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A PARAMETRIC STUDY OF A RANKINE-CYCLE
NUCLEAR SPACE-POWER SYSTEM

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A PARAMETRIC STUDY OF A RANKINE-CYCLE
NUCLEAR SPACE-POWER SYSTEM

by

Richard A. Egen
William S. Hogan
David A. Dingee

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A PARAMETRIC STUDY OF A RANKINE-CYCLE NUCLEAR SPACE-POWER SYSTEM

Richard A. Egen, William S. Hogan, and David A. Dingee

As an adjunct to an experimental program which is developing fuel materials for advanced high-temperature reactors for nuclear space-power systems an analytical program has been developed to aid in the selection of materials and experimental conditions. The program provides guidance by indicating the effects of changes in fuel-material variables on powerplant characteristics. The material variables investigated included composition (coupled with cladding thickness), pin diameter, temperature, burnup, and fission-gas release. The effect of these variables as well as others (e.g., reactor thermal power) on the system specific weight (weight per unit electrical power) are determined for two fluid-loop, Rankine-cycle, fast-reactor powerplants of minimum specific weight. The analytical program has been transcribed for use on a Bendix G-20 electronic digital computer.

INTRODUCTION

A program is in progress at Battelle to develop fuel materials for advanced high-temperature reactors for nuclear space-power systems. In developing a refractory fuel, the metallurgist must choose a limited number of experimental conditions from a large possible number. To do this intelligently it is important to know what effects the materials variables have on the characteristics of a power plant which may use the fuel. One of the uses of the fuel materials being developed will be in advanced space-power systems such as liquid-metal-cooled fast reactors coupled to a Rankine-cycle power-conversion system. The present program consisted of studying the effect on the specific weight (weight per unit electrical power) of such systems caused by variations in materials parameters. The materials variables investigated were: (1) composition (coupled with cladding thickness), (2) pin diameter, (3) temperature, (4) burnup, and (5) fission-gas release.

A space nuclear power system is greatly influenced by mission considerations such as payload, duration, power requirements and, being manned or unmanned. It is impractical to develop a method of analysis which includes all of these considerations as specific variables. It was therefore decided to adapt and generalize information from an available system design for use in a computer program. The most useful design study⁽¹⁾ available for this purpose was SPUR.* This study was particularly appropriate since it was based on use of uranium carbide fuel, which is the fuel of major interest in the current Battelle program.

(1) References at end of text.

* The SPUR design is a two-loop system comprising a nonboiling, lithium-cooled reactor, a heat exchanger, Rankine-cycle power-conversion equipment, and a nuclear radiation shield. The reactor is cylindrical, and the radiation shield has the form of a truncated cone. The reactor is located coaxially against the smaller base of the shield. The placement of the remaining equipment and radiator is within the shield's shadow.

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The output from the computer program which was developed is an aid in making a comparative selection of the optimum materials conditions. For example, suppose the initial selection of fuel was undiluted UC in a 10-mil niobium cladding operating at 2400 F to a burnup of 23.6×10^{20} fissions per cm^3 (8 uranium a/o), and that experiments eventually showed severe swelling under these conditions. The swelling could possibly be eliminated either by reducing the temperature, alloying the UC, or increasing the cladding thickness or strength. In studying the comparative advantages of each of these possibilities, it is important to choose experimental conditions that give equivalent system specific weights. In this example, a direct quantitative comparison is therefore needed between the effect of temperature and dilution.

The results discussed in this report provide this information, and are being used at Battelle to guide the current materials program. It is anticipated that these calculated results will be of steadily increasing value as additional materials properties are obtained. The basic analytical program is sufficiently flexible to allow application, after minor changes, to a wide variety of possible space reactor systems.

DISCUSSION OF ANALYSIS

For the purposes of this study the space-power system was reduced to a series of 80 coupled equations which describe the neutronics, heat transfer, and fluid flow and system weights. Briefly, the powerplant analysis was divided into two general areas: (1) the reactor system and (2) the power-conversion system. The former comprises the core, reflector, pressure vessel, the nuclear radiation shield and associated components. The power-conversion system consists of the heat exchanger, turbogenerator, heat rejection radiator (with meteoroid protection), and associated equipment. In formulating design equations in these two areas, standard design techniques were used whenever possible. In a number of cases these were modifications of the SPUR design equations. The development of the equations in special areas is discussed in detail in Appendix A.

The analytical program was transcribed for use on the Bendix G-20 digital computer. The computer program is used by specifying reactor thermal power, maximum fuel-pin surface temperature, fuel composition, fuel-pin diameter, shield angle, and fractional release of fission gas. The program selects values of the core length-to-diameter ($\frac{L}{D}$) ratio, volume ratio of coolant to structural material including fuel ($f_{c/s}$), and average temperature rise across the core ($\overline{\Delta T_R}$) to complete the system design and calculate specific weight. The computation proceeds to optimize the choice of these last three variables. Each of these "optimization" parameters ($\frac{L}{D}$, $f_{c/s}$, and $\overline{\Delta T_R}$) is varied in steps over a prescribed range and the resulting systems are compared at each step. After all these variables have been considered the system with the over-all minimum specific weight is selected. This process is repeated for another set of the former variables (i. e., power, temperature, etc.). Figure 1 shows the sequence of calculation in simplified form for one set of design and optimization parameters. A single iteration as shown on Figure 1 takes about 0.15 sec of computing time.

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FIGURE 1. SEQUENCE OF CALCULATIONS FOR ONE SET OF DESIGN PARAMETERS
Symbols are defined in Table I

FIGURE 1. SEQUENCE OF CALCULATIONS FOR ONE SET OF DESIGN PARAMETERS

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Range of Variables

The design and optimization parameters and other input constants of interest are summarized in Table 1. Fuel-pin diameters were selected for each power range to provide reasonable heat fluxes, stresses, and numbers of pins for criticality. Thus pin diameters considered at high powers were smaller than those considered at lower powers. The shield angles considered, 7 and 30 deg, correspond to possible changes in mission requirements; the smaller angle is consistent with an unmanned payload and the larger with a crew compartment.

Imposed Limitations

Several limiting design conditions are applied to the program. In the heat-transfer analysis of the reactor core the maximum fuel-pin surface temperature (hottest fuel pin) is restricted to the maximum allowable* temperature, a value which, being a part of the input information, can be varied. The fuel-pin cladding stress induced by fission-gas pressure is also accommodated. The gas-accumulation volume included in each pin is sized to limit this stress according to experimentally determined temperature- and time-dependent cladding-failure stresses. (2) This accumulation volume determines the fuel pin's total length which in turn affects the coolant pressure drop in the core (and, hence pumping power) and the reactor radiation shield through its effects on the core length. A minimum coolant-to-structural ratio, $f_{c/s}$, exists for the case of fuel pins in contact. Rather than include in the program the necessary mechanism to account for this limit, minimum input values of $f_{c/s}$ are selected to accommodate this limit. In the heat-exchanger analysis a departure from nucleate boiling conditions was not allowed.

RESULTS OF CALCULATIONS

The results of the parametric study are shown in Figures 2 through 18. The data calculated with this program are not to be regarded as final engineering data. Rather they only indicate, for the purposes discussed, the effects of changes in certain core design parameters on systems similar to the reference design. A sample tabulation of the data calculated is given in Appendix B. It should be noted that because the analytical program determined minimum-specific-weight systems for the various design parameters the reactor systems represented in these curves may differ from one another in the "optimization" parameters (e. g., $\frac{L}{D}$, $\overline{\Delta T_R}$, $f_{c/s}$). The sample data of Appendix B indicate this variation.

* Compatibility problems between cladding and fuel material are expected to control the upper limit.

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TABLE 1. SELECTED PROGRAM INPUT DATA

Symbol	Definition	Values Used
<u>Design Parameters</u>		
$\bar{P}_C$	Reactor total thermal power output	2,510, 8,200, and 26,800 kw
$T_{f,max}$	Maximum allowable fuel-pin surface temperature	1,800, 2,000, 2,200, 2,400, and 2,500 K
d_f	Fuel-matrix diameter	0.25 to 1.1 cm
f_{dil}	Volume fraction diluent in fuel matrix	0.1, 0.3, 0.5, 0.7
ϕ	Included angle of shadow shield	7 and 30 deg
K	Fission-gas-release fraction	0.0, 0.1, 0.2, 0.4, and 1.0
<u>Optimization Parameters</u>		
$f_{c/s}$	Ratio of coolant to structural material volumes in core	0.244 to 3.45 ^(a)
ΔT_R	Average coolant temperature rise across core	60, 80, 100, 120, and 140 F
L/D	Ratio core length to diameter	0.5, 0.7, 0.9, 1.1, 1.2, 1.3, and 1.5
<u>Miscellaneous Input Constants</u>		
t_ℓ	Core power-generation lifetime	10^4 hr
δ_{cool}	Hot-channel factor - coolant temperature rise	1.44
δ_{film}	Hot-channel factor - film temperature rise across fuel-coolant interface	2.27
δ_f	Hot-channel factor - fuel matrix rise	1.56
η_s	Over-all thermal to electrical conversion efficiency	0.14
η_g	Mechanical to electrical conversion efficiency of generator	0.88
Ω	Fraction of fuel diluent used as cladding	0.0 and 1.0

(a) The minimum value used depends on d_f and the condition that the pin centers are spaced by 1.03 pin diameter.

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FIGURE 2. M_{spec} VERSUS f_{dil} FOR THREE POWER LEVELS AND FOUR FUEL-PIN-SURFACE MAXIMUM TEMPERATURES ASSUMING A 7-DEG TOTAL SHIELD ANGLE

Within indicated fuel-pin-diameter ranges M_{spec} is effectively independent of fuel-pin diameter, d_f .

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FIGURE 3. M_{spec} VERSUS f_{dil} FOR THREE POWER LEVELS AND FOUR FUEL-SURFACE MAXIMUM TEMPERATURES ASSUMING A 30-DEG TOTAL SHIELD ANGLE

Within indicated fuel-pin-diameter ranges M_{spec} is effectively independent of fuel-pin diameter, d_f .

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FIGURE 4. M_{spec} VERSUS d_f FOR SEVERAL POWER LEVELS AND DILUENT FRACTIONS ASSUMING A 30-DEG TOTAL SHIELD ANGLE

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FIGURE 5. BURNUP VERSUS f_{dil} FOR SEVERAL POWER LEVELS AND MAXIMUM FUEL-SURFACE TEMPERATURES ASSUMING A 7-DEG TOTAL SHIELD ANGLE

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FIGURE 6. BURNUP VERSUS f_{dil} FOR SEVERAL POWER LEVELS AND MAXIMUM FUEL-SURFACE TEMPERATURES ASSUMING A 30-DEG TOTAL SHIELD ANGLE

There is a slight direct dependence of burnup with fuel-pin diameter which is noticeable only at higher power levels.

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FIGURE 7. RELATION OF NUMBER OF FUEL PINS AND THERMAL STRESS WITH FUEL-PIN DIAMETER ASSUMING A 7-DEG TOTAL SHIELD ANGLE AND AN AVERAGE THERMAL POWER LEVEL OF 2510 KW

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FIGURE 8. RELATION OF NUMBER OF FUEL PINS AND THERMAL STRESS WITH FUEL-PIN DIAMETER ASSUMING A 30-DEG TOTAL SHIELD ANGLE AND AN AVERAGE THERMAL POWER LEVEL OF 2510 KW

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FIGURE 9. RELATION OF NUMBER OF FUEL PINS AND THERMAL STRESS WITH FUEL-PIN DIAMETER ASSUMING A 7-DEG TOTAL SHIELD ANGLE AND AN AVERAGE THERMAL POWER LEVEL OF 8200 KW

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FIGURE 10. RELATION OF THERMAL STRESS AND NUMBER OF FUEL PINS WITH FUEL-PIN DIAMETER ASSUMING A 30-DEG TOTAL SHIELD ANGLE AND AN AVERAGE THERMAL POWER LEVEL OF 8200 KW

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FIGURE 11. RELATION OF THERMAL STRESS AND NUMBER OF FUEL PINS WITH FUEL-PIN DIAMETER ASSUMING A 7-DEG TOTAL SHIELD ANGLE AND AN AVERAGE THERMAL POWER LEVEL OF 26,800 KW

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[illegible]

FIGURE 13. VARIATION OF SYSTEM SPECIFIC WEIGHT WITH TEMPERATURE OF FUEL-PIN SURFACE AND WITH FISSION-GAS-RELEASE FRACTION, ASSUMING A 7-DEG TOTAL SHIELD ANGLE

An average power of 330 kw (e), a diluent fraction of 0.1, and a cladding thickness of ~14 mils are assumed.

FIGURE 14. RELATION OF REACTOR SYSTEM WEIGHT FRACTION TO DILUENT FRACTION UNDER THREE OPERATING CONDITIONS AND ASSUMING A 7-DEG TOTAL SHIELD ANGLE

Within the indicated fuel-pin diameter ranges, the reactor system weight fraction is effectively independent of fuel-pin diameter.

FIGURE 15. RELATION OF REACTOR SYSTEM WEIGHT FRACTION TO DILUENT FRACTION AT THREE OPERATING CONDITIONS ASSUMING A 30-DEG TOTAL SHIELD ANGLE

Within the indicated ranges of fuel-pin diameters, reactor system weight fraction is effectively independent of fuel-pin diameter.

FIGURE 16. RELATION OF RADIATOR WEIGHT FRACTION TO DILUENT FRACTION AT THREE OPERATING CONDITIONS AND ASSUMING A 7-DEG TOTAL SHIELD ANGLE

FIGURE 17. RELATION OF RADIATOR WEIGHT FRACTION TO DILUENT FRACTION AT THREE OPERATING CONDITIONS AND ASSUMING A 30-DEG TOTAL SHIELD ANGLE

FIGURE 18. EFFECTS OF VARIATION OF L/D , f_c/s , AND ΔT_R ON M_{spec}

Assumption: $\bar{P}_c = 8200$, $f_{\text{dil}} = 0.3$, $T_{f \text{ max}} = 2200 \text{ F}$, and $d_f = 0.7 \text{ cm}$.

General Parametric Study

The data shown in Figures 2 through 12 represent systems involving no fission-gas release. This assumption was made for reasons to be discussed later, and forms the basis of the majority of the calculations.

The variation of system specific weight (minimum) with diluent fraction, power generation, and maximum allowable fuel-surface temperature is shown in Figures 2 and 3. The variation is given for two total shielding angles, 7 and 30 deg. As shown in Figure 4 the data are effectively independent of fuel-pin diameter within the ranges given. Burnup, expressed as the number of fissions per unit volume of the fuel matrix versus the fuel-matrix diluent fraction, is presented in Figures 5 and 6. The variation of the number of fuel pins comprising the core and the variation of the stress developed at the surface of the fuel matrix due to the thermal power generation is shown in Figures 7 through 12 as a function of fuel-pin diameter.

The following discussion is an example of how these data might be used to guide the selection of materials parameters for experimental study. Suppose it is desired to study the behavior of fuel pins for a space reactor design characterized by a maximum fuel pin-surface temperature of 2000 F, a thermal power-generation rate of 2510 kw [335 kw(e)], a fuel diluent fraction of 0.1, and a total shield angle of 7 deg. Figure 2 indicates the system specific weight would be 12.8 lb per kw(e) and Figure 5 shows that the burnup would be 8.7×10^{20} fission per cm^3 fuel matrix (2.94 uranium a/o). According to Figure 7 if the number of fuel pins in the core is restricted to a maximum of 2000 then pin diameters from 0.40 cm to perhaps 1.4 cm are applicable with corresponding thermal stresses from 0.9×10^4 psi to about 10×10^4 psi.

If a burnup of 8.7×10^{20} fissions per cm^3 fuel matrix is considered too high the only choice for this 335 kw(e) system is to increase the diluent fraction. Considering an increase to 0.5, for example, then according to Figure 5, the burnup is reduced to 3×10^{20} fissions per cm^3 fuel matrix. However, if the maximum allowable pin-surface temperature is maintained at 2000 F the system specific weight becomes 16.3 lb per kw(e), an increase of 27.5 per cent (Figure 2). The change in fuel diluent fraction has effected a change in the number of fuel pins and the thermal stress also (Figure 7). At the diluent fraction of 0.5 it is necessary to consider pin diameters greater than 0.61 cm if the number of fuel pins is to be limited to a maximum of 2000.

Some reduction in the system specific weight is achieved if the maximum allowable pin-surface temperature is increased. For a diluent fraction of 0.1 and power range 335 kw(e) an increase from 2000 F to 2400 F results in a specific weight reduction of about 2 lb per kw(e). Such a change has only a minor effect on the burnup, the number of fuel pins, or the thermal stress (Figures 5 and 7).

For higher power systems it is obvious from Figures 2, 5, and 7 that diluent fractions can be increased to reduce the burnup with little increase in the system specific weight. Furthermore the burnup is now influenced by changes in the maximum allowable pin-surface temperature.

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Effects of Fission-Gas Release

Some initial calculations were made to determine the effects of fission-gas release from the fuel matrix. The amount of gas successfully retained depends on the amount of gas released, the volume provided in each pin for its containment, and the strength and thickness of the cladding which forms the walls of the containment volume. Including a parametric study of these variables in the systems program greatly increases the total number of calculations. At the present time there are not sufficient data available to justify a complete study of the fission-gas effect. Accordingly, the fission-gas-release effects were examined only for one pin diameter and one power level.

A constant cladding thickness was assumed for these preliminary calculations. In a complete evaluation of the fission-gas-release effects it would be desirable to determine systems of minimum specific weight for which the fuel-pin cladding thickness was considered as an optimization parameter. The design criterion for the containment of the fission gas was a maximum gas pressure-induced cladding stress equivalent to the 10,000-hr temperature-dependent cladding-failure stresses. ⁽²⁾

These data are shown in Figure 13. In this graph the variation of system specific weight versus maximum allowable fuel-pin-surface temperature is presented as a function of the fraction of fission gas released from the fuel matrix. As this gas-release fraction increases, the system specific weight increases due to the effects of the fuel-pin containment volume on the core length. The increased core length in turn affects the shield size and also the coolant pumping power through its effects on the coolant pressure drop in the core. For these reasons the data indicate a trend toward a fuel-pin-surface temperature that reaches an optimum maximum allowable value for fixed high fission-gas-release fractions. Since the gas release fraction is actually a function of operating temperature and is expected to increase with increasing operating temperature, the optimum in the maximum allowable pin-surface temperature is expected to be more pronounced than indicated in Figure 13.

Trend Analysis

As previously stated and as shown in Appendix B, only a portion of the calculated data has been presented in the figures discussed so far. A large portion of the calculated data indicates the interplay between system components that results from the machine's selection of the system having minimum specific weight. Some of this information provides insight into the previously discussed data. For example, the characteristic curves of system specific weight versus diluent fraction change with power generation (see Figures 2 and 3). This can be explained in part with the data presented in Figures 14 through 17. In these graphs the radiator weight fraction and the reactor and shield weight fraction (fraction of total system weight) are shown as a function of diluent fraction, total shield angle, and power generation. It can be noted in these graphs that for high-power systems the reactor and shield are only a small fraction of the total system weight (i. e., the waste-heat radiator is the controlling weight). Thus, limited changes in the reactor weight do not affect the over-all weight. The reverse is true for low-power systems. It is obvious from these figures and Figures 2 and 3 that the savings in system specific weight with increased operating temperature is primarily a result of the reduction in the weight of the radiator.

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Data of this nature are also helpful in suggesting design principles for a nuclear space powerplant before beginning the detailed work. The data point out which components will dominate the over-all system weight. This knowledge, coupled with considerations of component design problems, allows an initial choice of values for power generation and operating temperature. Advantages which may result from a particular choice of parameters can be balanced against the estimated development difficulties. As an example, it is interesting to note the rather wide range of values of reactor design parameters (e.g., core length and diameter and pin diameter) that yield about the same system specific weight. Figure 18 shows the results of varying several parameters separately around the values chosen by the program for the minimum-specific-weight system with a thermal generation of 8200 kw, a fuel-matrix diluent fraction of 0.3, a maximum allowable fuel-pin-surface temperature of 2200 F, and a fuel-pin diameter of 0.7 cm. It appears that powerplants of this general type are not particularly sensitive to small variations in the core geometry. This coupled with the previously mentioned insensitivity of system specific weight to the fuel-pin-diameter allows flexibility in the design of the reactor portion of the system.

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- (1) "SPUR Phase 1 Final Report: Part 1, Summary Description of the Power System; Part 2, Summary Design Analysis and Test Results; Part 3, Reactor Loop Design Analysis", ASD-TR-61-656. Secret.
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APPENDIX A

DISCUSSION OF KEY AREAS IN THE
DEVELOPMENT OF THE ANALYTICAL PROGRAM

The assumptions, methods of analysis, and their justifications in certain areas of the development of the analytical program as applied to the calculations based on the SPUR concept require detailed statement and explanation and are discussed in the following sections.

The Reactor System

An expression relating fuel volume fraction and ratio of coolant volume to structural volume to the diameter of a critical, spherical, reflected assembly was a fit to data reported in Reference (A-1).^{*} The spherical model was reflected by 4-in. -thick BeO and contained fully enriched UC fuel and metal diluent. The model was applied to all cores. As formulated for use in this program the spherical critical core diameter can be expressed by

$$d_c (\text{spherical}) = 437 (f_f 10^2)^{-0.765} + 0.386 \left(\frac{1}{f_{c/s}} \right)^{-0.945},$$

where

$d_c (\text{spherical})$ = diameter of critical system, cm

f_f = volume fraction of uranium in core

$f_{c/s}$ = ratio of coolant to structural volumes in core
(structural and diluent are equivalent in this analysis).

The expression is considered applicable for $0.55 \leq f_f \leq 0.70$ and $0.50 \leq f_{c/s} \leq \infty$.

The spherical-core criticality data were converted to cylindrical-core data in the usual manner by equating the spherical buckling (calculated at the approximate operating temperature) to the sum of the cylindrical radial and axial bucklings. The cylindrical core configuration chosen was a uniform array of clad fuel pins in a triangular lattice.

The initial fuel loading was computed to include effects of fuel burnup. The core lifetime at design power operation was taken as 10^4 hr. An amount of fuel equal to 1.4 times the average fuel burned per pin during the design lifetime was assumed added to each fuel pin to compensate for the nonuniform effect of burnup on nuclear criticality. The fuel burnup during operation was computed for the central (or hottest) fuel pin in terms of the number of fissions per unit volume of fuel matrix (fuel and diluent material).

^{*} References at end of this Appendix.

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In the heat-transfer and fluid-flow analysis of the core, the friction factor was used assumed to be of the form:

$$f = 0.046 R_e^{-0.2} .$$

For the heat-transfer analysis, the expression

$$Nu = 0.5 p_e^{0.5}$$

was used. This last expression is applicable to heat transfer of liquid metals in parallel flow through bundles of circular rods. (A-1)

The reactor inlet and outlet coolant temperatures were determined through standard heat-transfer and fluid-flow design techniques. The inlet coolant temperature is immediately obtained by determining the temperature rise from inlet to the maximum fuel-pin-surface temperature under the condition of letting the maximum fuel-pin-surface temperature equal the maximum allowed by metallurgical considerations. This latter limit is a program design parameter and was prescribed to have several values. The average coolant outlet temperature is then obtained by addition of the average coolant temperature rise across the core. This temperature rise is an optimization parameter in this program.

The reactor coolant inlet temperature was determined in the center of the core (i. e. , for the hottest fuel pin). An orificed flow condition was assumed as in the SPUR design. The hot-channel factors calculated for the SPUR ≈ 330 kw(e) system were used in this analysis and have been listed in Table 1 in the text. These hot-channel factors were considered to be representative of those which might occur in systems of this nature. Because the complete engineering design details of the core are not considered in this analysis, a rigorous analysis of hot-channel factors is not justifiable.

For most calculations the fuel and cladding were homogenized for heat transfer and other calculations. This artifice removes one variable, cladding thickness, from consideration. Thermal stresses are therefore defined only for the fuel body. * These stresses are useful in evaluating possible cracking which might affect the fission-gas-release mechanisms.

Although no specific reactor was to be designed, some configuration was required for determining the weights of reactor components such as the pressure vessel, the reflector, coolant inventory, etc. Furthermore, since these components determine in part the over-all dimensions of the reactor, they greatly influence the weight of the accompanying radiation shield. In this respect, the SPUR design for the 300 kw(e) plant was used to size these components by linearly relating their dimensions to those of the core. Thus each reactor computed by the program is essentially a scaled SPUR reactor with regard to the reflector, pressure vessel, plenum radiation shield, and included coolant volume.

* The mechanical properties of pure UC were used throughout. This leads to a conservative estimate of stresses. The procedure was necessary since the diluent material is unspecified.

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The coolant pressure drop over the entire reactor, necessary for computing pumping power required for coolant circulation, is a strong function of the design of such reactor areas as inlet and outlet plenums. The method of analysis allows a calculation of pressure drop only in the core region. The SPUR design for the 330-kw(e) plant was again used to scale the core pressure drop to the total reactor system (excluding primary heat exchanger). The program computed the same total core pressure drop when extended to the 1-Mw(e) system as did the rigorous reference analysis. The reactor system coolant pump was assumed to have the same electrical-to-hydraulic power conversion efficiency as the SPUR 330-kw(e) system (i. e. , 0.49). The weight of the pump was scaled with power level according to the SPUR designs. A 50 per cent variation in the pressure-drop scaling produced negligible effects in the specific weight of several calculated systems, further justifying this particular scaling.

Reference was again made of the SPUR concept design for the ~330-kw(e) system to determine the thicknesses of the various sections of the radiation shield. The shield has the form of a right circular truncated cone; its sections are also of the same form, but with varying heights. Since the shield is a major weight contributor, system calculations were made for 7- and 30-deg total shield angles. As in the case of the SPUR reference design, the controlling reactor diameter was that for fully closed reflector control segments. That is, the imaginary cone formed by extending the shield in the direction of the reactor did not completely enclose the reactor if the reflector control segments were open. (Reactor control is effected by adjusting the neutron leakage from the reactor. This is accomplished by moving reflector segments which are pivoted on axes parallel to the reactor axis.)

It is to be expected that the shield-segment thicknesses for maximum effectiveness will change with reactor output. Determining the proper thicknesses for each output is not only a complex analysis but is, again, a problem of specific design. Therefore, the shield-segment thicknesses of the SPUR 330-kw(e) system were used for all calculations. Shield-segment thicknesses for the SPUR 1-Mw(e) system differed slightly from the 330-kw(e) case. The effect of these differences were calculated; they lead to a 3 per cent change in system specific weight at 1 Mw(e).

Power-Conversion System

A generalized analysis of heat exchangers^(A-2) was performed in order to obtain scaling equations to be applied to the SPUR 330-kw(e) system. In obtaining these equations, several assumptions and approximations were made and are discussed below.

No property variation with temperature was considered except the change in the power-conversion-system working-fluid (potassium) vapor density. This effect was included in the expression for maximum "burnout" heat flux. The variations in other properties are not large, and their effect on heat-transfer coefficients and friction factors is greatly decreased since they are raised to fractional powers.

In the boiling section, the heat-transfer coefficient on the reactor-coolant (lithium) side was assumed to control heat transfer. The maximum temperature difference between the reactor coolant and the boiling working fluid was limited to avoid departure

from conditions of nucleate boiling, i. e., to prevent "burnout". The boiler has an over-all heat-transfer coefficient of about $4000 \text{ Btu}/(\text{hr})(\text{ft}^2)(\text{F})$ and the coefficient^(A-3) of the lithium side is on the order of $4400 \text{ Btu}/(\text{hr})(\text{ft}^2)(\text{F})$; hence, this assumption appears justified if there is no departure from nucleate boiling.

In the preheat section of the boiler the heat-transfer coefficients of both the reactor-coolant and the working-fluid sides were used to determine the over-all coefficient. Based on SPUR designs, it was assumed that one-eighth of the total heat was transferred in the preheat section.

Various means of scaling the boiler were examined. Increasing the tube diameter from the SPUR value and maintaining constant flow velocities yield a weight increase as the square of the power and was therefore not considered further. It was also assumed that decreasing the tube diameter was not practical; hence, all scaling equations are based on constant tube diameters.

A scaling factor n where $0 \leq n \leq 1$ was introduced to generalize constant tube-diameter scaling. A value $n = 0$ corresponds to constant-velocity scaling with an increased number of tubes and $n = 1$ corresponds to a constant number of tubes with changing velocities. The value of n that will yield a minimum-specific-weight system may vary with each system because choosing a value involves trading boiler weight for pumping power and its subsequent specific weight penalty. Application of this general analytical scheme to the heat-exchanger data available for the SPUR designs indicated that constant velocity scaling (i. e., $n = 0$) would predict these data. Accordingly, constant-velocity scaling was applied to the SPUR 330-kw(e) system heat exchanger for use in this program.

A generalized consideration of turbogenerator^(A-4) systems was also required to estimate their weight as a function of turbine inlet temperature. Comparisons have been made with the SPUR 330-kw(e) turbine design studies^(A-5) which were based on a turbine inlet temperature of 1950 F. The comparisons would be equally applicable to systems of higher power.

The following assumptions were made in the weight estimates:

- (1) Turbine stages were designed with 50 per cent reaction. This is a reasonable assumption for this type of turbine and seems to be consistent with what is known about the reference design.
- (2) As the turbine inlet temperature is increased, the turbine tip speed would be reduced in proportion to the square root of the ratio of the 10,000-hr rupture strength of the disk material at any given temperature to the strength at 1950 F. This maintains equal safety factors at all temperatures.
- (3) The turbine efficiency could be maintained at the reference-design value provided the correct ratio of nozzle discharge velocity to tip speed would be maintained. This requires the number of stages to be increased as the inlet temperature increases.

- (4) The condensing temperature was assumed to be three-fourths of the inlet (boiling) temperature.
- (5) At inlet temperatures below 1950 F, tip speed could not be increased because of the blade-erosion problem.

The turbine was examined in three ways. First, it was assumed that the necessary reduction in tip speed would be attained by reducing turbine rpm, holding the same disk size. The available energy would be divided between stages so that the correct ratio of nozzle velocity to tip speed would be maintained. As tip speed decreased, additional stages would be added. Tip speed had to be adjusted to arrive at integral numbers of stages and to permit operation at speeds which could produce 3200-cycle power. This approach can be dismissed for several reasons: the turbine gets very heavy, the alternator weight increases as rotational speed is reduced, and it is generally not possible to maintain the exactly correct nozzle velocity-to-tip speed ratio.

The second approach was to assure that turbine rpm would be held constant, with tip-speed reductions accomplished by reducing disk diameter. Additional stages would be added as necessary. If the weight of a turbine disk is assumed to be proportional to the square of the disk diameter, this approach results in a decrease in disk weight which is just enough to offset the weight increase produced by adding stages. The total turbine weight is thus unchanged as temperature increases. Since turbine efficiency is practically unchanged, the entire power-conversion-system weight would be practically constant over the temperature range considered. There are some minor influences on efficiency which would be present. For example, rotor windage losses are proportional to the number of stages, the density of the fluid, the cube of the tip speed, and the square of the rotor diameter. The first two factors tend to increase the windage loss and the last two to decrease it as inlet temperature increases. Another effect to consider is leakage between stages, which depends on local pressure, fluid density, and leakage area. Also, flow losses through the stator and rotor passages would depend somewhat on flow velocity, passage geometry, and length of the flow path. The net result of these factors cannot be assessed without detailed study. It has been assumed that they would have a small influence on efficiency.

A third approach would be to maintain the 24,000-rpm shaft speed, reduce the turbine diameter as required by stress considerations, but keep the same number of stages as inlet temperature is increased. Turbine efficiency would decrease as temperature increased, requiring an increase in flow rate to maintain the 330-kw electrical power outlet. This would require an increase in boiler size, radiator size, pump size, pump power, and reactor heat output. In favor of this approach would be a decrease in turbine weight. The net effect on the system of all these changes has not been determined.

The turbine efficiency was estimated by assuming that efficiency versus tip speed could be represented by a parabola, with a maximum efficiency of 0.832 at a tip speed of 673 ft per sec. The mass flow rate was assumed to be inversely proportional to efficiency, although this assumption neglects the increase in pumping power. The influence of all the effects would be difficult to assess. It is not likely that over-all system weight would be any less than that resulting from the second-approach analysis.

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The analysis of the second approach indicates that turbine weight should be practically unchanged by a change in inlet temperature between 1750 and 2200 F. Turbine efficiency was not precisely determined for each temperature. There would be no gross changes in efficiency, but there could be small changes caused by a number of minor effects. These changes would probably be in the direction of decreasing efficiency with increasing temperature, with a net reduction in going from 1950 to 2200 F of possibly 2 or 3 per cent and a small gain in going from 1950 to 1750. Determining the exact magnitude of these changes in efficiency would require a detailed analysis, which was beyond the scope of this study.

With regard to power-level variation of turbogenerator weight, the SPUR designs were again referred to and the turbogenerator weights scaled for power-level changes accordingly. The power-conversion-system working-fluid pump was attached to the turbogenerator unit and driven directly by the main shaft of the unit. The hydraulic pumping power (assumed equal to the mechanical pumping power) was subtracting from the developed turbine power prior to conversion to electricity, thus accounting for pumping losses in this part of the system.

The heat-rejection space radiator is difficult to analyze in general since not only mission considerations enter but also launching problems because of the generally large size of this component. Reference (A-6) presents data for liquid alkali-metal systems in the range of temperatures and power levels of interest. The data give radiator specific weights versus working fluid, boiling temperature, and system electrical output. The condensing temperature was assumed to be three-fourths of the boiling temperature. The data included meteoroid protection on the basis of 90 per cent probability of no punctures for 1 year's operation. The specific weight dependence on boiling temperature of these data was used for this analysis. The specific weight dependence on the system power output was not in complete accord with the available data on the SPUR designs. Because of the many areas of similarity between the SPUR designs and the analytical program that existed by this time, the SPUR designs dependence on power output was adopted for the program.

The weights of the remaining miscellaneous components of both the reactor and power-conversion systems such as piping and controls were lumped together and scaled for power output according to the SPUR designs. No temperature variation of these components was considered.

References

- (A-1) Friedland, A. J. , Dwyer, O. E. , Maresia, M. W. , and Bonilla, C. F. , "Heat Transfer to Mercury in Parallel Flow Through Bundles of Circular Rods", Paper 62, 1961 International Heat Transfer Conference.
- (A-2) Whitacre, G. R. , BMI, Private Communication.
- (A-3) "SPUR Phase 1 Final Report: Part 5, Conversion Loop Heat-Transfer Analysis", ASD TR 61-656. Secret.
- (A-4) Irwin, J. R. , and Coffin, C. L. , BMI, Private Communication.

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- (A-5) "SPUR Phase I Final Report: Part 4, "Conversion Loop, Turbomachinery, Electrical and Controls Analyses", ASD TR 61-656. Secret.
- (A-6) Haire, A. , and Hays, L. , "Energy Conversion Systems Reference Handbook", WADD-TR-60-699, Vol VII (September, 1960).

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APPENDIX B

SAMPLE OF CALCULATED DATA

Table B-1 contains a sample of the data calculated with this program. For each set of design parameters listed to the left the calculated results apply to the minimum-specific-weight system for the range of the optimization parameters listed in Table B-2. The values of the optimization parameters corresponding to each minimum-specific-weight system are also presented in Table B-1. It is important to notice that system parameters such as the "optimization" and the calculated parameters may differ as the design parameters are changed. Thus the curves of Figures 2, 3, etc., do not show the response of the "same" power plant to the change of the variables listed. They only show how the minimum-specific-weight system varies. The details of a particular minimum-specific-weight system are to be found in the detailed data of which Table B-1 presents only a small portion.

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TABLE B-1. SAMPLE RESULTS OF PARAMETRIC STUDY OF NUCLEAR SPACE POWER SYSTEMS^(a)

An average power level of 2510 kw and a diluent function of 0.1 are assumed.

Design Parameters		Optimization Parameters			Calculated Results ^(b)									
$T_{f, cool}$	d_f	$f_{c/s}$	L/D	ΔT_R	n'	B. U.	$T_{R, out}$	$T_{K, sat}$	σ_θ	M_{R+M_S}	M_{RAD}	$E_{P, cool}$	$E_{GEN, net}$	M_{spec}
2500	1.1	1.71	1.3	120	249	9.012	2437	2244	282911	538	140.7	5.838	338.00	4.7123
	0.9	1.71	1.3	120	371	9.012	2440	2247	189878	538	139.9	6.713	337.13	4.7217
	0.7	1.71	1.3	120	613	9.012	2442	2249	114918	538	139.2	8.132	335.72	4.7390
	0.5	1.71	1.3	140	1201	9.012	2435	2243	58655	538	141.4	7.816	335.93	4.763
	0.3	2.05	1.3	140	3359	8.850	2437	2244	20735	552	140.9	8.132	335.62	4.8060
2400	1.1	1.71	1.3	140	249	9.012	2337	2156	282911	538	175.8	5.977	337.74	4.839
	0.9	1.71	1.3	140	371	9.012	2340	2159	189878	538	174.8	6.851	336.87	4.8478
	0.7	1.71	1.3	140	613	9.012	2342	2161	114918	538	173.9	8.270	335.46	4.8650
	0.5	2.05	1.3	140	1210	8.850	2344	2162	57561	552	173.3	7.429	336.30	4.8909
	0.3	2.05	1.3	140	3359	8.850	2337	2156	20735	552	176.0	8.309	335.30	4.9400
2200	1.1	1.71	1.3	100	249	9.012	2147	1988	282911	538	275.2	8.763	334.78	5.1983
	0.9	2.05	1.3	100	374	8.850	2148	1989	186227	552	274.4	7.160	336.38	5.2108
	0.7	2.05	1.3	100	617	8.850	2151	1992	112883	552	272.7	8.553	335.00	5.2269
	0.5	2.40	1.3	100	1220	8.680	2152	1993	56425	566	271.6	8.048	335.50	5.2582
	0.3	2.40	1.3	120	3387	8.680	2145	1987	20324	566	276.2	8.451	335.00	5.3113
2000	1.1	2.05	1.3	80	250	8.850	1955	1818	278595	552	447.9	10.00	333.32	5.8015
	0.9	2.40	1.3	80	378	8.680	1957	1819	182597	566	446.1	8.496	334.83	5.8126
	0.7	2.40	1.2	80	657	8.668	1959	1821	110423	571	443.5	9.168	334.16	5.8310
	0.5	2.75	1.2	80	1298	8.502	1961	1823	55256	586	441.3	8.981	334.35	5.8647
	0.3	2.75	1.3	100	3416	8.509	1954	1817	19920	581	449.3	9.526	333.72	5.9214

(a) Ranges of variables considered in the sample calculations are listed in Table B-2.

(b) Definitions of calculated terms:

n'	Number of fuel pins	M_{R+M_S}	Weight of reactor plus shadow shield, kg
B. U.	Burnup, fissions per cm^3 of fuel matrix	M_{RAD}	Weight of radiator, kg
$T_{R, out}$	Average reactor coolant outlet temperature, F	$E_{P, cool}$	Power to pump coolant in reactor primary loop, kw(e)
$T_{K, sat}$	Working fluid (potassium) saturation temperature, F	$E_{GEN, net}$	Net generator output, kw(e)
σ_θ	Circumferential fuel-pin stress induced by heat generation, psi	M_{spec}	System specific weight, kg per kw(e)

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TABLE B-2. RANGES OF VARIABLES CONSIDERED IN
SAMPLE CALCULATIONS OF TABLE B-1

<u>Design Parameters</u>	
$\bar{P}_c$	2510 F
f_{dil}	0.1
T_{fmax}	2000, 2200, 2400, and 2500 F
d_f	0.3, 0.5, 0.7, 0.9, and 1.1 cm
<u>Optimization Parameters</u>	
$f_{c/s}$	1.71, 2.05, 2.40, 2.75, 3.10, 3.45, and 4.5
L/D	0.7, 0.9, 1.1, 1.2, 1.3, and 1.5
ΔT_R	60, 80, 100, 120, and 140 F

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