A	N/A N/A
PCU Pump	0.182 (0.083)	0.455 (0.206)	0.911 (0.413)	1.366 (0.620)	0.463 (0.210)	1.158 (0.525)	2.317 (1.051)	3.476 (1.577)
<u>PRESSURE DROP, psi (Newtons/ cm²)</u>								
Reactor Loop-NaK Pump	0.97 (0.680)	1.12 (0.784)	1.40 (0.980)	1.66 (1.163)	1.00 (0.700)	1.17 (0.820)	1.44 (1.010)	1.74 (1.22)
Regenerator (Liquid Side)	0.024 (0.017)	0.40 (0.280)	0.62 (0.434)	1.40 (0.980)	0.004 (0.003)	0.025 (0.017)	0.10 (0.070)	0.25 (0.175)
Regenerator (Vapor Side)	0.016 (0.011)	0.10 (0.070)	0.40 (0.280)	0.90 (0.630)	0.022 (0.015)	0.250 (0.175)	0.56 (0.392)	1.33 (0.931)
Boiler	1.30 (0.91)	6.20 (4.34)	20.0 (14.0)	39.6 (27.8)	1.30 (0.91)	6.20 (4.34)	20.0 (14.0)	39.6 (27.8)
Condenser-Radiator	0.13 (0.09)	0.62 (0.43)	2.00 (1.40)	3.96 (2.77)	0.13 (0.09)	0.62 (0.43)	2.00 (1.40)	3.96 (2.77)
<u>SPECIFIC RADIATOR AREA, ft²/KW_T (M²/KW_T)</u>	11.0 (1.03)	11.0 (1.03)	11.0 (1.03)	11.0 (1.03)	11.5 (1.075)	11.5 (1.075)	11.5 (1.075)	11.5 (1.075)
<u>RADIATOR AREA, ft² (M²)</u>	352 (33.0)	881 (82.5)	1762 (165.0)	2643 (247.4)	433 (40.5)	1082 (101.3)	2165 (202.6)	3248 (304.0)
<u>TOTAL SYSTEM MASS, lb (Kg)</u>	1073 (487)	1823 (829)	2972 (1350)	4069 (1848)	1171 (533)	2042 (929)	3418 (1554)	4699 (2135)
<u>SYSTEM SIZE, ft (M)</u>								
Launch Height	15.2 (4.63)	16.5 (5.03)	15.5 (4.72)	19.8 (6.03)	15.2 (4.63)	16.5 (5.03)	15.5 (4.72)	19.8 (6.03)
Deployed Height	40.4 (12.31)	57.5 (17.53)	68.0 (20.73)	88.2 (26.88)	40.4 (12.31)	57.5 (17.53)	81.5 (24.84)	107.0 (32.61)
Diameter (max)	5.8 (1.77)	8.3 (2.53)	12.3 (3.75)	13.0 (3.96)	5.8 (1.77)	8.3 (2.53)	12.3 (3.75)	13.0 (3.96)
Dose Plane Separation Distance	30.0 (9.14)	50.0 (15.24)	60.0 (18.29)	100.0 (30.48)	30.0 (9.14)	50.0 (15.24)	60.0 (18.29)	100.0 (30.48)
<u>NUMBER OF RADIATOR SECTIONS</u>	2	3	4	4	2	3	5	5

Table 3.17. Reactor - ORCEPS Parametric Data - Overall Performance/Design Characteristics

Net Electrical Power Level KW_e	FLUOROZENE M (FL-M)				40/60 HEXAFLUOROBENZENE/PENTAFLUOROBENZENE (RC-1)			
	10	25	50	75	10	25	50	75
<u>FLOW RATE, lb/sec (Kg/sec)</u>								
Reactor Loop-NaK Pump	2.1 (0.952)	6.0 (2.72)	11.0 (5.0)	17.0 (7.72)	2.7 (1.225)	7.6 (3.45)	14.2 (6.45)	20.8 (9.44)
Reactor Loop- T/E Converter NaK Flow	0.14 (0.064)	0.69 (0.313)	1.34 (0.608)	2.03 (0.922)	0.164(0.075)	0.82 (0.372)	1.59 (0.723)	2.40 (1.09)
Cycle Working Fluid Loop	0.423(0.192)	1.056(0.479)	2.114(0.959)	3.170 (1.438)	0.511(0.232)	1.277(0.579)	2.555(1.159)	3.832(1.738)
Jet Pump Loop	N/A N/A	4.22 (1.92)	12.68 (5.76)	25.36 (11.53)	N/A N/A	N/A N/A	N/A N/A	N/A N/A
PCU Pump	0.423 (0.192)	5.28 (2.40)	14.79 (6.72)	28.53 (12.97)	0.511 (0.232)	1.277 (0.579)	2.555 (1.159)	3.832 (1.738)
<u>PRESSURE DROP, psi (Newtons/ cm²)</u>								
Reactor Loop-NaK Pump	0.40 (0.280)	0.44 (0.308)	0.56 (0.392)	0.70 (0.49)	0.40 (0.280)	0.48 (0.336)	0.60 (0.420)	0.74 (0.518)
Regenerator (Liquid Side)	0.002(0.0014)	0.025 (0.0175)	0.12 (0.084)	0.25 (0.176)	0.009(0.0063)	0.05 (0.035)	0.22 (0.154)	0.50 (0.35)
Regenerator (Vapor Side)	0.05 (0.035)	0.28 (0.196)	1.06 (0.742)	2.5 (1.75)	0.022(0.0154)	0.25 (0.176)	0.56 (0.392)	1.33 (0.931)
Boiler	1.30 (0.91)	6.20 (4.34)	20.0 (14.0)	39.6 (27.8)	1.30 (0.91)	6.20 (4.34)	20.0 (14.0)	39.6 (27.8)
Condenser-Radiator	0.13 (0.09)	0.62 (0.43)	2.00 (1.40)	3.96 (2.77)	0.13 (0.09)	0.62 (0.43)	2.00 (1.40)	3.96 (2.77)
<u>SPECIFIC RADIATOR AREA, ft²/KW_T (M²/KW_T)</u>								
	11.5 (1.075)	11.5 (1.075)	11.5 (1.075)	11.5 (1.075)	10.5 (0.965)	10.5 (0.965)	10.5 (0.965)	10.5 (0.965)
<u>RADIATOR AREA, ft² (M²)</u>								
	400 (37.2)	1000 (92.9)	2000 (185.8)	3000 (278.7)	451 (41.9)	1129 (104.9)	2258 (209.8)	3387 (314.6)
<u>TOTAL SYSTEM MASS, lb (Kg)</u>								
	1113 (505)	1922 (873)	3192 (1451)	4380 (1991)	1171 (532)	2063 (938)	3464 (1574)	4769 (2168)
<u>SYSTEM SIZE, ft (M)</u>								
Launch Height	15.2 (4.63)	16.5 (5.03)	15.5 (4.72)	19.8 (6.03)	15.2 (4.63)	16.5 (5.03)	15.5 (4.72)	19.8 (6.03)
Deployed Height	40.4 (12.31)	57.5 (17.53)	68.0 (20.73)	107.0 (32.61)	40.4 (12.31)	57.5 (17.53)	81.5 (24.84)	107.0 (32.61)
Diameter (max)	5.8 (1.77)	8.3 (2.53)	12.3 (3.75)	13.0 (3.96)	5.8 (1.77)	8.3 (2.53)	12.3 (3.75)	13.0 (3.96)
Dose Plane Separation Distance	30.0 (9.14)	50.0 (15.24)	60.0 (18.29)	100.0 (30.48)	30.0 (9.14)	50.0 (15.24)	60.0 (18.29)	100.0 (30.48)
<u>NUMBER OF RADIATOR SECTIONS</u>								
	2	3	4	5	2	3	5	5

The variation of radiator area and the resulting condenser-radiator/deployment structure weights as a function of working fluid and net electrical power level are shown in Figures 3.73 and 3.74. A specific radiator weight of 0.65 lb/ft^2 (including a deployment boom at 2 lb/ft and micrometeorite shielding) was assumed. This value was based upon the use of aluminum which is compatible with the ORCEPS condensing temperature range of 200°F to 225°F and previously defined micrometeorite penetration models (i.e., References 31, 32 and 33).

The weight of the PCU consists of the various components that comprise this overall ORCEPS subsystem. These include the following subassemblies:

- Regenerator
- Turbine Assembly (including housing, nozzle block, rotor shaft bearings, and seals)
- Pump Assembly (including axial inducer, radial impeller, diffuser, and discharge scroll)
- Generator and Power Conditioner
- Piping-Start-Up Accumulator
- Controls and Valves
- Jet Pump (including preheater) if utilized

Based upon the design approach and technology discussed in Section 3.3.5, the weight of the regenerator as a function of working fluid and net electrical power output was determined. These data are shown in Figure 3.75.

The turbine and housing weights for the various working fluids were determined from the preliminary design data depicted in Tables 3.18 to 3.21. The most significant parameter in establishing these weights is the turbine pitch diameter since the geometry of all the other components comprising the overall turbine assembly are contingent upon this dimension. The turbine pitch diameter, in turn, is a function of the various cycle conditions, rotational speeds, and design factors such as specific speed and specific diameter. The resulting values obtained for the turbine pitch diameters for the various working fluids is shown in Figure 3.76. The turbine efficiency achievable is a function of many detailed design parameters. The determination of these data is beyond the scope of this study. However, in general, if the turbine velocity ratio (for a single stage turbine) can be maintained at 0.5 or higher, a high value of turbine efficiency can be predicted. As may be noted in

Figure 3.73. Radiator Area versus Net Electrical Power Output

Figure 3.74. Condenser-Radiator/Deployment Structure Weight versus Net Electrical Power Output

Figure 3.75. Regenerator Weight versus Net Electrical Power Output

Table 3.18. ORCEPS-PCU Turbine Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	TOLUENE (CP-25)			
Cycle Flow Rate, m, lb/sec	0.182	0.455	0.911	1.366
Turbine Inlet Temperature, °F	750	750	750	750
Turbine Inlet Pressure, psia	500	500	500	500
Turbine Exit Temperature, °F	570	570	570	570
Turbine Exit Pressure, psia	10.5	10.5	10.5	10.5
Turbine Exit Specific Volume, v ₂ , ft ³ /lb	11.92	11.92	11.92	11.92
Turbine Speed, N, rpm	96,000	86,000	76,000	72,000
Turbine Inlet Enthalpy, H ₁ , BTU/lb	275.7	275.7	275.7	275.7
Turbine Exit Enthalpy, H ₂ , BTU/lb	188.4	188.4	188.4	188.4
Turbine Isentropic Head, ΔH _{isen} , BTU/lb	87.3	87.3	87.3	87.3
Turbine Isentropic Head, H _{isen} , ft	67,942	67,942	67,942	67,942
Turbine Specific Speed, N _s = $\frac{N\sqrt{cfs}}{(H_{isen})^{0.75}}$	33.61	47.58	59.52	69.04
Turbine Specific Diameter, D _s	2.16	1.62	1.33	1.26
Turbine Pitch Diameter, D _p = $\frac{D_s \times 12 \times \sqrt{cfs}}{(H_{isen})^{0.25}}$, in.	2.36	2.81	3.25	3.77
Turbine Velocity Ratio, U/C ₀	0.48	0.51	0.53	0.58
Turbine Efficiency, %	85	85	85	85

Table 3.19. ORCEPS-PCU Turbine Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	HEXAFLUOROBENZENE (CP-28)			
Cycle Flow Rate, $\dot{m}$, lb/sec	0.463	1.158	2.317	3.476
Turbine Inlet Temperature, °F	750	750	750	750
Turbine Inlet Pressure, psia	399	399	399	399
Turbine Exit Temperature, °F	631	631	631	631
Turbine Exit Pressure, psia	22.1	22.1	22.1	22.1
Turbine Exit Specific Volume, v_2 , ft ³ /lb	2.92	2.92	2.92	2.92
Turbine Speed, N, rpm	96,000	86,000	76,000	72,000
Turbine Inlet Enthalpy, H_1 , BTU/lb	260.5	260.5	260.5	260.5
Turbine Exit Enthalpy, H_2 , BTU/lb	226.6	226.6	226.6	226.6
Turbine Isentropic Head, ΔH_{isen} , BTU/lb	33.9	33.9	33.9	33.9
Turbine Isentropic Head, H_{isen} , ft	26,383	26,383	26,383	26,383
Turbine Specific Speed, $N_s = \frac{N\sqrt{cfs}}{(H_{isen})^{0.75}}$	53.88	76.38	95.53	110.81
Turbine Specific Diameter, D_s	1.40	1.24	1.21	1.19
Turbine Pitch Diameter, $D_p = \frac{D_s \times 12 \times \sqrt{cfs}}{(H_{isen})^{0.25}}$, in.	1.55	2.13	2.96	3.57
Turbine Velocity Ratio, U/C_0	0.51	0.63	0.77	0.88
Turbine Efficiency, %	85	85	85	85

Table 3.20. ORCEPS-PCU Turbine Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	FLUOROZONE M (FL-M)			
Cycle Flow Rate, $\dot{m}$, lb/sec	0.423	1.056	2.114	3.170
Turbine Inlet Temperature, °F	750	750	750	750
Turbine Inlet Pressure, psia	300	300	300	300
Turbine Exit Temperature, °F	640	640	640	640
Turbine Exit Pressure, psia	7.24	7.24	7.24	7.24
Turbine Exit Specific Volume, v_2 , ft ³ /lb	7.53	7.53	7.53	7.53
Turbine Speed, N, rpm	96,000	86,000	76,000	72,000
Turbine Inlet Enthalpy, H_1 , BTU/lb	291.1	291.1	291.1	291.1
Turbine Exit Enthalpy, H_2 , BTU/lb	253.8	253.8	253.8	253.8
Turbine Isentropic Head, ΔH_{isen} , BTU/lb	37.3	37.3	37.3	37.3
Turbine Isentropic Head, H_{isen} , ft	29,029	29,029	29,029	29,029
Turbine Specific Speed, $N_s = \frac{N\sqrt{cfs}}{(H_{isen})^{0.75}}$	76.97	109.03	136.35	158.17
Turbine Specific Diameter, D_s	1.24	1.10	0.95	0.85
Turbine Pitch Diameter, $D_p = \frac{D_s \times 12 \times \sqrt{cfs}}{(H_{isen})^{0.25}}$, in.	2.04	2.85	3.48	3.82
Turbine Velocity Ratio, U/C_0	0.64	0.80	0.86	0.89
Turbine Efficiency, %	85	85	85	85

Table 3.21. ORCEPS-PCU Turbine Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	40/60 HEXAFLUOROBENZENE-PENTAFLUOROBENZENE (RC-1)			
Cycle Flow Rate, $\dot{m}$, lb/sec	0.511	1.277	2.555	3.832
Turbine Inlet Temperature, °F	750	750	750	750
Turbine Inlet Pressure, psia	300	300	300	300
Turbine Exit Temperature, °F	634	634	634	634
Turbine Exit Pressure, psia	25	25	25	25
Turbine Exit Specific Volume, v_2 , ft ³ /lb	2.64	2.64	2.64	2.64
Turbine Speed, N, rpm	96,000	86,000	76,000	72,000
Turbine Inlet Enthalpy, H_1 , BTU/lb	242.1	242.1	242.1	242.1
Turbine Exit Enthalpy, H_2 , BTU/lb	211.6	211.6	211.6	211.6
Turbine Isentropic Head, ΔH_{isen} , BTU/lb	30.5	30.5	30.5	30.5
Turbine Isentropic Head, H_{isen} , ft	23,737	23,737	23,737	23,737
Turbine Specific Speed, $N_s = \frac{N\sqrt{cfs}}{(H_{isen})^{0.75}}$	58.34	82.57	103.19	119.79
Turbine Specific Diameter, D_s	1.60	1.23	1.20	1.18
Turbine Pitch Diameter, $D_p = \frac{D_s \times 12 \times \sqrt{cfs}}{(H_{isen})^{0.25}}$, in.	1.80	2.18	3.01	3.63
Turbine Velocity Ratio, U/C_0	0.62	0.68	0.82	0.94
Turbine Efficiency, %	85	85	85	85

Figure 3.76. Turbine Pitch Diameter versus Net Electrical Power Output

Figure 3.77, this range of velocity ratios is achievable for all the candidate working fluids.

The pump assembly weights for the various working fluids were determined from the preliminary design data depicted in Table 3.22 to 3.25. For this assembly, the radial impeller outside diameter is the most significant dimension for establishing the subsystem weights. The radial impeller outside diameter is a function of the cycle flow rate, pump pressure rise and speed. For these values the pump specific speed and specific diameter can be determined. The required radial impeller outside diameter can then be calculated. The results are shown in Figure 3.78. The pump performance is also based upon many detailed design parameters. However, for pumps operating in a specific speed range of from 385 to 2283 (on gpm basis), an overall efficiency of 0.65 is achievable utilizing judicious hydrodynamic design techniques. Another important pump design factor is the suction specific speed. This parameter, based upon the available net positive suction head, establishes the pump inlet geometry necessary to avoid cavitation. Values in the range of 15,000 to 30,000 (on gpm basis) are readily achievable with the use of proper pump inlet design techniques. The pump suction specific speed requirements, for the various working fluids, are depicted in Figure 3.79. If any difficulty was encountered in meeting the requirements, the most straightforward approach would be to increase the sub-cooling at the pump inlet.

The overall weights of the Turbine Assembly and Pump Assembly are shown in Figure 3.80. The remaining components comprising the overall ORCEPS-PCU are the piping and start-up accumulator, controls and valves, generator (including power conditioning unit) and the organic working fluid. These are shown in Figures 3.81, 3.82, and 3.83. The total PCU weights as a function of the various working fluids are depicted in Figure 3.84.

3.7 CANDIDATE SYSTEM TRADEOFFS

The parametric weight data generated in Sections 3.2, 3.3, and 3.6 was summarized in Tables 3.26 and 3.27 to facilitate a tradeoff analysis of the various candidate power systems. The total system mass and specific system mass for the four candidate systems is shown in Figure 3.85. The lightest system was the one employing CP-25 (toluene) as a working fluid. Typically, at a nominal value of 50KW_e , the difference between the CP-25 system (2972 lbs)

Figure 3.77. Turbine Velocity Ratio versus Net Electrical Power Output

Table 3.22. ORCEPS-PCU Pump Characteristics

	Net Electrical Power, KW_e			
	10	25	50	75
	TOLUENE (CP-25)			
Working Fluid				
Pump Pressure Rise: ΔP , (psi)	520	520	520	520
Pump Pressure Rise: H , (ft)	2,208	2,208	2,208	2,208
Pump Flow Rate: $\dot{m}$, (lb/sec)	0.132	0.455	0.911	1.366
Q , (gpm)	1.67	4.16	8.34	12.50
v , (cfs)	0.0037	0.0093	0.0186	0.0279
Pump Speed: N , (rpm)	96,000	86,000	76,000	72,000
Pump Specific Speed: $N_s = \frac{N\sqrt{v}}{(H)^{0.75}}$ (cfs basis)	18.13	25.75	32.18	37.34
Pump Specific Speed: $N_s = \frac{N\sqrt{Q}}{(H)^{0.75}}$ (gpm basis)	385	545	681	790
Pump Net Positive Suction Head: ΔP_{sv} , (psi)	2.5	5.0	7.5	10.0
Pump Net Positive Suction Head: H_{sv} , (ft)	7.34	14.7	22.0	29.4
Pump Suction Specific Speed: $S = \frac{N\sqrt{v}}{(H_{sv})^{0.75}}$ (cfs basis)	1,309	1,105	1,020	953
Pump Suction Specific Speed: $S = \frac{N\sqrt{Q}}{(H_{sv})^{0.75}}$ (gpm basis)	26,100	23,400	21,600	20,200
Pump Specific Diameter: D_s	0.80	0.75	0.72	0.70
Pump Impeller O.D.: $D_o = \frac{D_s \times 12\sqrt{v}}{(H)^{0.25}}$, (in)	0.355	0.443	0.544	0.603
Pump Efficiency:	0.65	0.65	0.65	0.65

Table 3.23. ORCEPS-PCU Pump Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	HEXAFLUOROBENZENE (CP-28)			
Pump Pressure Rise: ΔP , (psi)	397	397	397	397
Pump Pressure Rise: H , (ft)	926	926	926	926
Pump Flow Rate: $\dot{m}$, (lb/sec)	0.463	1.158	2.317	3.476
Q , (gpm)	2.30	5.75	11.51	17.26
v , (cfs)	0.0051	0.0128	0.0257	0.0386
Pump Speed: N , (rpm)	96,000	86,000	76,000	72,000
Pump Specific Speed: $N_s = \frac{N\sqrt{v}}{(H)^{0.75}}$ (cfs basis)	40.84	57.96	72.58	84.27
Pump Specific Speed: $N_s = \frac{N\sqrt{Q}}{(H)^{0.75}}$ (gpm basis)	867	1228	1536	1782
Pump Net Positive Suction Head: ΔP_{sv} , (psi)	5.0	10.0	15.0	20.0
Pump Net Positive Suction Head: H_{sv} , (ft)	11.65	23.30	34.95	46.60
Pump Suction Specific Speed: $S = \frac{N\sqrt{v}}{(H_{sv})^{0.75}}$ (cfs basis)	1,087	917	848	793
Pump Suction Specific Speed: $S = \frac{N\sqrt{Q}}{(H_{sv})^{0.75}}$ (gpm basis)	23,100	19,400	17,900	16,800
Pump Specific Diameter: D_s	0.80	0.75	0.72	0.70
Pump Impeller O.D.: $D_o = \frac{D_s \times 12\sqrt{v}}{(H)^{0.25}}$, (in)	0.371	0.463	0.570	0.632
Pump Efficiency:	0.65	0.65	0.65	0.65

Table 3.24. ORCEPS-PCU Pump Characteristics

	Net Electrical Power, kW _e			
	10	25	50	75
Working Fluid	FLUOROZONE M, (FL-M)			
Pump Pressure Rise: ΔP , (psi)	313	313	313	313
Pump Pressure Rise: H , (ft)	810	810	810	810
Pump Flow Rate: $\dot{m}$, (lb/sec)	0.423	1.056	2.114	3.170
Q , (gpm)	2.38	5.95	11.90	17.85
v , (cfs)	0.0058	0.0145	0.0290	0.0435
Pump Speed: N , (rpm)	96,000	86,000	76,000	72,000
Pump Specific Speed: $N_s = \frac{N\sqrt{v}}{(H)^{0.75}}$ (cfs basis)	43.1	68.2	85.2	98.9
Pump Specific Speed: $N_s = \frac{N\sqrt{Q}}{(H)^{0.75}}$ (gpm basis)	975	1,382	1,727	2,003
Pump Net Positive Suction Head: ΔP_{sv} , (psi)	5.0	10.0	15.0	20.0
Pump Net Positive Suction Head: H_{sv} , (ft)	12.9	25.9	38.8	51.8
Pump Suction Specific Speed: $S = \frac{N\sqrt{v}}{(H_{sv})^{0.75}}$ (cfs basis)	1,074	902	833	778
Pump Suction Specific Speed: $S = \frac{N\sqrt{Q}}{(H_{sv})^{0.75}}$ (gpm basis)	22,000	18,300	16,900	15,800
Pump Specific Diameter: D_s	0.80	0.75	0.72	0.70
Pump Impeller O.D.: $D_o = \frac{D_s \times 12\sqrt{v}}{(H)^{0.25}}$, (in)	0.386	0.480	0.589	0.653
Pump Efficiency:	0.65	0.65	0.65	0.65

Table 3.25. ORCEPS-PCU Pump Characteristics

	Net Electrical Power, KW _e			
	10	25	50	75
Working Fluid	40/60 HEXAFLUOROBENZENE-PENTAFLUOROBENZENE (RC-1)			
Pump Pressure Rise: ΔP , (psi)	295	295	295	295
Pump Pressure Rise: H , (ft)	792	792	792	792
Pump Flow Rate: $\dot{m}$, (lb/sec)	0.511	1.158	2.317	3.476
Q , (gpm)	2.98	7.46	14.9	22.4
v , (cfs)	0.0066	0.0166	0.0332	0.0498
Pump Speed: N , (rpm)	96,000	86,000	76,000	72,000
Pump Specific Speed: $N_s = \frac{N\sqrt{v}}{(H)^{0.75}}$ (cfs basis)	52.2	74.2	92.8	107.6
Pump Specific Speed: $N_s = \frac{N\sqrt{Q}}{(H)^{0.75}}$ (gpm basis)	1,110	1,573	1,965	2,283
Pump Net Positive Suction Head: ΔP_{sv} , (psi)	5.0	10.0	15.0	20.0
Pump Net Positive Suction Head: H_{sv} , (ft)	13.4	26.8	40.2	53.6
Pump Suction Specific Speed: $S = \frac{N\sqrt{v}}{(H_{sv})^{0.75}}$ (cfs basis)	1,114	941	867	811
Pump Suction Specific Speed: $S = \frac{N\sqrt{Q}}{(H_{sv})^{0.75}}$ (gpm basis)	23,700	19,900	18,400	17,202
Pump Specific Diameter: D_s	0.80	0.75	0.72	0.70
Pump Impeller O.D.: $D_o = \frac{D_s \times 12\sqrt{v}}{(H)^{0.25}}$, (in)	0.408	0.510	0.625	0.693
Pump Efficiency:	0.65	0.65	0.65	0.65

Figure 3.78. Pump Impeller Outside Diameter versus Net Electrical Power Output

Figure 3.79. Pump Suction Specific Speed (GPM Basis) versus Net Electrical Power Output

Figure 3.80. Turbine & Housing Weight & Pump Assembly Weight versus Net Electrical Power Output

Figure 3.81. PCU Component Weight versus Net Electrical Power Output

Figure 3.82. Weight and Speed Characteristics for Homopolar Inductor Generators

Figure 3.83. Organic Working Fluid Weight versus Net Electrical Power Output

Figure 3.84. Overall PCU Assembly Weight versus Net Electrical Power Output

Table 3.26. Reactor - ORCEPS Parametric Data - System Mass Summary

Net Electrical Power Level, KW_E	Cycle Working Fluid															
	Toluene (CP-25)								Hexafluorobenzene (CP-28)							
	10		25		50		75		10		25		50		75	
System Components	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)
<u>Reactor Loop</u>																
• Reactor	410	186	515	235	610	277	700	318	425	194	530	241	640	291	725	330
• Shield	140	63.5	165	75	210	95.5	210	95.5	140	63.5	165	75	210	95.5	210	95.5
• Nak Pump	30	13.7	40	18.2	55	25.0	75	33.1	33	15.0	44	20.0	60	27.3	80	36.4
• Thermoelectric Converter Module	15	6.8	20	9.1	40	18.2	70	31.8	15	6.8	20	9.1	50	22.7	75	33.1
• Volume Accumulator	15	6.8	38	17.3	76	34.6	114	51.8	17	7.7	43	19.6	86	39.1	129	58.6
• Nak Piping	28	12.7	69	31.7	138	63.5	207	95.2	32	14.6	81	36.5	162	73.0	243	109.5
• Nak - Cycle Fluid Boiler	26	11.8	66	29.6	132	59.1	198	88.7	29.6	13.4	74	33.6	148	67.2	222	100.9
<u>Power Conversion Unit</u>																
• Turbine and Housing	15.0	6.8	30.0	13.6	43.0	19.6	53.0	24.1	15.0	6.8	30.0	13.6	43.0	19.6	53.0	24.1
• Pump and Diffuser/Scroll	10.0	4.6	20.0	9.1	32.0	14.6	39.0	17.7	10.0	4.6	20.0	9.1	32.0	14.6	39.0	17.7
• Generator and Power Conditioner	15.0	6.8	25.0	11.4	38.0	17.3	46.0	20.9	15.0	6.8	25.0	11.4	38.0	17.3	46.0	20.9
• Regenerator	5.6	2.5	14.0	6.4	28.1	12.8	42.1	19.1	10.9	4.9	27.1	12.3	54.3	24.7	81.4	37.0
• Working Fluid	37.4	13.4	73.5	33.4	147.0	66.8	220.0	100.0	50.0	19.1	105.0	47.7	210.0	95.4	315.0	143.2
• Piping - Startup Accumulator	77.0	35.0	125.0	56.8	178.0	80.9	220.0	100.0	77.0	35.0	125.0	56.8	178.0	80.9	220.0	100.0
• Controls - Valves	20.0	9.1	50.0	22.7	100.0	45.4	150.0	68.2	20.0	9.1	50.0	22.7	100.0	45.4	150.0	68.2
• Jet Pump	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A
• Overall PCU	180	82	337	153	566	257	777	353	198	90	382	174	655	298	904	411
<u>Condenser - Radiator/Deployment Structure</u>	229	104	573	260	1145	520	1118	781	281	128	703	320	1407	640	2111	960
Total System Mass	1073	487	1823	829	2972	1350	4069	1848	1171	533	2042	929	3418	1554	4699	2135
Specific System Mass, lb/ KW_E (Kg/ KW_E)	107.3	48.7	72.9	33.2	59.4	27.0	54.2	24.6	117.1	53.3	81.7	37.2	68.4	31.1	62.7	28.5

Table 3.27. Reactor - ORCEPS Parametric Data - System Mass Summary

Net Electrical Power Level, KW_E	Cycle Working Fluid															
	Fluorozene M (FL-M)								40/60 Hexafluorobenzene-Pentafluorobenzene (RC-1)							
	10		25		50		75		10		25		50		75	
System Components	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)	(lb)	(Kg)
<u>Reactor Loop</u>																
• Reactor	405	184	500	227	600	273	675	307	415	189	525	239	635	289	720	328
• Shield	140	63.5	165	75	210	95.5	210	95.5	140	63.5	165	75	210	95.5	210	95.5
• Nak Pump	30	13.7	40	18.2	55	25.0	75	33.1	33	15.0	44	20.0	60	27.3	80	36.4
• Thermoelectric Converter Module	15	6.8	20	9.1	40	18.2	70	31.8	15	6.8	20	9.1	50	22.7	75	33.1
• Volume Accumulator	16	7.3	40	18.2	80	36.4	120	54.6	20	9.1	50	22.7	100	45.4	150	68.2
• Nak Piping	30	13.6	76	34.5	152	69.1	228	103.6	35	15.9	86	39.1	173	78.6	259	117.7
• Nak - Cycle Fluid Boiler	27.8	12.6	69.5	31.6	139.0	63.2	208.5	94.8	30.8	14.0	77.0	35.0	154	70.0	231	105.0
<u>Power Conversion Unit</u>																
• Turbine and Housing	15.0	6.8	30.0	13.6	43.0	19.6	53.0	24.1	15.0	6.8	30.0	13.6	43.0	19.6	53.0	24.1
• Pump and Diffuser/Scroll	10.0	4.6	20.0	9.1	32.0	14.6	39.0	17.7	10.0	4.6	20.0	9.1	32.0	14.6	39.0	17.7
• Generator and Power Conditioner	15.0	6.8	25.0	11.4	38.0	17.3	46.0	20.9	15.0	6.8	25.0	11.4	38.0	17.3	46.0	20.9
• Regenerator	6.9	3.1	17.3	7.9	34.7	15.8	52.0	23.6	8.0	3.6	20.0	9.1	40.0	18.2	60.0	27.3
• Working Fluid	45.0	20.4	92.5	42.0	185.0	84.1	277.5	126.1	44.5	20.2	91.5	41.6	183.0	83.2	274.5	124.8
• Piping - Startup Accumulator	77.0	35.0	125.0	56.8	178.0	80.9	220.0	100.0	77.0	35.0	125.0	56.8	178.0	80.9	220.0	100.0
• Controls - Valves	20.0	9.1	50.0	22.7	100.0	45.4	150.0	68.2	20.0	9.1	50.0	22.7	100.0	45.4	150.0	68.2
• Jet Pump	N/A	N/A	2.2	1.0	5.0	2.3	7.0	3.2	N/A	N/A	N/A	N/A	N/A	N/A	N/A	N/A
•• Overall PCU	189	86	362	164	616	280	844	384	189	86	362	164	614	279	842	383
<u>Condenser - Radiator/Deployment Structure</u>	260	118	650	295	1300	591	1950	886	293	133	734	334	1468	667	2202	1001
Total System Mass	1113	505	1922	873	3192	1451	4380	1991	1171	532	2063	938	3464	1574	4769	2168
Specific System Mass, lb/ KW_E (Kg/ KW_E)	111.3	50.5	76.9	34.9	63.8	29.0	58.4	26.6	117.1	53.2	82.5	37.5	69.3	31.5	63.6	28.9

Figure 3.85. Total System Mass and Specific Mass versus Net Electrical Power Output

and the heaviest system, RC-1, (3464 lbs) was 492 pounds. At this same power level the specific system mass for the candidate reactor-ORCEPS systems varied from approximately 60 to 70 lb/KW_e. At 14.3 to 16.7 watts/lb, all of the candidate reactor-ORCEPS power systems are very attractive configurations for space power applications at the 50KW_e power level.

The variations in the weights of the candidate systems can best be seen by referring to Figure 3.86. In this normalized plot where CP-25 is designated as 1.00 (i.e., lightest system), the FL-M system is seen to be approximately 4 to 8% heavier. Similarly, the CP-28 system is 9% to 16% higher in weight and the RC-1 configuration is 9% to 18% heavier. Hence, on the basis of system weight alone, the two most attractive organic Rankine cycles are CP-25 and FL-M.

The volumes of the four candidate reactor-ORCEPS configurations are related to the required radiator area, number of radiator sections, dose plane separation distance, deployed height and maximum diameter. Figure 3.87 depicts the variation of radiator area and number of radiator sections as a function of net electrical power level. Similarly, Figure 3.88 indicates the impact of the selected dose plane separation distances on the deployed height and maximum system diameter for the various power level systems. From these figures it can qualitatively be assessed that the CP-25 system would be smallest in volume of all the four candidate systems. Here again, the FL-M system is next smallest in volume with the CP-28 and RC-1 systems the largest. By increasing the peak cycle temperature of the CP-28 system from 750°F to 1000°F, it would be possible to reduce its volume and weight to that approaching CP-25 or FL-M. However, this would only be accomplished at the expense of reduced reactor life (see Figure 3.1).

Another important factor in arriving at an optimum candidate system is the vapor side pressure drop in the regenerator. This parameter, when combined with the pressure drop in the condenser-radiator, establishes the inlet pressure to the cycle pump. This pressure, together with the degree of subcooling achieved in the cycle, determines the available net positive suction head and the required suction specific speed for the cycle pump. The regenerator vapor side pressure drop for the four candidate working fluids is shown in Figure 3.89. The fluid with the highest pressure drop was FL-M. This results from a combination of the high regenerator throughput (see Figure 3.90) and

Figure 3.86. Normalized Total System Mass
versus Net Electrical Power Output

Figure 3.87. Radiator Area and Number of Radiator Sections versus Net Electrical Power Output

Figure 3.88. Dose Plane Separation Distance, Deployed Height and Maximum Diameter versus Net Electrical Power Output

Figure 3.89. Regenerator Liquid and Vapor Side Pressure Drops versus Net Electrical Power

Figure 3.90. Normalized Regenerator Weight (CP-25 = 1.0) versus Regenerator Throughput for Candidate Working Fluid at 50KW_e Power Level

comparatively high turbine exit specific volume (see Table 3.20) experienced in the FL-M cycle. As a result of this, the FL-M systems in the 25KW_e to 75KW_e range require the addition of a jet pump in order not to exceed an upper limit of 30,000 for the cycle pump suction specific speed (see Figure 3.71 for schematic arrangement). This adds slightly to the weight and complexity of the FL-M system. However, because of the simplicity of the jet pump, the weight penalty of its inclusion is an insignificant amount (less than 0.2% of total system weight) and does not change the achievable performance level of the FL-M system relative to the other working fluids.

3.7.1 Candidate System Selection

The significant characteristics of the four candidate power systems at a nominal output power level of 50KW_e is shown on Table 3.28. The CP-25 system is the lowest in weight, and requires the least radiator area. With a comparatively low turbine exit pressure of 10.5 psia, and an operating speed of 76,000 rpm, the cycle pump suction specific speed requirement is only 21,600. This is well below an achievable upper limit of 30,000, thus precluding any potential cavitation problems and eliminating the need for a pump suction pressure boosting jet-pump. A melting point of -140°F assures that no freezing problems will occur during the launch, orbital injection, and in-orbit start-up, operational phases. In addition, the PCU and overall system efficiencies are the highest and the magnitude of the turbine and pump specific speeds are sufficiently high to assure achievement of the turbine ($\eta_t = 0.85$) and pump ($\eta_p = 0.65$) efficiency goals. Because of the foregoing, the reactor ORCEPS system using CP-25 as a working fluid was selected as the optimum system for this study.

3.7.2 Commonality And Standardization Considerations

The power level range considered during this study varied from 10-75KW_e. This range was considered to be consistent with future DoD and NASA space power applications for the 1980 to 1990 time frame. To be cost effective it would be desirable if one nominal size system could be utilized for the overall range with minimal performance and/or weight penalties. To evaluate this potential for commonality and standardization, a nominal 50KW_e system using toluene (CP-25) as a working fluid, was considered. This system would be up-rated 25KW_e to 75KW_e and down-rated 40KW_e to provide a minimum of 10KW_e.

Table 3.28. Comparison of Significant Characteristics for Four Candidate Power Systems at Nominal 50KW_e Power Output Level (Speed = 76,000 rpm)^e

Working Fluid	Molecular Weight	Melting Point (°F)	Turbine Exit Pressure (psia)	Cycle Pump Suction Specific Speed (S)	System Weight (lb)	Radiator Area (ft ²)
CP-25	92.13	-140	10.5	21,600	2972	1870
FL-M	214	- 52	7.24	16,900	3192	2000
CP-28	186	42	22.1	17,900	3418	2320
RC-1	175.3	- 44	25.0	18,400	3464	2258

Working Fluid	PCU Efficiency (%)	Overall System Efficiency (%)	Turbine Inlet Temperature (%)	Condensing Temperature (°F)	Turbine Specific Speed	Pump Specific Speed
CP-25	24.7	19.6	750	208	59.5	1020
FL-M	23.3	18.6	750	200	136.4	1725
CP-28	21.9	18.4	750	200	95.5	1536
RC-1	19.7	16.0	750	214	103.2	1965

To accomplish this, use would be made of standard radiator sections. Each section would be 475 sq. ft. and would be manifolded together to provide the total radiator area required for each discrete power level. The standard 10KW_e and 25KW_e power level systems would utilize the nominal 50KW_e reactor loop and PCU. Control valves (standardized) would be employed to vary the reactor loop E/M pump and the organic cycle loop turbine/pump flow rates. This would result in off-optimum design point operation of these components. However, the nominal system (i.e., 50KW_e) reactor thermal output power will be more than adequate to meet the requirements of the lower net electrical power levels systems. For the 75KW_e power level system, use of the nominal 50KW_e power system combined with the standardized 25KW_e power system would be made. The impact of the use of these standardized systems on the total reactor-ORCEPS weights is indicated in Table 3.29. The 25KW_e and 75KW_e

Table 3.29. Impact of Standardization on Reactor-ORCEPS System
Weights (Nominal 50KW_e - CP-25 Working Fluid System)

System Parameter	Power Levels			
	10	25	50	75 ⁽¹⁾
Required Radiator Area, ft ²	376	944	1870	2820
Number of Radiator Sections	1	2	4	6
Standard Radiator, Area/Section, ft ² /unit	475	475	475	475
Standard Radiator Area, ft ²	475	950	1900	2850
Radiator Weight, lb	290	580	1159	1738
Radiator Weight Penalty, lb	61	7	14	20
PCU Weight Penalty, lb	386	229	0	355
Reactor Loop Weight Penalty, lb	597	348	0	948
Total Standard System Weight Penalty, lb	1044	584	14	1303
Optimum System Total Weight, lb	1073	1823	2972	4069
Total Standard System Weight, lb	2117	2407	2986	5393
Magnitude of Weight Penalty, %	97	32	0.05	32
Optimum System Specific Mass, lb/KW _e	107.3	72.9	59.4	54.2
Standard System Specific Mass, lb/KW _e	211.7	96.3	59.7	71.6

(1) Uses two 50KW_e reactor-ORCEPS systems plus one standard 6 section radiator.

systems experience a 32% weight penalty. The 10KW_e system experiences a 97% weight penalty. However, since the 10KW_e system has a total weight (i.e., 2117 lbs) which is less than any of the other standardized systems, no launch and orbit injection operational difficulties would be encountered if the same launch vehicle (i.e., Space Shuttle) and orbit injection vehicle (i.e., IUS) were utilized. The results of this standardization approach are depicted in Figure 3.91. As may be noted, in the 10KW_e to 75KW_e range, the maximum weight penalties are only 1044 lbs. (10KW_e power level) and 1303 lbs. (75KW_e power level). In view of the considerable potential non-recurring cost savings that would result from standardization, as well as the reduced development time, this appears to be a very attractive option for consideration by future program planners.

3.7.3 Alternate Standardization Concept

As was noted in the section on working fluid selection (Section 3.3.3), there are several organic Rankine cycle working fluids that could be considered for the reactor-ORCEPS system. One property of these fluids which influences the thermodynamic performance of an organic Rankine cycle is the molecular weight (M). For the original nine candidate fluids considered, the molecular weight varied from 84 to 416. In general, the higher the molecular weight, the lower is the available energy (i.e., BTU/lb) for a given set of turbine operating characteristics (i.e., inlet temperature and pressure and outlet temperature and pressure). Hence, for a given power level, the required working fluid flow rate increases with increased molecular weight. This can be seen by referring to Figure 3.92. At a nominal power level of 50KW_e and for a constant turbine efficiency of 85%, the required flow rates for the nine candidate organic fluids varies from approximately 0.7 to 6.0 lb/sec. However, this characteristic of organic working fluids may permit varying the output power level of a given size power system, simply by changing the working fluid. For example, turbine performance is dependent on a large number of factors, including nozzle and blading aerodynamic performance, leakage, Reynold's number effects and parasitic losses. However, basically the turbine performance depends upon the momentum change across the rotor which is best described by the turbine hydraulic efficiency equation. This equation reflects the basic turbine efficiency from which the losses and other effects must be subtracted to determine the net output power. The

Figure 3.91. Total System Mass and Specific System Mass versus Net Electrical Power Output

Figure 3.92. Working Fluid Flow Rate and Nozzle Velocity Factor versus Molecular Weight

effects of nozzle and rotor performance are included in the hydraulic efficiency equation which for an impulse type turbine may be expressed as follows:

$$\eta_H = 2 \frac{U}{C_0} (1 + \psi_R) (\psi_N \cos \alpha - \frac{U}{C_0})$$

where,

η_H = hydraulic efficiency

U = turbine tip speed, ft/sec

C_0 = isentropic nozzle spouting velocity, ft/sec

ψ_R = rotor blading velocity coefficient

ψ_N = nozzle velocity coefficient

α = nozzle angle, degrees

This equation defines the maximum attainable turbine efficiency, if there are no parasitic losses or leakage. As may be noted, the turbine hydraulic efficiency is a function of the velocity ratio, U/C_0 . Maximum efficiency is obtained when the turbine is operating at an optimum value of U/C_0 . The optimum U/C_0 for typical impulse turbines is approximately 0.5. However, by judicious design of the turbine rotor it is possible to operate at very near optimum efficiency over a broad range of U/C_0 values (i.e., 0.4 to 0.9). Now, for a given turbine,

$$U = \frac{\pi D_p N}{720}$$

where

N = turbine rotor speed, rpm

D_p = turbine pitch diameter, inches

Since D_p is constant for a standardized reactor-ORCEPS system, only N can be varied to change the power output of a system. However, if this is done, the turbine performance will decrease unless the design value of U/C_0 is maintained. It may be possible to accomplish this by varying the cycle working fluid (i.e., molecular weight) since, neglecting the effects of the nozzle inlet velocity, C_0 becomes equal to:

$$C_o = \psi_N \sqrt{2g \frac{k}{k-1} \times \frac{1545}{M} \times T_1 \left[1 - \left(\frac{P_2}{P_1} \right)^{\frac{k-1}{k}} \right]} = \sim K_1 \sqrt{\frac{1545}{M}}$$

where

T_1 = turbine inlet temperature, °R

P_1 = turbine inlet pressure, psia

P_2 = turbine exit pressure, psia

k = ratio of specific heats, C_p/C_v

K_1 = constant (if T_1 , $\frac{P_2}{P_1}$, k , and ψ_N can be maintained very nearly the same)

If K_1 can be kept very nearly constant by the judicious selection of various organic cycle working fluids and cycle conditions, then C_o becomes a function of $\sqrt{\frac{1545}{M}}$, a nozzle velocity factor. The variation of this factor as a function of molecular weight is shown in Figure 3.92. Hence, if the nozzle velocity factor and turbine rotor speed can be varied so that the value of U/C_o remains relatively constant, then the turbine performance can be maintained at near optimum conditions.

From the foregoing, it appears feasible that by properly matching the organic working fluid and the cycle flow rate to the required net electrical power output load, a single standardized reactor-ORCEPS system could be used over the overall 10 to 75KW_e range. The main reason for this is that there are enough organic Rankine cycle working fluids available to allow for varying the molecular weight and speed of a standardized reactor ORCEPS power system so that the net electrical power output can be tailored to the particular application. It is beyond the scope of this study to evaluate the feasibility of this concept for standardization in depth. However, it appears to be sufficiently promising to be worthy of future study efforts.

3.7.4 Candidate System Sensitivities

In conducting a broad technology based, conceptual design study of this type, it is often very necessary to make various system assumptions. This is particularly true when projections are being made regarding new developments which could include advancements in the state-of-the-art. Because of

this, it is desirable to review the output data of a study (particularly with regard to total system mass) to assess the impact of these assumptions on the results.

The percentage of the total weight that each major subsystem consists of is an important factor in evaluating the sensitivity of the total system mass. A shred-out of these values is depicted in Figure 3.93 for the CP-25 reactor-ORCEPS systems. It can be noted that the condenser-radiator subsystem is the major contributor to the total weight of the overall reactor-ORCEPS system (i.e., 21% at 10KW_e to 42% at 75KW_e). The reactor subsystem is the second heaviest with its greater impact felt at the lower power level systems (i.e., 38% at 10KW_e to 17% at 75KW_e). The reactor weight is a function of the thermal power output required and hence is contingent primarily upon the achievable NaK loop and PCU component efficiencies. Since the values assumed during this study are within the current state-of-the-art, no great variations in the reactor weight estimates is anticipated. The shield, NaK loop components and overall PCU also fall into this category and hence no significant weight variations would be anticipated for these subsystems.

The condenser-radiator subsystem total weight however, is a function of many design factors. These include heat rejection temperature, heat rejection material, thermal energy requirements, pressure drop, deployment boom, micro-meteorite armor, surface coatings, and structure (i.e., fins, tubes, manifolds, supports). The heat rejection temperature and thermal energy requirements, and allowable pressure drop are dependent upon cycle conditions and the general arrangement of the power system components. For example, if the heat rejected from the NaK loop T/E converter occurs in the regenerator (as is the case in all the reactor-ORCEPS candidate power systems), the heat rejection thermal energy requirements are reduced and hence a smaller radiator area can be utilized. Furthermore, if the available pressure drop between the regenerator gas-side outlet and the cycle pump inlet is great enough, a smaller number of radiator tubes and/or a decrease in tube diameter can be utilized. The weight of the meteoroid armor is based upon a great number of variables. These include meteoroid flux, density mass, size, velocity, penetration depth, and probability of occurrence. Also, target density, strength, elastic modulus and Brinell Hardness. In addition, various meteoroid penetration models exist (i.e., Summers, Bjork, Eicheberger). For example, using the values of

Figure 3.93. Percent of Total System Mass versus Net Electrical Power Output for CP-25 Reactor-ORCEPS System

meteoroid density of 0.4 g/cc, and a velocity of 30 km/sec (98,300 ft/sec), the room temperature penetration of a 0.001 gram meteoroid into a high-strength steel target (with properties; $\rho_t = 7.67$ g/cc, $E = 2 \times 10^6$ kg/cm² = 29×10^6 psi, BHN = 350 kg/mm²) for these three different models is shown in Table 3.30.

Table 3.30. Variation of Meteoroid Penetration as Function of Theoretical Model

Theoretical Model	Correlation Equation	Penetration Depth, cm, (0.001 g meteoroid)
Summers	$p = 1.75m^{1/3}$	0.175
Bjork	$p = 0.7m^{1/3}$	0.07
Eichelberger	$p = 2.9m^{1/3}$	0.29

As may be noted, there is a variation of a magnitude of 4 from the least value predicted (i.e., 0.07 cm) to the greatest (0.29). This would represent a variation in meteoroid armor weight of four times, depending upon which model was selected. In addition, the probability of survival (P) varies as a function of the cumulative meteoroid flux, ϕ , the exposed tube area (A_v), the mission duration (T), and the penetrating flux (ϕ_p). The penetrating flux is that percentage of micrometeors of the total cumulative meteoroid flux which have a mass equal to or greater than the mass required to penetrate a thickness, t, and is expressed as:

$$\phi_p = a \left(\frac{t}{FK} \right)^{-3b}$$

where

a = meteorite flux constant ($\sim 1.3 \times 10^{-15}$ number/m²-sec)

F = thin target factor (NASA/LeRC value = 1.75)

K = f (target and meteoroid properties)

b = general form equation constant

From the foregoing, the probability of no system failure, (P(0)), due to meteoroid damage is,

$$P(0) = e^{-\phi_p A_v T}$$

It should be noted that for a factor of only 10 (one order of magnitude) uncertainty in, a, the meteoroid flux, a factor of 1.78 (i.e., $a^{1/4}$) uncertainty in the meteoroid armor thickness requirement results, and if a factor of 10 is assumed for the uncertainty of the meteoroid density, an additional factor of 2.15 (i.e., $\rho_m^{1/3}$) results for determining the armor thickness. Finally, if the requirement for the probability of survival is increased for 0.9 to 0.99, an additional 40 to 50% weight penalty for the micrometeorite armor results. Other uncertainties include coating thickness or surface finishes and their associated weight, required to achieve an emissivity of 0.9, without degradation for 7 years, and the detail design weights of the fins, tubes, manifolds (including telescoping joints) support structure and deployment boom. These detail design factors were beyond the scope of this study. However, in order to arrive at realistic condenser-radiator weights (based upon 1980-1985 technology) and still not incur excessively conservative results, a specific condenser-radiator weight of 0.65 lb/ft² was assumed for this study. This value is more optimistic than that assumed by Atomics International in their studies (1.00-1.25 lb/ft²), but considerably more conservative than the LASL estimates (0.16-0.32 lb/ft²) in their heat pipe radiator designs for the fast reactor power system studies (see Figure 3.94).

Because the condenser-radiator subsystem constitutes a high percentage of the reactor-ORCEPS weight (Figure 3.93), further analysis and preliminary design effort is warranted in this technology area. For example, because of the high molecular weight of the organic working fluids, and the low condenser pressures (7.24 to 25 psia) and temperatures (200-214°F), the surface tension forces and viscous forces may be high enough so that no leak occurs despite penetration by an 0.001 gram (i.e., 0.00025 cc) meteoroid.

3.8 PROGRAMMATIC DATA

One of the objectives of the Reactor-ORCEPS Power Systems Study was to provide parametric data for the ORCEPS PCU in the 10 to 75 KW_e range and to select one or two candidate systems. These systems are potential candidate electrical power systems for various space applications envisioned for the 1985 to 1995 time period. Typical of these is the Space-Based Radar Surveillance System which would require a net output electrical power of from 25 to

Figure 3.94. Condenser - Radiator Weight versus Net Electrical Power Output

50 KW_e depending upon the mission requirements and antenna design. For this study the candidate 50 KW_e power system was selected since with standardization it can provide systems in the 10 KW_e to 75 KW_e power level range.

In the programmatic area it was desirable to provide both schedule and cost data to facilitate preparation of a preliminary Development Plan. This section of this study provides the necessary preliminary data inputs to formulate an overall Nuclear Reactor-ORCEPS Power System Development Program.

3.8.1 Development Plan

3.8.1.1 Program Phases

The development of the Reactor-ORCEPS has been divided into three phases. They are shown in Table 3.31.

Table 3.31. Preliminary Development Plan Phases

Phase No.	Description
Phase I	Preliminary design, fabrication and ground demonstration testing of a prototype flight system PCU configuration employing a simulated nuclear reactor heat source and concurrent development of the reactor heat source and NaK loop
Phase II	Design, fabrication, and ground qualification testing of a fully-integrated flight system PCU using the flight-type nuclear reactor and liquid metal NaK loop as the heat source
Phase III	Flight qualification of the overall nuclear reactor-ORCEPS power system in earth orbit using the Space Shuttle/Intermediate Upper Stage Launch System

Basically this plan includes prototype development phases (Phase I) for the ORCEPS - PCU and the reactor-NaK components and subsystems. Emphasis would be on demonstration of achieving required performance objectives and confirming the adequacy of the interface specifications and operational interactions between the reactor heat source subsystem and the ORCEPS - PCU subsystem development, ground demonstration and qualification tests (Phase II). Finally, an "all-up" flight qualification and system intergration and flight

demonstration test would be conducted in earth orbit using the Space Shuttle and IUS launch vehicles.

3.8.1.2 Development Schedule

The overall development schedule for the three phases consists of a six (6) year development and flight qualification program. The schedule for this reactor-ORCEPS program is summarized in Table 3.32. A more detailed Major Milestone Schedule should be prepared as soon as a specific application and mission are defined.

3.8.1.3 Cost Estimates

The Reactor-ORCEPS Program consists of the three phases briefly outlined in Sections 3.8.1.1 and 3.8.1.2. Broadly, these encompass prototype ground demonstration testing (Phase I), flight system integration and ground qualification testing (Phase II) , and flight system production, acceptance test, prelaunch checkout, launch, in-orbit start-up, and flight qualification testing (Phase III). During this study system, power levels in the 10 to 75 KW_e range were parametrically defined with regard to performance, weight, and size. The costs to carry out the three program phases have been based upon the candidate, standardized 50 KW_e system and summarized in Table 3.33. To facilitate future development planning these non-recurring costs have been time-phased.

3.8.1.3.1 Recurring Costs

The potential for commonality and standardization of the reactor-ORCEPS power systems exists (see Section 3.7) for many future space applications. To afford additional visibility for future program planning, estimates of the recurring costs for the ORCEPS-PCU for the 50 KW_e power level has been made. These are shown in Table 3.34 based upon CY 1977 dollars. The 10 KW_e and 25 KW_e systems consist of a down-rated 50 KW_e system using smaller, standardized condenser-radiators. The 75 KW_e power system using one standard 50 KW_e system plus one down-rated 25 KW_e power system. The cost penalties associated with standardization are shown in Figure 3.95.

Table 3.32. Preliminary Reactor-ORCEPS
Programmatic Schedule

Phase	Program Activity	Time Period
I	<p>System Design, Fabrication, and Component/ Subsystem Tests</p> <ul style="list-style-type: none"> • two (2) reactor-NaK loop subassemblies (1 spare) • two (2) ORCEPS-PCU's (1 spare) with only one (1) condenser-radiator (50KW_e power level) • conduct performance and limited life tests 	2-1/2 Years
II	<p>Overall System Development and General Demonstration/Qualification Tests</p> <ul style="list-style-type: none"> • one (1) flight configured 50KW_e reactor-ORCEPS power system • incorporate necessary design changes resulting from Phase I component/subsystem development tests • ground demonstration tests at AI/LASL reactor test facility • qualification tests at AI/LASL test facility • conduct performance, environmental, and accelerated life tests and confirm ability to meet launch, orbit injection, deployment and space start-up environments 	2-1/2 Years
III	<p>Flight Qualified System Integration and Flight Demonstration Tests</p> <ul style="list-style-type: none"> • fabrication and integration of one (1) flight configured, operational, 50KW_e reactor-ORCEPS system • installation in Space Shuttle payload compartment together with required IUS propulsion capability to place system into desired orbit • reactor-ORCEPS instrumented and provided with adequate uplink/downlink telemetry capability for control and data acquisition • obtain space operating life data to confirm performance, environmental compatibility, and system reliability • data obtained will increase confidence limits and reduce operational development risks 	1 Year

Table 3.33. Standardized 50 KW_e Reactor-ORCEPS Non-Recurring
Cost Summary (CY 1977 Dollars)

Program Phase	Years After Program Award						Total Cost
	1	2	3	4	5	6	
	Millions of Dollars						
<u>PHASE I</u>							
● Reactor-NaK Loop	5.0	5.0	2.5	----	----	-----	12.5
● ORCEPS PCU	2.0	2.0	1.0	----	----	-----	5.0
● Test Equipment	0.5	1.0	0.5	----	----	-----	2.0
● Facilities	0.5	2.0	0.5	----	----	-----	3.0
<u>PHASE II</u>							
● Reactor-NaK Loop	----	-----	5.0	5.0	2.5	-----	12.5
● ORCEPS PCU	----	-----	2.0	2.0	1.0	-----	5.0
● Test Equipment	----	-----	0.5	1.0	0.5	-----	2.0
● Facilities	----	-----	1.0	0.5	0.5	-----	2.0
<u>PHASE III</u>							
● Reactor Loop	----	-----	----	----	----	9.0	9.0
● ORCEPS PCU/C-R	----	-----	----	----	----	4.0	4.0
TOTAL	8.0	10.0	13.0	8.5	4.5	13.0	57.0

Table 3.34. Standardized Reactor-ORCEPS Recurring Cost Summary
(CY 1977 Dollars)

Reactor-ORCEPS Power Level (KW _e)	Number of Radiator Sections	Recurring Costs		
		Millions of Dollars		
		Reactor NaK Loop	ORCEPS PCU ⁽¹⁾	Total
50 (Standard System)	4	3.5	1.0	4.5
25 (Downrated 50KW _e System)	2	3.5	0.75	4.25
10 (Downrated 50KW _e System)	1	3.5	0.6	4.1
75 (One 50KW _e System plus one 25KW _e System)	6	7.0 ⁽²⁾	2.75 ⁽³⁾	9.75
100 (Two 50KW _e Systems)	8	7.0 ⁽²⁾	3.25 ⁽³⁾	10.25

- (1) Includes Condenser-Radiator
 (2) Two 50KW_e Reactor Subsystems Required
 (3) Two 50KW_e ORCEPS PCU's Required

Figure 3.95. Total Reactor-ORCEPS Recurring Cost versus Net Electrical Power Output

3.8.2 Development Risks

3.8.2.1 Technology Status

The reactor is a key component of ZrH reactor-ORCEPS power systems. It is basically a long-lived energy source with a temperature capability up to 1300°F and is compatible for use with a variety of power conversion systems. Six (6) ZrH reactors have been built and tested at various stages in earlier development programs. A proof-of-principle reactor test was conducted in 1959-1960, followed by test operation of a reactor more typical of a flight design in 1961-1962. Both of these reactors operated successfully with outlet temperatures up to 1200°F.

Two higher power (600 Kwt), higher temperature (1300°F) reactors were operated over periods of 7000-9000 hours to explore the limits of temperature, power density and life of the ZrH reactor concept. These tests revealed areas where improvements could be made in thermal and hydraulic features of the detailed reactor core design. Both these areas have since been improved through extensive design studies and engineering test programs. It is estimated that the resulting design changes, which include positive fuel element spacing, segmented fuel, a design limit on linear power density, and improved reactor inlet plenum design, along with greatly improved analytical techniques and tools, will permit the reactor to operate successfully at an outlet temperature of 1300°F in most applications.

Suitability of the ZrH reactor for space application was demonstrated in the SNAPSHOT flight test of the SNAP 10A system in 1965. The one-year life objective was exceeded in 1966 when the ground test of the FS-4, SNAP 10A completed 10,000 hours of continuous operation.

More than 43,000 hours of reactor operation have been logged to date. No test has been terminated by a malfunction in the reactor.

In the SNAPSHOT Program, system development was conducted for two basic purposes: (1) to assure compatibility of technical interfaces (structural, electrical, thermal) between the reactor power system and the launch vehicle; and (2) to provide performance evaluation, demonstration tests and final qualification of the reactor power system design. Prototype electrical SNAP 10A and Agena units were manufactured, mated, and subjected to extensive functional testing. Structural and thermal compatibility tests were also conducted.

Prototype and qualification systems were built and tested to explore system performance. This work included structural test units and thermal-vacuum performance test systems. The final qualification systems were subjected to a complete spectrum of initial performance tests, full qualification-level shock and vibration, transient startups and long-term endurance testing in a simulated space environment.

In the course of the ZrH reactor program, an excellent technology base including a large amount of component development and qualification work has been accumulated. Principal system components have been built in statistically significant numbers and subjected to a variety of tests at and beyond their design operating conditions to qualify them for the high temperature, hard vacuum, high radiation environment of their intended use (see Table 3.35). The majority of the system components have been tested in quantities numbering 50 or more and for total test hours ranging from 40,000 to several hundred thousand hours.

Table 3.35. U_{235} -ZrH Reactor-NaK Loop Major Component Experience

	<u>Number Units Tested</u>	<u>Total Test (Hrs)</u>	<u>Maximum Temperature (°F)</u>	<u>Maximum Duration Single Test (Hr)</u>
ZrH Reactor	6	43,000	1300	10,000
NaK Pumps & T/E Converters	59	212,000	1200	20,000
NaK Volume Accumulators	50	100,000	750	10,000
Control System Actuators	68	241,000	1125	24,400
Bearings (Beryllium Moderators)	93	241,000	1500	15,500
Radiation Shields	13	91,000	900	10,000

3.8.2.1.1 ORCEPS Power Conversion Units

The technology criteria upon which the ORCEPS-PCU has been based is, for the most part, state of the art. The turbine, pump, bearings, seals, alternator, heat exchangers, jet pumps, and controls utilize designs, materials, and fabrication techniques currently being employed in the Organic

Rankine cycle field. Hence, performance, weight and volume goals are readily achievable. At low peak cycle temperatures (750°F), using a conventional, proven, organic working fluid (CP-25), no significant problems with stress, corrosion, pyrolytic/radiolytic decomposition, or thermal differential expansion should be anticipated.

Within the United States, considerable experience with organic Rankine systems has been accumulated at TRW, Sundstrand, Aerojet, and Rocketdyne, on systems in the 1 to 100 KW_e power range. A total testing time of approximately 3 million hours has been accumulated. Over 2700 hours have been accumulated on a 1.5 KW_e system designed for the Navy and Air Force and over 3000 test hours were accumulated on an Air Force funded 6 KW_e system. These results are summarized in Table 3.35.

Table 3.36. Organic Rankine Cycle Testing Experience

- Approximately 3 Million Test Hours Accumulated On Nonnuclear Systems, Mostly In The 0.2 To 6.0 KW_e Power Range
- Based On These Units, Predicted MTBF Of ORCEPS-PCU Is >300,000 Hrs
- Units Have Also Been Fabricated And Tested At Power Levels To 100 KW_e
- 100 KW_e System Has Been Developed And Is Now Being Sold Commercially
- 3000 Hrs Accumulated On Jet Condenser For 6 KW_e Unit

ERDA has recently initiated two parallel contracts with Garrett-AiResearch (Phoenix) and Sundstrand Energy Systems to develop nuclear-powered (isotope) flight systems based on the Brayton and organic Rankine cycle technology. These developments encompass a 3-phase program in which Phase I will prove the flight system concept by ground test over a power range of 0.5 to 2.0 KW_e. Phase II will be a system qualification program on one or both systems based on funding and mission requirements and Phase III will be production of a flight unit. The overall schedule is based on an assumed flight system delivery date between mid-1981 and mid-1982.

3.8.2.2 Operational and Safety Characteristics

Operationally, the events associated with a reactor system can be divided into four phases: (1) prelaunch, (2) launch and prestartup orbiting, (3) startup and full power operation, and (4) post-operation. Safety and control are key items of interest during each of these phases.

3.8.2.2.1 Prelaunch Phase

The prelaunch phase would be approximately the same as that which occurred on the SNAP 10A flight system. System fabrication, assembly, and acceptance testing takes place at the ERDA Field Laboratory located in the Santa Susana Mountains in the west end of the San Fernando Valley. The system would undergo shock and vibration testing and complete performance testing. The performance tests take the form of separate nuclear and non-nuclear thermal acceptance testing. The non-nuclear portion of the test is run using electrical heaters to bring the system to design point thermal and electrical power levels, temperatures and hydraulic conditions. All nuclear tests are run at very low reactor power levels to preclude generation of significant fission products.

The nuclear acceptance tests result in a fission product inventory of less than 10 mCi, producing a dose rate of less than 5 mrem/hr at 1 ft from the reactor vessel. This rate is low enough to permit personnel to work in the immediate vicinity of the reactor and to allow for shipment of the system by land or sea transportation. For all practical purposes the system is non-radioactive.

The main safety concern during this phase is prevention of inadvertent reactor criticality. During shipment, the reflector is replaced with a poison sleeve to preclude criticality under accident conditions. After the reflector is installed at the launch site, void filler blocks are used to prevent movement of reflector segments to a critical position. In addition, the reflector segments are locked in the shutdown position.

3.8.2.2.2 Launch and Prestartup Orbiting Phase

Just before launch, the void-filler blocks are removed and the reflector segments unlocked. Safety analysis has assessed the effects that could occur on or near the launch pad should a reactor excursion result from the launch

vehicle falling into the water deluge system or a pool of propellant. Because there is essentially no fission product inventory at this time, the consequences would be very slight. As a matter of fact, the exclusion radius required because of a potential propellant fire is greater than that required because of a potential reactor excursion.

With vehicle ascent from either the Eastern or Western Air Force Test Range, the anticipated impact line is largely over the ocean. Although there is some small probability of the reactor falling on land, a nuclear accident is highly improbable. The risk assessment to the general public is negligible. After the reactor has attained a satisfactory orbit, prior to startup, there is no risk to the public.

3.8.2.2.3 Startup and Full Power Operation

A ground command will initiate stepping-in of the control segments. The reactor will reach criticality and the segments will continue to step in. Eventually, the reactor power will start to increase rapidly. Since the reactor has a negative temperature coefficient (its reactivity decreases with temperature) the transient is self-limiting. Following a period of minor power oscillations, the reactor will linearly ramp to full power. Reactor power, temperature, NaK flow rate and electrical power all increase to their design levels as a result of the stepping in of the control segments. When design electrical power is achieved, stepping of the segment stops. Start-up time is usually of the order of a few hours, but can be reduced by using a multiple stepping rate controller. During the first few days, frequent changes in control segment position are made to account for short-term changes in reactivity. Subsequently, the control segments will be moved only about once each week. Long-term control of the system will depend on the power system type. The general approach would be to preclude significant thermal cycles and to only vary electrical output as required.

Since the reactor system is always in a satisfactory, operating life orbit, (> 100 years) there is no risk to the general public during this phase.

3.8.2.2.4 Post-Operation Phase

After the mission has ended, the reactor system will be shut down by ground command or by an on-board timer. Shutdown is accomplished by withdrawal of the control segments. Because the reactor will have operated at

tens or hundreds of kilowatts of power for several years, a substantial fission product inventory will have accumulated. Although detailed evaluation of reactor power history is required, these products normally decay to acceptable levels in less than 100 years. If the system is in an orbit which will provide an orbital decay time in excess of 100 years, the reactor will reenter the atmosphere with negligible fission products. For typical spacecraft-power system ballistic coefficients, an orbit altitude of roughly 400 n. mi or more will require more than 100 years before reentry occurs. In this case, there is no significant risk associated with accumulated fission products.

3.8.2.3 Environmental Impact

As summarized above, in Section 3.8.2.2, safety is a key item of interest during all reactor-ORCEPS operational phases. Pre-launch performance testing with very low reactor power levels will preclude generation of any significant fission products. With vehicle ascent from either the Eastern or Western Air Force Test Ranges, any anticipated abort would most probably result in an ocean impact. After the reactor has attained a satisfactory orbit, prior to startup, there is no risk to the public. Once the system is operational in space, its orbit will provide an orbital decay time in excess of 100 years. As a result, the reactor will reenter the atmosphere with negligible fission products. It is anticipated that an environmental impact statement can be prepared that would satisfy all the requirements of the Environmental Protection Agency.

APPENDIX A

ESTIMATION OF RADIATION DOSE TO ORGANIC WORKING FLUIDS

The estimated radiation dose received by the organic fluid in ZrH reactor-organic Rankine space power systems, was calculated for the 10 and 75 KW_e systems by Atomics International. These two systems represent the extremes in system power levels in this study. There was no reason to believe that intermediate size systems would have higher organic doses, and it was felt that linear interpolation is sufficiently accurate.

In a ZrH reactor-organic Rankine system, there are two main sources of radiation; the reactor itself, and the NaK coolant which becomes radioactive during operation. In all systems, a shadow shield is provided to limit the integrated dose at the dose plane of the system to 10^{13} fast neutron fluence and 10^7 rads of gamma radiation. The organic working fluid, however, will receive a larger dose than this because it is located between the shield and the dose plane, and the intensity of radiation varies inversely as the square of the distance between source and receiver.

The radioactive NaK which circulates between the reactor and the power conversion unit (PCU) components below the shield represents an unshielded source of radiation dose to the organic fluid. This dose is all due to the gamma radiation resulting from the decay of Na^{24} , which emits a 1.37 and a 2.75 MeV gamma ray per disintegration, and K^{42} , which emits a 1.52 MeV gamma ray in 18% of its disintegrations. Because the half-lives of Na^{24} (15 hr) and K^{42} (12.4 hr) are very short, both isotopes quickly reach equilibrium activity during reactor operation.

There are two components in the PCU which contain NaK and organic fluid. These are the NaK-organic heat exchanger (boiler) and the TE modules which power the NaK pump. The total amount of radiation that the system organic inventory would receive during its residence in these components is shown in Table A-1. When performing the calculations, attenuation of neutrons or gamma rays by structural materials was ignored which means that the doses were conservatively overestimated.

The bulk of the system organic fluid is contained in the radiator. The dose received by this organic fluid is also shown in Table A-1 as are the totals for the system.

For this study, calculations were made of the radiolytic decomposition of toluene (CP-25) using dose plane doses of 10^{13} nvt and 10^7 rads and G values of 2.0 molecules/100 ev for polymerization and 0.001 molecules/100 ev for gas formation. These G values were appropriate but the higher doses shown in Table A-1 were used.

Table A-1. Estimated Radiation Dose to Organic Working Fluid

System Power, KW _e	10			75		
Radiation Source	Reactor		NaK	Reactor		NaK
	n	γ	γ	n	γ	γ
Component Dose	nvt	rads	rads	nvt	rads	rads
Boiler	7×10^{12}	7×10^6	6×10^7	1.4×10^{13}	1.4×10^7	3×10^7
TE Pump Modules	3×10^{13}	3×10^7	2×10^8	1.1×10^{14}	1.1×10^8	9×10^7
Radiator	4×10^{13}	4×10^7	5×10^5	4×10^{13}	4×10^7	2×10^5
TOTAL	7.7×10^{13}	7.7×10^7	2.6×10^8	1.6×10^{14}	1.6×10^8	1.2×10^8
Total Fluence, nvt	7.7×10^{13}			1.6×10^{14}		
Total Gamma Dose, rads	3.4×10^8			2.8×10^8		

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