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CORE DESIGN STUDY FOR A 500 MWe FAST OXIDE REACTOR

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December 28, 1961

Atomic Power Equipment Department
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REACTOR TECHNOLOGY

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ABSTRACT

A 500 MWe sodium-cooled, $\text{PuO}_2\text{-UO}_2$ fueled, fast spectrum reactor design is presented, which is calculated to have a fuel cycle cost in the vicinity of 1 mill/ekw hr, and to have important safety features due to an appreciably negative Doppler effect.

I SUMMARY AND CONCLUSIONS

The 500 MWe sodium-cooled, PuO_2UO_2 fueled, fast spectrum reactor design presented is calculated to have a 1975 fuel cycle cost of 0.8 mills/ekw hr. The cost for present day economic conditions (1962) is estimated to be 1.3 mills/ekw hr.

The design is based on an average discharge fuel exposure of 100,000 MWD/T*, an average specific power of 1,000 kw per kg of fissionable plutonium, and a maximum linear power generation of 18.5 kw/ft. These conditions have been achieved in capsule experiments and indications are that a design based on these conditions is feasible.

The high exposure and linear power generation can lead to substantial quantities of released fission gas, as was experienced during the irradiation experiments. The present design assumes 100% fission gas release which is contained in the storage volume provided by extending the tubing length of each fuel rod an amount approximately equal to the core length. The storage volume (or reservoir) allows a fuel-to-structure volume ratio of 2 which permits a high core conversion ratio and does not significantly penalize the hydraulic characteristics of the core.

An important feature of the large, fast ceramic fueled reactor is its large negative Doppler effect. A negative Doppler effect of approximately two dollars is calculated for the reference design due to the fuel temperature increasing from the normal operating temperature to the melting point, at the position of maximum volumetric power generation. The available excess operating reactivity is chosen to be limited to a value such that the Doppler effect would terminate the initial prompt excursion in the event that the excess reactivity should be inserted instantaneously. The refueling interval resulting from this limit on excess operating reactivity is approximately ten weeks!

The 500 MWe power output of the reference design is achieved with a core 3.25 ft in height by 6.5 ft in diameter. The core height/core diameter of 0.5 leads to a reduced reactivity effect from fuel slumping and fuel assembly bowing and also reduces the hydraulic penalty of the fission gas reservoir.

For a given core size, a core composition can be chosen so that the reactivity inserted from a total loss of sodium is zero. The core composition of the reference design corresponds to this condition and is as follows: fuel - 33 v/o; coolant - 50 v/o; and structure - 17 v/o.

* Megawatt days per short ton of Pu and U.

While the reference design power is 500 MWe, reactors ranging from 300 to 1,000 MWe were considered. No significant differences in fuel performance were found.

Some of the important parameters of the reference design are summarized in Table I.

TABLE I: Summary of Reference Design

Reactor Power - 517 MWe	Core Power Density - 395 kw/l
Core Height - 3.25'	Max. Linear Power Generatn. -18.5 kw/ft
Core Diameter - 6.5'	Pu Conc. in Core (fiss. Pu) - 12.8%
Blanket Thickness - 1.25'	Breeding Ratio - 1.00
Core Composition, Fuel - 33 v/o Sodium - 50 v/o Structure - 17 v/o (Stainless Steel)	Δk from 100% Na Loss - 0 Dollars
Fuel Rod Dia. - 0.25" O.D.	Δk Ex. Oper. React. - +1.8 Dollars
Fuel Exposure - 100,000 MWD/T*	Δk Doppler Effect. - -1.8 Dollars
Specific Power - 1.000 kw/kg fiss. mat'l.	Coolant Temperature Inlet - 785°F Outlet - 1050°F
* Megawatt days per short ton of Pu and U.	Pressure Loss - 70 psi

II INTRODUCTION

From the beginning of the Atomic Energy Program, fast reactors have been of interest. Their ability to operate with a net gain in fissionable material makes possible the expansion of our energy resources. In addition, this ability implies the possibility of power production with very low fuel costs. It is this latter aspect upon which attention is focussed in this report. A reactor design is described, which results in low calculated fuel costs.

Breeding, by itself, does not insure good fuel economics. High inventory, reprocessing, and inventory charges can overbalance the benefits of the negative depletion costs. In the design presented, the inventory charges are kept low by utilizing a liquid metal coolant and operating at a high fuel specific power (1,000 kw/kg of fissionable material). The fabrication and reprocessing charges are kept low by utilizing a ceramic fuel and operating the fuel to very high exposures (100,000 MWD/T). Capsule irradiation tests have shown that PuO_2 - UO_2 fuel has the ability to reach this burnup level while maintaining its integrity. Results of these tests are reported in GEAP-3833 (1).

The use of ceramic fuel, operating at very high burnup, presents two problems important to the safety of the reactor. First, the ceramic fuel with a density less than theoretical, a

probability of operating with central voids, and a tendency to crack means that the axial expansion of the fuel cannot be counted upon to provide an inherent negative prompt temperature and power coefficient. (In the small metallic fueled fast reactors, i.e., EBR-I and II, FERMI, the fuel expansion is the mechanism relied upon for stability and safety.) It has been found possible, however, to design large ceramic fueled reactors which have a significant negative Doppler reactivity coefficient. The negative reactivity effect is due to the increased absorption in the U-238 resonances upon heating of the fuel. It is believed that in a ceramic core the Doppler provides a shutdown mechanism more reliable than expansion of fuel.

The second problem involves the attainment of high burnup without large requirements of excess operating reactivity. To this aim, the high loading of a fast reactor is an asset, since reactivity effects associated with burnup vary inversely with the fissile fuel loading (i.e., they depend upon the percentage of fuel burned). The reactivity swings are further reduced in the reference design by a high conversion ratio in the core, and the use of a refueling scheme involving relatively frequent refueling of only part of the core (e.g., in a ten-batch refueling scheme, refueling would take place at burnup increments of 10,000 MWD/T averaged over the core, although only the ten per cent of the core which has reached 100,000 MWD/T is actually replaced.)

Results of the physics analysis relating to nuclear performance and reactor safety in a fast oxide reactor were previously reported in GEAP-3287 (2) and GEAP-3646 (3). Therefore, the details of the physics analysis are not included in this report.

In the sections which follow, the conclusions pertaining to reactor safety are first summarized. Thereafter, considerations are described leading to a core design optimized for economics within the constraints of the safety requirements. Finally, the economics of the system are described. The technical details of the various analyses are included in the appendices.

III SAFETY CONSIDERATIONS

Questions relating to reactor safety in a large sodium-cooled $\text{PuO}_2\text{-UO}_2$ fueled fast reactor have been discussed in references 2 and 3, which are reports of studies performed by the Atomic Power Equipment Department of the General Electric Company, for the United States Atomic Energy Commission. On the basis of these studies, two major ground rules were established for the design of the FCR (Fast Ceramic Reactor). These are:

1. The loss of sodium from the reactor should result in a reactivity addition less than one dollar.
2. The available excess reactivity should be limited to a value such that the Doppler effect can terminate the initial prompt excursion without gross damage to the core, in the event that the excess reactivity is instantaneously inserted.

It is recognized that these conditions are restrictive and that further study in the area of accident analysis probably will indicate that some relaxation is permitted. However, it is important to note that even with the restrictive conditions, it appears that an attractive design can be obtained.

In reference 3, the calculations of the Doppler coefficient, for fast ceramic reactors, are described, and it is shown that the second requirement above implies that the operating excess reactivity must be less than

$$\frac{-\Delta k_{\text{Dop}}}{2} + 1$$

where Δk_{Dop} is the reactivity reduction due to the Doppler effect caused by the fuel temperature at the point of maximum volumetric power generation increasing to the melting temperature.

$$\frac{-\Delta k_{\text{Dop}}}{2} + 1 \text{ is hereafter referred to as the Doppler effect.}$$

The Doppler effect is shown on Figure 1 as a function of core size and composition. It is to be noted that one and one-half to two dollars are available to counteract positive insertions of reactivity. It is assumed in Figure 1 that the reactor is operating at rated power at the time of the reactivity insertion. The Doppler effect is considerably larger in the cold condition at startup, but the excess reactivity is also greater.

The large Doppler effect results mainly from three interrelated factors. First, in the relatively large low leakage systems considered, there is a degraded fast neutron spectrum, with a significant neutron flux occurring in the energy region of strong U-238 resonances below 10 kev. A second factor is the low Pu-239 to U-238 isotopic ratio limiting the positive Doppler contribution of the Pu-239. The third factor is the large scattering cross-section per fuel atom, which enhances the negative Doppler effect of the strongly self-shielded U-238 resonances, and diminishes the positive Doppler effect of the weakly shielded Pu-239 resonances (3).

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FIGURE 1 - CHANGE IN REACTIVITY DUE TO TOTAL SODIUM LOSS AND DOPPLER EFFECT

The reactivity increment resulting from a total loss of sodium is also shown on Figure 1, where it is noted that either negative or positive changes of reactivity can be obtained, depending on core size and composition. As the core size increases, the total neutron leakage decreases. Since the major negative contribution to the sodium coefficient is due to the increase in neutron leakage with loss of sodium, the sodium coefficients are more positive for larger reactors. Variation of the volume fraction of sodium changes the relative amount of reactivity due to the positive effect of spectral hardening, and the negative effect of leakage. Hence, a composition can be chosen for a given core size, which results in a zero or negative reactivity change from a loss of sodium.* This condition also results in a small sodium coefficient. Sodium contents are called for which are generally higher than necessary from thermal-hydraulic considerations alone. As noted on Figure 1, the reactivity change due to a loss of sodium is sensitive to core size and sodium volume fraction. This suggests the importance of being able to accurately predict the sodium coefficient as a function of size and sodium content.

In arriving at the reference design to be presented below, the excess operating reactivity was limited to that which can be controlled by the Doppler effect, and the core composition was chosen on the basis of a zero change in reactivity from a total loss of sodium.

IV FUEL DESIGN

The fuel in the reference reactor is designed for a burnup of 100,000 MWD/T** and a specific power of 1,000 kw/kg of fissionable Pu. Increasing the burnup will reduce the fabrication and reprocessing costs, while increasing the specific power will decrease the fuel inventory.

It is desirable to make the fuel rod diameter as large as possible to minimize the unit fabrication cost; however, large diameter rods and high specific power result in a high linear power generation and fuel temperature. These fuel parameters are related as follows:

$$Q_L \propto f k d^0 \propto (Q_w) (D_f)^2$$

* The increase in fuel rod temperature accompanying the loss of sodium may result in changes in fuel geometry. A zero change in reactivity from a loss in sodium will insure that the loss of sodium does not compound the effect of changes in fuel geometry.

** Megawatt days per short ton of Pu and U.

where

Q_L = Linear Power Generation

$\int k d\theta$ = Integral of Thermal Conductivity and Temperature Difference
across Fuel Radius

D_f = Fuel Diameter

Q_w = Power per Unit Weight of Fuel, Specific Power

Hence, the fuel diameter can be fixed by establishing the maximum linear power generation and fuel temperature, and by specifying the specific power. The fuel diameter is calculated to be 0.22"* (clad O.D. = 0.25"), based on a maximum linear power generation of 18.5 kw/ft, and an average specific power of 1,000 kw/kg of fiss. Pu.

The fuel temperature for these design conditions is estimated to be in the vicinity of 1600-2400°F for the average fuel temperature and 3100-5300°F for the maximum central temperature. (See Appendix B for method of analysis). Accurate predictions of temperature are difficult to make at the present time for two reasons:

1. The thermal conductivity of $\text{PuO}_2\text{-UO}_2$ (like UO_2) is not known with certainty, particularly after high burnup, and
2. The heat transfer coefficient across the fuel-clad interface is unpredictable.

Evaluation of the Doppler effect described earlier, is dependent on a knowledge of the fuel temperature; hence, it is important that the uncertainties in temperature be resolved.

The Atomic Power Equipment Department of the General Electric Company has conducted experiments with $\text{PuO}_2\text{-UO}_2$ for the United States Atomic Energy Commission at fuel exposures up to 100,000 MWD/T, and linear power generation up to 22 kw/ft. From the post-irradiation studies, it was concluded that these are feasible design conditions for the mixed oxide fuel. No appreciable swelling was observed; however, fission gas releases in excess of 50% were measured. A brief discussion of the experiments and observations is given in Appendix A, while more extensive coverage is reported in reference 1, 4 and 5.

It is proposed in this design that the large quantity of released fission gas resulting from the high fuel temperature and burnup be accommodated in a gas storage reservoir at the end

* Based on the following

- (1) Ratio of maximum-to-average volumetric power generation = 2.0
- (2) Ratio of fiss. Pu to total Pu and U = .128
- (3) Fuel density = 10.5 g/cc (95% of theoretical density)

of each fuel rod, as shown on Figure 2. The size of the reservoir is dependent on the pressure containing ability of the cladding. A study of the cladding stresses in which both heat transfer and pressure stresses were considered indicated that an optimum ratio of the fuel diameter to clad thickness existed which will contain the maximum pressure (6). A pressure of approximately 1800 psi can be contained for the design linear power generation of 18.5 kw/ft and stainless steel type 347 cladding at a maximum hot spot temperature of 1250°F adjacent to the full zone. Assuming 100% release, the quantity of gas generated after 100,000 MWD/T and contained at 1800 psi, 1050°F requires a reservoir volume approximately equal to the core volume.

A reservoir attached to the end of a fuel rod, for collection of fission gases, appears feasible as indicated by the specimen irradiation tests, and is hydraulically acceptable as illustrated in the following section on coolant conditions. One concern over this type of arrangement is the possibility of fuel migration into the gas space; however, no evidence of this type of behavior was found in the 38 specimens with reservoirs, in spite of the existence of a high temperature near the end of the fuel zone. A short buffer zone (1/4") of nonfissionable uranium dioxide separated the fuel from the reservoir, as compared to a normal fuel element with a low power density blanket region (~15") separating fuel and reservoir. A second concern is that in the reactor where a blanket separates the reservoir from the active fuel, there is the possibility of clogging the free space with condensing vapors; however, it is believed that voids and cracks in the fuel will provide a passageway.*

It was shown in reference (2) that optimum nuclear characteristics are obtained when the structure in the core is minimized. A fuel-to-structure ratio of 2/1 was calculated to have a core conversion ratio of 0.66 for the reference core size and composition. This provides a refueling interval of approximately 10 weeks for the reference specific power of 1,000 kw/kg of fiss. Pu, and for the safety condition that the excess operating reactivity does not exceed that which can be controlled by the Doppler effect. The ten week refueling interval is based on the lower estimated fuel temperature (see Table B-V, Appendix B). The interval would drop to eight weeks if the fuel operated at the higher temperature. The effect of a change in refueling interval on the plant operating cost remains to be evaluated before the significance of this decrease in refueling interval can be evaluated.

* See Appendix A, part (b) for a brief description of the irradiation studies presently planned for the purpose of testing the proposed fuel design.

FIGURE 2 - DIAGRAM OF FUEL ROD (NOT TO SCALE)

Analysis of the cladding, spacing and fuel element channel requirements indicates that the fuel elements can be designed with a 2/1 volume ratio of fuel-to-structure. A fuel diameter-to-cladding thickness ratio of 15/1 is required to contain the fission gas pressure. The fuel element channel is 0.040" thick. A number of spacer designs can be considered to maintain the 100 mils spacing between fuel rods (0.35" pitch, 0.25" clad O.D.). One method of spacing consists of a hollow wire of 100 mils O.D., 80 mils I.D. helically wrapped on the fuel rod with a 6" pitch. Grid type spacers can be designed which have about the same or less structural volume. Other considerations such as pressure loss and stability will influence the choice of the preferred design.

Eliminating the internal gas pressure by venting the fission gases to the coolant, instead of containing the gases in the reservoir, may result in an increase in the ratio of fuel-to-structure; hence, an increase in the refueling interval for the same excess operating reactivity.

V COOLANT CONDITIONS

The average sodium outlet temperature was set at 1050°F which, in turn, can produce steam at 900°F. A net thermal efficiency of 37% is obtained with a steam pressure of 1500 psi, and a single reheat to 900°F (7). Increasing the steam temperature would improve the plant efficiency; however, the fuel cost of the fast ceramic breeder is estimated to be approximately 1 mil/ekw hr, while the capital costs may be in the vicinity of 4 mills/ekw hr, so that even small increases in capital costs, as a result of increasing the plant efficiency, will easily override the savings in the small fuel cost. This conclusion applies to nuclear plants, in general, as compared to fossil-fired plants, where the fuel and capital costs are more nearly equal. Some additional study is required to properly identify the incentive for increases in operating temperature, and to set the temperature so as to minimize total power costs.

The coolant temperature rise across the reactor for the reference design is calculated to be 265°F for a primary pumping power equal to 2% of the plant electrical output. The corresponding pressure drop across the core is 70 psi. If so desired, the primary pumping power could be reduced to 1% by increasing the bulk temperature rise to 380°F. (See Appendix C for the analysis.)

As shown on Figure 2, the released fission gases are contained in a reservoir several feet in length, attached to the end of the fuel rod. Because of the sodium content required in the core to keep the reactivity insertion from a loss of sodium negligible, as described,

earlier, the presence of a long reservoir to contain fission gas does not present a hydraulic problem. Eliminating the reservoir length would reduce the pumping power by 20%; however, the equivalent of this savings could easily be obtained by increasing the coolant temperature rise approximately 15%, if so desired.

Orificing of individual elements to account for the spatial distribution in power generation provides a uniform reactor outlet temperature. Orificing gives a maximum hot channel temperature which is lower than that for a non-orificed reactor. For a nominal average outlet temperature of 1050°F, the orificed reactor is calculated to have a maximum outlet temperature including hot spot factors of 1185°F compared to 1340°F for a non-orificed reactor (see Appendix B for analysis). The difference in maximum temperature is particularly important in a fuel design where substantial pressure stresses are developed in the cladding as a result of released fission gas. The creep rate of even the strongest austenitic steels increases at a rapid rate with increasing temperature in the vicinity of 1200°F, so that penalties are paid in either having to increase the gas storage volume or reduce the fuel exposure.

VI REFERENCE DESIGN

Characteristics of the reference oxide design are summarized in Table II. The determination of the values shown is discussed in the following:

A. Reactor Size

Forecasts of power plant sizes indicate a trend toward 500 MWe or higher. The fast oxide reactor with sodium cooling can generate 517 MWe with a core size of 3.25' ht x 6.5' dia at a specific power of 128 kw/kg of Pu and U (1000 kw/kg of fiss Pu). Approximately 14% of the total power, 72 MWe, is generated in the 1.25' thick blanket surrounding the core. (The thickness of the blanket was not optimized and a reduction in fuel cost may be realized by reducing its size.)

While 500 MWe was chosen for the reference design, a wide range of power levels can be designed with only minor changes in performance. Power levels ranging from 300 MWe to 1,000 MWe were considered.

The low ratio of core height to core diameter, H/D, (equal to 0.5) leads to reduced leakage in the radial direction; hence, the potential reactivity increment due to fuel slumping is reduced substantially (3). The effect of fuel assembly bowing on reactivity is also reduced because of less dimensional change. Decreasing the H/D reduces the coolant temperature rise for a given pumping power as a result of the shorter elements.

TABLE II

Characteristics of Reference Design

Reactor Power, Net Electrical, Total	517 MWe
Thermal, Total	1395 MWt
Core	1200 MWt
Axial Blanket	113 MWt
Radial Blanket	82 MWt
Thermal Efficiency	37%
Reactor Size	
Core Height	3.25 ft
Core Diameter	6.50
Blanket Thickness	1.25
Fission Gas Reservoir, Core	2.9
Rad. Bl.	0.5
Total Fuel Element Length, Core + Axial	
Blanket + Reservoir	8.65
Radial Blanket + Reservoir	5.00
Composition	
Core and Axial Blanket, Fuel	33 v/o
Sodium	50
Structure	17
Radial Blanket, Fuel	63
Sodium	25
Structure	12
Core Fuel Performance	
Exposure, Average	100,000 MWD/T.*
Fuel $\int$ kd θ , Nominal Maximum	5,000 Btu/hr ft
Specific Power, Average	1,000 Kw/Kg Fiss. Pu
Specific Power, Average	128 Kw/Kg Pu + U
Surface Heat Flux, Nominal Maximum	0.96×10^6 Btu/hr ft ²

* Thermal Megawatt Days per Ton (2,000 lbs) of Metallic Pu + U

Power Density	395 kw/liter of core
Power Distribution, $P_{\max}/P_{\text{ave}}$, radial	1.7
axial	1.2
Total	2.0
Hot Spot Factor	1.5
Fuel Rod Size	
Core and Ax: Bl., Fuel Diameter	0.220 inches
Clad thickness	0.015
Clad, O.D.	0.250
Rod Spacing	0.100
Radial Bl., Fuel Diameter	0.420
Clad Thickness	0.015
Clad, O.D.	0.450
Rod Spacing	0.050
Fuel Assembly Size	
Core and Axial Blanket, Rods/Assy.	127
Width of Hex. Channel	4.16 inches
Channel Wall Thickness	0.040 inches
Space Between Sub-Assy.	0.050 inches
Number of Core Assy.	320
Radial Blanket, Rods/Assy.	
Width of Hex. Channel	4.16 inches
Channel Thickness	0.040 inches
Space Between Sub-Assy.	0.050 inches
Number of Rad. Bl. Assy.	330
Primary Coolant System Temperatures	
Sodium, Inlet	785°F
Sodium, Outlet	1050
Sodium, Outlet, Hot Spot	1185
Clad Surface, Hot Spot	1240
Sodium Flow	60×10^6 lb/hr
Sodium Velocity, Maximum	34 ft/sec
Sodium Velocity, Average	20 ft/sec
Fuel Element Pressure Loss	70 psi

Sodium Heat Transfer Coefficient

15.000 Btu/hr ft²°F

Nuclear Characteristics

Equilibrium Fuel Concentration, Average

Core, U-238 * 75.02 a/o

Pu-239 10.92

Pu-240 5.60

Pu-241 1.88

Pu-242 0.73

Am-243 0.02

Fission Product pairs 5.83

Axial Blanket, U-238 * 98.5 a/o

Pu-239 1.5

Radial Blanket, U-238 * 99.4 a/o

Pu-239 0.6

Breeding Ratio

Core 0.66

Axial Blanket 0.24

Radial Blanket 0.18

Total 1.08

Refueling Interval

10 weeks

Pu Loading in Core, fiss. Pu

1200 kg

Reactivity from 100% Loss of Sodium

0 dollars

Excess Operating Reactivity

+1.8 dollars

Reactivity from Doppler Effect $-\left[\frac{\Delta k_{Dop}}{2} + 1\right]$

-1.8 dollars

Sodium Coefficient

$+1.0 \times 10^{-6} \Delta k/k^{\circ}C$

Doppler Coefficient

$-9.2 \times 10^{-6} \Delta k/k^{\circ}C$

Control Rods

Shim Rods, Number

8

Worth per Rod

0.75 dollars

Safety Rods, Number

8

Worth per Rod

1.0 dollars

* Depleted U-238, < 0.4% U-235

This is particularly useful where a substantial length of tubing is added for the fission gas reservoir as discussed earlier. These technical advantages are thought to outweigh the small fuel cost penalty in having to fabricate more fuel elements of shorter length than would be the case of a H/D of unity.

B. Core Composition

The sodium content in the core was established by setting the reactivity change from a total loss of sodium equal to zero, as discussed on page 4. From Figure 1, it is seen that a sodium content of 50 v/o is required for the reference core size. The volume of stainless steel in the core (17 v/o) is determined by the structural requirements of the clad, spacers, and fuel assembly channel.

The radial blanket, because of its lower power density and burnup, requires substantially less coolant and structure than the core region as indicated by a comparison of the compositions in Table II.

The fuel pin design was previously discussed on page 6. As noted on Figure 2, each pin has its own reservoir 2.9 feet in length. The arrangement of core, blanket, and reservoir are schematically shown on Figure 3. The fuel rod diameter is determined by the specified linear power generation and specific power. The diameter of the radial blanket fuel rod, shown on Figure 4, is based on the volumetric heat generation rate near the blanket-core interface at the end of the blanket life. In this study, the blanket life is assumed to be equal to the core life. It should be noted that optimization of the blanket life may lead to a longer period than the core life, in which case the radial blanket rod diameter may be reduced.

The number of fuel rods per core assembly is determined by limiting the reactivity worth of a new element to less than \$1.00. The radial blanket fuel assemblies are sized to match the core fuel assembly size, as shown on Figure 5.

The excess operating reactivity is set equal to the magnitude of the Doppler effect, as discussed on page 4. The refueling interval is determined by a consideration of the permissible excess operating reactivity, specific power, and core breeding ratio. A refueling interval of 10 weeks, shown in Table II, results in refueling batch sizes which are 6.5% of the core loading.

C. Control Requirements

Preliminary control requirements have been determined and are summarized as follows:

FIGURE 3 - DIAGRAM OF CORE ARRANGEMENT

FIGURE 4 - DIAGRAM OF RADIAL BLANKET ROD (NOT TO SCALE)

SECTION A-A: RADIAL BLANKET ELEMENT

SECTION A-A: CORE FUEL ELEMENT

FIGURE 5 - DIAGRAM OF FUEL ELEMENT ASSEMBLY (NOT TO SCALE)

	Normal Reactivity Requirements, Dollars
Fuel Burnup	1.8
Doppler effect, cold to hot	3.7
Change in core size and sodium density, cold to hot	~ 0.5
TOTAL	6.0

As noted, reactivity changes which must be controlled during normal operation amount to six dollars. Eight shim rods each worth 75 cents will be used to control this reactivity. The shim rods are specified to consist of stainless steel cans 2 inches in diameter filled with boron carbide that is enriched in the B-10 isotope. The composition is as follows:

45 v/o $B_4^{10}C$, 25 v/o Na, and 30 v/o steel.

In addition to the eight shim rods, eight safety rods, each worth one dollar, are employed for shutdown margin and to cover the emergency requirements due to loss of shim rod control or fuel motion. The safety rods consist of 25 v/o $B_4^{10}C$, 25 v/o Na, and 50 v/o steel. Cooling holes will be required in both shim and safety rods.

Hence, a total of 16 control rods are utilized in a core of approximately 300 assemblies. The control rods are distributed symmetrically about the core.

VII FUEL CYCLE ECONOMICS

Fuel cycle costs * are outlined in Table III for several sets of economic and technological assumptions. Case A is considered to be representative of 1962 economic and technological conditions. Case B is based on an estimated reduction in fabrication and reprocessing costs due to experience acquired with production quantities. Case C includes anticipated changes in the value of plutonium and its use charge as a result of private ownership of plutonium produced in thermal reactors. The conditions of Case C are expected by 1975.

* The method of cost analysis is discussed in detail in Appendix D. It should be noted that the cost of capitalizing the first core is included in the total cycle cost, since the cost of the core is a function of specific power and unit fabrication and should be considered in studies involving these parameters.

TABLE III

Fuel Cycle Cost Estimate for Reference Design

<u>Breakdown of Cost</u>	<u>A *</u>	<u>B</u>	<u>C</u>
Fabrication	0.64 mills/ekw hr	0.32	0.32
Reprocessing	0.22	0.11	0.11
Net Depletion	-0.09	-0.09	-0.07
Plutonium Inventory	0.24	0.24	0.36
1st Core Capitalization	<u>0.25</u>	<u>0.12</u>	<u>0.12</u>
	1.26 mills/ekw hr	0.70	0.84
 <u>Unit Costs</u>			
Fabrication			
Core and Axial Blanket, \$/kg Pu and U	270	135	135
Radial Blanket "	104	52	52
Reprocessing			
Core and Axial Blanket, \$/kg Pu and U	63	33	33
Radial Blanket "	58	30	30
Plutonium Value			
Fissionable Isotopes, \$/gm Pu	9.00	9.00	6.50
Nonfissionable Isotopes, \$/gm Pu	1.50	1.50	1.10
Plutonium Inventory Chge., %	4.75	4.75	10
1st Core Capitalization, %	14	14	14
Plant Load Factor, %	80	80	80

* A - 1962 Economic and Technological Conditions

B - 1962 Economic Conditions Plus Reductions in Fabrication and Reprocessing Costs

C - 1975 Economic and Technological Assumptions

The reference design favors a low plutonium value because of the relatively high plutonium inventory and low breeding ratio. Inflated plutonium prices which act as a subsidy for other reactor types are disadvantageous to the reference design. There is, however, a higher breeding ratio and lower inventory at which an increase in plutonium price would be profitable. However, these combinations appear beyond present technology when oxide fuel is used and lead to safety conditions considered unwise at the present time.

The influence of specific power on total fuel cost was considered. For a fixed linear power generation, the specific power is inversely proportional to the square of the diameter so that the specific power is increased at the expense of decreasing the fuel diameter. The optimum specific power and fuel diameter are dependent on the linear power generation as well as the plutonium value and interest rate, unit fabrication cost, and the variation of the unit fabrication cost with diameter. The variation of total fuel cost is shown on Figure 6, as a function of the core specific power for 1975 economic conditions (see Table III). The unit fabrication cost is assumed to vary with diameter as $1/D^{1/2}$. Specific power of 1,000 kw/kg of fission Pu or greater is advantageous for the conditions specified. Increasing the specific power requires a corresponding decrease in the refueling interval so that this factor must also be taken into consideration. As the parameters involved in the optimization are established more firmly, optimization of specific power and fuel diameter will become more useful. However, it is felt that a specific power of 1,000 kw/kg of fission Pu is near the optimum value for the linear power generation presently used.

Reducing the sodium content in the core decreases the fuel cycle costs as shown in Figure 7, at the expense of increasing the reactivity insertions in the event of losing sodium from the core, as previously indicated on Figure 1. Sodium contents giving Δk values for total loss of sodium ranging from + \$1 to - \$1 are designated on Figure 7. The results indicate that a fuel cost penalty of less than 0.1 mills/ekw hr is paid for designing the reactor to have a negligible coolant reactivity coefficient.

The unit fabrication cost for the core and axial blanket has been estimated to be approximately \$135/kg by 1975 (see Appendix D). The cost for the large diameter radial blanket with depleted UO_2 is estimated to be \$52/kg. Accurate estimates of the commercial fabrication cost for plutonium fuels are difficult to make with the current level of experience. The influence of a change in the unit fabrication cost on the total fuel cycle cost is illustrated in Figure 8, where it is noted that doubling the unit fabrication cost of the core and axial blanket increases the total fuel cycle cost estimated for 1975, from 0.8 mills/ekw hr to

FIGURE 6 - EFFECT OF SPECIFIC POWER ON FUEL CYCLE COST

FIGURE 7 EFFECT OF SODIUM CONTENT ON FUEL CYCLE COST

FIGURE 8 - INFLUENCE OF FABRICATION COST ON TOTAL FUEL CYCLE COST

approximately 1.2 mills/ekw hr. Thus, it is seen that, in spite of the high fuel exposure in the core, there is a substantial incentive to keep unit fabrication costs as low as possible.

Because of the need for a fission gas reservoir, the axial blanket and core region were fabricated into one rod. This penalizes the fabrication cost of the axial blanket with the small diameter rod and presence of plutonium in the core region. Assuming that the core and axial blanket could be fabricated as separate rods, the axial blanket fabrication cost could be reduced because of the increased rod size (lower unit fabrication cost) and absence of plutonium during fabrication. Venting the fission gases to the coolant may permit separate fabrication; however, half of the indicated savings could be achieved with the reservoir design since the bottom axial blanket need not be attached in either case.

In the foregoing, the life of the blanket (axial and radial) was assumed to be equal to the core life. Increasing the life of the blanket permits a reduction in the weight to be processed per kw-hr, thereby resulting in a reduction in the blanket fabrication and reprocessing cost. However, the amount of plutonium in the blanket increases and the cash value it represents is unavailable for a longer period of time. It appears from an examination of the cost of the blankets for the reference design (shown in Table D-II, Case 1 of Appendix D) that there is only a minor incentive, if any, for extending the life of the blanket. Also, the thickness of the blanket (1.25') was arbitrarily chosen after recognizing that the breeding ratio increases at the expense of increasing the fabrication and reprocessing cost as the blanket is enlarged. It is recommended, therefore, that in the detailed design of this reactor, consideration should be given to optimizing the blanket life and thickness.

APPENDIX A

Fuel Irradiation Study

(a) Preliminary Irradiation Testing

The fuel irradiation program was conducted by the Atomic Power Equipment Department of the General Electric Company for the Atomic Energy Commission, to study the behavior of $\text{PuO}_2\text{-UO}_2$ fuel under conditions of high burnup and linear power generation. Preliminary design studies indicated that exposures in the range of 50,000 to 100,000 MWD/T and a linear power generation of approximately 20 kw/ft (maximum) would be attractive to the fast oxide reactor concept. Plutonium-uranium oxide fuel specimens were prepared at the Vallecitos Atomic Laboratory and irradiated in the General Electric Test Reactor (GETR). After irradiation, the fuel specimens were examined in the Radioactive Materials Laboratory (RML) in order to evaluate the performance of the fuel.

Preliminary physics calculations for a large fast oxide reactor indicated that approximately 20% of the fuel would be plutonium. In order to maintain a relatively constant Pu-U ratio during the high burnup experiments, 50% of the uranium used in the fuel was U-235. Thus, the fuel composition selected for experimentation consisted of approximately 20% Pu-239 O_2 , 40% U-235 O_2 , and 40% U-238 O_2 .

The solid solution of plutonium and uranium oxide was prepared by coprecipitating the plutonium and uranium from a nitrate solution using ammonium hydroxide. The precipitate was reduced to oxide in a hydrogen-nitrogen atmosphere. After grinding the oxide, part of the fuel material was loaded into specimen tubes for swaging to 75% of theoretical density and the remaining material was fabricated into pellets which were sintered to approximately 95% of theoretical density (4, 5). A total of 40 specimens were prepared and irradiated, 20 swaged and 20 pelleted.

The fuel zone was approximately 0.150" in diameter and 1.6" in length for the pelleted specimens and 1.8" in length for the swaged specimens. The fuel was clad in stainless steel tubing with a diameter of 0.190" and a wall thickness of 0.016" and 0.020" for the pelleted and swaged specimens respectively. As illustrated in Figure A-1, a natural UO_2 pellet was installed at both ends of the fuel zone to protect the end cap and to provide a buffer zone between the hot fuel zone and relatively cool fission gas reservoir.

The reservoir, or fission gas plenum, as depicted in Figure A-1 was provided on 38 of the 40 specimens to prevent excessive pressure in the cladding from the large quantity of released fission gases anticipated.

Figure A -1. TYPICAL TEST SPECIMENS

The specimens were loaded into capsules which were designed to provide a maximum cladding temperature (external surface) in the vicinity of 1000°F. This was accomplished for a wide range of heat fluxes by using variable thicknesses of aluminum and zircaloy-2 inserts to control the resistance of the heat path.

Burnups ranging from 5,000 to 100,000 MWD/T were achieved at linear power generations of 7 to 22 kw/ft. Maximum surface heat fluxes ranged from 0.5 to 1.6×10^6 Btu/hr ft² (1).

A post-irradiation examination of the fuel was made to determine the fuel condition, central void characteristics, and fission gas release (1). The performance of the swaged and pelleted fuel was comparable and the fuel performed well up to the highest exposure of 100,000 MWD/T. No evidence of gross longitudinal fuel movement was found. Central voids were found in many of the specimens and the size of the central void observed can be explained by radial movement of the fuel, except for certain localized "Chamber" type voids.

Measurements of the percentage fission gas release showed values ranging from 11% to 72%, with averages of 40% and 52% for the pelleted and swaged specimens respectively. The use of a gas plenum was effective as a means to contain released fission gas, and the short buffer zone of natural UO₂ was effective in preventing migration of fuel into the gas plenum.

The conclusions from these experiments indicate that an exposure of 100,000 MWD/T is feasible for oxide fuel at a linear power generation of 20 kw/ft if high fission gas release is accepted. The large amount of released fission gas can be stored in a reservoir directly attached to the fuel zone as an extension of the fuel cladding. Fuel migration into the reservoir does not appear to be a problem, particularly in a typical fuel rod with an axial blanket of low power density separating the hot fuel from the relatively cool reservoir.

It remains to be demonstrated, however, that the gas passage way in the blanket zone will not become clogged with the condensing vapors of certain fission products. It is anticipated that there will be sufficient voids and cracks in the oxide fuel to provide a continuous gas path to the reservoir.

The possibility of redistribution of the plutonium relative to the uranium is currently being investigated. Conceivably, relative movement of the plutonium and uranium could occur because of differences in the vapor pressure and other properties of UO₂ and PuO₂.

Redistribution of the plutonium, if it did occur, could have serious consequences on the behavior of the reactor, both from the standpoint of hot spots and from the relative temperature of uranium and plutonium during a reactor transient. In the latter case, the concern is over the possibility that the plutonium with its positive contribution to the Doppler effect may increase in temperature before the uranium-238 with its negative component. This is a point which needs clarification and could conceivably dictate the permissible operating temperature.

(b) Planned Irradiation Studies

While the results from the preliminary irradiation testing program have been encouraging, additional experimental investigation is required to check certain features of, and thus strengthen confidence in, the reference fuel design. Initial phases of such investigation are currently underway in the following areas:

- (1) Transient irradiation tests of fuel specimens in TREAT to determine fuel and clad response to a range of over-power conditions, looking for example for axial fuel expansion, fuel redistribution, possible segregation of constituents and clad failure.
- (2) Steady-state irradiation of prototypical fuel rods or rod bundles in a fast flux facility such as EBR II. Observations on relatively long rods, not subjected to the flux depression and resultant distortion of radial temperature profile experienced in a thermal flux, will provide important information on longitudinal movement of fission gases to an end reservoir, for example.
- (3) In-pile tests of intentionally defected or vented fuel to observe the effects of sodium-logging and to determine the release of fission products other than the rare gases and their distribution in a simulated sodium coolant system.

APPENDIX B

Heat Transfer Analysis

Evaluation of the coolant, clad and fuel temperatures for the reference design are described in the following:

1. Coolant Temperature

The average outlet sodium temperature was set at 1050°F. Previous studies (7) indicated that 1050°F sodium could generate 900°F steam with a reasonable amount of heat transfer surface area. A net thermal efficiency of 37% was obtained with 1500 psi and a single reheat to 900°F.

The coolant temperature rise across the maximum power generation element is calculated to be 265°F (see Appendix C: Thermal-Hydraulic Analysis). Orificing of the fuel assemblies, to account for the radial power distribution in the core, produces essentially the same temperature rise from all assemblies. Hence, a uniform outlet temperature is obtained. With a specified nominal outlet temperature of 1050°F and a temperature rise of 265°F, the required inlet temperature is 785°F. Abnormal conditions such as maldistribution of flow and fissile material, nonuniformity in fuel dimensions, and flux distortion contribute to deviation from the nominal outlet temperature of 1050°F.

A listing of the major uncertainty factors is shown in Table B-I. The values assigned to each are those used in the analysis of the Enrico Fermi Plant (8). Common procedure is to multiply these effects to obtain a total "hot spot" factor of 1.5; however, some argument has been given for evaluating the combined effect with statistical methods (8). Table B-I illustrates the difference in the maximum coolant temperature that is obtained between the two methods. The statistical method is based on a confidence limit of 99.87% and the comparison is based on the nominal sodium temperature rise of 265°F for the reference design. Note that by using statistical methods, the additional coolant temperature rise due to the hot spot effect is reduced to approximately 40% of that obtained with the multiplied factor. In the design calculations, the conservative temperature of 1185°F was used.

Orificing of the flow was specified above to reduce the maximum coolant temperature. In Table B-II, the hot channel outlet temperature from the orificed core is

TABLE B-I

HOT SPOT EFFECT ON COOLANT TEMPERATURE RISE

<u>Uncertainty</u>	<u>Multiplication of Factors</u>	<u>Statistical Evaluation of Factors</u>	
		<u>3σ, °F</u>	<u>(3σ)²</u>
Maldistribution of Flow	1.15 .15 x 265 =	39.8	1580
Deviation from Nominal Dimensions	1.03	8.0	64
Maldistribution of Fissile Material	1.02	5.3	28
Maldistribution of Neutron Flux	1.05	13.2	174
Burnup in Fuel Rod	1.10	26.5	700
Power Measurement and Control	<u>1.08</u>	<u>21.2</u>	<u>450</u>
	1.5	(3 σ) ² =	2996
Coolant Rise due to Hot Spot Factors, °F	132.5(.15x265)	$\sqrt{(3\sigma)^2}$ =	55
Nominal Outlet Temperature, °F	<u>1050</u>		<u>1050</u>
Maximum Coolant Temperature, °F	1182.5		1105

TABLE B-II

INFLUENCE OF ORIFICING ON MAXIMUM COOLANT TEMPERATURE

	<u>Orificed</u>	<u>Non-Orificed</u>
Average Coolant Outlet Temperature, °F	1050	1050
Average Coolant Temperature Rise, °F	265	185
Coolant Inlet Temperature, °F	785	865
Nominal Maximum Coolant Temperature Rise, °F	265	315 *
Nominal Maximum Coolant Temperature, °F	1050	1180
Maximum Coolant Outlet Temperature Rise (Including Hot Channel Factor of 1.5) °F	400	475
Hot Channel Outlet Temperature, °F	1185	1340

* Maximum Power Element/Average Power Element = 1.7

compared with the temperature obtained from the non-orificed core. The comparison is based on the assumption of equal average outlet temperature, fuel element geometry, and pumping power. It is noted that the hot channel coolant temperature from the orificed core is approximately 150°F cooler than that from the non-orificed core. The difference of 150°F is significant at temperatures in the vicinity of 1200°F where the creep strength drops appreciably as the temperature increases. For example, the stress required to produce a creep rate of 1% in 10,000 hours at 1300°F is 60% of stress required at 1200°F for type 347 stainless steel (25).

2. Clad Temperature

A review of literature on liquid metal heat transfer (9, 10, 11, 12) indicates that a substantial spread in heat transfer coefficient prediction exists at the present time. This, however, is not a critical point with the reference design specified. With a maximum heat flux in the vicinity of 1×10^6 Btu/hr ft², and a coefficient in excess of 10,000 Btu/hr ft² °F, the temperature differential between coolant and clad is less than 100°F.

However, the heat transfer coefficient is compared between several of the expressions. Lubarsky and Kaufman, (9), reviewed the experimental investigations of liquid metal heat transfer, and after a re-evaluation of the data for fully developed flow, arrived at the following empirical equation which seemed to best fit the data:

$$Nu = 0.625 (Pe)^{0.4} \quad (1)$$

The nomenclature is listed on page 50

Earlier, Lyon (10), suggested the following equation for predicting liquid metal heat transfer:

$$Nu = 7 + 0.025 (\sigma Pe)^{0.8} \quad (2)$$

Lykoudis and Touloukian (11), developed the following expression for

$Re \leq 4.2 \times 10^6$ and $0 \leq Pr \leq 0.1$, similar in form to the equation by Lyon:

$$Nu = 7.0 + 0.066 (Re)^{0.72} (Pr)^{0.8} \quad (3)$$

The heat transfer coefficients calculated by the three expressions are compared in Table B-III for the conditions found in the reference design.

TABLE B-III

Comparison of Heat Transfer Coefficients

Investigator	Lubarsky and Kaufman (9)	Lyon (10)	Lykoudis and Touloukian (11)
Nusselt No, Nu	10.2	7.8	7.7
Heat Transfer Coef, h, Btu/hr ft ² °F	17,650	13,500	13,400

Using an average heat transfer coefficient of 15,000 Btu/hr ft² °F, the temperature differential between coolant and clad surface is determined for the point of maximum thermal flux and maximum sodium temperature.

Thermal flux at point of maximum volumetric power generation is

$$\frac{Q}{A} = \frac{4 \int k d\theta}{D} = \frac{4(5000)}{(.25/12)} = 960,000 \text{ Btu/hr ft}^2$$

$$(\Delta T)_{\text{film}} = \frac{Q/A}{h} = \frac{960,000}{15,000} = 64^\circ\text{F} = \Delta T \text{ at point of nominal maximum flux}$$

Thermal flux at point of maximum sodium temperature

$$Q/A = \frac{(Q/A)_{\text{max}}}{(Q_{\text{max}}/Q_{\text{min}})_{\text{axial}}} = \frac{960,000}{1.9} = 505,000 \text{ Btu/hr ft}^2$$

$$(\Delta T)_{\text{film}} = \frac{505,000}{15,000} = 34^\circ\text{F}$$

Assuming an uncertainty in the heat transfer coefficient of 20% and the thermal flux of 25%, the hot spot film gradient would be

$$\text{Point of maximum thermal flux: } (\Delta T)_{\text{film}} = 64 \times 1.2 \times 1.25 = 96^\circ\text{F}$$

$$\text{Point of maximum sodium temperature: } (\Delta T)_{\text{film}} = 34 \times 1.2 \times 1.25 = 51^\circ\text{F}$$

Similar calculations are made to obtain the temperature difference across the clad, based on an equivalent coefficient of

$$h_c = \frac{k}{x} = \frac{12.5 (12)}{0.015} = 10,000 \text{ Btu/hr ft}^2 \text{ °F}$$

Combining the sodium temperatures estimated in the preceding section with the temperature differences computed above, the cladding temperatures are shown in Table B-IV.

TABLE B-IV.

Cladding Surface Temperatures

	<u>Nominal Temp.</u>		<u>Hot Spot Temp.</u>	
	<u>Point of Max. Flux</u>	<u>Point of Max. Sodium Temp.</u>	<u>Point of Max. Flux</u>	<u>Point of Max. Sodium Temp.</u>
Thermal flux, Btu/hr ft ²	960,000	500,000	1,200,000	630,000
Sodium temp., °F	920	1,050	985	1,185
Clad-coolant temp. differential, °F	65	35	95	50
Clad temperature, O.D., °F	985	1,085	1,080	1,235
Clad temperature differential	100	55	130	70
Clad temperature, I.D., °F	1,085	1,140	1,210	1,305

The temperature of the cladding external surface at the hot spot is 1235°F*. Structural requirements for the cladding were based on 1250°F. The temperature of the cladding at the fuel-clad interface is approximately 50°F higher than 1250°F due to the temperature differential across the clad thickness; however, the stress at this point is low because of the compressive thermal stress tending to cancel the tensile pressure stress. (See reference 26 for derivation of stress variation across the tube wall under both thermal and pressure loading.)

3. Fuel Temperature

Thermal conductivity of not only mixed oxide but also UO₂ is uncertain at the present time, particularly after high burnup. There has been a trend toward specifying fuel performance and behavior as a function of $\int k d\theta$ rather than actual fuel temperature. Generally, the $\int k d\theta$ is obtained from system heat balances or heat flux calculations from thermocouple data external to the fuel. This, however, does not alleviate the

* With conditions where the bulk temperature rise through a fuel assembly is significantly larger than the change in the film temperature gradient along the fuel length the maximum surface temperature is near the exit point.

need to know the thermal conductivity in order to determine the actual fuel temperature. For example, the magnitude of the Doppler effect that is available is dependent on the fuel temperature. Hence, the temperatures must be established if the available Doppler effect is to be evaluated accurately.

It has been reported that irradiated UO_2 , with an initial clearance between fuel and clad of zero, has an effective thermal conductivity for fuel and contact of > 2.5 Btu/hr ft $^{\circ}\text{F}$ (13). It was observed that the effective thermal conductivity decreased to an average of 1 Btu/hr ft $^{\circ}\text{F}$ as the initial diametral clearance was increased to approximately 3.5 mils (fuel diameter = 0.360") after which no dependence on initial clearance was observed. The conductivity also appeared to be independent of the gas within the gap.

L. Bates reported an observation which substantiates the concept that radiation plays an important role in heat transfer within solids at high temperature (14). His observations indicate that an average thermal conductivity of approximately 2 Btu/hr ft $^{\circ}\text{F}$ is obtained between the temperature limits of 950°F and 4750°F .

Earlier, Robertson, et al, reported an average value of 1.6 Btu/hr ft² $^{\circ}\text{F}/\text{ft}$ for irradiated UO_2 up to $4,900^{\circ}\text{F}$ (15).

Another area of uncertainty lies in predicting the coefficient of heat transfer at the clad-fuel interface. Gap conductances ranging from 10 to 5,000 Btu/hr ft² $^{\circ}\text{F}$ have been calculated from data obtained in reference 13. The conductance is quite dependent on the initial clearance as evidenced by the conductance of 5,000 Btu/hr ft² $^{\circ}\text{F}$ for an initial zero clearance decreasing to 50 Btu/hr ft² $^{\circ}\text{F}$ for an initial diametral clearance of 1.5 mils.

Assuming the coefficient of heat transfer at the clad-fuel interface to be 1,000 Btu/hr ft² $^{\circ}\text{F}$ for cases B and C, the uncertainty in the actual fuel temperature is illustrated in Table B-V for the reference design conditions. Values are shown for the central fuel temperature at the point of maximum heat generation and the average fuel temperature at the point of average heat generation. These results illustrate the need to learn more accurately the thermal conductivity of the oxide and the coefficient of heat transfer across the fuel-clad interface for not only UO_2 but mixed oxide as well, if one is to predict the fuel temperature that will be experienced in the reactor.

TABLE B-V

Fuel Temperature

	<u>A</u>	<u>B</u>	<u>C</u>
Effective conductivity of fuel and contact, Btu/hr ft ² °F/ft	2.5		
Average conductivity of fuel, Btu/hr ft ² °F/ft		2.0	1.6
Heat transfer coef. at fuel clad interface, Btu/hr ft ² °F		1,000	1,000
Average fuel temperature, °F	1,600	2,200	2,400
Maximum central fuel temp., °F	3,100	4,700	5,300

APPENDIX C

Thermal-Hydraulic Analysis

Computation of the sodium temperature rise and core pressure drop is summarized in the following:

Heat generated per fuel rod

$$Q = 4\pi L \int k d\theta \left(1 + \frac{Q_{Ax} B1}{Q_{Core}} \right) \quad \text{(See Page 52 for nomenclature)} \quad (1)$$

Heat transported

$$Q = V\rho A_c C_p \Delta T_b \quad (2)$$

Coolant Temperature Rise

The coolant temperature rise is obtained by equating expressions (1) and (2) and solving for ΔT_b .

$$\Delta T_b = 4\pi L \int k d\theta \left(1 + \frac{Q_{Ax} B1}{Q_{Core}} \right) / V\rho A_c C_p \quad (3)$$

Pressure Loss

$$\Delta P = 4f' \frac{L}{D} \frac{\rho}{2g} V^2 \quad (4)$$

where the friction faction, f' , is the Fanning friction factor, f , times a correction f'/f , for the effect of the spacers

$$f' = (f'/f)f \quad (5)$$

With Reynolds numbers ranging from 5000 to 200,000, the fraction factor, f , can be correlated by the following (16):

$$f = \frac{0.016}{(Re)^{0.2}} \quad (6)$$

While this expression for the friction factor was determined from measurements of flow loss within tubes, the work of Le Tourneau, Grimble, and Zerbe (17) has shown close agreement for parallel flow between rods in a rod bundle.

The use of spiral wrap wires along the rod for maintaining rod spacing, however, represents a significant departure from the test geometry used in (17), where the rods were supported at the two ends. Several investigators have looked at the influence of spiral wrap wires on the friction factor (18, 19, 20). Generally, the results can be expressed in terms of the ratio of the friction factor with spacing to the friction factor with no

spacing (defined here as f'/f). A value of f'/f of approximately 1.75 was used for the flow conditions and geometry in the reference design. A significant spread in the data was observed among the references; indicating the need for experimental measurements on the assembly configuration.

An additional factor of 1.10 was introduced into equation (4) to account for the loss of pressure in the entrance nozzle and exit from the rod bundle.

A relation between the coolant temperature rise and pressure loss is obtained by combining equations (3) and (4). This relation is shown on Figure C-I for the reference design fuel geometry and heat generation rate.

The optimum coolant temperature rise and pressure loss is dependent upon several considerations. Increasing the temperature rise and decreasing the pressure loss reduces the pumping power per ekw; however, as the temperature rise is increased, problems associated with thermal stresses increase. Optimization of these factors is beyond the scope of this report; however, the relation between pumping power and coolant temperature rise and pressure loss is described below.

Pumping Power

The primary pumping power can be related to the reactor power in the following manner:

$$Q_P = \frac{W 144 (\Delta P_T)}{J\rho}$$

$$Q_R = WC_p \Delta T_b$$

$$\text{or } \frac{Q_P}{Q_R} = \frac{144}{c_p J\rho} \times \frac{\Delta P_T}{\Delta T_b}$$

Where ΔP is the combined pressure loss of fuel element and coolant system.

Considering the combined pump and motor efficiency to be 75% and the net plant efficiency to be 37%, and letting the pressure drop of the primary loop external to the fuel elements equal 50 psi, the coolant conditions are shown on Figure C-I for values of primary pumping power (input) equal to 1, 2, and 3% of the net plant output. 2% was chosen for the reference design with a corresponding coolant temperature rise of 265°F and fuel element pressure loss of 70 psi. The primary pumping power could be reduced to 1% by increasing the ΔT to 380°F and reducing the fuel element pressure drop to 40 psi.

FIGURE C-1 - COOLANT TEMPERATURE RISE AND PRESSURE LOSS ACROSS THE REFERENCE FUEL ELEMENT

Fuel Rod Vibration

The analysis of fuel rod vibration is based on the work of Burgreen, Byrnes, and Benforado (21). While the empirical relations were derived from data taken with water, the use of sodium should present no serious problem.

It has been shown that the natural frequency of a fuel rod can be predicted by using the modulus of elasticity and moment of inertia of the cladding (22). The presence of oxide apparently has no significant effect on stiffness; however, the fuel weight is used in determining the weight per unit length. It was also found that the constant for hinged ends, 30.8, gave better correlation than the clamped end constant, 70. With these considerations, calculations were made to determine the amplitude of vibration for a given spacing interval. The results are summarized below:

Distance between Spacing inches	Rod deflection, δ , mils
6	0.5
12	3.9
24	33.4

Rod support every six inches should be adequate to prevent vibrational problems. The stress due to a deflection of 0.5 mils is approximately 100 psi.

The analysis was based on rods supported with transverse plates. How applicable these results are to bundles of rods supported by spiral wrap wires is difficult to assess; hence, hydraulic tests will be required to insure satisfactory design.

APPENDIX D

Fuel Cycle Economics

The elements of fuel cycle cost include the fabrication and reprocessing of fuel elements, credit for the plutonium produced, inventory charges on the plutonium in the fuel cycle, and capitalization of the cost of fabricating the first core. Although capitalization of the first core is frequently not included in the fuel cycle costs, the cost of the core is a function of the specific power and unit fabrication cost and should be included in studies involving these parameters.

In the following discussion, unit costs and values of essential economic parameters are summarized, and the method of analysis is described for the reference design. Finally, a fuel cost breakdown of other cases studied is summarized in Table D-II.

A. Unit Costs

Unit costs and values of the essential economic parameters are summarized in Table D-I. Other parameters pertinent to the fuel cycle cost can be found in the description of the reference design in Table II, Page 12.

The costing is based on both 1962 and projected 1975 estimates in terms of 1962 dollars. Explanations of the values used are given below.

1. Fabrication: Estimates of the cost of fabricating plutonium bearing oxide fuel have been made for pins of the dimensions specified for the reference design (23, 24). A flow diagram for the fabricating process is outlined, and estimates of the personnel and equipment required are shown in reference (23). The costing is based on the assumption that reactor power stations totaling 3,000 MWe would be using fuel of similar specifications. The average discharge fuel exposure of the core is set at 100,000 MWD/T.

In summary, the cost of fabricating the $\text{PuO}_2\text{-UO}_2$ fuel pins for the core and axial blanket to the specifications of the reference design (see Table II, page 12) is estimated to be approximately \$135/kg of Pu and U. The depleted UO_2 radial blanket rod is estimated to have a fabrication cost of about \$50/kg U. These results include the cost of converting the nitrate available from the reprocessing plant to oxide and a 1% loss of plutonium and uranium.

TABLE D-I

Summary of Basis for Fuel Cycle Cost Analysis

	Current Basis (1962)	Projected Estimates (1975)
Fabrication *		
Core and Axial Blanket, \$/kg Pu and U	270	135
Radial Blanket, \$/kg U	104	52
Reprocessing		
Shipping, Core and Ax. Bl., \$/kg Pu and U	15	10
Rad. Bl., \$/kg Pu and U	10	7.00
Separation, \$/kg	44	20
Losses	1%	1%
Plutonium Value, Pu-239 and 241, \$/gm Pu	9.00	6.50
Pu-240, \$/gm Pu	1.50	1.10
Inventory Charge on Plutonium, % per year	4.75	10
Plant Load Factor, %	80	80
Thermal Efficiency, Net, %	37	37
1st Core Capitalization, %	14	14

* Includes conversion of nitrate to oxide and a loss of 1%.

The results summarized above are estimated for the year 1975. It is assumed that the unit cost for 1962 fabrication is 270 \$/kg for the plutonium bearing fuel and 104 \$/kg for the natural UO_2 fuel.

2. Reprocessing Cost:

- a. Shipping Spent Fuel - Established freight tariffs covering the shipment of irradiated fuel have not been established. In the absence of established rates, the following assumptions have been made. The current charges for the core and axial blanket are assumed to be \$15/kg.

The radial blanket, because of its reduced plutonium content and heat generating rate is charged at \$10/kg, which is consistent with current values used in

estimating UO_2 shipping charges. The 1975 projected cost is estimated to be \$10/kg for the core and axial blanket and \$7/kg for the radial blanket.

- b. Separation - Present day reprocessing cost estimates in this report * are based on the AEC's Multi-Purpose Chemical Processing Plant whose capital and operating costs were initially estimated by the AEC to total \$15,300 per day. Since then, this cost has escalated to \$17,000 per day, for a unit processing cost of \$17.00/kg of low enrichment uranium (daily capacity of 1000 kg for fuel containing 3% or less fissionable material). An estimated amount is added to this cost to cover the time required to get the plant on the line and to clean the equipment at the end of the batch. This time is arbitrarily set equal to the reprocessing time, except that it will not be less than two nor more than eight days.

The reprocessing time for a batch of fuel depends on its enrichment, and can be determined from Figure D-1. The permissible concentration for plutonium is assumed to be two-thirds of that for U-235 because of plutonium's lower critical mass. The concentration was established by blending the core and both blankets together. The advantage of blending is obtained from the low concentration of plutonium in the radial and axial blankets, which would limit the daily throughput of the processing plant by total weight rather than by the weight of fissionable material if processed separately from the core. Hence, the blending of the low concentration of the blankets with the high concentration of the core results in a reduction of the total separation cost. The average concentration is approximately 4%. The permissible rate is approximately 800 kg per day according to Figure D-1. Approximately 3500 kg of fuel (metal) are discharged per refueling. Hence, the reprocessing time is 4.5 days plus another 4.5 days for cleanup, leaving a net separation cost of 44 \$/kg of U Pu.

In estimating projected costs, it is believed that improvements in reprocessing plant load factors and experience gained from commercial processing of fuel will reduce the unit cost of separation to 20-30 \$/kg Pu and U (24). It is assumed that as a sufficient load of similar fuel develops to occupy a reprocessing plant full time, the down time associated with plant starting and cleaning per batch will be eliminated.

* A study of fabrication and reprocessing methods specifically adapted to the fast oxide reactor is currently being done (24).

FIGURE D-1 - FUEL REPROCESSING RATE

- c. Conversion of nitrate to oxide - included in the fabrication cost.
- d. Losses - 1% of the plutonium and uranium are assumed to be non-recoverable during reprocessing of the fuel - (another 1% is included in the fabrication of fuel). The worth of the losses are based on the plutonium values shown below.

The total unit reprocessing cost is then

	<u>1962</u>		<u>1975</u>	
	<u>Core and Ax. Bl.</u>	<u>Rad. Bl.</u>	<u>Core and Ax. Bl.</u>	<u>Rad. Bl.</u>
Shipping, \$/kg	15	10	10	7
Separations \$/kg	44	44	20	20
Losses \$/kg	<u>4</u>	<u>4</u>	<u>3</u>	<u>3</u>
Total	63	58	33	30

3. Value of Plutonium

The price of plutonium metal for the present time is taken to be \$12/gm Pu, regardless of isotopic composition. This price has been established by the government for the purchase of plutonium from foreign countries and for domestic research. The cost of converting plutonyl nitrate into metallic buttons is currently set by the government at \$1.50/gm so that the price of plutonium in the form of nitrate is \$10.50/gm.

The above prices do not necessarily bear a relation to the actual value of plutonium as a reactor fuel. It is believed that as plutonium is commercially utilized as reactor fuel, the price will be influenced by the prevailing price of U-235 and the relative nuclear worth of the plutonium isotopes. Considering (1) the fissionable isotopes of plutonium (Pu-239 and Pu-241) to be equal in value to U-235 at an enrichment of 2.7% (typical of thermal, H₂O or graphite moderated reactors with stainless steel clad) and (2) the worth of Pu-240 to be one-sixth of the fissionable isotopes, a price for plutonium is obtained which is a function of anticipated changes in the value of U-235. Based on the price of enriched uranium as revised by the AEC in July, 1961, the price for the plutonium isotopes with these assumptions is as follows:

Pu-239 and 241 = \$9.00/gm of Pu as plutonyl nitrate

Pu-240 = \$1.50/gm of Pu as plutonyl nitrate

The price of enriched uranium is based on the cost of refined ore at \$17.60/kg U₃O₈ (\$8/#), UF₆ feed at \$23.50/kgU and separative work at \$37.29/kgU.

It is anticipated that by 1975 the price of enriched uranium will be further reduced based on the cost of refined ore at \$13.20/kg U_3O_8 (\$ 6/lb U_3O_8), UF_6 feed at \$18.04/kgU, and separative work at \$25/kgU. The corresponding plutonium price is then estimated to be

Pu-239 and 241 = \$6.50/gm of Pu as plutonyl nitrate

Pu-240 = \$1.10/gm of Pu as plutonyl nitrate

4. Inventory Charge

The interest rate is taken at 4.75% for the 1962 estimate and 10% for 1975. An out-of-pile inventory is included consisting of:

Cooling, Shipping, and Reprocessing	5 months
Fabrication	3 months

The plutonium produced in the axial and radial blanket was included as inventory.

5. First Core Capitalization

The cost of fabricating the first core and blanket and the cost of converting the nitrate to oxide, is capitalized at the rate of 14% per year.

6. Fuel Life

The average core exposure is taken to be 100,000 MWD/T. The axial and radial blanket are considered to be refueled with the same average lifetime as that of the core; however, the incentive for altering the blanket refueling shedule was discussed on page 24.

7. Plant Efficiency

The net plant efficiency is 37%, based on steam conditions of 900°F/1500 psi. These conditions are consistent with the sodium outlet temperature of 1050°F.

8. Plant Load Factor

The load factor is assumed to be 80%.

B. Evaluation of Reference Design

The method used in evaluating the fuel cycle cost is illustrated for the reference design previously outlined in Table II, page 12.

1. Fuel Throughput

The throughput is dependent on the fuel exposure and plant efficiency in the following manner:

$$\left(\frac{\text{kg}}{\text{ekw-hr}}\right)_C^* = (\text{MWD}/T)_C \times \frac{1}{\eta} \times 10^{-3} \frac{\text{MW}}{\text{kw}} \times \frac{\text{Days}}{24 \text{ hours}} \times \frac{908 \text{ kg}}{T} \quad (1)$$

where MWD/T = average fuel exposure

η = net plant thermal efficiency

T = 2000 # of Pu and U

Note: Weight in this analysis is always expressed in terms of metal, not oxide.

Similar expressions are written for the axial and radial blanket. Since the days in the reactor are assumed to be the same for the axial and radial blanket, the fuel exposure of the blanket is related to that of the core by correcting for differences in specific power. For example,

$$\left(\frac{\text{MWD}}{T}\right)_A = \left(\frac{\text{MWD}}{T}\right)_C \times \left(\frac{P_A}{P_C}\right) \times \left(\frac{V_f}{V_f}\right)_C \quad (2)$$

where $\frac{P_A}{P_C}$ = ratio of power in axial blanket to core

$\frac{(V_f)_C}{(V_f)_A}$ = ratio of fuel volume in core to axial blanket

$$\text{also } \left(\frac{\text{MWD}}{T}\right)_R = \left(\frac{\text{MWD}}{T}\right)_C \times \frac{P_R}{P_C} \times \left(\frac{V_f}{V_f}\right)_C \quad (3)$$

Equation 1 determines the weight of fuel required per kw-hr generated in the core. Since the power delivered from the generator originated in both core and blanket, it is necessary to weight the throughput accordingly.

$$\frac{(\text{kg})_C}{(\text{ekw-hr})_G^{**}} = \left(\frac{\text{kg}}{\text{ekw-hr}}\right)_C \times \frac{(\text{kw-hr})_C}{(\text{kw-hr})_G} \quad (4)$$

where $(\text{kw-hr})_C/(\text{kw-hr})_G$ is the fraction of the total power generated in the core.

The axial and radial blanket are evaluated similarly.

Values of power and fuel fraction, fuel exposure, and throughput are tabulated below.

* C = core

A = axial blanket

B = radial blanket

** (ekw-hr)G = Total ekw-hr delivered from the generator (net)

	<u>Core, C</u>	<u>Axial Blanket, A</u>	<u>Radial Blanket, R</u>
Ratio of power to core power	1.00	0.094	0.068
Fraction of total power	0.86	0.081	0.059
Vol. of core fuel/vol. of fuel	1.00	1.30	0.42
Fuel exposure, MWD/T	100,000	12,200	2,850
Throughput, kg/(ekw-hr)G	8.8×10^{-7}	6.8×10^{-7}	21.1×10^{-7}

2. Fabrication Cost

The fabrication cost per ekw-hr is as follows:

$$\text{Mills/ekw-hr} = \text{kg/ekw-hr} \times \$/\text{kg} \times 10^3$$

		<u>1962</u>		<u>1975</u>	
	<u>Thruput kg/ekw-hr</u>	<u>Unit Cost \$/kgU and Pu</u>	<u>Mills/ekw-hr</u>	<u>Unit Cost \$/kgU and Pu</u>	<u>Mills/ekw-hr</u>
Core	8.8×10^{-7}	270	0.24	135	0.12
Ax. Bl.	6.8×10^{-7}	270	0.18	135	0.09
Rd. Bl.	21.1×10^{-7}	104	0.22	52	0.11
Total Fabrication Cost,	<u>Mills ekw-hr</u>		0.64		0.32

3. Reprocessing Cost

		<u>1962</u>		<u>1975</u>	
	<u>Thruput kg/ekw-hr</u>	<u>Unit Cost \$/kgU and Pu</u>	<u>Mills/ekw-hr</u>	<u>Unit Cost \$/kgU and Pu</u>	<u>Mills/ekw-hr</u>
Core	8.8×10^{-7}	63	0.06	33	0.03
Ax. Bl.	6.8×10^{-7}	63	0.04	33	0.02
Rad. Bl.	21.1×10^{-7}	58	0.12	30	0.06
Total	Mills/ekw-hr		0.22		0.11

4. Depletion of fissionable material

Since the breeding ratio is greater than 1, the depletion charge is a credit of the following amount:

$$\text{Mills/ekw-hr} = \$/\text{g} \times \frac{\text{g Pu Dest}}{\text{ekw-hr}} \times \frac{\text{Pu Credit}}{\text{Pu Dest}} \times 10^3$$

$$\begin{aligned}
 \frac{\text{g Pu Dest}}{\text{ekw-hr}} &= \frac{\text{atoms fiss}}{3.20 \times 10^{-11} \text{ watt-sec}} \times \frac{10^3 \text{ W}}{\text{kw}} \times \frac{3600 \text{ sec}}{\text{hr}} \times \frac{239 \text{ g}}{\text{Mol}} \\
 &\times \frac{1}{6.06 \times 10^{23}} \frac{\text{Mol}}{\text{Atom}} \times \frac{\text{th kw}}{.37 \text{ ekw}} \times \frac{C_{49} + F_{49}}{F_{49} + F_{28} + F_{40}} \\
 &= 1.2 \times 10^{-4} \left(\frac{C_{49} + F_{49}}{F_{49} + F_{28} + F_{40}} \right)
 \end{aligned}$$

where C = number of captures per second

F = number of fissions per second

The bracket term is the number of Pu-239 and 241 atoms destroyed per fission, and has a value of 1.08. This includes a correction for the contribution of the fissioning of U-238 and Pu-240 to the power produced and the non-fission absorption of Pu-239 and 241.

$$\frac{\text{Pu credit}}{\text{Pu Dest}} = \frac{\text{Pu Formed}}{\text{Pu Dest}} - \frac{\text{Pu Dest}}{\text{Pu Dest}} = \text{B.R} - 1$$

where B.R = Breeding ratio = Captures in U-28 and Pu-40/Absorptions in Pu-49 and Pu-41

= 1.08 for the reference design

Hence, the plutonium credit amounts to the following:

Value of fiss. Pu \$/gm Pu	1962 9.00	1975 6.50
Pu Credit, Mills/ekw-hr	0.09	0.07

Credit for the non-fissionable isotope, Pu-240, is negligible since only one atom of the non-fissionable isotope is calculated to be formed for every four atoms of the fissionable isotopes. Furthermore, the value of the non-fissionable isotope is assumed to be one-sixth of the fissionable isotope.

5. Plutonium Use Charge

The weight of plutonium consists of the in-pile material which is dependent on the specific power and the out-of-pile material, which is dependent on throughput and length of time required for shipping, cooling, and processing.

$$\begin{aligned} \text{Mills/ekw-hr} = & \frac{\$ \times 10^3}{\text{kg Pu}} \times \text{Int. Rate} \times \left[\frac{\text{kg Pu}}{\text{kw}} \times \frac{\text{Th kw}}{.37 \text{ ekw}} \right. \\ & \times \frac{\text{year}}{8760 \text{ hr}} \times \frac{1}{0.8 \text{ LF}} \times \frac{\text{kw}_{\text{core}}}{\text{kw}_{\text{total}}} + \\ & \left. \frac{\text{kg Pu and U}}{\text{ekw-hr}} \times \frac{\text{kg Pu}}{\text{kg Pu and U}} \times \text{out-of-pile time} \right]_{\text{core}} \end{aligned}$$

$$\text{where } \frac{\text{kw}}{\text{kg Pu}} = 1,000 \text{ kw/kg fiss. Pu} \times \frac{2}{3} \frac{\text{fiss Pu}}{\text{Total Pu}} = 667$$

$$\frac{\text{kw}_{\text{core}}}{\text{kw}_{\text{total}}} = 0.86$$

$$\frac{\text{kg Pu and U}}{\text{ekw-hr}} = 8.8 \times 10^{-7}$$

$$\frac{\text{kg Pu}}{\text{kg Pu and U}} = \frac{\text{fiss. Pu}}{\text{Pu and U}} \times \frac{\text{total Pu}}{\text{fiss. Pu}} = .128 \times 3/2 = .192$$

out-of-pile time = .67 year (8 months)

Similar calculations were made for the radial and axial blanket. Hence, the plutonium use charge is as follows:

	<u>1962</u>	<u>1975</u>
Int. Rate, %	4.75	10
Pu Value		
fiss Pu, \$/gm Pu	9.00	6.50
non-fiss. Pu, \$/gm Pu	1.50	1.10
average Pu, \$/gm Pu	6.50	4.70
Pu use charge Mills/ekw-hr	<u>1962</u>	<u>1975</u>
Core	0.19	0.29
Ax. Bl.	0.02	0.03
Rad. Bl.	<u>0.03</u>	<u>0.04</u>
Total	0.24	0.36

6. First Core Capitalization

The cost of capitalizing the first fuel load is as follows:

$$\text{Mills/ekw-hr} = \frac{(\$/\text{kg} \times \text{kg}) \times \text{Int. Rate} \times 10^3}{\text{Net electrical power} \times \text{operating hours/year}}$$

Net electrical power = 517,000 ekw

Operating hours per year = 8760 x Load Factor.

$$= 8760 \times .8 = 7,000 \text{ hours/year}$$

	<u>1962</u>		<u>1975</u>	
	<u>Unit Cost</u>	<u>Mills/ekw-hr</u>	<u>Unit Cost</u>	<u>Mills/ekw-hr</u>
Core	270	0.10	135	0.05
Ax. Bl.	270	0.07	135	0.03
Rad. Bl.	104	<u>0.08</u>	52	<u>0.04</u>
Total		0.25		0.12

C. Summary of Fuel Cycle Costs

A breakdown of the fuel cycle cost for the cases studied is shown in Table D-II. The results are based on 1975 economic assumptions.

TABLE D-II SUMMARY OF FUEL CYCLE COSTS (1975 Economic Assumptions)

Case	1 (Ref. Design)	2	3	4	5	6	7	8	9
Total Reactor Power, MWe	517	533	545	500	258	387	647	775	338
Reactor Core size, height, ft.	3.25	3.25	3.25	3.25	3.25	3.25	3.25	3.25	2.6
diameter, ft.	6.5	6.5	6.5	6.5	6.5	6.5	6.5	6.5	5.2
Specific Power, kw/kg fiss Pu(core)	1000	1000	1000	1000	500	750	1250	1500	1000
Reactivity from total loss of Na, \$	0	1.0	1.4	-1.0	0	0	0	0	0
Core Composition, Fuel, V/o	33.3	35.0	36	31	33.3	33.3	33.3	33.3	38
coolant	50.0	47.5	46	53	50.0	50.0	50.0	50.0	43
structure	16.7	17.5	18	16	16.7	16.7	16.7	16.7	19
Pu Concentration, core, %	12.8	12.5	12.4	13.2	12.8	12.3	12.8	12.8	13.5
Ax. Bl.	1.5	1.4	1.4	1.5	1.5	1.5	1.5	1.5	1.5
Rad. Bl.	0.6	0.6	0.6	0.6	0.6	0.6	0.6	0.6	0.8
Breeding Ratio	1.08	1.10	1.11	1.06	1.08	1.03	1.08	1.08	1.11
Refueling interval, weeks	10	11	11.5	9	20	13.3	8	6.7	9.6
Fuel dia, clad O.D., in.	0.25	0.25	0.25	0.25	0.35	0.23	0.23	0.20	0.25
Fuel Cost, mills/ekw/hr									
Fabrication, core	0.12	0.12	0.12	0.12	0.10	0.11	0.12	0.13	0.13
Ax. Bl.	.09	.09	.09	.09	.08	.08	.10	.10	.12
Rad. Bl.	.11	.11	.11	.11	.09	.10	.11	.12	.15
Total	0.32	0.32	0.32	0.32	0.27	0.29	0.33	0.35	0.40
Reprocessing, core	0.03	0.03	0.03	0.03	0.03	0.03	0.03	0.03	0.03
Ax. Bl.	.02	.02	.02	.02	.02	.02	.02	.02	.02
Rad. Bl.	.06	.06	.06	.06	.06	.06	.06	.06	.07
Total	0.11	0.11	0.11	0.11	0.11	0.11	0.11	0.11	0.12
Fuel Depletion	-0.07	-0.09	-0.10	-0.05	-0.07	-0.07	-0.07	-0.07	-0.10
Pu Inventory Chg, core	0.29	0.29	0.29	0.29	0.58	0.39	0.23	0.19	0.28
Ax. Bl.	.03	.03	.03	.03	.06	.04	.02	.02	.03
Rad. Bl.	.04	.04	.04	.04	.08	.05	.03	.03	.05
Total	0.36	0.36	0.36	0.36	0.72	0.48	0.28	0.24	0.36
1st Core Capt'l Chg, core	0.05	0.05	0.05	0.05	0.08	0.06	0.04	0.04	0.05
Ax. Bl.	.03	.03	.03	.03	.05	.04	.03	.02	.04
Rad. Bl.	.04	.04	.04	.04	.07	.05	.04	.03	.05
Total	0.12	0.12	0.12	0.12	0.20	0.15	0.11	0.09	0.14
Tot. Fuel Cycle Cost, Mills/ekw hr	0.84	0.82	0.81	0.86	1.23	0.96	0.76	0.72	0.92

Cases 1 through 4 illustrate the effect of sodium content on the fuel cycle cost. The sodium contents studied cover changes in reactivity introduced by 100% loss of sodium (ΔK_{sod}) ranging from - 1.0\$ to + 1.4\$. As previously noted on Figure 1, page 5, this range of reactivity is obtained within a relatively narrow band of sodium content so that only a small effect on fuel cycle cost is experienced.

The effect of core specific power is shown with cases 5 through 8. The values of core specific power investigated range from 500 to 1500 kw/kg fiss. Pu.

The fuel cost of a core smaller than the reference design is shown in Case 9. The core height and diameter are 2.6 feet and 5.2 feet respectively as compared to 3.25 feet by 6.5 feet for the reference design. This core has a fuel cycle cost higher by <0.1 Mills/ekw hr than the reference design. The increase in cost is due to a higher unit fabrication cost as a result of shorter length rods. Optimization of the blanket thickness, which was kept constant at 1.25 feet, probably would reduce the difference.

NOMENCLATURE

Nu	= Nusselt Number, $Nu = hD/k$
Re	= Reynolds Number, $Re = GD/\mu$
Pr	= Prandtl Number, $Pr = C_p\mu/k$
Pe	= Peclet Number, $Pe = Re \times Pr$
σ	= Ratio of eddy diffusivity for heat transfer to eddy diffusivity for momentum transfer
h	= Coefficient of heat transfer
k	= Coefficient of thermal conductivity
ρ	= Density
μ	= Viscosity
C_p	= Specific heat at constant pressure
L	= Length
D	= Hydraulic diameter
x	= Clad thickness
A_c	= Coolant flow area
V	= Fluid velocity
W	= Flow rate
Q	= Heat generation rate
Q/A	= Heat generation rate per unit surface area
Q_p	= Pumping Power
Q_R	= Reactor power
$\int kd\theta$	= Integral of the product of the coefficient of thermal conductivity and the radial temperature difference within the fuel. Linear power generation, $Q_L = 4\pi \int kd\theta$.
$Q_{Ax Bl.}/Q_{Core}$	= Ratio of axial blanket to core heat generation rate
$(Q_{Max.}/Q_{Min.})_{Axial}$	= Ratio of maximum to minimum volumetric heat generation rate in axial direction.
ΔT_b	= Coolant temperature rise across the fuel element
ΔP	= Pressure loss across the fuel element
f	= Friction factor
g	= Acceleration of gravity
J	= 778 ft-lb per Btu

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