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LARGE FAST REACTOR DESIGN STUDY

January 1964

prepared for

Chicago Operations Office
U.S. Atomic Energy Commission
Contract AT(11-1)-1316

ALLIS-CHALMERS

ATOMIC ENERGY DIVISION

and

Atomic Power Development Associates

Babcock & Wilcox Co.

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FINAL REPORT
for
LARGE FAST REACTOR
DESIGN STUDY

January 1964

prepared for
Chicago Operations Office
U. S. Atomic Energy Commission
Contract AT(11-1)-1316

ALLIS-CHALMERS

Atomic Energy Division
Milwaukee 1, Wisconsin

The Babcock & Wilcox Company

ATOMIC ENERGY DIVISION

in association with

**UNITED ENGINEERS
& CONSTRUCTORS INC**

Vitro ENGINEERING COMPANY
A DIVISION OF VITRO CORPORATION OF AMERICA

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1. SUMMARY

1.1 GENERAL

This study was undertaken as part of the Atomic Energy Commission long-range planning program. Its object was to establish the preliminary characteristics of a 1000-Mwe sodium-cooled fast-breeder reactor. The reactor concept studied employed stainless steel clad plutonium-uranium oxide fuel. Design goals of a breeding ratio of 1.2 and a sodium outlet temperature of 1200 F were set.

1.2 RESULTS

The results of the study show that a practical 1000-Mwe reactor with an outlet temperature of 1200 F and a breeding ratio of 1.32 can be designed having a fuel cycle cost of 0.69 mills/kwhr at 0.8 plant factor. The use of an annular core has resulted in essentially eliminating the large positive sodium-void-reactivity coefficient normally found in a core of this volume. Because of the low sodium void coefficient and a significant negative Doppler coefficient the reactor has been shown to have desirable self-limiting power-increase characteristics, even without safety assembly scram, during the credible accidents of control-rod runout and loss of primary coolant flow.

In addition to the small sodium void coefficient, the annular core geometry allows the use of an efficient radial blanket which achieves a high conversion ratio without making the blanket excessively thick. Further, a radial blanket which is separable from the core, allows efficient blanket management without any interference with the core management program. Other advantages of the annular core are a low peak-to-average power distribution allowing a higher-average core power density and a mechanical arrangement that provides space for a more optimum placement of control assemblies. The reference core is shown in Fig. 1.1 and a sectional view of the reactor in Fig. 1.2. Pertinent characteristics of the reactor are given below:

Reactor Power	2500 Mwt
Coolant Outlet Temperature	1200 F
Breeding Ratio	1.32
Core Life (full power)	836 days
Core Burnup	100,000 Mwd/MT
Doubling Time (full power)	14.7 yr
Mean Fission Energy	190 kev
Fertile/Fissile Ratio	4.57
Core	13 ft OD x 9 ft ID x 4 ft high
Blanket (inner and outer)	1-1/2 ft thick x 6 ft high

	<u>Core</u>	<u>Blanket</u>
Conversion Ratio	0.52	0.80
Pin Diameter	0.300 in.	0.607 in.
Cladding	316 SS	316 SS
Material	$\text{PuO}_2\text{-UO}_2$	depleted UO_2
Number of Assemblies	498 fuel, 48 control	858

The safety characteristics were considered to be the most important technical area to be investigated in the study. Earlier studies on intermediate-size sodium-cooled fast reactors identified a positive reactivity coefficient associated with removal of sodium from the core. Calculations on large-volume cylindrical cores indicated a reactivity increase of over \$10 for voiding of the core. The consequences of such a coefficient appear intolerable and for that reason an effort was made to design the core in a way that would reduce or eliminate reactivity increases due to this effect. Doppler broadening of the U-238 resonances results in the largest negative nuclear coefficient relied on to terminate a large excursion. Uncertainties as to the magnitude of the Doppler coefficient and the desirability of having prompt, reasonably large net negative temperature and power coefficients emphasize the need for reducing or eliminating any substantial positive contribution to the overall coefficients. Thus, the principal objectives of the study were to investigate methods for reducing the positive sodium void coefficient.

Since the positive reactivity effect of sodium removal is due primarily to absorption and spectral changes, a study of the effect of changes in core geometry and composition was made. Using a large cylindrical core as the basis of comparison, calculations were made for the following variations:

- (1) sodium volume fraction
- (2) coolant composition
- (3) fuel material composition
- (4) core length-to-diameter ratio
- (5) beryllium moderator volume fraction
- (6) annular blanket regions within the core
- (7) reflector regions on core boundaries.

For practical compositions (based upon mechanical and thermal considerations) it was soon determined that leakage effects were most beneficial in reducing the sodium-void coefficient. Other variations had undesirable effects on the coefficient or resulted in an intolerably low breeding ratio. To overcome the obvious disadvantages of a low, large diameter core the annular core was considered and shown to possess the desired characteristics. Since voiding of the core would also result in voiding of the region above the core, an upper axial reflector was investigated and calculations have shown that this reflector further lowers the void coefficient.

The design chosen as reference for detailed calculations for the study is an annular core 4 ft high and 2 ft thick. The bottom end of the core has a 6-in. thick axial blanket and the top end an 18-in. axial sodium reflector. The inner and outer diameters of the core are covered by an 18-in. thick radial blanket. Control is accomplished with 36 shim and 12 safety assemblies containing boron carbide.

Lack of experience on large dilute plutonium cores has led to a very conservative approach to the problem of reactor safety. Further effort is necessary and desirable to optimize thermal and nuclear performance of the reactor. Improvements in fuel cycle costs while maintaining present safety criteria may be possible by increasing the fertile material concentration.

The same conservative assumption employed with safety has also been used in most of the mechanical design. Recent data from fuel irradiation tests lend assurance that burnup of 100,000 Mwd/MT is possible. The fuel element structure has been designed on this basis. The vessel design, fuel handling system design and control assembly drive design are all predicated on principles that allow construction with present technology.

The reactor vessel has been designed for single-pass parallel-flow of core and blanket coolant (Fig. 1.3). Refueling and reactor servicing is accomplished by a rotatable top shield plug which also contains the control assembly drives and reactor nuclear instrumentation.

Reactor building and plant layouts have been made and both on-site and off-site reprocessing of fuel considered. On-site reprocessing of core fuel and recovery of blanket plutonium have been shown to be economically advantageous and should continue to show promise of low fuel cycle costs for future large breeder reactors. The on-site reprocessing of fuel provides for simplified control of fissionable material by the reactor operator and minimizes out-of-reactor inventory, shipping costs, and blanket plutonium recovery costs.

1.3 CONCLUSIONS

The design described above has been based on minimum extrapolation of current technology. The objective of designing a reactor with a low sodium-void coefficient, consistent with a high breeding ratio, reasonable doubling time, long core life, low fuel-cycle cost, and high reactor outlet sodium temperature has been achieved. Principal emphasis in this study was placed on a detailed understanding of the sodium coefficient and the design of a reactor having a low sodium-void coefficient.

Further engineering and physics analysis of the annular core and a plant design study are required to understand the characteristics of the system in greater detail, to optimize the plant with respect to the cost of power; and to further define the R and D program.

In this study, several specific development programs have been identified which are critical in the present AEC fast reactor program schedule. Of particular importance is an early start in the construction of a critical facility for the study of large dilute plutonium cores.

On the basis of current technology and the AEC R&D program now planned, it is believed that an economically attractive large fast reactor plant is potentially marketable in the mid 1970's, if a vigorous program leading to an early prototype is pursued.

SIMPLIFIED CORE SECTION

FIG. 1.1

**1000 MWE
FAST BREEDER REACTOR
ANNULAR CORE**

FIG. 1.2

FIG. 1.3

REACTOR FLOW DIAGRAM

2. INTRODUCTION

The Report to the President -- 1962 on civilian nuclear power emphasized that "the nuclear power program should continue on an expeditious basis. Commission support should continue with added emphasis on stimulating industrial participation." Among important objectives, both short and long range, there should be "intensive development and, later, demonstration of breeder reactors to fill the long-range needs of utilizing fertile as well as fissile fuels. An important corollary area is the development of economical chemical reprocessing methods."

The work reported in this document has been performed under a study sponsored, in part, by the AEC. The purpose of the study is to contribute to the long-range sodium-cooled fast-reactor program referred to by determining the characteristics and fuel-cycle economics of a large fast breeder reactor. This study shows that the goals of the fast-reactor program can be met and indicates the areas in which research and development may be required to successfully build a large fast-breeder reactor plant: optimizing the reactor and achieving a low capital investment and minimum operating costs. However, the study was limited in scope and time and is, thus, only the first step toward defining the target large fast-breeder reactor plant. Particular emphasis has been placed on those items considered essential to sustain long-term interest in this type of reactor -- safety and fuel-cycle economics.

The approach taken in this 1000-Mwe study is rather conservative, using, essentially, today's technology in the nuclear, thermal, and mechanical design. It was considered more important to provide a sound basis for the design in this initial step, recognizing that the design might not be optimum, rather than attempt to take into account many improvements which might be forthcoming in the future.

The bulk of fast-reactor knowledge and experience is on relatively small metallic-fueled cores. Compared to these cores, which are quite well understood, the advanced large fast reactor concepts introduce a number of novel problems related to safety. These problems originate from three important differences between the present and advanced concepts:

- (1) Use of plutonium rather than U-235 fissile material.
- (2) Use of ceramic rather than metallic fuel elements.
- (3) Cores in the several thousand rather than hundred-liter volume size.

The major problems of the advanced system arising from these differences are core sodium void coefficients which tend to be positive, and fuel expansion properties of a type that could result in nonreproducible reactivity feedback in power transients. There is virtually a complete lack of critical experiment data on large dilute plutonium systems and there is some doubt as to the actual values of some of the predicted characteristics of these advanced fast reactors. In line with the philosophy of a conservatively safe design, the approach chosen has been to eliminate or minimize the effect of these two quantities as safety problems.

The positive sodium-void coefficient is regarded as a very serious problem if the magnitude of the coefficient is as calculated. There are two possible reasons to consider it as such.

First, if the advanced reactor has a positive component in its feedback, then the possibility of an unstable behavior exists and must be considered. However, this does not appear to be a real problem since it appears quite certain that there are other feedback components which have a time response similar to the sodium, and which are also negative and large enough to override the sodium coefficient.

The other possible problem is that of sodium boiling and the formation of voids in the core channels. According to present calculations, void formation can cause a very large reactivity effect: on the order of several dollars, which is substantially more than could be overridden by Doppler broadening of the resonances. Indeed, voiding is the most serious concern. Once boiling commences the sodium channels are very rapidly voided because of the inherent low pressures of the sodium systems and the high specific volume ratio of the vapor and liquid. The abruptness of this voiding process has been demonstrated in some simulated uranium-sodium experiments. Therefore, voiding in periods of milliseconds is a mechanism for very rapid reactivity insertion and potential large-scale nuclear energy release. Furthermore, this accident has an autocatalytic feature if the positive void coefficient is dominant. The disconcerting thing about an approach which permits a substantial positive sodium-void coefficient is that the maximum accident and the necessity for containment have become much more real. In previous designs, the analyses have been based on highly conservative, pessimistic assumptions. Even though the energy release yielded was considered in providing the containment, the general feeling was that even if a meltdown did occur, it would be considerably less severe than was postulated. Therefore, the uncertainties in the highly complex analyses of energy release, energy dissipation and containment requirements could be tolerated. This may no longer be true in the case of very high-power output reactors. Furthermore, the maximum accident has become one that can be physically described and defined rather than one which could not be envisioned but was postulated in a very conservative fashion. In view of this as well as the autocatalytic tendencies, it was decided to try to eliminate large positive reactivity insertions by sodium boiling as a possible accident.

Another important difference occurs because of the different physical properties of ceramic as compared to metallic fuel elements. The ceramic elements may not have a prompt negative feedback due to thermal expansion of the fuel under certain conditions as does the metallic fuel. Thermal cycling may produce horizontal cracks within the ceramic material. In subsequent heating of the fuel it would then be possible for the fuel to move in a random fashion, which would permit the expansion effect to be non-reproducible and in some cases positive. The presence of the cracks also causes concern for the possibility of fuel slumping and rapid reactivity changes. Both these effects can be minimized by the use of a segmented pin design. In keeping with the chosen conservative philosophy in regard to safety, the decision was made to design for a minimum effect from fuel motion.

Thus, the major emphasis of the study was to determine methods of reducing or eliminating the positive sodium coefficient avoiding complete reliance on the Doppler coefficient for reactor safety, and to minimize fuel movement. In addition, techniques of design to achieve the lowest fuel-cycle cost and maximum plant availability were used. Some of the design targets were to:

- (1) Design for a high fuel burnup (50-100,000 Mwd/MT).
- (2) Use a fuel-management program and include sufficient excess reactivity that would provide for refueling no more than once a year.
- (3) Design the reactor in such a way that as few mechanisms as possible would be in the reactor during operation.
- (4) Design the fuel to contain all fission products within the active core region.

It should be reemphasized that the concept selection is based on theoretically determined characteristics without the benefit of experimental confirmation. Useful experimental information on Doppler coefficients, sodium coefficients and other critical items will be forthcoming from several planned programs. These include cold critical assemblies such as ZPR-VI and ZPPR, and power reactor experiments such as FARET. In addition, European programs in England, France, and Germany will contribute significantly to a better understanding of the physics of fast reactors.

3. REFERENCE REACTOR

3.1 CHOICE OF DESIGN

The major decision in the selection of an advanced fast reactor concept is whether or not a positive sodium-void coefficient should be accepted. Present theory indicates that this coefficient can be of sufficient magnitude to cause a reactivity increase of several dollars in accidents involving coolant boiling. An additional consideration is the autocatalytic nature of this accident. The negation of this reactivity effect has been the major factor influencing the selection of the annular core design with a sodium reflector.

In any practical design of a 2500 Mwt sodium-cooled fast reactor, it is not possible to have a negative sodium void coefficient at every position in the reactor. In this study it has been a design goal that loss of sodium from any channel should produce a negative or zero reactivity effect. Since boiling leads rapidly to voiding of a large fraction of the channel, positive void effects that occur only locally are acceptable. The parametric study in Appendix A is devoted to methods of satisfying this requirement and, at the same time, meeting the ground rules for breeding and thermal characteristics. Possible modifications to one or all three of the components of the sodium void coefficient (negative leakage, positive spectral, and absorption components) have been considered.

The spectral component is very complicated, involving the moderating properties of the coolant and the energy dependence of the cross sections of the core materials. Replacement of the fertile U-238 by the less fissionable Th-232 is not sufficient to negate the coefficient in the 1000 Mwe size range, as was found for smaller metallic-fueled cores. Another method of modifying the spectral component is the addition of some moderator which slightly flattens the neutron importance versus energy curve. However, unless fuel is displaced by the moderator, the improvement in the sodium coefficient is small. This approach is therefore not desirable as far as breeding and doubling time are concerned.

The most satisfactory solution to the sodium void problem is a high leakage core. This can be accomplished by utilizing a core with a high surface-to-volume ratio, such as a flat pancake, a cruciform, or an annulus. An arrangement of alternate annuli of core and blanket subassemblies is another possibility. The blanket zones would be made with sufficient thickness to decouple the various core zones. When this thickness is achieved, monitoring the flux level and controlling the individual zones introduce a very difficult instrumentation problem.

The capital costs of the reactor depend somewhat on vessel diameter. Of the large high leakage cores, the cruciform arrangement requires the largest vessel size. The annular and pancake cores require about the same vessel size, but the former is considered to have more advantages.

The design selected uses a combination of a thin annulus, which makes the effect of sodium voiding in the core section small, although slightly positive, and a sodium reflector above the core, which makes the effect of channel voiding zero or negative, even for inherently troublesome central channels. The core is 2 ft thick, 4 ft high, and has an 18-in.-thick sodium reflector above the core. Loss of sodium from the entire core causes a calculated reactivity increase of only about 30 cents; when voiding of the reflector is considered, the effect is a reactivity decrease of 2.8 dollars. Loss from the core section alone, of the channels occupying the central third of the core, causes a reactivity increase of about 1.5 dollars. Reflector voiding reduces this amount to a negligible reactivity effect. In addition to the obvious advantage of the safe sodium void coefficient, the following are other advantages of the annular design: (See Appendixes A and B for a detailed comparison of the annular core and a more conventional cylindrical core design.)

(1) The breeding ratio is 1.32, and the doubling time is, therefore, about 15 years based on a 100 percent plant factor. This is possible because of the high leakage to the radial blanket, where breeding can be done efficiently.

(2) The initial loading uncertainty is smaller for the annular core because of the relatively high edge fuel worth. Thus, a reasonable uncertainty of 5 percent in the estimated reactivity would have a corresponding uncertainty of 36 percent in the critical loading of the cylindrical core and of 17 percent in the annular core.

(3) The annular design gives an excellent peak-to-average power distribution (1.52). This distribution enables a high average core power output for the selected thermal criteria and helps to overcome the higher critical mass resulting from the increased leakage of the annular design. To achieve the same effect in right cylindrical geometry, it would be necessary to resort to a zone core loading.

(4) The annular design can be mocked up, and physics data can be obtained from present size facilities with only a small fraction of the plutonium inventory. For an outlay of about one-eighth of the critical mass (400 kgs), a zero power assembly could be constructed from which all the power independent physics data could be measured. The assembly would consist of a segment (not necessarily curved) of the annular core sufficiently long to have equilibrium conditions over a significant volume. It would be impossible to get the same degree of confidence in the measured data for a pancake core without the outlay of almost the entire critical mass (nearly 3000 kgs) and a larger facility.

(5) An even greater advantage of the annular geometry is its ability to be prototyped in a small volume and still be able to reproduce over the two principal axes conditions identical to the full size plant. In this case, the driver zones would be overcooled. It is possible that the physics prototyping of this core could be carried out in a facility of the same size as Fermi.

3.2 DESCRIPTION

The core selected as the reference design for the Large Fast Reactor Design Study is an annular configuration with a core volume of 7830 liters. The general arrangement is shown in Fig. 3.1. The 2-ft-thick, 4-ft-high annulus is 9 ft in inside diameter and 13 ft in outside diameter. The core is bounded on the inside and outside by a 1-1/2-ft-thick, 6-ft-high radial blanket. A 6-in.-thick axial reflector is located below the core, and a 1-1/2-ft-thick sodium reflector is located on the upper core boundary. Therefore, the total reactor configuration is 16 ft in outside diameter, 6 ft in inside diameter, and 6 ft high. The total volume of the core and blanket is 29,320 liters. The center is occupied by a support structure and instrumentation thimbles.

The mechanical design of the core has been performed with the concept of basic simplicity, for reduction in cost of major structural components, ease of remote fuel refabrication, and ease of fuel handling. Mechanisms, other than control assembly extension shafts, that could freeze or jam have been completely eliminated from the reactor during operation. No upper hold-down structure is needed because of the hydraulic design of the fuel assembly, and a parallel upflow is used in the core and blanket to free the top of the reactor of a structure which could interfere with refueling. The detailed core layout and elevation are shown in Fig. 3.2 and 3.3. There are 498 fuel assemblies containing 20.2 MT of uranium and plutonium oxide and 858 blanket assemblies containing 98.6 MT of uranium oxide in the reactor. All power control, reactivity loss compensations, and shutdown are accomplished with 48 control assemblies, located symmetrically around the core.

Nominal interassembly spacing is 0.050 in., but spacer pads are located on all core and blanket assemblies and on control assembly guide tubes so that essentially all clearance at the core top and axial midplane is removed in the hot operating condition, and the core is tight in the lateral direction. Structural members of the vessel restrain lateral movement of the core at both the outer blanket outside diameter and the inner blanket inside diameter. Further mechanical restraint to prevent gross axial fuel movement is provided by the segmenting discs between fuel pellets inside the fuel pins, as shown in Fig. 3.4.

Assembly coolant flow is orificed to maintain maximum outlet temperature from each of the fuel and blanket assemblies. Each core assembly is orificed individually by permanently placed, hard-surfaced inlet restrictions in the core-support grid plate. The blanket assemblies are orificed individually from a distribution header feeding coolant to groups of blanket assemblies; these headers are removable.

3.3 GENERAL DESIGN CHARACTERISTICS

Reactor Output, thermal, Mw

core*	2125
inner radial blanket*	185
outer radial blanket*	190
total	2500

* at midpoint of equilibrium fuel cycle.

Reactor Size, ft

Core

height	4
mean outside diameter	13
mean inside diameter	9

Inner Radial Blanket

height	6
mean outside diameter	9
mean inside diameter	6

Outer Radial Blanket

height	6
mean outside diameter	16
mean inside diameter	13

Axial Blanket

height	0.5
mean outside diameter	13
mean inside diameter	9

Composition, v/o

Core And Axial Blanket

fuel and void	29.4
sodium	40.0
structure and cladding	30.6

Inner And Outer Radial Blanket

fuel and void	55
sodium	30
structure and cladding	15

Nuclear Data

Critical Mass (Pu-239 + Pu-241), kg	2910
(U-238 + Pu-240)/(Pu-239 + Pu-241)	4.57
Breeding Ratio	
core	0.52
radial blanket	0.76
axial blanket	0.04
total	1.32

Median Fission Energy, kev	190
Mean Neutron Lifetime, sec	4.1×10^{-7}
Fissile Pu Destruction Rate (core), kg/full power year	1040
Fissile Pu Production Rate (total), kg/full power year	1370
Doubling time, years, 100% load factor	14.7
Fuel Life, full power days	836
Control Assemblies	
safety, number	12
total worth, % $\Delta k/k$	2.4
temperature override and regulating, number	6
total worth, % $\Delta k/k$	1.2
fuel depletion, number	30
total worth, % $\Delta k/k$	6
absorber material, 90% dense	B_4C

Core Performance

power density, kw/ ℓ	282
specific power, kw/kg Pu	733
average linear heat generation, kw/ft	8.6
power distribution	
$(P_{\max}/P_{\text{av}})_{\text{axial}}$	1.29
$(P_{\max}/P_{\text{av}})_{\text{radial}}$	1.18
$P_{\max}/P_{\text{av}}$	1.52

Core Fuel Dimensions

Fuel Assemblies, number	498
Pins per Assembly, number	123
Width of Hexagonal Assembly (flats), in.	4.45
Pin Outside Diameter, in.	0.300
Length of pin, ft	
core fuel portion	4.0
axial blanket portion	0.5
fission gas reservoir	0.5
total.	5.0
Pellet Material, mixture	$\text{PuO}_2\text{-UO}_2$
Pellet Density, % of theoretical	95
Cladding Material	316 SS
Cladding Thickness, in.	0.028
Sodium Flow (assembly at flux peak) lb/hr	2.158×10^5
Pressure Drop Through Pins (assembly at flux peak), psi	36.5

Radial Blanket Fuel Dimensions

Blanket Assemblies, No.	
inner radial blanket	276
outer radial blanket	582
total	858
Pins Per Assembly, No.	
Width of Hexagonal Assembly (flats), in.	4.45
Pin Outside Diameter, in.	0.607
Length of pins, ft.	6
Pellet Material	UO ₂ (depleted)
Pellet Density, % of theoretical	95
Cladding Thickness, in.	0.015
Cladding Material	316 SS
Sodium Flow (assembly at flux peak of inner blanket), lb/hr	6.9×10^4
Pressure Drop Through Pins (assembly at flux peak of inner blanket), psi	6.5

Primary System

Total Coolant Flow Rate (including 5% by-pass flow) . .	1.14×10^8 lb/hr (2.8×10^5 gpm)
Overall Coolant Temperature Rise, °F	250
Coolant Temperature Rise Through Core, °F.	263
Coolant Temperature Rise Through Radial Blanket, °F . .	263
Coolant Inlet Temperature, °F	950
Overall Core Pressure Drop, psi	app. 45
Coolant Loops, No.	6
Primary Heat Exchangers, No.	6
Primary Pumps, No.	6
Material of Primary System	316 SS
Reactor Vessel	
outside diameter, ft	20
overall height, ft	25
material	316 SS

SIMPLIFIED CORE SECTION

FIG. 3.1

FIG. 3.3

TYPICAL OXIDE FUEL CONSTRUCTION

COMPARTMENTED FUEL PIN (Reference)

4. ANALYSIS

4.1 PHYSICS AND SAFETY

4.1.1 Characteristics of the Annular Design

The physics data for the annular design are presented in the following tables and figures. These data are derived from 2-dimensional diffusion theory calculations on the model shown in Fig. 4.1. General reactor data and a neutron balance corresponding to the midpoint burnup in the equilibrium cycle are given in Tables 4-1 and 4-2.

The safety and control requirements are shown in Table 4-3. It will be noted that six assemblies are used for the temperature override of 0.2 percent $\Delta k/k$ instead of one or two assemblies of the same type as the safety assemblies. This is necessary because the special geometry of the core requires group movement of about six assemblies to maintain spatial power uniformity. These assemblies also provide the power regulating function.

Fuel cycle changes are given in Table 4-4. Depletion control by boron assemblies causes a reasonably small penalty in internal breeding. The delayed neutron and neutron lifetime data are listed in Table 4-5. The low value of beta is a result of the use of plutonium as the fissile fuel and small uranium-plutonium ratio.

The isothermal temperature coefficients and the power coefficients are given in Tables 4-6 and 4-7. The power coefficient is dominated by the negative Doppler effect. In units of $\Delta k/k/Mw$, the power coefficient of -0.87×10^{-6} is much smaller than Fermi, 20×10^{-6} , and other small reactors. In units of $\Delta k/k$ per one percent increase in power, the values are 19.6×10^{-6} and 40×10^{-6} , respectively. The dependency of the Doppler coefficient on temperature is shown in Table 4-8. At full power the coefficient is less than half of that at source power.

The results of the reactivity calculations on the annular core when various regions are voided of sodium are shown in Fig. 4.2. The calculations were made using the YOM-BNL-325 treatment of the sodium 2.8 kev capture resonance. Corrections were made to the results when Schmidt's new data became available (see Appendix D). Loss of the sodium from the core portion of two central fuel assembly rings (about one-third of the core) causes a reactivity increase of about 1.5 dollars, but loss from the core and reflector in these channels results in a slightly negative effect. These losses are discussed more fully in other sections of the report.

TABLE 4-1

GENERAL REACTOR PARAMETERS

Critical Mass (kg of Pu-239 + Pu-241) 2910

$\frac{\text{kg U-238} + \text{Pu-240}}{\text{kg Pu-239} + \text{Pu-241}}$ 4.57

	<u>Fission Distribution</u>	<u>Breeding Ratio</u>
Core	0.86	0.52
Radial Blanket	0.13	0.76
Axial Blanket	0.01	0.04
Total	1.00	1.32
Fraction of Fissions in U-238 + Pu-240	0.15	
Median Fission Energy, kev	190	
α Pu-239 + Pu-241 in core	0.215	
Mean Neutron Lifetime, sec	4.1×10^{-7}	
Average Delayed Fraction	0.0036	
Effective Delayed Fraction	0.0032	
Fissile Pu Destruction Rate, kg/yr	840	
Fissile Pu Production Rate, kg/yr	1090	
Doubling Time, operating years	18.4	
Subassembly Life, full power days	836	

TABLE 4-2
NEUTRON BALANCE

	<u>CAPTURES</u>	<u>FISSIONS</u>
Core		
S.S.	0.0324	
O	0.0009	
Na	0.0082	
Boron	0.0220	
F.P.	0.0367	
U-238	0.1252	0.0205
Pu-240	0.0301	0.0200
Pu-239	0.0466	0.2184
Pu-241	0.0041	0.0297
Total	<u>0.3062</u>	<u>0.2886</u>
Radial Blankets		
U-238.	0.2507	
Total	<u>0.2817</u>	<u>0.0481</u>
Axial Blanket		
U-238.	0.0130	
Total	<u>0.0161</u>	<u>0.0026</u>
Upper Reflector.	<u>0.0032</u>	
Absorber.	<u>0.0284</u>	
Inner Reflector	<u>0.0044</u>	
Outer Reflector	<u>0.0041</u>	
Leakage	<u>0.0166</u>	
Total	<u>0.6607</u>	<u>0.3393</u>

TABLE 4-3

REQUIREMENTS FOR SAFETY, SHIM,
AND REGULATING ASSEMBLIES

The safety, shim, and regulating assemblies are composed of 85 pins of natural B_4C at 90 percent of theoretical density with a depleted UO_2 follower section.

Worth of one assembly = 0.2 percent $\Delta k/k$ = 62.5 cents.

<u>Type of Assembly</u>	<u>Number</u>	<u>Worth</u>
Safety	12	2.4% $\Delta k/k$ = \$ 7.5
Temperature Override ⁽¹⁾	6	1.2% $\Delta k/k$ = \$ 3.75
Fuel Depletion ⁽²⁾	30	6.0% $\Delta k/k$ = \$18.75

(1) Required temperature override 0.2 percent $\Delta k/k$. These assemblies are also used for power regulation.

(2) Reactivity change due to average core burnup of 33,333 Mwd/MTM is 6.0 percent $\Delta k/k$. Time for 10 percent burnup of B_4C assemblies is 180 days.

TABLE 4-4
EQUILIBRIUM FUEL CYCLE

A. Core Loading, kg

<u>Isotope</u>	<u>Cycle Start (33,333 Mwd/MTM Average)</u>	<u>Cycle Midpoint (50,000 Mwd/MTM Average)</u>	<u>Cycle End (66,667 Mwd/MTM Average)</u>
U-238	12,100	11,920	11,740
Pu-239	3,020	2,830	2,640
Pu-240	1,610	1,600	1,600
Pu-241	280	280	280
F.P.	740	1,120	1,490

B. Breeding Ratio

<u>Core Average Exposure</u>	<u>IBR</u>	<u>BR</u>	<u>Doubling Time (0.8 P.F.)</u>
33,333 Mwd/MTM	.500	--	--
50,000 Mwd/MTM	.524	1.32	18.4 yr
66,667 Mwd/MTM	.548	--	--

TABLE 4-5
NEUTRON KINETICS DATA

A. Delayed Neutron Fraction

Averaged Delayed Neutron Fraction in Core, $\beta_{\text{core}} = 0.0036$

Effective Delayed Neutron Fraction in Core, $\beta_{\text{eff core}} = 0.0032$

B. Neutron Lifetime

<u>Region</u>	<u>Lifetime, (μ sec)</u>
Core	0.37
Overall weighted lifetime	0.41

TABLE 4-6

ISOTHERMAL TEMPERATURE COEFFICIENT

Doppler (at 1000 F)	- 3.2 × 10 ⁻⁶	Δk/k/°C
Pin Radial Expansion	+0.4	
Sodium Expansion		
core	+ 3.0	
reflector	- 1.6	
radial blanket	- 0.4	
Axial Expansion		
core	- 4.1	
radial blanket	+ 0.3	
Radial Expansion		
core	- 6.8	
radial blanket	+ 0.3	
Total	- 12.1 × 10 ⁻⁶	Δk/k/°C

TABLE 4-7
POWER COEFFICIENTS
 (at full power)

Doppler	- 0.628 x 10 ⁻⁶ Δ k/k/Mw
Pin Radial Expansion	+ 0.117
Na Expansion	
core	+ 0.085
reflector.	- 0.081
Core Axial Expansion	- 0.181
Core Radial Expansion	- 0.182
	<hr/>
TOTAL	- 0.87 x 10 ⁻⁶ Δ k/k/Mw

TABLE 4-8

DOPPLER COEFFICIENT

Isothermal Doppler Coefficient $\partial K/\partial T = -0.00266/T + 0.00208/T^{3/2}$ (T in °K)

	<u>Average Fuel Temperature</u>	<u>Doppler Temperature Coefficient</u>
Zero Power	510 C	$-4.0 \times 10^{-6} \Delta k/k/C$
Full Power	1250 C	$-1.9 \times 10^{-6} \Delta k/k/C$

	<u>Doppler Reactivity, $\Delta k/k$</u>
Zero Power	0
Full Power	- 0.0017
All Fuel at Melting Point	- 0.0034

4.1.2 Discussion of Results

4.1.2.1 Statics. Of the methods investigated in this report for negating the positive sodium void coefficient, all but the annular design have fallen short of the ground rule requirements for breeding and thermal output. It should be stressed, however, that an exhaustive study of all the methods has not been made, and it is possible that a combination of three or four of the methods could achieve the desired nuclear and thermal characteristics. The mechanisms causing the coolant void coefficient are very interdependent with those affecting core performance, and the time scale of the study did not permit optimization studies of such complex systems.

Complete loss of sodium from any subassembly channel must lead to a negative reactivity. Calculations on the annular core with the sodium reflector show that this goal has been met.

Further, only a hypothetical accident would cause total sodium loss in any subassembly channel. This accident is discussed later.

The early calculations made on the sodium void coefficients show that up to one-third of the positive component was contributed by the neutron capture effect in sodium. Now that the YOM-BNL-325 data for the 2.8 kev sodium capture resonance has been superseded by the Schmidt-Hibden data (see Appendix D), the contribution is only about a tenth. Since the total effect is the difference between the positive and the negative leakage component, the new cross-section data give a large improvement.

The calculations on the effectiveness of the reflector in negating the void coefficient are felt to be conservative, and calculations show (see Appendix A) that it could be twice as effective, as indicated in Table 2-7.

The annular design has a total breeding ratio of 1.32, sufficiently higher than the required 1.2, to allow scope for further optimization and for calculation error. The internal breeding ratio is low compared with the conventional cylindrical design, but a reasonable number of natural boron carbide rods provides adequate safety and shim control. The unique geometry of the system requires that several rods be moved together for coordinated control and that the strict management vigilance, during fuel changing associated with large thermal reactors, is necessary.

Although it is felt that the present design has achieved the main aims as far as sodium void coefficient and breeding ratio are concerned, further optimization is required before the design is finalized.

4.1.2.2 Dynamics and Safety. The reactivity coefficients as shown in Table 4-6 for the isothermal case, and in Tables 4-7 and 4-8 for power conditions, are composed of the prompt Doppler coefficient and the sum of the delayed expansion coefficients.

The Doppler coefficient is negative, but is only about half as large as it would be in the more conventional cylindrical oxide core; there are three reasons for this. First, a high leakage core has an inherently lower uranium-plutonium atom ratio. Second, the annular core being studied has a high volume fraction of steel which displaces some of the uranium oxide, thus reducing the moderating effect. Consequently, the low energy neutron flux, which is important for the Doppler coefficient, is reduced. Third, the high leakage reduces the low energy spectrum, since the probability of escape before making the number of collisions required for moderation down to 1 kev is greater.

The expansion coefficient consists of several components with the same time behavior. The expansion of the sodium in the core gives a positive effect which is reduced, but not negated, by the sodium reflector for isothermal changes. In the case of the power coefficient, the two almost cancel. The same holds true for the small density changes associated with the temperature changes, whereas for a total loss of sodium the negative effect of the reflector is dominant. By using a tight core design, bowing has been eliminated as a significant reactivity effect and can be neglected. The only remaining positive components are rather small; they are the pin radial expansion, displacing sodium, and the axial and radial expansion of the radial blanket. The latter effects are positive because the blanket fuel, which is a strong absorber, decreases in density and absorbs fewer neutrons with increasing temperature and, thus, has a positive reactivity effect.

These positive effects are more than outweighed by the negative components associated with the axial and radial expansion effect of the core, which come from the clad and structure expansion, respectively. These are the strongest negative components, although the radial expansion effect is not as large as in many other reactors. This is a result of the properties of the annular design, which include a rather flat radial flux distribution and material worth distribution across the core.

Although the net expansion coefficient gives a large contribution to the isothermal temperature coefficient, the Doppler coefficient is dominating in the power range because the fuel undergoes much larger temperature changes than clad and coolant. The power coefficients of sodium expansion in the core and reflector almost cancel because, for a given power change, the reflector temperature changes more than the average core sodium temperature. This is unlike the isothermal case, where the positive effect of core expansion predominates.

Because of the small temperature coefficients, the power regulating rods need only a small excess reactivity. These rods can be operated at a relatively low reactivity insertion rate of about 1 cent/sec. The shim rods must have a considerable amount of reactivity but can be moved at a much lower speed. The control problem and the safety problem associated with an accidental runout of all the regulating rods are, in the first order, independent of the temperature coefficients.

The safety studies, as discussed in detail in Appendix E, show that no damage is done to the core by credible accidents, such as control-rod runout or loss of the primary coolant flow. Both of these accidents could be expected to cause a scram because of high flux or short period. Damage to the core is defined as melting of a major portion of the fuel within the elements, which results in overheating and weakening of the clad or boiling of the coolant. The latter is not as likely if the primary system is slightly pressurized, which raises the boiling temperature by about 400 F.

In addition to the mentioned credible accidents, the hypothetical accidents in which scram does not occur are interesting because they describe the inherent response of the reactor, without the external action of the safety system. It can be shown that even these accidents do not damage the core. In the case of the flow failure without scram, the maximum coolant temperature is still 200 F below the boiling point. The control-rod runout at full power inserts 65 cents reactivity, whereas the limit of gross fuel melting and clad failure is 83 cents. Thus, for this accident, the safety margin is also adequate. In addition, it can be shown that the hypothetical instantaneous ejection of a control rod does not lead to conditions that are any more severe than a runout at normal speed, as long as the reactivity insertion does not exceed one dollar.

It seems reasonable for the present state of technology and experience to design cores for hypothetical accidents which are even more unlikely than the combination of two independent malfunctions at a time. Of this type, those in which sodium boiling occurs are of particular interest. The two possible causes of coolant boiling are flow decay, which inherently leads to boiling at the top of the core, and power excursion, which can cause boiling at either the top or center, depending upon the length of the period relative to the coolant transport time. If boiling is initiated at the top, the voiding in the reflector region occurs more extensively than in the core, and the resulting net reactivity effect is negative. For this situation, the reflector is fully effective in preventing power excursions as a consequence of coolant boiling. This would not be the case if boiling were to start at the axial center of the core, which is, however, very difficult to achieve. There is only one hypothetical accident situation conceivable as a consequence of equipment failure in which this condition might occur. If the reactor is at source power and the coolant flow is at a very low level, a control-rod withdrawal accident might cause boiling at the center. In this case, the core portion of the center channel is voided before the reflector and causes a positive reactivity insertion. This accident has not been studied in detail, and the magnitude of the insertion has not been determined. The excess reactivity must be compensated by the Doppler reactivity, and it is required for the finally optimized core design to ensure that there is enough Doppler reactivity associated with a tolerable temperature change to prevent a serious power excursion in such an accident.

Core meltdown is generally considered to result in the maximum hypothetical accident in the smaller, metallic-fueled fast reactors. The meltdown is postulated to occur without reference to specific causes, such as control or equipment failure, and assumed to be instantaneous and followed by collapse of the core at a rate determined by the acceleration of gravity. Such a meltdown could also be postulated for the large, advanced oxide-fueled reactors, although the coordinated meltdown of the entire core, as pictured, is probably even less likely. The rates of reactivity increase produced from such assumptions are of the order of 100 dollars per second, which is also the order of magnitude of the insertion rate for sodium voiding at the central point discussed previously. In both cases, there would be a prompt critical power excursion, terminated by a combination of the Doppler effect and the core disassembly.

The energy release from such maximum hypothetical accidents has not been investigated in detail for the particular reactor described in this report. However, some qualitative statements can be made about the influence of some of the general features of the design on the energy release. The core differs in three important respects from the fast reactors that have been built previously and analyzed in detail. These differences are: the over-all size, the annular shape, and the larger negative Doppler effect.

The annular shape is expected to have only a small influence on the energy release in hypothetical meltdown accidents. The central blanket is large enough so that the core flux and reactivity worth are nearly symmetrical about a surface, midway between the inner- and outer-cylindrical surfaces bounding the core. Thus, motion of the material both inward and outward during an excursion would tend to reduce reactivity. Since the inertia between the symmetry surface and the core surfaces is smaller than between the central axis and the core surface in a right circular cylinder the energy release may be slightly less for the annular core than for a right cylinder of the same volume. Thus, it is easier for the material to expand. However, this quality is offset to some extent by the flatter flux and reactivity worth distributions. The net result is that the annular nature has small influence on total energy release.

Energy release tends to be roughly proportional to size, other factors remaining the same, and, therefore, is much larger in cores of this size. However, studies of the influence of the Doppler effect on energy release show that even relatively weak negative Doppler coefficients can markedly reduce the energy density in the excursion. The explosive efficiency is less at the lower energy density. The negative Doppler effect in the annular design opposes the effect of size increase.

4.1.2.3 Uncertainties in Calculations. The state-of-the-art of calculating the nuclear characteristics of a several thousand liter plutonium-oxide core is such that there is great need for experimental verification. The comparison of calculation and measurement (see Appendix F) on ZPR-III Assembly No. 43 are indicative of the errors that may be present in cross-section data and in calculation techniques for predicting sodium void coefficients. It appears that the calculated values of the central coefficients

are considerably more positive than were measured. This theory is based on a single experiment in a simulated carbide core using a zone technique. If this discrepancy is real, then loss of sodium from the most reactive part of the annular core would cause only a small reactivity increase (20 to 30 cents), and loss from the whole core would decrease the reactivity. On the other hand, a similar error in the calculations of the conventional cylindrical design would still cause a substantial positive reactivity effect (1.5 dollars) on loss of sodium from the whole core.

The errors in the critical mass and the breeding ratio are expected to be much smaller. A large fraction of the breeding takes place in the radial blankets, where it is fairly insensitive to errors in the U-238 capture cross section. Errors of 10 to 20 percent in breeding ratio and up to 15 percent in critical mass are conceivable.

The control and safety rod worths may be associated with a 13 percent discrepancy, based on calculations of B_4C worth measurements in ZPR-III No. 43. A zero power mockup of the final design will determine the plutonium and boron-10 enrichment of the fuel and of the absorber rods, respectively.

The Doppler effect probably cannot be calculated more accurately than a factor of two. This inaccuracy arises from the uncertainties in the basic nuclear data, the sensitivity of the Doppler coefficient to the spectrum, and the influence of effects which are not well investigated (the overlap effect of resonances of different isotopes). However, one recently reported experiment on ZPR-III, Assembly No. 43 shows good agreement between the calculated and measured Doppler coefficient of U-238 (ANL 6764).

4.1.3 Calculational Techniques

4.1.3.1 Statics

4.1.3.1.1 Cross Sections. A nuclear data treatment which allows good analytical representations of sensitive reactor characteristics is a basic requirement for any study such as this.

A significant fraction of the neutron flux of a large oxide-fueled fast reactor core is below 10 kev. At 2.8 kev there is, in sodium, a large scattering resonance coinciding with a smaller, but not negligible, absorption resonance. Therefore, any attempts at an accurate calculation of the sodium void and Doppler temperature coefficients would require a multigroup cross-section set extending down to below 100 ev, with an adequate number of groups around 2.8 kev to describe the effect of the sodium resonance.

Two multigroup cross-section sets were used in this study. In the early calculations the YOM 16-group set was used. This set was not very satisfactory for temperature coefficient calculations since its lowest energy boundary was only 0.5 kev, and a smooth flux distribution was used to compute the group average cross sections in the

regions containing scattering resonances. The later calculations were performed with a 24-group set, which has a group structure identical with the YOM set down through Group 14; Groups 15, 16, and 17 cover the sodium resonance, and Group 24 is a thermal group. Apart from the studies made with some moderator in the core, the bottom three groups are superfluous to the general study. The group structures for the 10 and 24 group sets are given in Table 4-9.

It has been well established that a single set of multigroup cross sections is inadequate for predicting sensitive reactor characteristics, because effects dependent upon specific isotopic mixtures and concentrations arise. The presence of elements having scattering resonances (sodium, iron, nickel) can profoundly influence the effective group transport and elastic removal cross sections for the core mixture. The occurrence of a large scattering resonance (sodium, 2.8 kev) depresses the flux at this energy, and the effective group transport cross section for sodium can be a factor of 2 lower than if it has been calculated on the basis of an undepressed flux. The reactivity effect of a change in sodium density can thus be quite nonlinear.

Treatment of the effect of resonance scatters was effected using the ELMOE code, which performs a fundamental mode calculation in several hundred groups. The groups in the 24-group cross-section set, which cover the scattering resonance region, were each subdivided into many fine groups. Elastic scattering and transport cross sections for the light elements were computed (by the program) from an input library of Legendre coefficients. ANL has added a special set of Legendre coefficients for sodium, which has many resonances from 60 to 300 kev and whose widths are much smaller than the fine-group energy widths (0.0087 in lethargy units). These resonances have been measured and identified by Hibden, and the narrow resonance approximation was used with Hibden's data to compile the special library.

After computing the fine-group flux distribution, the code recomputes the macroscopic transport and elastic removal cross section of the mixture for each of the 24 groups that have been subdivided. It also recomputes explicitly the group elastic removal cross sections for each of the light elements.

ELMOE calculations were made on the chosen composition for the reference design. The fuel metal isotopic ratios, which affect resonance scattering very weakly, were estimated by preliminary diffusion theory calculations using the crude 24-group set. In the high energy pass case, the fine groups (630 in all) were distributed over the Coarse Groups 2 to 12 inclusively and in the low energy pass, Groups 8 to 18. The 5-group overlap was sufficient to give excellent matching. The calculations were repeated for 10, 25, 50, 75 and 100 percent sodium loss from the core. For the case of no loss of sodium, the group transport cross sections for the core mixture computed from the unmodified 24-group set, were compared with the values computed by ELMOE. To facilitate convenient manipulation of the data for diffusion calculations, differences between the two were arbitrarily attributed to iron so that the macroscopic cross sections for the mixture were the same as the ELMOE value. The iron loses its individuality in the macroscopic mixing. The correct values for the

elastic removal cross sections of the light elements were easily entered as changes in the 24-group cross-section library.

The differences in the group transport cross sections for the mixture between Sodium Density A and Sodium Density B were attributed to sodium. The group transport cross section for sodium was defined as:

$$(\sigma_{tr})_{Na} = \frac{(\Sigma_{tr})_{mix A} - (\Sigma_{tr})_{mix B}}{(N_{Na})_A - (N_{Na})_B}$$

where N_{Na} is the atomic density of sodium. A value of the $\sigma_{tr Fe}$ at any sodium density can easily be calculated given $\sigma_{tr Na}$ at a given sodium density, $\sigma_{tr Fe}$ is defined as follows:

$$\sigma_{tr Fe} = \frac{\Sigma_{tr} - (\sigma_{tr Na} N_{Na} + \Sigma_{tr'})}{N_{Fe}}$$

where $\Sigma_{tr'} = (\sigma_{tr N})_O + (\sigma_{tr N})_{Ni} + (\sigma_{tr N})_{Cr} + (\sigma_{tr N})_U + (\sigma_{tr N})_{Pu}$. The σ_{tr} of these elements are taken directly from the APDA 24-group library. Thus, to exactly match in a diffusion problem the Σ_{tr} calculated by ELMOE, it was necessary to change only the sodium and Fe transport cross sections.

The fine-group flux distributions within the Coarse Groups 15, 16 and 17 were used to compute group average cross sections for sodium absorption in the resonance region at 2.8 kev. The Breit Wigner resonance shape was calculated from the value of $g\Gamma_\gamma = 1.0$ ev assumed by YOM (where g is the statistical weight factor and Γ_γ is the radiation width) and the BNL 325 value of 410 ev for the total width.

A new estimation of the 2.8 kev sodium absorption resonance has recently been published by Schmidt, whose parameters are based on sodium absorption measurements in a thermal flux with a known high energy tail and Hibden's measurements of the total cross section at 2.8 kev. The resultant Breit Wigner absorption shape is a factor of 2 lower and narrower than the YOM-BNL 325 shape. The net result is that, in the oxide core being studied, the neutron absorption by the sodium is reduced by a factor of 4 when Schmidt's data is used. This change has a small effect on the critical mass and breeding but can alter the sodium coefficient significantly.

The nominal sodium temperature coefficient was calculated from multigroup perturbation theory. The ELMOE calculated effective transport cross section for sodium between 0 and 10 percent sodium loss was used for the sodium data. For estimating the effect of the total loss of sodium from all or part of the core and axial blankets, the difference in k_{eff} between two diffusion theory calculations was used.

Another effect, which has to be considered in calculating both the sodium coefficient and the Doppler coefficient, is that of the effective resonance integrals for the heavy absorbers (fission and capture). The resonance integral depends on the potential scattering cross section of the core mixture. Loss of sodium from the core can, in this way, reduce the Doppler coefficient by a factor of 2.

The results from Fischer's report were used in calculating the effective resonance integrals for the core mixture. A series of graphs are given in that report parametrically relating the effective group fission and capture cross sections, the total scattering of the core mixture, and the fuel temperature. Curves are given for Pu-239, Pu-240, and U-238. Because of its low concentration, infinitely dilute cross sections were assumed for Pu-241. The resonance self-shielding effects cover Groups 7 to 21 in the 24-group cross-section set, but the major differences occur in Groups 16 to 21. The macroscopic group transport cross section for the core mixture (calculated by ELMOE) was divided by the core atomic density of the particular absorber, and the effective group absorption (fission or capture) cross section, corresponding to the steady-state fuel temperature, was read off the curve. Even in the group with the severest flux depression (Group 16), the self-shielded cross sections, as calculated with the average transport cross section in the group, was close enough to the more exact method of averaging the self-shielded cross sections over the group.

The nuclear data which were used in the formulations described in the foregoing are summarized in Appendix D.

4.1.3.1.2 Calculational Models. The cross sections developed were used in the CRAM code, a program for the IBM 7094 which solves the multigroup diffusion equations in one dimension (slab, cylindrical or spherical geometry) or in two dimensions (RZ, XY, and R θ).

Because a large number of survey calculations were made in this study, it was necessary to rely upon 1-dimensional core representations. Two-dimensional cases were run only for the final design configuration and then, to restrict costs, in a reduced number of groups (CRAM is capable of 24 groups in 2-dimensional geometry, but this application is prohibitively expensive). Two-dimensional geometry can be simulated in a 1-dimensional model, however, by introducing a pseudo-absorption cross section to simulate the neutron leakage not represented in the one-dimensional model out of the reactor surfaces. Thus, a radial study of a 2-dimensional cylinder would be made by calculating an infinitely long cylinder with an absorption cross section, DB^2 , added to the macroscopic absorption cross section of the various mixtures in the core and radial blanket zones. The D would be extracted from the input data, and the B^2 would be entered as an estimation of the axial buckling, i.e., $\pi^2/(H + 2S)^2$, where H is the core height and S the axial reflector savings. A more exact treatment would require that a separate B^2 for each group and each zone be entered, but the diffusion theory code that was used does not have this flexibility.

The final reference design calculations in a 2-dimensional geometry were made with a reduced number of groups. Previous experience at APDA has shown that six groups can provide an adequate description of the reactor, when the cross sections for the six groups are computed from the flux spectra obtained from pseudo-2-dimensional calculations in 24 groups.

A comparison between the 24 Group and 6 Group models was made with a 1-dimensional representation of the reference annular core. Table 4-10 gives the results of the comparison. The differences in $\Delta k/k$ for 100 percent sodium loss in the core by perturbation and Δk methods are 20 percent and 6 percent, respectively.

As a further check, the 6-group spectrum and the 24-group spectrum reduced to 6 groups, as shown on Fig. 4.3. The agreement is very good. Space and energy distributions of the spectral plus the removal components of the sodium worth are shown in Fig. 4.4 and 4.5. The space distributions are taken directly from the results of CRAM calculations. The energy distributions were calculated by hand. The 24-group worth were reduced to 6 groups and normalized to the 6-group calculation.

Calculations of reactivity defects arising from sodium density changes give different results when determined, on one hand, by taking differences in K from separate CRAM cases, and on the other, by using perturbation theory. The reason for this difference is that the perturbation value applies to the principal axis only, whereas the Δk value is, approximately, a core average value.

The reactivity effect of loss of core sodium is highly dependent upon core-to-blanket and core-to-reflector leakage. The pseudo-2-dimensional technique adequately treats the leakage to the radial blanket. However, the axial leakage term is simulated by a $DB \frac{2}{Z}$ term, as described previously. Total sodium loss from the core and/or upper reflector would change the axial reflector savings, δ , and hence $B \frac{2}{Z}$. Since the sodium worth, which is the difference in k_{eff} between two calculations, is very small, any error in $B \frac{2}{Z}$ would cause an appreciable error in the calculated sodium worth. Total loss of sodium from the core in the pseudo-2-dimensional calculation gave a reactivity change of -0.0054, whereas the 2-dimensional calculation gave +0.0019. For comparison purposes, however, the pseudo-2-dimensional model was thought to be adequate.

TABLE 4-9
GROUP STRUCTURE

YOM	upper limit			APDA-24	<u>X</u>
	<u>u</u>	<u>E</u>			
1	0	10	mev ↓	1	0.132
2	1.0	3.668		2	0.213
3	1.5	2.225		3	0.232
4	2.0	1.35		4	0.179
5	2.5	0.825		5	0.116
6	3.0	0.500		6	0.067
7	3.5	0.300		7	0.034
8	4.0	0.180		8	0.017
9	4.5	0.110		9	0.010
10	5.0	67.0	keV ↓	10	
11	5.5	40.7		11	
12	6.0	25.0		12	
13	6.5	15.0		13	
14	7.0	9.1		14	
15	{ 7.5	5.5		15	
		3.36		16	
16	{ 8.5	2.04		17	
	{ 9.2	1.01		18	
	9.9	0.502		19	
	10.6	0.249		20	
	12.1	55.6	eV ↓	21	
	13.6	12.4		22	
	15.1	2.77		23	
	16.6	0.625		24	
			thermal		

TABLE 4-10
COMPARISON OF 24- AND 6-GROUP MODEL IN ONE DIMENSION

	<u>24-group</u>	<u>6-group</u>
k_{eff}	1.0039	1.0040
$\Delta k/k$ 100% Na loss in core		
Δk method	-0.0051	-0.0054
perturbation method		
spectral + absorption	0.0318	0.0274
leakage	-0.0104	-0.0103
total	-0.0214	-0.0171

4.1.3.2 Dynamics.

4.1.3.2.1 Coefficients. The calculation of the isothermal temperature coefficient and the power coefficient, which define the core reactivity feedback, is summarized in this section. Each coefficient is the sum of thermal expansion effects and the Doppler effect.

The Doppler coefficient, which is the only truly nuclear coefficient, is caused by the temperature dependence of the reaction cross sections of the fuel isotopes due to Doppler broadening of the resonances. It can be easily seen that Doppler broadening of the sodium resonances is so small compared to the natural resonance width that it can be completely neglected. The three fuel isotopes U-238, Pu-239, and Pu-240 were considered in the calculations. The isotope Pu-241 is present only in very small quantities, and its basic nuclear data are very little known. It is therefore treated as Pu-239.

The Doppler coefficient and the Doppler reactivity were obtained by a perturbation calculation based on the multigroup data from CRAM runs. The data of the two-dimensional calculation with six groups were used, and the neutron worth of each group was split up into the 24-group data in the ratio obtained from one-dimensional runs. The CRAM runs used the cross sections at 2000 K, so the Doppler coefficient at lower temperatures may be slightly in error because the perturbation calculation does not take into account any changes in the spectrum.

The temperature derivatives of the reaction cross sections were taken directly from the graphs in APDA-157. A detailed description of the methods of calculation is given in APDA-139 and APDA-157 and will not be repeated here. The nuclear data on which the calculation is based are summarized in Table 4-11.

The resulting negative Doppler coefficient comes mainly from U-238. The negative contribution of Pu-240 is small, and Pu-239 shows a positive Doppler coefficient which is about one-third of the effect in the fertile isotopes (Table 4-11). This is about the expected ratio, considering that the atomic ratio of U-238 to Pu-239 is about four, and that Pu-239 has a larger effect than the fertile isotopes due to the relatively larger worth of fission neutrons. The resulting isothermal Doppler coefficient is about inversely proportional to the temperature and can be represented by the expression:

$$\left(\frac{\partial k}{\partial T}\right)_{\text{Dop}} = -\frac{0.00266}{T} + \frac{0.00155}{T^{3/2}},$$

where T is in $^{\circ}\text{K}$. This can be written as a temperature coefficient per $^{\circ}\text{F}$ as follows:

$$\left(\frac{\partial k}{\partial T}\right)_{\text{Dop}} = -\frac{0.00266}{460 + T} + \frac{0.00208}{(460 + T)^{3/2}}$$

where T is in $^{\circ}\text{F}$. The breakdown in the contributions of the different isotopes at 1000 F is shown in Table 4-11.

The result is discussed in Sec .4.1.2. It should be mentioned here that the effect of overlap between some of the resonances of U-238 and Pu-239, which was identified and discussed by Codd and Collins is neglected in these calculations. This effect reduces the positive contribution of Pu-239 but when the proper averages over the resonance parameters are performed, the difference is not very large. It might enhance the negative Doppler coefficient in the case discussed here by 10-15 percent, perhaps less than that, because the neutron spectrum of the reactor discussed here is fairly hard for an oxide system, and the effect vanishes rapidly at high neutron energies. Furthermore, neglecting this effect is conservative and it was felt that the effort and computer time required for a reliable calculation of the overlap effect was not warranted.

TABLE 4-11

NUCLEAR DATA FOR DOPPLER CALCULATIONS IN U-238, PU-239, AND PU-240

	<u>U-238</u>	<u>Pu-239</u>	<u>Pu-240</u>
s-wave strength function, (ev) ^{1/2} . . .	1×10^{-4}	0.97×10^{-4}	1×10^{-4}
average spacing at low energy, ev . . .	18.5	2.52	10.9
average reduced neutron width			
s-wave, (ev) ^{1/2}	18.5×10^{-4}	2.44×10^{-4}	10.9×10^{-4}
p-wave, (ev) ^{1/2}	like U-235	from exp.data	like U-235
average fission width at low energy, ev .		0.0975	
average gamma width, ev	0.025	0.039	0.034

ISOTHERMAL DOPPLER COEFFICIENT AT 1000 F

U-238	$-4.21 \times 10^{-6} \Delta k/k/^{\circ}C$
Pu-239/241	$+1.35 \times 10^{-6} \Delta k/k/^{\circ}C$
Pu-240	$-0.32 \times 10^{-6} \Delta k/k/^{\circ}C$
	<hr/>
	$-3.18 \times 10^{-6} \Delta k/k/^{\circ}C$

The Doppler power coefficient was obtained from the above-mentioned isothermal coefficient by averaging numerically over the fuel temperature distribution in the core. The temperatures used are averages over the pin cross section. The neutron worth in group 5 (of 6) from the two-dimensional CRAM run was used as a weight factor. The major portion of the Doppler effect occurs in this group.

The calculation of the expansion coefficients in the isothermal case is straightforward. The fuel in the pin is segmented, so the axial core expansion is governed by the expansion of the clad.

The model for the radial expansion is a solid ring of steel that governs the expansion of the core. Assemblies are supported by a ring-shaped bottom support plate and an upper restraining device. The formula for the radial expansion effect is reproduced here as an example.

The change in k due to radial expansion is given by:

$$\delta k = 2 \alpha_s \delta T \left\{ - \int_{\text{core}} W_{\text{core}} dv + \int_{\text{core}} W_{\text{Na}} dv \right\} \\ + \alpha_s \delta T \left\{ R_{\text{in}} \int_{\text{inner edge}} ds (W_{\text{core}} - W_{\text{bl}}) + R_{\text{out}} \int_{\text{outer edge}} ds (W_{\text{bl}} - W_{\text{core}}) \right\}$$

with

α_s = coefficient of thermal expansion of the steel structure,

δT = temperature difference,

W = worth of material per unit volume,

dv = volume element of core,

ds = surface element,

R_{in} = inner core radius, and

R_{out} = outer core radius.

The first term describes the effect of the core-volume increase, lowering the effective density of the core material, and increasing the volume fraction of sodium. The second term describes the replacement of core material by blanket material at the inner blanket edge, and vice versa at the outer edge.

The worth integrals over the core volume are taken directly from the CRAM two-dimensional calculations, and the surface integrals were obtained by graphical integration. A summary of the worth integrals used is given in Table 4-12.

The radial expansion effect is more complicated in the power range, because it depends on the structural design of the core. In order to avoid a positive reactivity effect from bowing of the assemblies, the inward bowing of assemblies under a radial power gradient must be prevented. This is done by welding spacer pads to the fuel assembly support cans in the axial midplane of the core and designing a supporting structure

which insures that each spacer is in touch with every adjacent spacer. In such a tight core design, the thermal expansion of the tubes together with their spacers causes a thermal expansion of the core, which is governed by the expansion in the axial mid-plane and by the shape of the subassemblies under elastic deformation. The bowing occurs then in different directions above and below the spacers and thus causes only a second-order reactivity effect, which is negligible compared to the expansion effect.

TABLE 4-12

WORTH INTEGRALS FOR THE CALCULATION OF THE TEMPERATURE COEFFICIENTS

A. Worth Integrals Over the Core

axial zone No.		<u>fuel</u>	<u>stainless steel</u>	<u>coolant</u>
1		+0.054	+0.0006	+0.0003
2		+0.105	-0.0102	-0.0035
3		+0.129	-0.0157	-0.0052
4		+0.097	-0.0089	-0.0028
5		+0.047	+0.0019	+0.0010
		+0.432	-0.0323	-0.0102

B. Worth Integral Weighted with Radial Power Distribution

1		+0.057	+0.0002	+0.0002
2		+0.110	-0.0116	-0.0037
3		+0.135	-0.0180	-0.0059
4		+0.103	-0.0102	-0.0032
5		+0.049	+0.0018	+0.0009
		+0.454	-0.0378	-0.0117

The calculation of the reactivity coefficient through radial expansion is then done with the following expression, as the spacers are in axial zone No. 3.

$$\delta k = 2 \alpha_s (T_{s,m=3} - T_{in}) \Delta P/P \left[- \int W_{core} f(z) dv + \int W_{Na} f(z) dv \right]$$

$$+ \alpha_s (T_{s,m=3} - T_{in}) \Delta P/P \left[R_{in} \int_{\substack{\text{inner} \\ \text{core edge}}} (W_{core} - W_{bl}) f(z) ds \right.$$

$$\left. + R_{out} \int_{\substack{\text{outer} \\ \text{core edge}}} (W_{bl} - W_{core}) f(z) ds \right]$$

where:

$T_{s,m=3}$ = average temperature of the structure in the axial zone No. 3 at full power, and

$f(z)$ = radial displacement of the subassembly at height z under the force of the expanding structure and elastic forces, for unit expansion unhindered by elastic forces.

The rest of the symbols were defined before. Again, the first term takes account of the core material being removed from the original core volume by expansion, and the sodium replacing it, and the second describes the replacement of the core material by blanket material at the inner-core edge and, vice-versa at the outer edge.

The worth integrals occurring in these calculations are again listed in Table 4-12, and Table 4-7 shows the resulting power coefficient. The latter contains also contributions from the sodium expansion in the core and reflector and the radial pin expansion. The pin expansion is governed by the pellet expansion, which is much larger than the clad expansion due to higher temperature changes.

The power coefficient was calculated for five axial zones to correspond to the requirements of the dynamics code used. Again, the axial expansion effect follows the clad expansion. It results from removing fuel-pin material, that is fuel plus clad from the core, and putting this material at the top (if the bottom is suspended), where it displaces the sodium of the reflector. It can thus be represented by the formulas:

$$\delta k = -\alpha_s \frac{v_{pin}}{v_{tot}} \sum_{m=1}^5 \int \Delta T_{em}(r) W_{pin} dv + \alpha_s H \sum_{m=1}^5 \Delta T_{em}(r) (W_{core} - W_{refl}) ds$$

$$\Delta T_{em}(r) = P(r) \Delta T_{em}$$

$$\Delta T_{em} = (T_{em} - T_{in}) \frac{\Delta P}{P}$$

where:

α_s = coefficient of thermal expansion of the clad,

$T_{em}(r)$ = average clad temperature in axial zone m in a channel at radius r ,

T_{em} = average clad temperature in axial zone m in the average channel,

ΔT = temperature change associated with the relative power change $\Delta P/P$,

$P(r)$ = radial power distribution, normalized to one at the average channel, and

v_{pin}/v_{tot} = volume ratio of pin volume to total volume of the channel.

The worth integrals occurring in this formula, all of which have the weight factor $P(r)$, were found by graphical integration over the worth distributions which come out of the CRAM multigroup calculations. They are listed in Table 4-12.

Aside from the axial and radial expansion of the core, several effects contribute to the isothermal expansion coefficient. There is the sodium expansion in the core, top reflector, and radial blanket. The core sodium has a positive reactivity coefficient, which is reduced, but not negated by the effect of the reflector for small density changes. Minor contributions come from the expulsion of sodium by expansion of the pin, which is governed by the clad expansion, and from the expansion of the radial blanket.

The results are given in Table 4-6.

Materials data pertinent to coefficient calculations are listed in Appendix D.

4.1.3.2.2 General Model. The dynamic analysis for the 1000-Mwe annular core was done for unorificed flow by an adaptation of the FORE digital computer code. A specific set of materials data was calculated. Although the engineering design is orificed the assumption of unorificed flow was felt justified for the following reasons. The purpose of the work presented here is to study the dynamics behavior and the safety characteristics of the conceptual core design, chosen mainly from the point of view of achieving a negative sodium loss coefficient, because the results of this study will have bearing on the merits of this design. Since the present objectives are to gain a better understanding of a novel design, the goal was to do a careful analysis with a reasonable amount of computer work, choosing a model that has all the main features, but not the incidentals of the chosen design. Orificing of the flow is definitely incidental in this context, notwithstanding its importance for the engineering design.

On the other hand, dynamics studies for a core with orificed flow would be one order of magnitude more complicated than for an unorificed flow. As explained below, the computer code FORE used for these studies works with an average channel and calculates the temperatures for the hottest channel, but the hot-channel data enter the feedback only for an estimate of the fuel temperature profile to obtain the Doppler coefficient. Thus it is rather important to have a clear concept of the average channel, defined obviously as the channel with the average output. This channel does not change during the transients investigated here, because the power shape does not change.

The average temperature coefficients assigned to this channel can be calculated exactly if the following conditions are fulfilled. The flow is not orificed, all heat-transfer coefficients and thermal conductivities are independent of the temperature, and the power distribution is separable in r and z . Under the same assumptions, the average temperature coefficient does not change during the transients.

This can be made plausible in the following way: Let T_{in} be the coolant inlet temperature, and let the coolant velocity be the same in all channels. Let 1 and 2 be two

channels and assume that the ratio of their powers $P_2:P_1$ is constant in time. Take a temperature T_1 anywhere in fuel, clad, or coolant of channel 1, and let T_2 be the temperature at the equivalent location in channel 2. Then, under the above-mentioned assumption, the ratio $(T_2 - T_{in}):(T_1 - T_{in})$ is through all transients equal to the ratio $P_2:P_1$. This is an immediate consequence of the form of the heat transfer equations and the equation for convection in the coolant.

If the isothermal temperature coefficient $(\partial k/\partial T)_n$ associated with the temperature of the material n is given, the total feedback due to this coefficient in any horizontal disc of thickness δ at a height z is given by the expression

$$\delta k_n(z) = \left(\frac{\partial k}{\partial T} \right)_n \frac{\int_{\text{disc}} (T_n(r,z) - T_{in}) W_n(r,z) dv}{\int_{\text{core}} W_n(r,z) dv}$$

where W_n is the total worth of the material n . As the temperature difference is proportional to the radial power distribution, the integral can be written:

$$\delta k_n(z) = \left(\frac{\partial k}{\partial T} \right)_n \frac{(T_n(z)_{av} - T_{in}) \int_{\text{disc}} P(r) W_n(r,z) dv}{\int_{\text{core}} W_n(r,z) dv}$$

where $T_n(z)_{av}$ refers to the average channel. It is obvious now that the total feedback associated with the coefficient n is just the sum of expressions (second equation) over different axial zones.

It is obvious that the transition from the first equation to the second, which makes the definition and calculation of an effective temperature coefficient associated with the average channel possible, cannot be carried out if the flow is orificed.

Furthermore, it is impossible to define an average coolant velocity that is valid during transients if the flow is orificed. These two arguments lead to the decision to carry the dynamics studies out for an unorificed core, the behavior of which will be only slightly different from the orificed core. This analysis is possible in a consistent way.

As to the other assumptions mentioned above, the separability of the power in r and z is a very good approximation. The major deviation from the model is due to the variable thermal conductivity of the mixed oxide. However, neither the magnitude nor the variation of the conductivity is well known, so that the calculated temperature distribution in the fuel can only be considered to be approximate.

FORE is a digital computer program prepared by the General Electric Company for the purpose of calculating the dynamic behavior of fast reactors with particular emphasis on large ceramic-fueled fast reactors. The program was modified to meet the needs of this study, and adapted to the IBM 7094 computer at APDA.

In particular, the FORE program calculates the reactor power and fuel, coolant, clad, and structure temperature as a function of time in response to programmed and feedback reactivities changes. The feedback reactivity effects include the fuel Doppler effect and thermal expansion of the core materials.

In order to increase the applicability of the FORE program to this particular study the following modifications were made:

- (a) The geometry of the clad was changed from slab to cylindrical.
- (b) The fuel-pin geometry was changed from a solid cylinder to a hollow cylinder.
- (c) The coolant flow was allowed to change as a function of time.
- (d) The core expansion utilized in the feedback equations was modified to represent the design used in this study.

In the original FORE code the cylindrical clad was approximated by slab geometry for simplicity since the error in this approximation is negligible for a thin clad. However, the clad in this study was sufficiently thick that the curvature effect was felt to be significant. Therefore, the equations for the temperature were modified to account for the cylindrical geometry and the program was changed accordingly.

The fuel pin is a hollow cylinder, the area of the hole being approximately 16 percent of the fuel area. It was felt that this design could not be adequately simulated by adjusting the heat generation rate and the thermal conductivity in the solid fuel pin to adequately represent the hollow pin. Therefore, the steady-state and transient-heat-transfer equations were modified to allow calculation for this design.

An investigation of the effect of pump failure requires that the sodium flow be varied according to the pump-flow decay. The steady-state equations were modified to include a flow term while the transient equations were modified to include the flow term which could be varied as a series of linear changes with time.

The expansion of the core in the axial or radial direction for this study does not follow the basic relations described in the original FORE report due to the design of the fuel pin. Therefore, the equation for determining the expansion values were replaced by those applicable to the design (see Sec. 4.1.3.2.1) presented in this study.

All of the modifications made to the FORE program were checked for accuracy using either hand numerical calculation or other computer codes such as GHT which perform sections of the calculation with a known accuracy.

4.2 THERMAL AND HYDRAULIC ANALYSIS

The thermal and hydraulic characteristics were determined by conventional techniques. Simple computer codes were written for studies on pressure drop, coolant-temperature rise, fuel-pin-temperature distribution, and core composition based on geometric properties.

4.2.1 Core and Blanket Characteristics

4.2.1.1 Overall Power Generation. The reactor generates a total of 2500 Mwt. During a fuel cycle the relative contribution of core and blanket to the total power output varies according to the fuel burnup in the core and fuel build-up in the blanket. Figure 4.6 shows the variation of power and feed in the core and blanket. To establish the nominal thermal design, it was assumed that half a fuel cycle had been completed and the core and blanket were producing 85 and 15 percent of the total power, respectively.

4.2.1.2 Coolant-Temperature Rise. An overall coolant-temperature rise of 250 F was selected with the inlet temperature at 950 F. It is estimated that 5 percent of the total flow rate will bypass the core and blanket and result in a nominal temperature rise of 263 F in core and blanket. Figure 4.7 shows the core and blanket outlet temperatures and their variation over a fuel cycle. The nominal coolant-outlet temperature varies from 1222 F to 1204 F in the core region and from 1107 F to 1262 F in the blanket regions.

4.2.1.3 Orificing. The coolant flow to the core and blanket fuel assemblies is orificed to keep the reactor exit temperature at 1200 F without unduly penalizing the assemblies located at the radial flux peaks. Figure 4.8 shows the outlet temperature from the hot channel for a fuel pin at the radial flux peak. For a hot channel factor, F_H , with a coolant rise of about 1.4 the outlet temperature is about 65 F lower than an unorificed core.

4.2.1.4 Temperature Distribution in Fuel Pin. Figure 4.9 shows the axial flux profile. In Figures 4.10 and 4.11 temperatures for a fuel pin at the radial flux peak are plotted for nominal and hot-channel conditions. From these plots it is seen that the allowable operating temperature of the fuel cladding, as determined by mechanical properties, and the maximum fuel temperature are the limiting factors for the thermal design of the fuel element.

4.2.1.5 Holddown Force for Fuel Assembly. In connection with the determination of the pressure drop along the core fuel bundle receiving the highest flowrate an investigation was performed to assess the influence of the shape of the fuel assembly inlet portion on the requirement to hold the assembly securely in position. Decreasing the cross-sectional area of the inlet portion reduces the upward pressure while increasing the upward momentum. Thus, one condition will exist when the net force resulting from pressure and momentum forces and assembly weight is a minimum. From Fig. 4.12 it is seen that the net force -- which is equal to the required holddown force -- reaches a

minimum of about -80 lb. The part of the curve showing a negative force, indicates that no additional holddown force is required to keep the assembly in place.

For a practical design an inlet contraction producing less than the minimum negative force will probably be chosen to prevent excessive coolant velocities in the contraction.

Figure 4.13 shows the pressure difference between inlet plenum and assembly outlet as well as between assembly inlet and outlet as function of the inlet contraction.

4.2.2 Method of Analysis

4.2.2.1 Temperature Distribution in Fuel Pins. The temperature distribution in the fuel pins was calculated assuming radial heat conduction in the fuel pin only.

The individual fuel pellets in the cladding are separated by discs to prevent fuel movement. However, the discontinuous arrangement gives a slightly higher local heat-production rate than does a continuous arrangement of pellets. To account for this, a hot-spot sub-factor was introduced into the temperature-distribution calculations.

4.2.2.2 Power Shape. The axial power shape was approximated with very good overall agreement by a chopped cosine distribution of the heat sources centered at the core mid-plane. The cosine distribution was fitted in a manner as to slightly overestimate the temperatures at the top end of the pin due to the slightly asymmetrical power shape.

4.2.2.3 Coolant-Film Coefficient. Several correlations for the Nu-number for the turbulent heat transfer of liquid metals are plotted in Fig. 4.14. For the calculations, Curve b represented the equation $Nu = 5 + 0.025 Pe^{0.8}$.

4.2.2.4 Heat Transfer Across Gap Between Fuel Pellet and Cladding. Due to the difficulty in accurately predicting the heat transfer resistance across the gap between pellet and cladding, an equivalent constant film coefficient of 1000 Btu/hr-ft²-°F was assumed. Any contribution from radiative-heat transfer to this value was not considered separately. Thus the assumed value is expected to be conservative.

4.2.2.5 Thermal Conductivity of Fuel. Figure 4.15 shows several correlations for the thermal conductivity of 95 percent dense UO₂. In the absence of better information it was assumed for the thermal calculations that the thermal conductivity of a PuO₂-UO₂ mixture is adequately represented by the conductivity of UO₂ alone. The plots of Fig. 4.15 show a considerable spread in the reported data. For the thermal design calculations it was considered practical to use a constant value of 1.6 Btu/hr-ft-°F for the conductivity of the fuel mixture. The adaptation of this value is somewhat more optimistic than some of the reported correlations would justify. However, it does not take advantage of the increase of the conductivity at temperatures above about 2000 F as observed by one investigator.

4.2.2.6 Pressure-Drop Calculations. The method of calculating the pressure drop along a fuel bundle follows an approach described by A. N. de Stordeur. The total pressure drop is computed by summing the pressure drop along the bundle disregarding spacers plus the pressure drop across the spacers.

4.2.2.7 Loss of Flow. Based on primary dimensions shown in the reactor building drawings and extrapolation of intermediate heat-exchanger data from other sodium-cooled systems, the hydraulic characteristics of the primary system were determined. The change in flow as a function of time was determined by the method described by Love. The fluid inertia represents by far the largest contribution of stored energy and thus the energy in the pump and motor were neglected. Within the time duration of interest it was not considered necessary to take into account the contribution to circulation of thermal convection. The results of this analysis are shown in the section on reactor dynamics (Fig.E.6).

4.2.3 Discussion of Results

An analysis was performed to determine the uncertainties and their effect on the computation of the pin-temperature distribution. Figure 4.16 shows the resulting temperatures under the conservative assumption of multiplying the individual effects related to the particular temperature drops. The respective hot-spot factors are calculated as:

coolant rise	1.46
film-temperature drop	1.49
cladding-temperature drop	1.44
temperature drop through gap	1.44
fuel-temperature drop	1.52

In Table 4-13 the statistical combination of the effects is shown. It is assumed that the deviations are scattered around the nominal condition according to a Gaussian probability distribution. The confidence limit of the statistical method was chosen as 99.7 percent. The total deviation of the maximum fuel temperature (at the hottest pin section) from the nominal value was calculated to be 437 F, thus resulting in a maximum fuel temperature of 3861 F as compared to 4615 F using the multiplicative method. Figure 4.17 shows a comparison of the calculated maximum fuel temperature using the multiplicative and statistical method. In Fig. 4.18 a similar comparison of the mean cladding temperature is shown, the maximum temperatures are calculated as 1411 F and 1330 F, respectively.

TABLE 4-13

STATISTICAL ANALYSIS OF HOT SPOT FACTORS

nominal value	coolant temp.		flux drop		cladding drop		gap drop		fuel drop		$\Sigma(3\sigma)$	$\Sigma(3\sigma)^2$
	ΔT_c	142 F	ΔT_f	31 F	ΔT_{CL}	121 F	ΔT_c	733 F	ΔT_f	1447 F		
uncertainty	F_H	$3\sigma(^{\circ}F)$	F_{θ}	$3\sigma(^{\circ}F)$	$F_{\Delta T_{CL}}$	$3\sigma(^{\circ}F)$	$F_{\Delta T_G}$	$3\sigma(^{\circ}F)$	$F_{\Delta T_F}$	$3\sigma(^{\circ}F)$		
coolant maldistribution	1.10	14	--	--	--	--	--	--	--	--	14	196
deviation from nominal dimensions	1.13	18	--	--	--	--	--	--	--	--	18	324
	--	--	1.02	1	--	--	--	--	--	--	1	1
	--	--	--	--	1.16	19	--	--	--	--	19	361
	--	--	--	--	--	--	106	44	--	--	44	1936
	--	--	--	--	--	--	--	--	1.08	116	116	13,456
maldistribution of Pu-239 . . .	1.02	3	1.02	1	1.02	2	1.02	15	1.02	29	50	2500
maldistribution of flux	1.05	7	1.05	15	1.05	6	1.05	37	1.05	72	124	15,376
burnup of core fuel	1.10	14	1.10	3	1.10	12	1.10	73	1.10	145	247	61,009
coolant film heat transfer . . .	--	--	1.20	6	--	--	--	--	--	--	6	36
conductivity of cladding . . .	--	--	--	--	1.05	6	--	--	--	--	6	36
heat transfer through gap . . .	--	--	--	--	--	--	1.15	110	--	--	110	12,100
conductivity of fuel	--	--	--	--	--	--	--	--	1.20	289	289	83,521

$$\Sigma = 1044 \quad \Sigma = 190,852 = \Sigma[\Sigma(3\sigma)]^2$$

$$\sqrt{\Sigma[\Sigma(3\sigma)]^2} = 437 \text{ F} = \text{total deviation}$$

4.2.4 Summary of Thermal and Hydraulic Characteristics

Reactor thermal power*, Mw

core	2125
inner radial blanket	185
outer radial blanket	190
total	2500

Coolant system:

total coolant flow rate, lb/hr	1.14×10^8
overall coolant temperature rise, °F	250
coolant temperature rise through core, °F	263
coolant temperature rise through radial blanket, °F	263
coolant inlet temperature, °F	950

Fuel assembly size (core and blanket):

width of hexagonal assembly, in.	4.45
assembly wall thickness, in.	0.100
space between assemblies, in.	0.050
pins per assembly, number	
core	123
blanket	37

Core fuel:

pin:

outer diameter, in.	0.300
inner diameter, in.	0.244
length, ft	5
cladding thickness, in.	0.028

pellet:

outer diameter, in.	0.2405
inner diameter, in.	0.0998
active fuel length, ft	4
triangular pitch, in.	0.375
fuel composition	mixed oxide (UO ₂ + PuO ₂)
density of fuel, % of theoretical	95

performance:

average linear heat generation, kw/ft	8.6
average heat flux, Btu/hr-ft ²	0.373×10^6

power distribution:

(P _{max} /P _{av}) _{axial}	1.3
(P _{max} /P _{av}) _{radial}	1.18
P _{max} /P _{av}	1.52

maximum fuel temperature (beginning of fuel cycle), °F

nominal	3424
hot spot (multiplicative)	4615
hot spot (statistical, 99.7% confidence)	3861

*Unless noted, values are representative for conditions at middle of fuel cycle.

sodium flow (assembly at flux peak), lb/hr	2.158×10^5
pressure drop bundle (assembly at flux peak), psi	36.5
overall (assembly at flux peak), psi	45

Blanket fuel:

pin:

outer diameter, in.	0.607
inner diameter, in.	0.577
length, ft	6
cladding thickness, in.	0.015

pellet:

diameter, in.	0.574
active fuel length, ft	6
triangular pitch, in.	0.690
fuel composition	UO ₂
fuel density, % of theoretical	95

performance:

average linear heat generation, kw/ft:

inner radial blanket	3.01
outer radial blanket	1.47
sodium flow (assembly at flux peak of inner blanket), lb/hr.	6.9×10^4
pressure drop, bundle (assembly at flux peak of inner blanket), psi.	6.5

4.3 MECHANICAL DESIGN

This section discusses the mechanical design of the fuel assemblies. A simple, reliable, mechanical design yielding high thermal and hydraulic performance, as well as good nuclear characteristics, is required to assure continuous plant operation and low fuel cycle cost. In order to provide a sound basis for determination of core life, mechanical strength, and stability the following areas were looked at in some detail.

- (a) Fuel metallurgical lifetime
- (b) Fuel-pin vibration
- (c) Fuel-pin bowing
- (d) Fuel-assembly shroud can

The contract prescribed the use of stainless-steel cladding and a design target of a reactor outlet coolant temperature of 1200 F. The superior high-temperature properties of Type-316 suggested its use for both cladding and primary-system components. Another stainless steel, 19-9 DL, looked promising but it had not been used in sodium systems and there was wide divergence in creep and stress rupture property data. Thus, 316 SS. was selected on the basis of physical and chemical properties and ready availability and fabricability.

The mechanical concept of the fuel assembly has been based on ease of remote fabrication, since a practical fast-reactor economy will require recycle of the plutonium generated and the alpha, neutron, and gamma activity of recycle material will be high. A somewhat larger diameter than one based on thermal considerations alone was chosen. The basis of core flow and fuel assembly geometry selection is discussed in Appendix C.

4.3.1 Fuel-Pin Design

The basic design objectives of the fuel pins were as follows:

- (1) A diameter large enough to allow practical remote fabrication of pellet-type fuel.
- (2) A design capable of an average fuel burnup of 100,000 Mwd/MTM.
- (3) Sufficient space to accommodate the large volume of fission gases expected from high burnup fuel. It was assumed that 100 percent of the fission gas in the fuel above the recrystallization temperature and 50 percent of the gas in the fuel below the recrystallization temperature would be released.
- (4) Accommodation of all fission gas within the active core region so as to minimize core length (and thus vessel length) and to avoid gas piston effects that could cause fuel movement with a rapid change in coolant temperature.

All but the last of the design objectives were met. A compromise was made between clad thickness and fuel element length by incorporating a short fission-gas reservoir at

the lower end of the core where the presence of sodium coolant minimizes the amount of space required. Partitions in the form of fixed tungsten discs have been located every 3 in. inside the pin to prevent gross axial relocation of fuel. A central void has been introduced into both pellets and discs to assure gas communication with the reservoir and allow for a lower average central fuel temperature.

The metallurgical lifetime of the fuel element is defined by the ability of the cladding to contain the fission-product gases. The amount of gases released within the cladding is assumed to be a constant fraction of the amount of gases formed by fission and consequently is directly proportional to the burnup, that is to the heat-generation rate and to the time of exposure.

The pressure of the gases contained within the cladding is directly proportional to the amount of gas released and to the gas temperature. It is inversely proportional to the volume in which the gases are contained, that is the void spaces available within the cladding. Thus, the pressure of the gases released is a direct function of the time of exposure, for a given pin geometry, fuel void, and heat generation rate.

Since the effect of gas entrapped when the cladding was sealed is neglected in the computation of differential pressure across the cladding, the hoop stress is then directly proportional to the absolute pressure of the gases released and therefore to the time of exposure. It may be expressed by the equation $S = \beta N$.

Based on experimental data the creep rate may be expressed as an exponential function of the stress:

$$de/dN = bS^a$$

By eliminating the time between the two equations, the creep is defined by the differential equation

$$de = b/\beta S^a dS$$

The total creep, e , is then determined by integrating this equation between $S = 0$ for $e = 0$ and the value of the stress for rupture S .

Creep is then calculated and corresponding core pressure drop evaluated by computing the variation of the coolant velocity and of the equivalent diameter of the fuel assembly.

In the above equations,

- N = number of hours
- S = stress, psi
- e = total creep strain
- a = materials constant
- b = materials constant

The coefficient β represents the basic characteristics of the fuel element as geometry of the cladding, heat generation, amount and location of free spaces. It is independent of cladding material and temperature.

A typical curve showing the relationship of stress, operating hours at temperature, creep and β is seen in Fig. 4.19.

When β is selected, the time is then read on the line β in function of S . The lifetime is then defined by considering either the creep or the stress for rupture in 1000 hr.

Although the basic equations were written assuming retention of fission gases within the active core, an adjustment in the effective β can be made to account for a fission gas accumulation reservoir at the bottom of the core without changing the composition of the core. The dotted line on Fig. 4.19 shows the change in β accomplished by a 6-in. reservoir which was necessary to extend the time to reach 1000-hr rupture stress so as to allow a burnup of 100,000 Mwd/MTM.

Average value of the 1000-hr stress to rupture life for the cladding material was selected as the design lifetime criteria. Actual time to rupture when the pressure corresponding to this stress is reached will range from 500 to slightly less than 1000 hr. A further decrease in pressure of 15 percent caused by cladding creep is indicated and seems reasonable.

Although the effect of thermal stress and irradiation has been considered, the treatment has only been semi-quantitative. Partial-to-full annealing of thermal stress can be expected over the life of the fuel pins and thus the effect of strain cycling would be minimized. Based on anticipated thermal stresses, no serious problem leading to pin failure is anticipated. The nature of the problem does not readily lend itself to analysis and confidence must be established by irradiation testing of prototype fuel pins.

4.3.2 Fuel-Assembly Design

The principal design considerations in the fuel assembly were concerned with shroud-can deflection due to internal pressure, fuel-pin support to reduce amplitude of vibration and prevent thermal bowing, and lateral restraint of assembly to prevent excessive movement on change of power.

The assembly shroud-can thickness was established at 0.100 in. to prevent excessive deflection of the straight sides of the hexagon due to internal coolant pressure. Limiting deflection to 0.005 in. assures that there will be very little coolant bypass of the fuel pins during operation. An assembly spacing of 0.050 in. has been chosen to provide for ease of insertion and withdrawal for fuel handling. Support pads have been sized and located to assure a laterally tight core with a 250 F coolant-temperature rise through the core when the fuel assembly is correctly seated.

Even with pessimistic assumptions for flow and temperature maldistribution it can be shown that with the tight core, thermal bowing can be limited to 0.0026 in.

The support can provide lateral support for the fuel pin spacer grids. These grids are held in their axial position by tie rods and spacer sleeves. Two types of pin deflection can be anticipated:

- (a) that due to nonuniform heat generation around the pin, and
- (b) that due to flow-induced vibration.

Longitudinal spacing of the grids is determined so as to prevent movement of fuel pins that could induce mechanical failure or result in power fluctuations of the reactor.

4.3.3. Thermal Bowing

Circumferential variation of temperature around a fuel pin is expected to be negligible due to the low temperature drop through the sodium film and the good mixing due to turbulence induced by the spacer grids. A curved cantilever beam was assumed and the direction and magnitude of forces necessary to prevent deflection at grid support points were calculated.

A 5 F temperature difference was assumed and it was determined that, with spacer grids 12 in. apart, a maximum pin deflection of 0.0013 in. could be expected. Forces on the grid and stresses induced in the clad are low and were considered negligible.

4.3.4 Vibration

When the coolant flows in a direction parallel to the fuel-pin assembly, it induces vibrations of the self-excited type to the pins in the transverse direction at their fundamental damped frequency of vibration.

The fundamental frequency of a pin is proportional to the square root of its weight per unit length and inversely proportional to the square root of its elasticity EI ; it is inversely proportional to the square of the free vibrating length; that is, to the distance between two spacers or between one spacer and the assembly end.

As the pin vibrates in a viscous medium, the natural frequency of the pin vibration is reduced; the reduction factor for viscous damping in water was found experimentally, to be equal to 0.80. It was assumed to be 0.9 in sodium environment.

The deflection of the pin, directly proportional to the damped frequency, is also a function of the ratio of the hydrodynamic force of the coolant to the elastic force on the pin.

In brief, it was found that the amplitude of vibration is proportional to the free length to the fourth power, all other parameters (lattice arrangement, coolant velocity, density and viscosity, pin density, diameter, moment of inertia, modulus of elasticity) remaining constant.

The amplitude of vibration is also influenced by the manner in which the pin is supported in the grid. An end fixity corresponding to a pinned-end was assumed.

Since the maximum moment and, consequently, the maximum stress in the cladding are directly proportional to the deflection, it is seen that conservative values of moment and stress are thus obtained for the rigidly fixed end section of the fuel assembly.

For the reference fuel assembly a grid spacing of 12 in. was chosen to limit pin deflection due to vibration to 0.0014 in. The frequency of vibration is calculated to be 106 cycles per second but the maximum stress due to vibration is far less than that required to produce vibration induced fatigue failure.

4.4 FUEL CYCLE

4.4.1 Summary of Results

4.4.1.1 Fuel-Cycle Costs and Doubling Time. Fuel-cycle costs have been determined for both on-site and off-site fuel reprocessing and fabrication and for both AEC and private fuel ownership. The fuel-cycle costs for the reference conditions are presented in Table 4-14. On-site fuel reprocessing and refabrication has been selected as the reference mode of fuel-cycle operation because of its economic advantage and because of its greater potential for reductions of fuel inventory costs. This reduction in cost will become increasingly more important with the transition from government to private ownership of fuel. The doubling time for the reference core and blanket burnup conditions for a plant factor of 80 percent is 18.4 years.

4.4.1.2 Fuel-Management Program. A scatter refueling scheme, in which the fuel is discharged in one-third core batches, has been selected for core fuel management. This scheme was chosen because the low radial peak-to-average power ratio of the core reduced the improvement in fuel burnup usually obtained with an out-in fuel management program. An out-in radial blanket fuel-management program, in which the blanket is discharged in one-quarter blanket batches, has been chosen. This program was selected to avoid the large variation in coolant mixed mean outlet temperature which would occur if the radial blanket assemblies were maintained at a fixed radial position throughout their irradiation life.

4.4.2 Discussion of Results

4.4.2.1 On-Site Reprocessing. The schematic flowsheet for on-site fuel reprocessing and fabrication of fuel is shown in Fig. 4.20. This mode of operation eliminates shipping of the intensely radioactive spent fuel and achieves a minimum of fissile material inventory costs. The fuel-cycle facility, which is described in detail in Sec. 6.3, combines both reprocessing and fuel-fabrication operations in a single facility and, thus, does not yield separate fuel-cycle cost components for these items. The total fuel-cycle costs for the on-site mode of operation are presented in Fig. 4.21, as a function of core fuel exposure with a radial blanket fuel exposure of 133,333 core equivalent Mwd/MTM. The plant factor has been treated as a parameter with values of 80 and 90 percent. The distribution of fuel-cycle costs for the various burnup and plant factor combinations are summarized by individual components in Table 4-15. The values for private fuel ownership are approximate and were obtained by using a fuel inventory charge rate of 10 percent, rather than the 4.75 percent value applicable to AEC fuel ownership. A more detailed evaluation of the operation with private ownership of fuel may increase the fuel-cycle cost approximately 0.1 mill/kwhr.

The on-site processing facility has an instantaneous capacity which is proportioned to the fuel discharge rate of the reactor. As a result, the ex-reactor fuel inventory is essentially

TABLE 4-14

REFERENCE FUEL-CYCLE COSTS

fuel ownership	cost, mills/kwhr	
	<u>on-site reprocessing</u>	<u>off-site reprocessing</u>
AEC	0.69	0.78
Private	1.00	1.11

Basis:

Core fuel exposure	= 100,000 Mwd/MTM
Blanket fuel exposure	= 133,333 core equivalent Mwd/MTM
Plant factor	= 80 percent
Ex-reactor core fuel inventory	= 33 percent of in-reactor inventory

TABLE 4-15

SUMMARY OF FUEL-CYCLE COSTS - ON-SITE REPROCESSING

reactor plant factor	80 percent				90 percent	
core fuel exposure, Mwd/MTM	50,000	66,667	100,000	133,333	80,000	100,000
cost component	mills/kwhr				mills/kwhr	
fuel cycle plant fiscal charges	0.24	0.17	0.17	0.17	0.15	0.15
fuel cycle plant operating cost	0.53	0.42	0.32	0.28	0.36	0.30
Pu losses	0.07	0.06	0.04	0.04	0.05	0.04
Pu inventory cost	0.28	0.28	0.28	0.28	0.25	0.25
(AEC fuel ownership)						
replacement blanket fuel	0.16	0.16	0.16	0.16	0.16	0.16
Pu credit	(0.25)	(0.30)	(0.32)	(0.33)	(0.31)	(0.32)
core fabrication interest	0.02	0.02	0.02	0.02	0.02	0.02
blanket fabrication interest	0.02	0.02	0.02	0.02	0.02	0.02
<u>Total Fuel Cycle Cost</u>						
AEC fuel ownership	1.07	0.83	0.69	0.64	0.70	0.62
Private ownership	1.38	1.14	1.00	0.95	0.98	0.90

fixed at the amount represented by the discharge batch size. Fuel-cycle costs, as a function of fuel inventory, are shown in Fig. 4.22 for the reference core and blanket burnup conditions. The costs in Fig. 4.22 treat the plant factor as a parameter with values of 80 and 90 percent. By assuming that the reactor and power plant maintenance can be accomplished during the refueling shutdown period, a theoretical maximum plant factor can be computed for each value of refueling batch size. The time required for refueling has been determined for fuel handling batch sizes from one-twentieth to one-third of a core. The maximum plant factor is then the ratio of reactor operating time for the burnup increment to the sum of refueling time and burnup increment reactor operating time. The fuel-cycle cost, computed as a function of total fuel inventory as discussed above, can then be corrected for the maximum plant factor which applies for each refueling batch size. Because the plant factor decreases with decreasing batch size, the use charges increase, and a minimum is found in the curve of fuel-cycle cost as a function of the refueling batch size. The results of this analysis are shown in Fig. 4.23.

4.4.2.2 Off-site Reprocessing. The fuel-cycle cost for off-site fuel-reprocessing and conventional fabrication was obtained by the Guide to Nuclear Power Cost Evaluation method. The detailed values of various cost components for this mode of fuel-cycle operation are discussed in Sec. 4.4.3.2. As in the case of on-site reprocessing, the fuel-cycle costs for private fuel ownership conditions have been approximated by using a fuel inventory charge rate of 10 percent. The results of the off-site reprocessing fuel cost estimate are presented, as a function of core fuel exposure, in Fig. 4.24. The distribution of fuel-cycle costs for the various burnup and plant factor combinations are summarized by individual components in Table 4-16. A radial blanket fuel exposure of 133,333 core equivalent Mwd/MTM was used in computing these costs. The effect of varying radial blanket exposure on the fuel-cycle cost with a fixed core fuel exposure of 100,000 Mwd/MTM has also been investigated for the off-site reprocessing case. The total fuel-cycle cost, as a function of blanket exposure, is shown in Fig. 4.25 for plant factor values of 80 and 90 percent. It would appear that increasing the blanket exposure to approximately 200,000 core equivalent Mwd/MTM would permit a significant fuel-cycle cost reduction. However, carrying the blanket fuel to this exposure level will result in a greater variation in mixed mean coolant outlet temperature, which could negate the apparent fuel-cycle cost reduction. To prevent large variations in blanket coolant outlet temperature over an irradiation cycle, it may be necessary to rotate the assemblies 180° each time they are shuffled. In addition, consideration should be given to installing a thermostat-like device in the outlet of the blanket fuel assemblies.

The effect of reducing the radial blanket thickness by 25 percent was considered and was found to decrease the net breeding gain by about 30 percent. This would result in a fuel-cycle cost increase of approximately 0.07 mills/kwhr and would increase the doubling time to approximately 28 years at 80 percent plant factor.

4.4.2.3 Effect of Study Ground Rules on Fuel-Cycle Cost. The study ground rules fixed the net plant efficiency at 40 percent. Examination of the typical 1200 F plant simplified heat balance (Fig. 4.26) indicates that a net efficiency above 43 percent can be obtained.

Since the fuel-cycle cost is inversely proportional to net plant efficiency, the achievement of 1200 F outlet sodium temperature should permit fuel-cycle cost reductions on the order of 7 to 10 percent. Because of the importance of net plant efficiency in determining fuel-cycle costs, any real optimization of the fuel-cycle must be done by taking the entire power plant cycle into account.

While the fuel-cycle cost bases suggested by the Commission greatly facilitate comparison of the results of this study with those of other similar studies, the specification of government fuel ownership does not seem consistent with the fact that large fast reactors will not be operating in the immediate future. Any further fuel-cycle optimization studies should therefore be carried out on a private-ownership basis.

TABLE 4-16

SUMMARY OF FUEL-CYCLE COSTS - OFF-SITE REPROCESSING

reactor plant factor	80 percent			90 percent		
core fuel exposure Mwd/MTM	66,667	100,000	133,333	66,667	100,000	133,333
cost component	mills/kwhr			mills/kwhr		
core fuel fabrication	0.47	0.31	0.24	0.47	0.32	0.24
core fuel reprocessing and shipping	0.16	0.11	0.08	0.16	0.11	0.08
blanket fuel fabrication	0.16	0.16	0.16	0.16	0.16	0.16
blanket fuel reprocessing and shipping	0.13	0.13	0.13	0.13	0.13	0.13
Pu losses	0.06	0.04	0.03	0.05	0.03	0.03
Pu inventory (AEC ownership)	0.33	0.30	0.28	0.31	0.28	0.25
Pu credit	(0.32)	(0.32)	(0.32)	(0.32)	(0.32)	(0.32)
core fabrication interest	0.03	0.03	0.03	0.03	0.03	0.03
blanket fabrication interest	0.02	0.02	0.02	0.02	0.02	0.02
<u>Total Fuel Cycle Cost</u>						
AEC fuel ownership	1.04	0.78	0.65	1.01	0.76	0.61
Private ownership	1.41	1.11	0.96	1.35	1.06	0.89

4.4.3 Methods of Analysis

4.4.3.1 Input Information. Fissile material inventory and rates of radial blanket plutonium production were established for the equilibrium cycle conditions, as discussed in Appendix G. The composition of the one-third-core refueling batch is given in Table 4-17. The net plutonium production, as a function of radial blanket fuel exposure, is summarized in Table 4-18.

A preliminary estimate of the change in fissile material inventory with changes in core fuel exposure showed that, within the exposure range considered, the inventory change would be very small. The reference equilibrium core composition values were therefore used for all cases of fuel-cycle cost analysis.

TABLE 4-17

COMPOSITION OF CORE REFUELING BATCH

isotope (metal)	0 Mwd/MTM	50,000 Mwd/MTM	100,000 Mwd/MTM
U-238	4150 kg	3980 kg	3790 kg
Pu-239	1140 kg	942 kg	780 kg
Pu-240	540 kg	535 kg	510 kg
Pu-241	90 kg	90 kg	90 kg
F.P.	0 kg	373 kg	750 kg
Total kg	5920 kg	5920 kg	5920 kg

TABLE 4-18

PLUTONIUM PRODUCTION

source or use, kg	blanket fuel exposure, core equivalent Mwd/MTM		
	133,333	200,000	266,667
core discharge	370	1305	1740
axial blanket discharge	27	40	54
radial blanket discharge	<u>574</u>	<u>835</u>	<u>1052</u>
gross production	1471	2180	2846
reprocessing and fabrication losses	27	40	51
core makeup	<u>1230</u>	<u>1845</u>	<u>2460</u>
Net production	214	295	335

4.4.3.2 On-Site Reprocessing. The cost basis for the on-site fuel-cycle operation is presented in Sec. 6.3.8. The on-site fuel facility costs have been based on a plant with the capacity required to service the reactor operating with an average core fuel discharge exposure of 100,000 Mwd/MTM, and an average radial blanket discharge exposure of 133,333 core equivalent Mwd/MTM. Different values of fuel exposure require different capacity fuel-cycle facilities. Estimates of these costs for the core fuel exposure range

66,667 - 133,333 Mwd/MTM were obtained by adjusting the reference plant annual operating costs. Both capital and operating cost estimates were made for the 50,000 Mwd/MTM core fuel exposure case because the required capacity was too high to permit reasonable extrapolation of the reference plant case.

4.4.3.3. Off-Site Reprocessing. The open fuel-cycle cost estimates were prepared using the Guide to Nuclear Power Cost Evaluation methods whenever appropriate. The design operating and economic parameters used in this analysis are given in Table 4-19. A core fuel fabrication cost of 350 dollars per kilogram of contained uranium and plutonium was used for these calculations. This value, which was based on estimates for the reference fuel assembly design, is consistent with other recent plutonium fuel fabrication cost studies when allowance is made for the cost of fuel segmentation. This value also compares reasonably well with the on-site fuel processing and refabrication unit cost.

The standard cost estimating method was modified in three ways:

(1) The cost of the depleted uranium for the radial and axial blankets was included in the cost of fabricating the fuel assemblies. Therefore, any uranium loss charges were similarly included in the fabrication cost. No credit was taken for the recovered blanket uranium, and, accordingly, no charge was assessed for converting the depleted uranyl nitrate to uranium hexafluoride.

(2) Plutonium credit was computed on the basis of delivering plutonium nitrate to the fuel supply agency, rather than plutonium metal buttons. This is considered appropriate because, for oxide fuel systems, the nitrate form can be introduced more readily into the fabrication of new fuel material.

(3) No use charge was assessed for the bred plutonium inventory in the blankets. This approach seems reasonable in view of the fact that most of the plutonium generated in the blanket is returned directly to the core in the on-site reprocessing and fabrication facility. A more refined analysis of the blanket fuel cycle should be based on a present worth technique, in which the value of the excess plutonium, potentially available for sale by more frequent discharge of blanket fuel, is considered.

TABLE 4-19

FUEL COST DATA - OFF-SITE REPROCESSING

<u>design parameters</u>	<u>units</u>	<u>core value</u>	<u>blanket value</u>
fuel composition		$\text{PuO}_2\text{-UO}_2$	UO_2
cladding material		316 s.s.	316 s.s.
fuel enrichment when charged to reactor (Pu-239 + Pu-241)/U + Pu	%	20.8	0.3 U-235 (depleted U)
fuel enrichment when discharged from reactor (PU-239 + Pu-241)/U + Pu	%	16.8	0.2
average fuel exposure	Mwd/MTU	100,000	133,333 core equivalent
plutonium concentration in fuel			
charged to reactor Pu-239 + Pu-241	g/kg (U+Pu)	--	0
discharged from reactor			
Pu-239 + Pu-241	g/kg (U+Pu)	--	32.14
rated gross power level	mwt	2125	375
rated net power level	mwe		1000
reactor fuel loading, initial.	MT(U+Pu)	17.760	MTU 86.776 (19.970 including axial blanket)
total fuel discharged per initial fuel loading	MT(U+Pu)	15.510	MTU 84.076 (17.720 including axial blanket)
fuel management program			
new fuel		random, 1/3- core lots	out-in, 1/4- blanket lots
spent fuel processing		1/3-core lots	1/4-blanket lots
<u>operating parameters, set by industry</u>			
predicted plant operating factor	%		80, 90
shipping time, nuclear fuel material			
AEC to conversion site	days	20	20
conversion site to fabricator	days	20	20
shipping time, fabricator to reactor	days	20	20
shipping time, recycle scrap to AEC	days	20	20
shipping time, spent fuel			
reactor to chemical processing site	days	20	20
chemical processing site to AEC	days	*	*
throughput rates, new fuel processing plant			
conversion	MTU/mo.	4.0	4.0

* included in process lag time, spent fuel processing plant

	<u>units</u>	<u>core value</u>	<u>blanket value</u>
fabrication	MTU/mo.	4.0	4.0
scrap recovery	MTU/mo.	**	**
process lag time, new fuel processing plant			
conversion	days	15	15
fabrication	days	45	45
scrap recovery	days	***	***
process lag time, spent fuel processing plant			
separation	days	30	30
conversion	days	5	5
time interval between delivery of fuel batch to reactor site and charging to reactor	days	30	30
spare fuel maintained on hand at all times, exclusive of discrete charging batches	MTU	0.592	1.0
batch size charged to reactor per refueling	MT(U+Pu)	5.92	MTU 21.694
batch size discharged from reactor per refueling	MT(U+Pu)	5.17	MTU 21.019
number of discharge batches accumulated for chemical processing campaign. . .		1	1
irrecoverable losses during conversion of UF ₆ to UO ₂	%	1.0	1.0
irrecoverable losses during fabrication	%	/	/
conversion and fabrication, recycle to AEC	%	10.0	10.0

operating parameters, set by AEC

minimum decay cooling period for irradiated fuel	days	120	120
irrecoverable loss during chemical separation, U	%	1.0	1.0
irrecoverable loss during chemical separation, Pu	%	1.0	1.0

- ** included in throughput rates, new fuel processing plant, conversion
 *** included in process lag time, new fuel processing plant, fabrication
 / included in irrecoverable losses during conversion of UF₆ to UO₂

	<u>units</u>	<u>core value</u>	<u>blanket value</u>
irrecoverable loss during conversion, U .	%	///	///
irrecoverable loss during conversion, Pu .	%	#	#
chemical separation plant, processing rate	MTU/day	0.206	1.0
chemical separation plant, turnaround time	days	8	8
uranium conversion plant, processing rate	MTU/day	1.0	1.0
plutonium conversion plant, processing rate	kg Pu/day	--	--

economic parameters, set by industry

new fuel processing costs (exclude shipping, use charges and losses for nuclear fuel material)			
conversion	\$/kg U	##	##
fabrication	\$/kg U	350	50
scrap recovery	\$/kg U	##	##
shipping charge, nuclear fuel material			
AEC to conversion site	\$/kg U	1.50	1.50
conversion site to fabricator	\$/kg U	1.50	1.50
shipping charge, fabricator to reactor	\$/kg U	1.50	1.50
shipping charge, recycle scrap to AEC	\$/kg U	1.50	1.50
shipping charge, reactor to chemical processing site	\$/kg U	16.00	16.00

economic parameters, set by AEC

use charge rate, nuclear fuel material .	%/year	4.75	4.75
uranium price at enrichment prior to irradiation (as UF ₆)	\$/kg U	2.50	2.50
uranium price at discharge enrichment (as UF ₆)	\$/kg U	2.50	2.50
conversion charge, UNH to UF ₆	\$/kg U	5.60	5.60
conversion charge, Pu nitrate to Pu metal	\$/g	delivered to AEC as nitrate solution	
Pu price (credit)	\$/g	10.00	10.00
shipping charge, chemical process site to AEC receiving plants (U and Pu) . .	\$/kg	1.00	1.00
separations plant daily charge	\$###	18,926	18,926

- /// included in irrecoverable loss during chemical separation, U
included in irrecoverable loss during chemical separation, Pu
included in new fuel processing costs, fabrication
AEC projected escalation for first half calendar year 1964

*Same composition as core
 **Same composition as outer blanket

CORE MODEL FOR TWO-DIMENSIONAL CALCULATIONS

FIG. 4.1

COMPARISON OF SODIUM LOSS FROM VARIOUS CORE SECTIONS

FIG. 4.2

CORE NEUTRON SPECTRA

FIG. 4.3

FIG. 4.4

RADIAL DEPENDENCE OF SODIUM WORTH

FIG. 4.5

ENERGY DEPENDENCE OF SODIUM WORTH

CORE AND BLANKET POWER DISTRIBUTION

FIG. 4.6

CHANGE IN CORE AND BLANKET EXIT TEMPERATURES

FIG. 4.7

INFLUENCE OF ORIFICING ON HOT-CHANNEL COOLANT EXIT TEMPERATURES

FIG. 4.8

AXIAL POWER PROFILE

FIG. 4.9

FUEL PIN TEMPERATURE DISTRIBUTION AT RADIAL FLUX PEAK
- NOMINAL

FIG. 4.10

FUEL-PIN TEMPERATURE DISTRIBUTION AT RADIAL FLUX PEAK-HOT SPOT
(beginning of fuel cycle, hot-spot factor multiplied)

FIG. 4.11

INFLUENCE OF INLET NOZZLE ON HYDRAULIC HOLDDOWN FORCE FIG. 4.12

INFLUENCE OF INLET NOZZLE ON REQUIRED PLENUM PRESSURES

FIG. 4.13

SODIUM HEAT TRANSFER CHARACTERISTICS

FIG. 4.14

THERMAL CONDUCTIVITY OF UO_2

FIG. 4.15

FUEL-PIN TEMPERATURE DISTRIBUTION AT RADIAL FLUX PEAK-HOT SPOT
(beginning of fuel cycle, hot-spot factor multiplied)

FIG. 4.16

MAXIMUM FUEL TEMPERATURE AS INFLUENCED
BY HOT-SPOT TREATMENT

FIG. 4.17

AVERAGE CLADDING TEMPERATURE AS
INFLUENCED BY HOT-SPOT TREATMENT

FIG. 4.18

- a. hot spot factors multiplied
- b. hot spot factors statistically combined
(99.7% confidence limit)
- c. nominal condition
- d. coolant temperature

RELATION OF STRESS, FUEL LIFETIME, AND CREEP

FIG. 4.19

FUEL CYCLE FLOWSHEET - ON-SITE REPROCESSING

FIG. 4.20

EFFECT OF FUEL EXPOSURE ON FUEL CYCLE COST
- ON-SITE REPROCESSING

FIG. 4.21

EFFECT OF EX-REACTOR INVENTORY ON
FUEL-CYCLE COST - ON-SITE REPROCESSING

FIG. 4.22

EFFECT OF REFUELING BATCH SIZE
ON FUEL-CYCLE COST -- ON-SITE
REPROCESSING

FIG. 4.23

EFFECT OF FUEL EXPOSURE ON FUEL CYCLE COST
- OFF-SITE REPROCESSING

FIG. 4.24

EFFECT OF RADIAL BLANKET FUEL EXPOSURE
ON FUEL-CYCLE COST -- OFF-SITE REPROCESSING

(core fuel exposure constant at 100,000 Mwd/MTM)

FIG. 4.25

FIG. 4.26

5. REACTOR SYSTEMS

5.1 CORE ASSEMBLIES

5.1.1 General

The locations of each of the assemblies are shown in Fig. 3.1. They are discussed in detail in the following sections.

5.1.2 Fuel Assembly (Fig. 5.1)

The fuel assembly consists of a hexagonal support can containing 123 fuel pins. Each pin consists of a Type-316 stainless-steel tube, 0.300 in. in outside diameter with a wall thickness of 0.028 in. and approximately 5 ft long, which contains 48 in. of UO_2 - PuO_2 core fuel, 6 in. of depleted UO_2 lower-axial-blanket fuel and has a 6-in. gas reservoir adjacent to the lower end closure.

The extruded and sintered mixed-oxide core fuel pellets are 0.241 in. OD by 0.100 in. ID by 2.85 in. long. The central void accommodates fission-gas and allows for volume change in case of meltdown which may occur during severe transients. To prevent reactivity insertion due to fuel slumping during reactor operation, each fuel pellet is located within the fuel pin by tungsten segmenting discs which are fixed within the cladding tube on 3 in. axial centers. The 6-in. column of lower-axial-blanket depleted UO_2 pellets have a segmenting disc only at the ends of the blanket region. A hydraulic holddown nozzle is located at the lower end of the hexagonal support can. The nozzle assembly includes a backup mechanical holddown latch which is actuated by a central rod connected to the handling fitting located at the upper end of the fuel assembly. If the latch should become inoperable, the fuel-removal equipment can apply sufficient force to yield the latch internals and free the assembly from the grid plate. A sodium reflector region is incorporated in each fuel assembly by extending the support can 24 in. above the core fuel. The fuel-pin bundle is rigidly positioned on a 0.375-in. triangular pitch and supported within the support can by a structure consisting of six spacer grids located on 12 in. axial centers by a set of three tie rods and spacer sleeves at alternate corners of the fuel-pin bundle. The fuel-pin spacer grids are similar in construction to those used in the Fermi Core A fuel assemblies. However, to minimize the reduction in coolant flow area in the grid regions each grid consists of two half sections in which the positioning slats are located between alternate rows of fuel pins. Each grid provides six point support for each pin.

The fuel pin and support structure are fixed within the assembly by tie wires which pass through a row of fuel-pin end plugs, a set of ferrules which are an integral part of the lower spacer grid, and the support-can wall. Core spacer pads are located on the six corners of the support can at the core midplane elevation and the core top elevation. When the reactor is at operating temperature the inner blanket, core, and outer blanket assemblies are held rigidly between the inner and outer core support structures by the pads.

5.1.3 Blanket Assembly (Fig. 5.2)

The blanket assembly employs the same basic construction features used in the fuel assembly. It consists of a hexagonal support can containing 37 blanket elements. Each blanket element consists of a Type-316 stainless-steel tube, 0.607 in. in outside diameter with a wall thickness of 0.015 in. and approximately 6 ft long, containing 72 in. of 0.574-in. diam. depleted UO_2 pellets. Because the power level and plutonium content of the blanket elements are low, the likelihood of fuel slumping is negligible and therefore no pellet segmenting discs are required. A simple transition piece and conical support nozzle is located at the lower end of the support can. The upper end of the support can extends approximately 6 in. above the blanket region and contains a number of penetrations to engage the fuel handling grapple. The blanket-element bundle is positioned on a 0.690-in. triangular pitch within the support can by four spacer grids located on 24 in. axial centers. Each of the support grids is welded to the support can after the blanket-element bundle is inserted. The blanket elements themselves are held within the support can by tie wires similar to those employed in the fuel assembly. The spacer grids are also of similar construction to those used in the fuel assembly. Core spacer pads are located on the six corners of the support can at the core midplane elevation and the core top elevation and hold the inner blanket, core and outer blanket assemblies rigidly between the inner and outer core support structures when the reactor is at operating temperatures.

5.1.4 Control Assemblies

5.1.4.1 Shim and Regulating Assemblies (Fig. 5.3). The shim and regulating assemblies are similar in construction to the fuel assemblies. They consist of a hexagonal support can containing 85 pins. Each pin consists of two 4-ft-long Type-316 stainless-steel tubes, 0.300-in. in outside diameter with a wall thickness of 0.028 in. The upper tube is a poison section containing natural boron carbide pellets; the lower tube is a follower section containing depleted UO_2 pellets. One end of each of these tubes is closed identically with the fuel pins, while the other end of each has a half-round extension. These extensions are joined to form the pin.

A nose piece consisting of a transition section and a dashpot piston is attached to the lower end of the support can. The transition section contains slots to admit sodium coolant to the pin bundle. The support can extends above the pin bundle so that the top of the can is aligned with the top of the adjacent fuel assemblies when the control assembly is in its lowest position. The support can extension contains penetrations to engage the handling grapple and a female fitting for connecting the control assembly to the drive extension. The pin bundle is rigidly positioned on a 0.375-in. triangular pitch and supported within the support can by a structure consisting of nine spacer grids located on 12-in. axial centers by a set of six tie rods and spacer sleeves located at the corners of the bundle. The spacer grids are similar in construction to those used in the fuel assembly. The pin bundle and support structure is fixed within the support can by tie wires which pass through a row of the composite pin central joints, a set

of ferrules which are an integral part of the middle-spacer grid, and the support-can wall. The control assembly is positioned within the core structure by a hexagonal guide tube of the same size as the fuel-assembly support can. A cylindrical extension of the guide tube penetrates the grid support plate and contains a conical transition piece and the dashpot cylinder at its lower end. Coolant inlet orifices are located in the conical transition piece. The upper portion of the guide tube which terminates 6 in. below the top of the core fuel assemblies has penetrations to engage the handling grapple. The guide tube is connected to the grid plate by a breech-lock-type clamping device which permits removal and installation of guide tubes with the blanket shuffling equipment. The guide tubes have core spacer pads on each corner at the core midplane and core top elevations which locate them rigidly within the core structure when the reactor is at operating temperature.

5.1.4.2 Safety Assemblies (Fig. 5.4). The safety assemblies are identical to the shim and regulating assemblies except that the bottom of the boron carbide poison section is located 4 in. above the upper end of the depleted UO_2 follower section. This is accomplished by increasing the length of the oxide follower pin 3 in. and fixing the top of the UO_2 column with a segmenting disc.

Figure 5.5 gives details of pins for all assemblies.

5.2 REACTOR VESSEL AND INTERNALS (Figs. 5-6 and 5-7)

5.2.1 General

The reactor vessel and internals house the core and blanket assembly and contain the sodium coolant, directing its flow in proper proportions through the core and blanket assemblies, and an inert cover gas. A rotating-head provides direct overhead access to the entire core and blanket area through integral ports.

Design conditions are 200 psig with a maximum metal temperature in the vessel wall of 1000 F. Allowable stresses from Section VIII of the ASME Code have been used in the vessel evaluation. The sodium coolant enters the vessel at 950 F. Its mixed outlet temperature will be 1200 F. Thermal baffling and a small amount of bypass coolant at inlet temperature must be used to prevent the high temperature outlet sodium from contacting the pressure vessel or structural portions of the internals. The inner support cylinder is designed for an external pressure of 100 psig and the grid support plate for 100 psig differential pressure upward, both at a maximum metal temperature of 1000 F.

Type 316 stainless steel is used throughout the vessel, internals, and portions of the top closure plugs in contact with the coolant. Hardened inserts are provided for the plug bearing races, and low-alloy steel is used for the closure bolting rings and the top cover plates of the plugs.

Several distinguishing features of this arrangement as compared to previous sodium-cooled reactors should be noted. The coolant inlet nozzles have been placed above the core centerline in the same shell course as the outlet nozzles. This shell course has increased thickness to provide inherent reinforcement for the nozzles and the necessary stability to accommodate the coolant piping reactions. In view of the coolant downflow area between the vessel wall and the inner shell, the basic vessel shell is not stepped at its upper end and an internal support cylinder attached to the vessel wall is provided for the closure plug support bearing. Because of their massive size and the intentional absence of any integral features affecting operation or lifetime, the internals will not be removable. An internal support ring, located as closely as possible to the outlet nozzles to minimize thermally induced motions, permanently ties the vessel shell and the internal cylinder together. Thermal baffle arrangements and inlet coolant bypass flow are used to maintain this support area at or near inlet sodium temperature. The vessel shell extending above this internal support ring supports the entire complex and accommodates the temperature gradient between the lower vessel and ambient temperature in the region of the vessel support and closure flange. An annular gas-filled gap extending above the maximum coolant level insulates this portion of the vessel shell from all heat input except at its juncture with the internal support ring and promotes a more uniform axial gradient in the shell. The top full opening is closed by a top rotating shield plug and a loose bolting ring attached to the upper vessel flange. Jacking screws are arranged to raise the plug off its bearings and make up a static O-ring seal for pressure operation.

5.2.2 Description of Components

The reactor pressure vessel consists of three distinct cylindrical shell courses and a bottom elliptically dished head. The upper shell course is 20 ft OD by 1-3/4 in. thick,

and while a part of the pressure containment, serves principally as a support skirt for the entire complex. Its upper end terminates in a 21-ft OD closure flange for the vessel top closure. The middle shell course is 20 ft OD by approximately 3 in. thick and provides the 12 coolant nozzles (six 30-in. ID inlet nozzles and six 31-in. ID outlet nozzles). The internal support ring is considered a part of this shell course. The lower vessel shell course and the integral bottom closure head are 20 ft OD by 1-3/4 in. thick. They contain no openings or penetrations and have no major structural attachments. The interior of the bottom head is fitted with an inner pan structure which will catch and safely hold the core in the event of meltdown. The main vessel is approximately 25 ft from the top face of the closure flange to inside the bottom head plate.

As described previously, the top of the vessel is closed by a loose bolting ring attached to the top vessel flange to hold down the closure-shield plug. The bolting ring is 18 in. high by 21 ft OD and because jacking bolts are provided for rigging the plug, seals need be removed from the vessel only when replaced. Inflatable temporary seal rings between the vessel wall and the plug maintain the inert atmosphere in the vessel during primary seal maintenance. The stepped closure-shield plug itself consists of a welded shell with integral bottom and top end plates. The interior cavity is filled with pellet- and/or powder-type shielding after vessel erection. The basic shield plug is 7-ft thick and the bottom cover plate is the vessel closure head. Beneath this plate is a 1-ft-thick laminated layer of gas-filled thermal baffle plates which shield the plug from the high-temperature outlet sodium. A small (7-ft OD) rotating shield plug is eccentrically located in the large plug and is identical in construction and arrangement to the large plug. Access ports with stepped shield plugs are provided in the small plug and 48 control assembly drives penetrate both plugs in a pattern symmetric to the core plan.

The inner cylindrical shell serves as a flow baffle between inlet flow and the core-blanket area, supports the grid plate, and also functions as a thermal shield between the core-blanket area and the pressure-vessel shell. Above the core-blanket, this inner shell forms the outer boundary of the high-temperature coolant plenum. Thin plate thermal baffles are located inside this shell to attenuate the temperature difference existing across the baffle shell to acceptable limits. Six flared nozzles in the inner shell, fitted with thermal baffle liners, are provided to pass the outlet coolant through the inlet plenum to the vessel outlet nozzles. No structural connection exists between the inner shell nozzles and the vessel outlet nozzles. Coolant leakage at the juncture of the two sets of nozzles flows behind the nozzle thermal baffles and serves to force the temperature transition between the vessel and the outlet pipes further down stream from the vessel shell. A lateral support grid attached to the inner shell wall defines the outer boundary of the core-blanket area near the top of the blanket assembly support cans.

The bottom grid plate is approximately 10-in. thick and is permanently attached to the lower end of the inner shell. It supports and locates the core and blanket assemblies, and distributes inlet coolant flow to them. The positions provided for the fuel assemblies include an entrance cone for the Venturi nozzles on those assemblies. The

positions provided for the control assemblies include a removable guide tube extending upward to approximately the same height as the fuel assemblies, and an extension below the grid plate to accommodate the travel of the control assemblies. The lower portion of the extension serves as a snubber for the control assemblies. The guide tubes are rotated free of the grid plate holddown fasteners by the blanket shuffling manipulator and replaced by a handling sequence similar to fuel assembly replacement. A central cylindrical support structure on top of the grid plate defines the inner boundary of the annular core-blanket arrangement. Only enough coolant to cool the central structure will be channeled through it.

A secondary containment jacket is provided outside of the pressure vessel to contain the sodium in the event of leakage from the vessel. This outer vessel may also be used as a gas-heating jacket to aid initial heatups of the system. Thermal insulation for the reactor is attached to the exterior of the secondary jacket.

The entire reactor complex is supported from the underside of the vessel top closure flange. This flange rests upon a number of radially extending structural beams imbedded in the reactor service room floor. Shim plates and angle clips for holddown are installed between the vessel flange and the beams.

5.3 CONTROL ASSEMBLY DRIVES (Fig. 5-8)

5.3.1 Shim and Regulating Assembly Drives

The shim and regulating assembly drives provide reactivity control of the reactor to compensate for lifetime effects, operating Doppler, and power output control.

The shim and regulating assembly drive mechanism utilizes a ball nut assembly which is attached to the upper end of the control assembly extension shaft and is driven from above by a conventional gearbox-motor unit. The control assembly can be uncoupled from the extension shaft by inserting the coupling tool through the access plug in the gearbox housing. A pair of guide rollers running in recessed grooves in the housing wall prevents rotation of the ball nut assembly.

The position of the assembly is continuously transmitted to the control center by a stacked synchro transmitter.

5.3.2 Safety Assembly Drives

The safety assembly drives scram the reactor by rapidly inserting poison elements.

The safety assembly actuator is cocked by a ball nut assembly. After the actuator is cocked, the ball nut assembly, which is driven by a gearbox-motor unit, is returned to the full down position and the safety assembly extension shaft is supported by the magnetically actuated scram latch.

The gearbox-motor unit has a position indication transmitter and a full up indicator switch verified the cocked mode of the safety assembly.

The safety assembly is uncoupled from the extension shaft by inserting the coupling tool through an opening in the top of the scram latch magnet.

5.4 INSTRUMENTATION AND CONTROL

5.4.1 Nuclear Instrumentation and Control System

The nuclear instrumentation system consists of 18 information channels, 12 for power operation and 6 for startup. Reactor pressure and temperature are also monitored providing additional control and safety information.

The reactor design permits the use of a variable-sodium-flow, fixed-outlet-temperature control system. Control information is obtained from neutron flux level, reactor outlet temperature, and sodium flow rate.

Neutron flux information for the 18 channels is obtained from 21 neutron detectors, symmetrically placed near the periphery of the inner blanket. For increasing blanket neutron-capture efficiency and detector efficiency it may be desirable to install a graphite island in the central core region inside the inner core support structure. The nuclear core size and configuration require a control system different from the standard design. Since the center portion of the graphite island would be borated, the neutron flux presented to an individual detector is essentially a function of the average flux of the adjacent core sector. As a result, successful control of the reactor is highly dependent upon the flux coupling between the core sections.

To insure accurate flux measurement, each detector has an associated neutron flux wire. At periodic intervals the flux wire may be withdrawn from the instrument thimbles and counted. This gives the operator a check upon neutron detector information.

5.4.2 Instrument Thimbles

Twelve instrument thimbles extend down through the reactor vessel into the graphite island, providing access to the core for the neutron detectors. The thimbles are situated every 30 degrees around the periphery of the graphite. Non-borated graphite is used between the thimble and the reactor core to provide thermal neutrons for the detectors.

The double walled instrument thimbles are constructed of Type-316 stainless steel and the insulating space between the walls is filled with argon. A dry inert gas is forced through the interior of each thimble to cool the detector. Each thimble with a movable detector contains an instrument drive system for separately moving the neutron detectors and flux wires out of the core.

The exact size and position of the instrument thimbles is dependent upon neutron, gamma, and heat flux. Although a detailed study has not been conducted to determine the exact thimble position, the envisioned configuration places the center of the thimbles in a ring, 26 in. from the core center.

5.4.3 Neutron Detectors

During regular operation, neutron information is obtained by measuring the thermalized neutron flux in the graphite island. Twelve compensated ion chambers, located in six instrument thimbles, are used for power-range operation. The power-range instrument thimbles, which are located 60 degrees apart, each contain two fixed, compensated ion chambers; one chamber supplies neutron information for a linear power channel, the other for a logarithmic power channel. Each of the chambers is located directly adjacent to one of the six regulating assemblies. The detector is designed to operate under the following maximum ratings, 500 F and 10^{11} nv, thermal neutron flux.

Three proportional counters, positioned 120 degrees apart, are used during the first startup to measure the lowest decades of neutron flux. These detectors will be withdrawn by the instrument drive system before high-gamma-flux damage results.

Six fission detectors located every 60 degrees are used during subsequent startups to monitor the intermediate range of operation. For protection, the fission detectors are withdrawn from the reactor core as soon as power operation is attained. Three of the fission detectors share instrument thimbles and information channels with three proportional detectors.

5.5 REACTOR COVER GAS

Argon has been chosen as the cover gas for free sodium surfaces occurring in the primary system because its chemical inertness and atomic weight make it particularly suitable for this application. Previous experience with argon as a cover gas for sodium systems has been favorable. Its heavier-than-air properties assure some blanketing of sodium surfaces, even in open containers.

All primary system components are either hermetically sealed, to prevent loss of argon or entry of air, or are provided with seals that allow movement and yet maintain the integrity of the system. Although there is up to 50 psi argon pressure on the system during operation, this pressure is reduced to several inches of water equivalent during the refueling operation, when the valves are operated and large components are allowed to rotate. System gas volume during operation is very low, and an appreciable volume of gas exists only during refueling when the sodium level is lowered. Other than for possibly a failed-fuel detection system or for gas purges required on control-assembly drives, the cover gas is static during reactor operation. Circulation and clean-up during refueling would minimize the possible escape of gaseous fission products that may be present.

The gas-handling system, which includes storage tanks, compressors, filters, absorption beds, etc., would be similar to those proposed previously. It is intended for this equipment to be located outside of the reactor building as shown in Fig. 6.16.

GRID DETAILS

		DRAWING STATUS
NO.	TITLE	DWG. NO.
DESCRIPTION		

[illegible]

FIG. 5.2

FIG. 5.4

FUEL PIN

SAFETY PIN

SHIM & REGULATING PIN

RADIAL BLANKET ELEMENT

REV.	DATE	DESCRIPTION	BY	CHKD.

ISSUING STATUS

EXTENDING BACKLOG	DATE	STATUS

DESIGN ENGINEER	APPROVALS	DATE

DESIGN	APPROVALS	DATE

ALLIS-CHALMERS MANUFACTURING COMPANY 3000 BROADWAY HOBOKEN, N. J.	LFRDS PIN DETAILS
---	------------------------------------

REV. NO. 41-564-7-X01

REV.

FIG. 5.9

FIG. 5.7

FIG. 5.8

6. POWER PLANT

6.1 REACTOR BUILDING

Although it was not within the scope of the Design Study it was felt that it would be beneficial to do an equipment layout of the primary system and the reactor building to better establish the primary loop characteristics and to assist in the definition of problems associated with fuel handling. Figures 6.1 and 6.2 show the conceptual layout of a typical reactor containment building. It should be noted that a cylindrical steel building typical of reactor containment vessels has been used. No attempt has been made in this rough layout to establish the size of the building on the requirement for containment of any particular accident; it is drawn for pictorial purposes only and its dimensions established by equipment and piping requirements. An important feature of the building is the arrangement of the primary loops. Six recirculation loops have tentatively been chosen on the basis of equipment and pipe size. Although larger pumps and heat exchangers can be built (a much larger heat exchanger is now being developed by the AEC) it is yet to be established that this will result in maximum equipment reliability or system safety. It is likely that the capital costs associated with six loops will be higher than that associated with three or four loops. With the equipment layout as shown, it was assumed that the reactor vessel and each of the pumps and heat exchangers was anchored in the position shown. This seems reasonable at this stage of design since it is difficult to anticipate pipe reactions and external forces on the equipment due to the interaction of the primary and secondary heat-removal-loop piping. Thus, with the 30-in. recirculation pipe used, layout results in the need for rather large pipe expansion loops between various pieces of equipment. Fewer loops and resulting larger pipe diameter (42-in. minimum) would require a much more complicated, space-consuming piping arrangement if flexible expansion joints are to be avoided. In addition to the loop equipment, space has been provided for storage of large primary loop components and reactor equipment that may be radioactively contaminated and require storage space for activity decay.

The other two important features to be noted in these figures are associated with the fuel handling and reactor servicing system. A two-trolley polar crane has been provided. Its span and width is sufficient so that, for refueling, the main crane rails can be oriented in the direction of the fuel transfer tunnels and all subsequent operations carried out by only the movement of two trolleys. The arrangement of the fuel transfer tunnels is such that although the tunnels penetrate the diameter of the containment building, the building is designed with an internal blister such that the transfer tunnels are actually outside of containment. The valves on the fuel transfer tubes provide the containment barrier during plant operation.

Shielding and pipe penetrations have been arranged so as to avoid radiation streaming from penetrations. With a final plant design it could prove desirable, from a maintenance point of view, to isolate individual loops by shielding separation walls.

Heat tracing, probably electric, of the sodium piping will be required to prevent freezing of the sodium during initial startup and a long shutdown of the plant.

6.2 REACTOR FUEL HANDLING SYSTEM

6.2.1 General

The overall fuel handling system is designed to utilize conventional hardware and proven methods wherever possible. Reliance on precise indexing is avoided by lowering the sodium level in the vessel to provide direct viewing of in-core operations. Generous clearances in moving parts are permitted by utilizing lead-in-chamfers which permit grapples and casks to "find" positions. Use of the rotating plug system for indexing, combined with forced cooling in the cask, permits a direct removal cycle from the core to the initial step in the disposal cycle (wash cell) eliminating in-vessel transfer operations. Motions required to remove fuel consist of a direct vertical lift, horizontal travel, and a direct vertical discharge. Handling operations involved in refueling have been segregated into four functions. These are: (1) Removal of fuel from core, (2) Transfer of spent fuel from the reactor building and processing for storage, (3) Replacement of fuel, and (4) Shuffling of the blanket. To simplify and optimize equipment, separate components are provided for each of these functions. This also permits overlapping of the duty cycles to reduce refueling time and interdependence of equipment.

With the exception of the grapples, all equipment operates outside of the reactor vessel and may be readily serviced. As component drive mechanisms operate in essentially room environments, conventional equipment may be utilized and lubricated as required. No in-vessel mechanisms or structures are used. The direct-handling system described, reduces the overall height of the reactor vessel to the minimum required by hydraulic, shielding, and thermal requirements; simplifies vessel design (no offsets); and permits direct access (through gas locks) to all regions of the core and blanket.

6.2.2 Basis for Design

The basic design criteria for the fuel handling system are:

(a) Cooling is required to accommodate decay heating (approximately 60,000 Btu/hr) in order to avoid in-vessel decay storage of spent assemblies.

(b) The fuel assembly is handled in an inert atmosphere at all times until the wash-cell cycle is started.

(c) No contamination of the atmosphere within the reactor vessel is permitted. All penetrations will be sealed and all transfers of equipment and assemblies are made through gas locks.

(d) Direct visual observation of the core is required. The sodium level will be lowered to expose the handling fittings of the assemblies. Provision for core lighting and observation periscopes is required.

(e) Cover gas (at a pressure not to exceed 6 in. of water) shall not be permitted to escape into the containment building.

(f) The sodium temperature during refueling will be reduced to approximately 350 F.

6.2.3 Description of Refueling System

The basic components used for refueling the reactor are:

- (a) The fuel-removal cask.
- (b) The fuel-installation cask.
- (c) The plug-handling cask.
- (d) The access port valve.
- (e) The blanket-shuffling equipment.
- (f) The wash cells.
- (g) The rotating-plug indexing system (part of vessel).

6.2.4 Description of Components

Equipment is provided for handling fuel assemblies, blanket assemblies, and control assemblies. A double rotating plug top shield is used for indexing. Fuel assemblies are transported directly from the core to a wash cell by incorporating provisions for decay-heat cooling during transport. Major items of equipment are:

(a) Fuel Removal Cask (see Fig. 6.3). The fuel removal cask is used for removing spent fuel, blanket, and control assemblies from the core and transporting them to the wash cells. This cask consists of a shielded tube containing a telescoping, hexagonal guide tube suspended on a grapple which is raised and lowered by a chain hoist. The guide tube provides an unobstructed containment for the assemblies during removal from the core and directs the coolant gas flow through the assembly. In the event that the assembly should become lodged in the cask (severely distorted element, meltdown in cask due to coolant failure, etc.), the cask internals can be discharged into a service pit and new internals installed.

The lower end of the bottom guide tube is chamfered to permit it to align itself against the adjacent fuel assembly support cans. This centers the guide tube (and grapple) over the assembly to be removed. The guide tube, when fully down, rests against the 1/4-in. offset of the adjacent fuel assemblies and assists in holding these assemblies in position during removal.

The cask is sealed by a shielded valve on the lower end and by a dome enclosing the mechanism at the top. A bank of finned tubes surrounds the upper portion of the cask to serve as a radiator for cooling. A bail is provided for attaching the cask to an overhead crane.

A seal surface is attached to the valve by means of a high spring rate bellows. This permits a positive seal against the access port valve without resting the cask weight on the reactor plug or requiring precise indexing of the cask. (The cask is essentially a

large pendulum which may be deflected slightly by the chamfer on the port valve (approximately 1/4 in.) as it engages the access port valve.)

The grapple and guide-tube hoist assembly consists of an idler sprocket which positions the grapple centrally in the cask. This sprocket is supported on load cells to give a continuous read-out of grapple load. A drive sprocket is supported by a shaft which penetrates the top casing through a seal. Power is furnished by a conventional worm gearbox driven by an electric motor. An air motor is connected to the drive to provide emergency power in event of electrical failure. The air motor is connected to the compressed gas cylinder used for valve operation.

Electric power for the cask is tapped from the overhead crane and the cask controls are attached to the pendant type crane controller. A cylinder of compressed argon is carried on the cask for actuating the valve and for emergency grapple hoist operation.

Interlocks are provided to prevent accidental movement of the crane during unloading, to prevent opening of the lower valve when the cask is not sealed to the wash cell or access port valve, and to limit downward travel of the cask. Continuous read-out of lifting loads on the grapple is provided by use of a load cell mount on the idler sprocket.

Continuous argon circulation through the assembly is provided by two Rootes type blowers mounted above the radiator section and discharging into the cask cavity. The guide tubes direct the coolant flow through the assembly. Two methods of circulation are incorporated. While the guide tube extends into the core, the blowers will be drawing cover gas from the reactor vessel through the radiator section and returning through the assembly. When the guide tube is retracted and the lower valve closed, the blowers will circulate argon within the cask. The coolant gas sweep will assist the draining of the sodium from the assembly as it is removed from the core.

Some of the experience with the cask-removal cart and handling pots at Fermi indicates that the residual sodium on the fuel (or blanket) assemblies could be as much as 1/2 lb and that a portion of this will drip off. If this is true, it may be necessary to install a pan to catch this sodium drip.

Figure 6.4 shows a concept for incorporating such a pan. Since the cask temperature is considerably above the melting point of sodium, the sodium will be liquid in the catch pan. When the cask is ready to discharge the assembly into the steam-cleaning unit, the catch pan is first slowly tilted, using the power cylinder; the liquid sodium flows toward the spout, which is shaped so as to direct the sodium down into the open entrance to the cleaning unit; and most of it will fall into the catch basin in the foot valve at the bottom of the cleaning unit. During the cleaning cycle, it will react with the steam and water and be removed.

The drip pan has an electric strip heater attached to its underside for heating. This will be used for warmup and for emergency use if sodium should solidify in the pan.

The cask is indexed to the access port valve by locating keys. These keys provide a tapered lead-in to force the cask to rotate (on the crane hook swivel) into position as it is lowered over the pre-set access port valve.

(b) Fuel Installation Cask (see Fig. 6.5). The fuel installation cask is a lightly shielded tube containing a grapple attached to a push rod which is driven through rack and pinion gearing to obtain a stroke of 26 ft. The cask is sealed at the lower end by a valve and capped at the upper end. The lower end seats on the access port valve to seal. A hose reel supplies argon to the grapple through a flexible metal hose. This cask is handled by the auxiliary crane. Power leads and controls are similar to those used with the Fuel Removal Cask.

(c) Plug Handling Cask (see Fig. 6.5). This lightly shielded cask is used for removing access plugs and replacing them with plugs incorporating lighting, viewing, or other equipment. The cask consists of a shielded tube with a valve on the lower end and a gas-tight enclosure containing a commercial chain hoist on the upper end. Storage facilities are provided inside the building for the plugs when not in use. During setup, this cask is only used for refueling and is handled by the auxiliary crane.

(d) Access Port Valve (see Fig. 6.5). This valve is fastened to the smaller of the two rotating plugs during setup for refueling and forms the lower half of a gas lock when the fuel removal or installation casks are in place. The valve rotates on its base to provide angular indexing of the cask. The bearing race is relieved to permit the valve to "bottom-out" and relieve the load on the balls in the event that the fuel removal cask is accidentally lowered to rest on the valve. The upper part of the valve provides a tapered guide for landing the fuel removal and replacement casks and dual "O" rings for sealing. An air cylinder is used for opening and closing the valve. This cylinder is pneumatically interlocked to prevent opening the valve unless a cask is in position. The valve is manually rotated and locked to establish proper cask orientation over the assembly.

(e) Blanket-Shuffling Equipment (see Fig. 6.5). This equipment is basically the same as the fuel replacement cask. The stroke (14 ft) is shorter as blanket assemblies are not removed from the core during shuffling. The grapple is shielded to prevent streaming. The grapple drive and cask valve are the same as the fuel replacement cask. A lightweight unshielded, rotatable valve is provided for attachment to the access port in the smaller plug. The shuffling equipment is bolted directly to this valve during use.

(f) Wash Cells and Equipment (see Fig. 6.6). Spent assemblies are discharged directly into a wash cell upon removal from the core to eliminate the need for intermediate sodium-filled storage pools. However, before spent fuel elements can be transported to the fuel-storage pools, residual sodium clinging to their surface must be removed.

Although sodium reacts with air to form sodium oxide and air is readily available, the conventional reactant for cleaning sodium from surfaces is steam.

The above reactions show that:

(1) Over twice as much heat per mole Na is generated when sodium reacts with oxygen (air) than when it reacts with steam.

(2) Na_2O is the intermediate product in the air oxidation of sodium because sodium hydroxide would be formed during any washdown or subsequent wetting. This material, Na_2O , is very corrosive and any particles not reacted with water may cause corrosion in the system.

To minimize the chemical reaction heat release and attendant thermal problems, air must be excluded from the steam-cleaning facility during the sodium cleanup cycle.

To reduce the thermal problems caused by the heat release in the Na-HOH reaction, the initial steam will be diluted with inert gas. Argon is proposed for the diluent for compatibility with the fuel transfer cask coolant. An economic and technical evaluation of the use of alternate non-reactive gases (such as nitrogen) will be considered in the further design studies. For the purposes of this discussion, however, only argon has been considered.

The wash cells are two-piece assemblies with the upper chamber containing inlet lines for steam, drying air, water, and argon used in the cleaning cycle. The upper chamber is fixed in the building floor inside the containment building and closed with a shielded valve similar to the access port valve. The lower chamber of the cell is mounted on a carriage which travels into the storage pool. The two halves are joined by raising the lower chamber (with water operated cylinders) to engage a shielded, plate-type seal (Gask-O-Seal) which is replaced as a unit. An endless cable system, with drum attached, at the pool end of the canal is attached to the carriage. All maintenance on the lower chamber and carriage is done in the pool area while the upper chamber is taken into the containment building for replacement. Steam, air, and gas inlets are located in the upper chamber and connected to building services through the fuel-transfer tunnel in order to limit containment penetration to the shielded valve. A slightly different alternate system for cleaning is described in the Appendix.

(g) Rotating Plug Index System (see Figs. 6.7 and 6.8). The rotating plugs and the access port valve are used to index the access port over the fuel or blanket assembly to be moved and to align the hexagonal grapples and guide tubes for entry. The plugs are stainless steel shells which are filled with shielding materials. Closure bolting rings are used to provide operational seals. A modified Wilson seal is used to contain core cover gas during refueling and inflatable seals are provided to permit replacing operational and refueling seals.

The plugs rotate on loose balls (similar to commercial turntable construction). During power operation, the weight of the plugs is removed from the balls to prevent creep and Brinelling damage. The lower-ball races incorporate "trash racks" and V-grooves to allow any foreign materials to be cleared from the races by the balls. The limited speeds, infrequent use, and absence of long-term static loading on the balls should result in unlimited service life for these components. Should emergency replacement become necessary, loading holes may be drilled into the races from above, the balls replaced individually, and the holes plugged.

The rotating plugs are driven by axial piston air motors driving pinions through reduction gears. The pinions engage an internal gear formed by segments bolted to the closure bolting rings. The use of multiple drives cancels the lateral forces on the plug. Guide rollers are installed to assist in holding the plugs centered.

Ports are provided for the installation of lighting and viewing equipment. These ports are plugged during operation. Penetrations are provided for safety-, shim-, and regulating-assembly drives.

The closure bolting rings are radially scribed and a pointer is fixed to the corresponding rotating member. The plugs are indexed visually by the operator as precise indexing is not required. Minor indexing errors are compensated for by the self-centering action of the guide tube and major errors may be corrected by the cask operator prior to final travel of the guide shroud.

New-fuel storage positions are provided in the containment building.

Replacement fuel will be preheated to approximately 400 F before insertion into the core. This preheating operation will be carried out either in the fuel cycle facility or in the fuel storage positions in the containment building.

6.2.5 Refueling Operation Preparation

The following operations are required to ready the reactor for refueling.

(1) The reactor is shut down, cooled to approximately 350 F, the cover gas flushed and reduced in pressure to approximately 5 in. of water (gauge).

(2) Safety and control assemblies are driven to the full-in position and the drives retracted to clear the core.

(3) All casks are evacuated and backfilled with argon.

(4) The access port valve is installed and the access port plug removed. (Valve is left closed and plug stored.)

(5) Valves are installed over additional plugs which are removed and replaced with observation and lighting plugs as required (wash cell valves may be used).

- (6) The wash cells are positioned, trial cycled, and readied.
- (7) The fuel removal cask is attached to the 125-ton building crane.
- (8) The fuel replacement cask is attached to the 30-ton building crane.
- (9) Replacement fuel is preheated to 400 F.

(10) The rotating plugs are lowered onto their balls by removing the jacking screws from the cover flanges. (Install the hollow ram jacks on the alternate studs, remove intermediate jacking screws, lower plugs with the jacks and finally remove the jacks and studs.)

(11) Attach power leads (air and electrical) to the plug drive motors, access port valves, etc.

6.2.6 Fuel- and Blanket-Handling Procedure (see Figs. 6.6 and 6.9)

To remove a fuel assembly, the rotating plugs and access port valve are turned to their predetermined azimuth locations. The cask (purged and backfilled with argon) is positioned over the valve and lowered to the proper elevation. (A limit switch stops the crane before the cask bottoms-out on the valve.) The space between valves is purged and the cask and access valves are opened. The guide tube and grapple are lowered as an assembly into the core. The guide tube orientation with the assembly to be removed is established by the six adjacent assemblies. The operator may visually verify position before lowering the grapple into engagement. The grapple engages automatically and the assembly is then hoisted into the guide tube. (The coolant blowers are energized as the assembly clears the core.) Hoisting is continued until the upper limit switch stops the hoist. Cask and access port valves are closed and the cask is moved to the wash cell.

During the time the spent assembly is transported to the wash cell and discharged into the cell, and the cask is returned to the reactor, a new assembly is installed (using the fuel replacement cask) and the plugs and valve are re-oriented to the next assembly to be removed.

Blanket shuffling requires that one blanket assembly be removed and placed in a storage position. The rotating plugs and the blanket shuffling mechanism are then positioned over the nearest assembly to be shuffled and the push rod is extended until the grapple engages. The assembly is retracted into the plug until it clears the core. The plugs and access valve are then positioned over the empty channel and the assembly lowered into position. Shuffling should be so programed that minimum plug rotation is required to minimize shuffling time.

Fuel assemblies are washed by flushing with dry steam, wet steam, and water in that order. Following cleaning, the wash cell lower chamber is lowered to clear the upper

chamber and the cell and assembly are withdrawn into the storage pool. The assembly is then removed and placed in a storage rack. The lower chamber is repositioned under the upper chamber and sealed. The cell is then drained of pool water, dried with an air blast and backfilled with argon. Two wash cells are provided to be used alternately. The wash and transfer cycle starts as soon as the removal cask starts its return to the reactor and terminates in time to receive the second following assembly.

6.2.7 Refueling Time Study

An 8-month refueling cycle has been selected which requires the replacement of 166 fuel assemblies, 69 inner-blanket assemblies, and 146 outer-blanket assemblies and the shuffling of the remainder of the blanket assemblies. The time requirements for specific functions are given below.

- (1) Approximately 24 hr will be required to bring the plant down from power, cool the primary sodium system, depressurize the primary system, and clean up the primary cover gas.
- (2) The setup time in the containment for the refueling components and preparation of the shielded head will be less than the 24 hr required for plant shutdown.
- (3) Twenty minutes are required for spent assembly removal, transport and discharge to the wash cell, and return of the cask to the reactor.
- (4) Less than 20 min are required for new fuel insertion and reindexing of the shield plug.
- (5) The individual wash-cell cycle is approximately 30 min, so two wash cells have been provided to avoid extending the refueling period.
- (6) An average time for assembly shuffling of 8 min has been estimated.
- (7) It was assumed that startup would require approximately 24 hr.

TABLE 6-1
SHUTDOWN TIME SUMMARY

	<u>time, hr</u>
Reactor shutdown	24
Replacement of 166 fuel assemblies, 69 inner-blanket assemblies, and 146 outer-blanket assemblies at 20 min each	127
Shuffle 643 blanket assemblies at 8 min each	86
Reactor startup	<u>24</u>
Total	261

6.3 FUEL-CYCLE FACILITY

6.3.1 Fuel-Cycle Description

The integrated reprocessing plant concept has been considered for application to the fast reactor for a number of years (EBR-II, KAPL Fast Oxide Breeder Program, FCR program) and has entered the stage of pilot plant demonstration for the oxide fuels through the A-C/Italian/USAEC PCUT program. It is interesting to note that the core fuel reprocessing capacity required for the 1000 Mwe mixed-oxide-fueled fast reactor is almost identical with the ultimate capacity capability of the PCUT demonstration plant. Thus, the detailed design and cost estimating experience of the PCUT program provides a very sound basis for estimating the costs of this integrated fuel cycle facility.

As discussed in Sec. 4.4, an integrated fuel cycle for the large fast reactor has been shown to be economically advantageous and also to provide the power-plant operator with simplified control of the fissionable material in the system through the elimination of spent-fuel shipping and the withdrawal of large quantities of fissionable plutonium from the fuel agency. This section describes the fuel-cycle operations, the facility in which the fuel reprocessing and refabrication is accomplished and also presents the basis upon which the integrated fuel-cycle costs have been estimated.

Because of the high burnup obtainable the mixed-oxide fuel in the fast reactor core, the throughput of the core fuel is very small compared with the capacity of centralized fuel-reprocessing facilities. This low throughput introduces two problems:

- (1) A very large installed fast reactor is required to support a reprocessing plant of nominal 1 ton/day capacity.
- (2) Small on-site reprocessing facilities, because of their low throughput, must suffer some unit-cost penalty.

The problem of accumulating sufficient reprocessing load from fast reactors for a centralized plant is even more difficult than accumulating a similar load from thermal reactors since the discharged fast-reactor fuel is enriched to approximately 15 percent in fissile plutonium isotopes. Assuring safe shipment of such fuel (from a criticality point of view) may appreciably increase the volume of shielding required per unit of spent fuel over that which can be achieved with thermal reactor fuel. The unit-cost penalty associated with the lower processing capacity of the integrated plant is real in the case of the fission-product decontamination and fuel-reconstitution portions of the fuel-cycle operation. However, the single-purpose design of the head-end and fuel-reconstitution equipment tends to minimize this cost penalty. Furthermore, a much larger fraction of the unit fuel-cycle cost is represented by the fuel-fabrication than the reprocessing operation. The single-purpose design of fabrication portion of the integrated facility permits large unit-cost reductions. This reduction effect is further enhanced by the inability of independent fuel-manufacturing

facilities to increase capacity in any fashion other than increasing the number of fuel-fabricating lines because of criticality control limitations.

The annular core geometry of the large fast reactor in which the axial blanket is only a small fraction of the total blanket has two important advantages:

- (1) processing of the axial-blanket fuel with the core fuel can be done with only about a 10 percent plant capacity increase in the core-fuel process,
- (2) the large volume of radial-blanket fuel in which most of the breeding gain is realized can be processed at an economically optimum rate rather than at a rate controlled by the core burnup limitation since it is mechanically separable from the core fuel.

An important blanket-plutonium-recovery advantage accrues to the integrated fuel cycle approach since a very compact anion exchange plutonium recovery process can be incorporated in the core reprocessing facility with a relatively modest increase in capital cost. This results in plutonium-blanket-recovery unit costs which are equal to or less than those which can be obtained by shipping the blanket material to an off-site centralized reprocessing facility. Since the blanket plutonium is recovered as a nitrate solution the bred material can be introduced directly into the core fuel recycle process and only the net plutonium breeding gain requires further processing for sale to the fuel agency.

The depleted blanket uranium is not separated from the fission products by the anion exchange plutonium-recovery process. Recovery of the low value depleted uranium is not economically justified. However, by storing it as liquid waste the material would be available for recovery and use in blanket fabrication at any time when such an operation would be less expensive than preparation of UO_2 from diffusion plant tailings. This mode of fuel cycle operation is therefore economically attractive to the power plant operator and yet consistent with conservation of fuel resources.

6.3.2 Design Basis

The recovery and reuse of fissionable material from spent-fuel assemblies involves several chemical and mechanical processes which are made more difficult because of the high radioactivity of the spent fuel. Remote handling is therefore required in all the processing steps, not only because of the fission-product activity but because of the radioactivity associated with uranium-237, plutonium-238, plutonium-240, plutonium-241, and plutonium-242. Each step in the chemical process must be designed to preclude the possibility of criticality, and both the mechanical and chemical processes must be carried out remotely, from behind shielding walls.

The fuel cycle plant has been designed for a nominal instantaneous capacity of 30 kg of uranium-plutonium oxide core fuel and 100 kg of depleted UO_2 radial-blanket fuel

per day. Assuming an 0.8 plant factor spent fuel is discharged from the reactor at a rate of 1/3 core (6711 kg mixed oxide, 166 assemblies) every 348 days. Radial-blanket spent fuel is discharged at a rate of 1/4 blanket (24,652 kg oxide, 215 assemblies) every 348 days. The core fuel assemblies can be reprocessed and refabricated in 250 operating days. The blanket fuel can be reprocessed, makeup plutonium returned to the core fuel process, and the excess plutonium repurified and sold in the same operating period. The plant design departs in three ways from that of existing radiochemical plants. First, since the core fuel throughput is small, it is possible to rely on geometric criticality control. The small size of the plant also permits a maintenance and service technique based on rack-mounted units with quick-disconnect couplings. The second major departure from conventional radiochemical processing technology is the integration of fuel recovery and refabrication operations. If the fuel is to be recycled, the advantage of this integration seems obvious. Separation of these functions would only be sensible if a large-capacity plant were required. The third departure from conventional design results from the less severe requirements for fission-product decontamination of the process materials. In the plutonium cycle, remote fabrication is required owing to alpha and neutron activity. Thus, since some degree of remote fabrication is necessary in any case, there is no need to remove all fission products from the fuel material. This permits a considerable simplification in the process and plant design.

In existing plants spent fuel material would be put through three solvent extraction cycles to remove essentially all fission-product activity, and fresh fuel would be fabricated by direct manipulation in glove boxes. In this recycle plant, only one solvent extraction cycle is used, and the fresh fuel is fabricated in a shielded warm cell. Detailed design and construction procurement of the Italian PCUT demonstration plant has shown that the cost penalty incurred when using a warm cell rather than a lightly shielded glove box facility is small compared to the large simplification and cost savings realized in the design and operation of the plant using only one solvent extraction cycle.

An anion-exchange plutonium separation process developed at the Hanford Laboratory lends itself to the compact rack-mounted equipment design approach employed in the hot cell. The addition of only three hot cell racks, blanket fuel disassembly and chopping machines, enlargement of the spent fuel storage pool, and the addition of another set of waste storage tanks permits on-site blanket plutonium recovery. The plant capital and operating cost increments required for this process addition are small and therefore result in a low blanket processing unit cost.

6.3.3 Process Description

6.3.3.1 Core and Axial Blanket Fuel. The fuel reprocessing sequence consists of four major phases: disassembly, recovery of the fuel material, reconstitution of the recovered fuel material, and fabrication of a new assembly. The reprocessing sequence is represented schematically in Fig. 6.10.

6.3.3.1.1 Disassembly. After steam cleaning the spent fuel assemblies are moved through the fuel transfer tunnel to the fuel-cycle-building storage pool. In an isolated section of the pool, a remotely-operated disassembly machine cuts off the structural fittings and separates the individual fuel pins from the spacer-grid support structure. The pins are loaded into a shielded magazine and carried from the pool to a chopping mechanism above the chemical processing cell.

6.3.3.1.2 Recovery of Fuel Material. The chopping mechanism cuts the pins into short segments that fall into a dissolver tank where the fuel material is dissolved in a highly concentrated nitric acid solution, leaving the empty segments of the stainless-steel tubing. The resulting nitric acid solution containing fission products and the nitrates of plutonium and uranium is adjusted so that its concentration is suitable for the next step. The properly adjusted solution is then pumped into a mixer-settler device where the plutonium and uranium are separated from the fission products by a modified Purex type solvent extraction process. A single extraction cycle provides sufficient fission product decontamination, since some of the uranium and plutonium isotopes in the recycled fuel contribute a significant residual source of activity.

The aqueous product solution from the mixer-settlers is concentrated and then adjusted by adding plutonium nitrate solution from the blanket plutonium recovery process, to ensure the proper plutonium-uranium ratio. Ammonium hydroxide is then added to the adjusted product solution, precipitating plutonium hydroxide and ammonium diuranate. The resulting slurry is centrifuged to remove the supernatant liquid. The plutonium hydroxide-ammonium diuranate which are then passed through a hydrogen atmosphere calcining furnace where the volatile materials are driven off and the dioxides of uranium and plutonium are formed. These mixed oxides of uranium and plutonium are the end product of the fuel recovery phase. In the next phase, this material is formed into pellets for use in the fuel assemblies.

6.3.3.1.3 Reconstitution. The dry mixed oxides of uranium and plutonium are passed through a "Mikro-Pulverizer" to break up any aggregates formed during the calcining step. The resulting oxide powder is blended with water, an organic binder, and a plasticizer in a Muller-Mixer prior to loading the resulting "clay" into an extrusion press. There the blend is extruded in 3 ft lengths, which are air dried and cut to pellet length. The "green" pellets are then sintered at temperatures up to 1700 C in a hydrogen atmosphere furnace. The extrusion process is sufficiently reproducible so that no pellet grinding step will be required. The sintered pellets are finished by squaring the ends on a cutoff wheel. An automatic inspection device incorporating pneumatic gauges and an electromagnetic balance will permit continuous measurement of dimensions and continuous weighing of the finished pellets. Density calculations will be obtained from a simple computer circuit using these measurements as a continuous input.

6.3.3.1.4 Refabrication. Refabrication operations are simplified wherever possible by preassembly and inspection of structural components prior to their transfer into the warm cell. The finished mixed oxide pellets, virgin depleted UO_2 lower-axial blanket

pellets and the segmenting discs are loaded into new cladding tubes which have one end closure welded in place. The segmenting discs are then fixed by rolling the cladding into the groove on the outer edge of the disc. The loading device is designed so that the disc spacing is maintained throughout the rolling operation. The pin is then evacuated, backfilled with helium, the end plug inserted, and the final closure weld made. Groups of completed pins are helium leak tested with a mass spectrometer. A batch of 123 pins is then loaded into the preassembled spacer grid and tie-rod structure. The pin bundle is inserted into the support can-nozzle assembly and fixed in place by inserting tie wires through rows of end closures and the support can walls. The tie wires are secured by welding one end to the support can wall. The completed fuel assembly is inspected, cleaned and checked for surface contamination and transported to the storage pool in a lightly-shielded transfer cask.

6.3.3.2 Radial Blanket Fuel. The blanket reprocessing sequence consists of three major phases: disassembly, recovery of the plutonium, and processing and sale of the excess plutonium. Refabrication is not involved in the blanket fuel cycle since separation and fission product decontamination of the depleted uranium is not economical. The spent uranium will be stored in a waste tank farm so that if depleted uranium values should increase appreciably in the future the waste solution could be recovered and used as blanket material. Consideration should also be given to converting the uranyl nitrate solution to a solid form for storage, by denitration and acid recovery.

6.3.3.2.1 Disassembly. After discharge from the reactor, the spent blanket assemblies are steam cleaned and transferred to the fuel cycle building storage pool. A remotely operated disassembly machine similar to that used for the fuel assemblies cuts off the support-can structure and separates the blanket elements from the spacer grids. The elements are loaded into a shielded magazine and transported to the chopping mechanism located above the hot cell dissolver system.

6.3.3.2.2 Plutonium Recovery. As in the core fuel process, the chopping mechanism shears the blanket elements into short segments and charges the pieces directly into the dissolver vessel. The blanket material is charged to the dissolver every few hours. Dissolvent acid is added continuously and the solution of uranyl nitrate and plutonium nitrate overflowing from the dissolver is cooled to 60 C in a hold tank. The chemical composition of the solution leaving the dissolver is adjusted by the dissolvent and feed rate or feed concentration so that the dissolver product is at the proper concentration for the anion-exchange, plutonium-recovery process.

The dissolver product containing uranium, plutonium, and fission products flows into one of the two anion-exchange columns where the plutonium is preferentially adsorbed by the anion-exchange resin. The stream leaving the column contains uranium, fission products, and a small amount of plutonium, which may be lost toward the end of the adsorption cycle. When one column is loaded with plutonium the feed is diverted to the other column. The first column is then washed to remove residual uranium and fission products. Following the wash cycle the plutonium is eluted from the column and stored temporarily in the plutonium catch tank.

The liquid waste from the process consists mainly of the uranium stream and acid wash. It has been conservatively assumed that all this waste is stored without further treatment in cooled stainless-steel tanks. However, the high cost of waste storage strongly suggests that in the actual plant, these nitric acid wastes should be recovered and reused in the dissolution of the uranium.

6.3.3.2.3 Sale of Excess Plutonium. The blanket plutonium in excess of that transferred to the core-fuel process stream as fissile material makeup is collected in a product-storage vessel. When a processing batch has been accumulated, the plutonium nitrate solution is further purified with a second anion-exchange cycle. By proper scheduling of plant operations this cleanup step can be done in the same equipment used for the primary plutonium recovery operation. The purified plutonium nitrate product solution is collected in critically safe shipping flasks and sold to the fuel agency (AEC).

6.3.4 Processing Equipment

The chemical processing equipment installed on the hot-cell racks is typical of that found in radiochemical plants employing geometric criticality control. The process vessels are fabricated from 4-1/2 in. diameter pipe, the mixer-settlers are built in a slab configuration, and in a few cases, fixed poison annular tanks are used. The warm cell equipment has been selected and designed for manipulator operation and in most cases standard pieces of equipment with modifications for remote operation have been used. The following sections briefly describe the equipment which cannot be made by modifying standard devices.

6.3.4.1 Disassembly Machine -- Fuel Assembly. The disassembly machine operates in the pool under 20 ft of water. The fuel assembly is clamped in place and moved forward to the circular milling cutters which are positioned to cut off the nozzle assembly and support can just below the lower spacer grid. The assembly is indexed and the support can is severed just above the lower support grid. The three tie rods are each cut at the same axial position. The fuel pin bundle is reclamped in the machine. The fuel bundle is then separated into individual pins by cutting through the lower spacer grid and pin end closures just above the tie wire region.

6.3.4.2 Disassembly Machine -- Radial Blanket Assembly. This machine is very similar to the device used for disassembling the fuel assemblies but requires only two cutting operations, the first of which severs the support can immediately above the lower spacer grid. The blanket element bundle is pulled free of the support can and attached spacer grids. The bundle is then reclamped and the second cutting operation separates the individual elements by cutting the end closures, grid, and support can immediately above the tie wires.

6.3.4.3 Chopping Machines. The chopping mechanisms are in the floor of the corridor above the hot cell. The transfer cask containing the fuel pins or radial blanket elements is moved into the corridor and placed over the appropriate chopping mechanism. - The fuel

pins are dropped one by one, from the magazine of the cask into the chopping mechanism. The chopping blade is actuated by a hydraulic unit which moves the blade in both directions. The chopped pieces fall through a pipe into the dissolver in the hot cell. Air leaks into the dissolver through the pipe at a velocity of 20 fps. A ball valve seals this pipe when the dissolver is operating.

6.3.4.4 Extrusion System. The basic units of the extrusion system are

- (1) the hydraulic cylinder, which provides the force to extrude the clay through the die;
- (2) the extrusion chamber, die, and die holder;
- (3) the hauloff device, which is synchronized with the extrusion rate by a servomechanism; and
- (4) the cutoff device, which ensures that green extrusions of the maximum allowable length are produced.

A slightly modified commercial piston-type extruder is the basic machine component. The hauloff and cutoff devices are specially fabricated components. The complete unit is mounted on a single bed to ensure proper alignment of the extrusion chamber with the hauloff device.

6.3.4.5 Sintering Furnace. Because most failures are likely to occur in the high-temperature section, the warm-cell furnace is arranged with the preheat and cooling sections mounted on moving dollies and the center (high-temperature) section is a tank that can be simply plugged into or unplugged from the furnace bed. A pusher system, which introduces the sintering boats through a side port, moves the boats through the furnace, and discharges them through a side port at the exit end, is mounted on the low temperature sections of the furnace. The high-temperature portion of the furnace is designed to be as inexpensive as possible. Thus, disposal, rather than repair, of failed units will permit a considerable savings in maintenance time without imposing excessive maintenance costs.

6.3.4.6 Pellet and Segmenting Disc Loading Machine. This machine is designed to:

- (1) accept a fuel-cladding tube with one end plug welded in place,
- (2) fill the tube with core fuel pellets, axial blanket pellets and segmenting discs,
- (3) roll the cladding tube into the segmenting discs,
- (4) partially insert the second end plug,
- (5) evacuate the fuel tube and backfill with helium, and

(6) seat the end plug.

The column of fuel pellets, segmenting discs, and depleted UO_2 axial blanket pellets is supported by a handling rod which runs through the central hole in these pieces and accurately positions the segmenting discs. The loaded handling rod is inserted into the cladding tube, and the segmenting discs are then rolled into place beginning with the one nearest the closed end of the fuel pin and progressing to the other end. The handling rod is then withdrawn from the pin, the second end closure is partially inserted, the tube is evacuated and backfilled with helium and the end plug is seated, trapping the helium in the tube. The pin with the seated end plug is then removed from this machine and transferred to the end plug welder. The handling rod is loaded manually with the use of jigs which position the segmenting discs accurately. The machine is operated with remote "through-the-wall" manipulators and valve controls located in a panel outside the warm cell. The machine is located so that the operations can be observed through a shielding window.

6.3.4.7 Bundle Assembly Machine. The pre-assembled tie rod and spacer grid sub-assembly is clamped to the bed of the bundle assembly machine and 123 fuel pins are inserted into a ram and pin guide section. The ram pushes the fuel pins through the spacer grids structure until the bundle is properly aligned axially. The positioning holes in the end closures are aligned to receive the tie wires. The assembled pin bundle is then checked for dimensional alignment and transferred to the final assembly machine.

6.3.4.8 Final Assembly Machine. This machine holds the pre-assembled support can and nozzle and hold-down latch subassembly and the fuel pin bundle in proper alignment and slides them together until the positioning holes in the pin bundle are aligned with the tie wire positioning holes in the support can. The tie wires are then inserted through the assembly and welded to one wall of the support can. The upper handling fitting which is also clamped in the machine is then fitted into the top of the support can and welded to the latch operating rod to complete fabrication of the assembly. After inspection of those dimensions controlling core structure positioning, the completed assembly is removed from the machine and transferred to the cleaning station.

6.3.5 Design of Reprocessing and Refabrication Cells

The operations described above are carried out in two shielded cells: a hot cell for operations with material of high activity and a warm cell for operations involving lower levels of activity.

6.3.5.1 Hot Cell. The hot cell in the upper right of Fig. 6.11 contains and shields the high activity level chemical process equipment. The space for active process equipment is 44 ft long, 11.5 ft wide, and about 14 ft high.

The hot-cell equipment is mounted on 14 racks.

The blanket plutonium recovery process equipment is located on three racks at one end of the hot cell to facilitate shielding the ion-exchange columns from the higher activity level core fuel dissolution and separation process.

Each rack is a plug-in unit similar to an electronic plug-in chassis. All feed lines, instrumentation, and control lines and interconnections between racks are made through three shielding plugs on the rack which fit into and seal the north wall of the cell.

To service or change the equipment on any rack the rack must be removed by remote handling devices to the decontamination cell adjoining the hot cell. The rack-transfer mechanism consists of a translating carriage and push-pull device. This arrangement permits any rack to be remotely transferred from its operating position in the hot cell to the decontamination cell and replaced with another rack. Pipe connections to the feed lines, sample lines, instrumentation, etc., are made and broken from the piping corridor, in a low radiation environment.

6.3.5.2 Decontamination Cell. The decontamination cell is 12.5 ft long, about 14 ft wide, and slightly less than 17 ft high; it is equipped with wash-down facilities. The decontamination cell had one shielding window, one pair of heavy-duty manipulators, and special wall plugs duplicating the north-wall plug system in the hot cell. There are two doors in the decontamination cell; one to the decontamination area and one to the hot cell.

The ceiling of the decontamination cell has removable shielding plugs for handling material vertically in the cell and for removal and introduction of material and equipment from the service corridor on the first floor.

6.3.5.3 Warm Cell. The warm cell is a shielded enclosure which permits remote handling of the low-activity chemical processing equipment, and of the mechanical equipment necessary for fuel-pellet production and fuel-assembly fabrication.

The warm cell is shown in the center of Fig. 6.12; it is 13 ft wide, 62 ft long, and 16.5 ft high. The warm cell is equipped with glass shielding windows and a pair of manipulators for each of the windows. The equipment also includes a General Mills No. 100 mechanical arm and a 3-ton hoist of the General Mills type, which are used primarily to assist in servicing the machines and transferring material.

In addition to openings in the cell wall required for manipulators and machinery operators, there are removable roof plugs opening to the service corridor to permit the passage of machinery and equipment, including the cell cranes. Clearance around the cell in the cell operating area is about 14 ft, to allow for the removal of the manipulators.

A transfer lock near the end of the warm cell is used to raise the refabricated fuel assemblies into the transfer cask in the corridor over the warm cell. Non-radioactive materials are also introduced into the cell through the transfer lock.

Equipment in the warm cell is fastened to base plates with locating pins and quick-disconnect clamps, and is easily removable. When a piece of equipment must be repaired or replaced, the quick-disconnect clamps are removed, the item is enclosed in a polyethylene bag and moved beneath one of the cell covers by the cell crane. The equipment is lifted out of the cell with the corridor crane and placed in the decontamination cell. After decontamination the equipment will be repaired and returned to service or replaced.

6.3.6 Waste Handling

6.3.6.1 Gaseous Wastes. Gaseous wastes will be discharged through two off-gas headers: the dissolver off-gas header for gases of fission-product activity, and the vessel off-gas header for gases of lower fission-product activity. Since the equipment in the dissolver, feed-adjustment and high-level waste evaporator systems has the highest potential fission-product activity, it will be vented through the dissolver off-gas header. All other radiochemical process equipment will be vented through the vessel off-gas header.

The two headers join downstream of the pressure-reducing stations and empty into a gaseous-waste header maintained at a lower pressure by a high-capacity vacuum pump. The gaseous wastes are passed through a caustic scrubber where ruthenium, iodine, and oxides of nitrogen are removed together with particulate matter which can be scrubbed out of the air stream. The scrubber vent gas will be heated slightly to prevent condensation, then sent to a filter for further removal of particulate matter. Before the off-gas is discharged to the stack it will be monitored to ensure that no harmful quantities of radioactivity are discharged to the atmosphere. A small compressor and hold-up tank system is provided to retain dissolver off-gases in the event of abnormally high activity.

6.3.6.2 Liquid Wastes. Concentrated high-level wastes will be stored in stainless-steel tanks housed in underground concrete vaults, and must be cooled to dissipate the fission-product decay heat. The waste solution from the blanket plutonium recovery process which contains depleted uranium and fission products is stored in a separate set of cooled-stainless-steel tanks. Low-level acid wastes generated in the feed adjustment steps contain rather large quantities of nitric acid and will also be stored in stainless-steel tanks placed in an underground concrete vault. Low-level neutralized wastes are those which either contain neutralized irrecoverable acids or are basic. These wastes will be concentrated and stored in carbon-steel tanks in an underground concrete vault. Process wastes with very low activity levels will be sent to a retention basin for discharge to the environment. This will avoid gross contamination of the cold wastes in the retention basins. Samples of the retained wastes will be taken and analyzed prior to discharge to the river.

6.3.6.3 Solid Wastes. The support can and related structural members of both the fuel and radial blanket assemblies which are stripped from the fuel bundles by the disassembly machines located in the storage pool will be accumulated and sealed in containers suitable for burial, and transported in a shielded cask to the waste area for

burial. The dissolver baskets filled with leached cladding segments are removed from the dissolver, encased in pipe sections, and transported to the burial area in a shielded transfer cask.

Fuel assembly structural parts and materials which are scrapped in the warm-cell-fabrication process are of low activity level and are collected in pipe casings which are welded shut, decontaminated, and sent to the burial area.

6.3.7 Building

The reprocessing building is adjacent to the reactor building so that the fuel removal tunnel can connect directly to the spent-fuel storage pool. The single-story service building, which is essentially a wing of the reprocessing building, houses the plant offices, locker rooms, lavatories, a utility and compressor room, and a laboratory room. The stack and waste building are located away from the reprocessing building.

6.3.7.1 Design Considerations. A plan of the ground floor of the reprocessing building is shown in Fig. 6.12.

The building layout reflects four principal requirements:

- (1) Underwater transfer of spent fuel from the reactor building and disassembly of fuel- and radial-blanket assemblies.
- (2) The transfer of materials and equipment into and out of the pool, decontamination cell, hot cell, and warm cell, either with or without shielded containers according to the activity of the material being handled. This necessitates a service corridor extending over these areas.
- (3) The use of the rack concept and through-the-wall connections for the high-level chemical process system.
- (4) Through-the-wall manipulation from both side-faces of the warm cell.

The building also ensures that work flows smoothly from a higher to a lower radiation level throughout the processing and fabrication cycle. The pipe access area has a relatively low level of radiation to permit easy interconnection between the process equipment and three supporting facilities: the sampling system, the transmitter equipment for instrumentation signals, and the chemical makeup area, all of which are conveniently located on the floor above.

The control room is beneath the transmitter equipment room and adjoins the pipe access area. The cells, the pipe access area, the control room, the cell operating area, and the decontamination area are integral and isolated from the rest of the building.

6.3.7.2 Supporting Areas and Facilities. The ground floor of the building has a machine shop, receiving area, hot change room, laboratories, electrical switchgear room, and a manipulator and instrument repair shop in support of the main operating areas.

The second floor of the main building (Fig. 6.13) houses the cold makeup area, transmitter area, sample area, hot analytical facility, and filter room.

6.3.7.3 Heating and Ventilation. The facility is ventilated by three separate systems: one services the areas where hot phases of the process are carried out; a second, the potentially contaminated (warm) areas of the processing building; and the third, the cold area or service building. The hot system supplies air through roughing filters and steam coils. This air is distributed through three separate flow paths to the corridor area and warm cell, to the decontamination area and hot cell, and to the receiving and service areas. Pressure differentials are maintained so that the flow of air is always from the colder to the hotter areas. Some of the air is exhausted directly to the filter and stack system; the remainder is exhausted through the hot cell and warm cell which are maintained at the lowest pressure level. All the exhausted air of this system is discharged through an absolute filter system to a 200-ft stack.

The warm system supplies air through roughing filters and steam coils to the control room, cell operating areas, and the various support areas. Pressure differentials are maintained so that the flow of air is always from the cold areas to the potentially hot or warm areas. Air from this system is discharged through high-efficiency filters to a short exhaust port on the roof.

Quick-closing dampers on the inlet ducts of both systems and on the outlet duct of the warm system prevent the release of excessive activity to the atmosphere. These shutoff dampers, together with visible and audible evacuation alarms, are actuated by

(1) high-activity levels detected by monitors on the exhaust duct of the warm system and

(2) reversal of normal pressure differentials between the hot and cold areas of the plant.

The cold system services the service building wing and supplies filtered and tempered air to the various office areas. Air from this system is discharged through roof ventilators.

6.3.8 Cost Estimate

6.3.8.1 Capital Cost. The required nominal capacity of the on-site reprocessing and refabrication plant for the large fast reactor core fuel cycle is identical with that of the PCUT demonstration plant. The equipment for chemically processing the plutonium-uranium mixed oxide is essentially identical with the equipment employed to process the mixed

thoria-urania fuel. Therefore, the capital cost estimate for the large fast reactor fuel cycle facility has been based on the detailed construction cost estimate of the PCUT plant with the following modifications:

- (1) the warm-cell construction cost has been increased to reflect the increased cell length required to fabricate the longer fast reactor fuel assemblies;
- (2) the warm-cell mechanical fabrication equipment cost has been increased to include additional and more complex machines required for assembling the mechanically, more complicated fast reactor fuel assembly;
- (3) the hot-cell construction cost has been increased to reflect the additional racks required for the blanket plutonium recovery process equipment;
- (4) the basic process building cost has been increased to reflect the increase in hot-cell and warm-cell size;
- (5) the costs of the mechanical and chemical process equipment for the blanket plutonium recovery process have been added; and
- (6) the cost of the additional waste storage tanks required for blanket reprocessing have been added.

Since the fuel-cycle facility is only a small part of the total nuclear power station, it has been assumed that costs of such items as site clearance, access roads, and bringing utility services to the reprocessing area are included in the power plant construction costs.

The total construction cost, including all indirect construction costs, startup costs, interest during construction, and a 10 percent contingency, is \$8,271,400. This amount is itemized in Table 6-2. An important factor is that the concurrent construction of the fuel-cycle facility and the power plant permits some savings in architect-engineering and other indirect construction costs.

6.3.8.2 Operating Cost. As in the case of the plant capital costs, the annual operating costs for the fuel-cycle facility have been estimated with the use of the detailed PCUT costs as a guide. The labor costs are based on 3-shift-5-day per week plant operation. The labor, operation supply, utility, and waste storage costs include separately estimated costs for the blanket plutonium recovery operation. The fuel-assembly parts cost has been estimated on the basis of the conceptual fuel-assembly design drawings. The fixed charges on the plant investment have been estimated with a charge rate of 14 percent, since the processing plant has a guaranteed market for the life of the reactor; and, therefore, the capital investment only has to be returned at the rate of the reactor itself. The annual operating cost is \$2,226,400, and the annual fixed charges are \$1,158,000. The principal operating costs are summarized in Table 6-3, and the labor costs are itemized in Table 6-4.

TABLE 6-2

CAPITAL COST SUMMARYDirect Construction Costs

<u>Item</u>	<u>Cost</u>
Basic process building and service building wing (includes excavation and backfill)	\$ 813,800
Cells, basement, and pool (includes manipulators and windows) . . .	750,600
Building materials handling equipment	87,000
Heating and ventilation (includes stack)	132,000
Building electrical	123,000
Process vessels	156,900
Process special items (includes mixer-settler, centrifuges)	81,500
Process instrumentation	351,000
Process piping (including building piping and plumbing)	436,600
Process electrical	267,000
Process mechanical	610,700
Waste storage area structures	233,000
Waste tanks	933,000
Sample system and analytical cell	242,000
TOTAL DIRECT CONSTRUCTION COST	\$5,218,100

Indirect Construction Costs

General and administrative	417,500
Subtotal	5,635,600
Miscellaneous construction costs	84,500
Subtotal	5,720,100
Engineering and inspection cost	
Architectural and engineering services	629,200
Subtotal	6,349,300
Process engineering	444,400
Subtotal	6,793,700
Startup cost	340,500
Land and land rights (included in power plant cost)	
Subtotal	7,134,200
Interest during construction (24 mo.)	385,300
Subtotal	7,519,500
Contingency (10%)	751,900
TOTAL CAPITAL COST	\$8,271,400

TABLE 6-3

SUMMARY OF ANNUAL OPERATING COSTLabor Costs

Direct operating personnel and supervision	\$ 313,800
Maintenance labor and supervision	71,500
Analytical labor and supervision	94,500
Health and safety labor	<u>32,500</u>
Labor costs	\$ 502,300
Indirect payroll costs at 20% *	<u>100,500</u>
Subtotal	\$ 602,800
Operating supplies (not including fuel assembly parts)	178,400
Fuel-assembly parts	705,000
Maintenance supplies	50,000
Utilities	165,400
Waste storage	185,500
Fuel for perpetual care for wastes	<u>72,000</u>
Subtotal	\$1,859,100
Contingency at 5%	93,000
General works expense.	118,300
G&A**	<u>156,000</u>
	\$2,226,400

* From TID-7025.

** 14% of the following:

Direct and indirect labor
Maintenance supplies
Utilities
Waste storage
Contingency
General works expense

TABLE 6-4
PLANT LABOR

<u>Labor Classification</u>	<u>Labor Requirements</u>		<u>Yearly Rate</u>	<u>Total</u>
	<u>Per Shift</u>	<u>Total</u>		
Direct operating personnel and supervision				
Plant manager	-	1	\$15,000	\$15,000
Secretary	-	1	4,000	4,000
Shift supervisor	1	3	10,000	30,000
Chief chemical operator	1	3	7,500	22,500
Chief mechanical operator	1	3	7,500	22,500
Chemical operators	4	12	6,000	72,000
Mechanical operators	7	21	6,000	126,000
Planning engineer	-	1	9,500	9,500
Accountability clerk	-	1	5,800	5,800
Engineering assistant	-	1	6,500	6,500
Subtotal				\$313,800
Maintenance labor and supervision				
Maintenance foreman	-	1	\$ 8,500	\$ 8,500
Maintenance crew	3	9	6,000	54,000
Janitors	1	2	4,500	9,000
Subtotal				\$71,500
Analytical labor and supervision				
Supervisor	-	1	\$10,000	\$10,000
Chemist	-	1	8,000	8,000
Counting room	-	1	7,000	7,000
Mass spectrograph	-	1	8,000	8,000
Emission spectrograph	-	1	8,000	8,000
Shift chemist	1	3	7,500	22,500
Shift analysts	2	6	6,500	39,000
Subtotal				\$94,500
Health and safety, labor and supervision				
Health physicists	1	3	\$ 7,500	\$22,500
TOTAL OPERATING LABOR COST				\$502,300

The blanket plutonium recovery costs have been conservatively estimated with the assumption that the highly acid uranium-fission product waste solution will be stored in cooled stainless-steel tanks. The volume of waste could be reduced by recovering acid from the wastes, thereby allowing the uranium to be stored in a more concentrated solution; however, capital cost would not be reduced because of the investment in acid-recovery equipment. The major saving would be in the operating cost, since the waste tankage and perpetual waste care charges are proportional to waste volume.

6.3.9 Off-Site Fuel Reprocessing

Although the reference fuel cycle employs on-site reprocessing and refabrication of both core and blanket fuel, the fuel-cycle operations for off-site reprocessing have been considered, and the facility requirements for fuel shipping have been established.

6.3.9.1 Off-Site Reprocessing Sequence. The off-site fuel-cycle process steps are shown schematically in Fig. 6.14. Spent fuel assemblies are removed from the reactor core with the fuel transfer cask and are transported to the steam-cleaning facility where residual sodium is removed. From steam cleaning, the assembly is transferred to the cutoff pool, where (comparatively) the relatively low activity structural sections are cut off. After trimming, the fuel assemblies go to the failed-fuel detection section. Leaking assemblies are canned; then both non-failed and canned failed-fuel assemblies go to the reactor plant pool storage for decay prior to shipping.

Decayed fuel assemblies are loaded into a shipping cask and transferred to an off-site reprocessing plant. The shipping cask is received and unloaded. The fuel assemblies are further cooled in the reprocessing plant pool storage and are then reprocessed, and the uranium product is reconverted. Plutonium from the blanket element is blended with the reconverted uranium, and the LFR fuel assembly is refabricated. The fuel assemblies are shipped to the LFR plant for inspection and storage. As required, the fuel assemblies are transferred to the reactor containment building, where they are heated and transferred to the loading cask. From the loading cask, the fuel assemblies are loaded into the reactor core.

Figure 6.15 is a concept of the storage pool with necessary fuel-handling equipment and provisions for shipment of the fuel assemblies to an off-site reprocessing plant. Specifically, the features required for off-site shipment comprise:

- (1) the shipment cask,
- (2) an appropriate 7 x 8 ft area of the pool for loading the shipment cask,
- (3) an overhead crane large enough to handle the shipment cask (a minimum of 20 tons capacity is required, but it could be substantially larger, depending on final design of the cask),

- (4) a cask washdown and monitoring area at building floor level,
- (5) a rail siding, and
- (6) special cars for transporting casks, depending on final design criteria as to reprocessing site.

While these items occupy a relatively small part of the area shown, they represent a substantial capital cost over the items which would replace them for on-site reprocessing.

The pool sketch shows two general pool regions: one for receiving, monitoring, and cutting assemblies coming from the reactor, and one for storage and loading for shipment. These areas are separated by a watertight gate and partition of adequate strength to empty either pool while maintaining water in the other. This partition permits one pool to be cleaned or maintained while any radioactive items are stored in the other. Normally, the gate between the regions is open.

A powered personnel bridge travels the length of the pool. This bridge carries grapples, a hoist, lights, and any other equipment necessary to transport fuel assemblies from any part of the pool to any other part. The assemblies are handled and stored in a vertical position. Provision is made to keep the assemblies at a specific minimum distance from one another, to avoid the possibility of developing a critical mass.

Elements received from the steam-cleaning units are normally placed in the underwater cut-off saw, where the non-nuclear ends are removed. This process reduces the fuel element from 102 in. to about 60 in. in length. The elements are then taken to the large storage pool and held for shipment.

If monitoring of the reactor indicates a failed-fuel assembly, the assemblies are isolated in the canning station and are then transferred to the holdup area for monitoring. The holdup area is large enough to hold all assemblies required to keep the reactor unloading operative, while providing adequate time for monitoring. Assemblies which prove to be normal are removed from isolation cans and are, thereafter, treated as normal assemblies, as described in the foregoing.

The general storage area is large enough to hold all 498 fuel assemblies from the reactor and a margin of 10 percent. Normally, some of this space is used for handling and storing blanket assemblies because only one-third of the fuel assemblies will usually be removed from the reactor at one time. Final determination of the capacity of this area, of course, depends on the ultimate design of the fuel-transfer sequence.

For shipment, assemblies are brought from storage by the bridge to the cask area and placed in the cask-assembly basket. This basket can hold several assemblies, the largest number that can be considered safe. When the basket is full, it is lifted by the bridge hoist and lowered into the shipping cask. Then the overhead crane picks up the cask cover and places it on the cask. The crane then lifts the cask from the pool and places

it at the washdown area for washdown by a hose. Then, the cover is clamped down, and the cask is monitored for radiation. If acceptable, the cask is placed on the flat car and fastened down.

6.3.9.2 Facility Requirements for Off-Site Shipping. Prior to reprocessing, spent fuel assemblies are:

- (1) steam cleaned,
- (2) examined for cladding failure,
- (3) canned or segregated (in the case of failed-fuel elements), and
- (4) stored in pools prior to shipment for off-site reprocessing.

Steps (1) and (2) are not affected by the reprocessing. The type of canning process for failed-fuel assemblies can be affected by a choice of on-site or off-site reprocessing.

If a failed-fuel assembly is to be processed on-site, the fuel assembly can be segregated in a separate pool or canned. The canning process can be simple since the fuel assembly will be under water at all times. When a failed-fuel assembly is canned (long) prior to shipment for off-site reprocessing, much greater precautions must be taken in the canning process. If the gap between the can and the fuel assembly is to be filled with water for heat transfer purposes, then the entire post-canning thermal history of the assembly must be calculated to determine if the can and its closures are strong enough to withstand any pressure buildup which might result if the temperature of the fuel assembly is significantly higher than during the canning operation.

The requirements for pool storage are not greatly affected by whether on-site or off-site reprocessing is used. If fuel assemblies are to be shipped off-site, the pools must be deep enough to receive the shipping cask and provide adequate water cover. In addition, the weight of the shipping cask may determine the capacity of the crane in the fuel-handling area.

6.4 PLANT LAYOUT

A typical plot plan for large fast reactors with an on-site fuel-cycle facility is shown in Fig. 6.16. The plant layout is similar to that which would be expected with any liquid-metal-cooled reactor. The addition of the chemical reprocessing and fuel refabrication facility has added waste tank farms, holdup basins, and other equipment peculiar to a facility of this type, but these additions have not imposed an unduly large penalty on the space required for the complete plant.

In the event that a plant of this type would be built without the integrated reprocessing facility, new fuel and spent fuel handling facilities will still be required. In this case

Building 5 of Fig. 6.16 would be replaced by a much smaller building, the interior of which would be similar to that shown on Fig. 6.15 and in the same general location. Building 4 would be slightly increased to accommodate the storage of new fuel elements, in addition to the blanket fuel storage space already provided. Buildings 13 and 18 would be reduced in size, and Buildings 6, 21, 23, 24, 26, and 27 would be considerably reduced and, in some cases, eliminated.

PLAN VIEW

ALLIS CHALMERS MFG. CO.			
LARGE FAST REACTOR DESIGN STUDY			
REACTOR CONTAINMENT BUILDING			
PLAN			
UNITED ENGINEERS & CONTRACTORS INC.			
PHILADELPHIA			
DESIGNED BY	DESIGNED BY	DESIGNED BY	DESIGNED BY
CHECKED BY	CHECKED BY	CHECKED BY	CHECKED BY
DATE	DATE	DATE	DATE
SCALE	SCALE	SCALE	SCALE
NO. OF SHEETS	NO. OF SHEETS	NO. OF SHEETS	NO. OF SHEETS
THIS SHEET IS THE	THIS SHEET IS THE	THIS SHEET IS THE	THIS SHEET IS THE
OF	OF	OF	OF
12489 F 2			

FIG. 6.1

FIG. 6.3

FORM NUMBER 1000-02-0

FIG. 6.4

FIG. 6.5

FIG. 6.7

FIG. 6.8

FIG. 6.9

SCHEMATIC FLOWSHEET - ON-SITE REPROCESSING

FIG. 6.10

HOT CELL AND WARM CELL

FIG. 6.11

REPROCESSING BUILDING GROUND FLOOR PLAN

FIG. 6.12

SECOND FLOOR PLAN OF REPROCESSING BUILDING

FIG. 6.13

FORM NUMBER 1000.02-0

FIG. 6.14

LEGEND

- 1-WAREHOUSE & SHOP
- 2-GAS BUILDING
- 3-SODIUM HANDLING & STORAGE BLDG.
- 4-FUEL HANDLING & REPAIR BLDG.
- 5-FUEL REPROCESSING BLDG.
- 6-VENTILATION BLDG.
- 7-REACTOR BLDG.
- 8-STEAM GENERATOR BLDG.
- 9-TURBINE GENERATOR BLDG.
- 10-HEATER BAY
- 11-BOILER HOUSE
- 12-CONTROL ROOM
- 13-SERVICE BLDG. & FUEL FACILITY OFFICES
- 14-INTAKE STRUCTURE
- 15-OIL STORAGE TANKS
- 16-WATER STORAGE TANK
- 17-SEWAGE PLANT
- 18-HEALTH PHYSICS BLDG.
- 19-GUARD HOUSE
- 20-SWITCHYARD
- 21-STACK
- 22-WATER TREATMENT
- 23-WASTE DISPOSAL BLDG.
- 24-RETENTION BASIN
- 25-SPENT FUEL STORAGE POOL
- 26-HIGH LEVEL WASTE
- 27-LOW LEVEL WASTE
- 28-TRANSFORMER YARD

ALLIS-CHALMERS MFG. CO.

LARGE FAST REACTOR DESIGN STUDY
PLOT PLAN

UNITED ENGINEERS & CONSTRUCTORS INC
PHILADELPHIA

CHECKED	INSPECTED	CORRECT	APPROVED
DATE 2-12-63	ESTIMATE	ORIGINATING OFFICE	PHILA.
SCALE 1"=100'-0"	ACCOUNT	T-2409-LL-1	
	ITEM		

7. COST ANALYSIS

This summary of costs is presented as an easy reference for information found in other sections. Only those equipment items that are part of the reactor vessel and fuel handling system have been included in the cost estimate. Fuel cycle data are found in Secs. 4.4 and 6.3. Equipment costs are not presented elsewhere, but the description and mechanical design drawings can be found in Secs. 5 and 6.2.

7.1 FUEL CYCLE

Fuel-cycle costs (see Table 7-1) have been determined for both on-site and off-site reprocessing and refabrication of fuel. In addition to the 4-3/4 percent use charge rate specified by the study ground rules, fuel-cycle costs with an inventory charge rate of 10 percent have been calculated to indicate representative costs that would exist with private ownership of fuel. In general, the Guide to Nuclear Power Cost Evaluation (TID 7025) has been used to evaluate the off-site reprocessing of fuel. A preliminary design evaluation of the on-site facility has been used to determine that part of the fuel cost. The on-site facility is the reference mode of fuel-cycle operation for this study.

TABLE 7-1

FUEL-CYCLE COSTS

<u>reprocessing</u>	cost-mills/kwhr for fuel inventory charge rate	
	4.75%	10%
on-site	0.69	1.00
off-site	0.78	1.11

Basis: core exposure = 100,000 Mwd/MT
blanket exposure = 133,333 core equivalent Mwd/MT
plant factor = 80 percent
fuel inventory = 133 percent of core inventory

7.2 EQUIPMENT

Table 7-2 lists the cost for the core components. Table 7-3 gives the reactor equipment costs, and Table 7-4 gives the costs for the refueling equipment. These estimates are based on the drawings in Secs. 5 and 6.2.

TABLE 7-2

CORE COMPONENTS

<u>item</u>	<u>unit cost, dollars</u>	<u>number required</u>	<u>total cost, dollars</u>
fuel assembly	12,500	498	6,225,000
radial blanket assembly . . .	5,100	858	4,376,000
shim and regulating assembly .	6,000	36	216,000
safety assembly	6,000	12	72,000

TABLE 7-3

REACTOR EQUIPMENT

<u>item</u>	<u>number required</u>	<u>procurement cost, dollars</u>	<u>erection cost, dollars</u>	<u>total dollars</u>
reactor vessel				
vessel and internals . .		7,188,000	250,000	7,438,000
vessel supports		5,000	25,000	30,000
safety assembly drives				
prototype unit 1		160,000		
production units 12		120,000	50,000	330,000
shim assembly drives				
prototype unit 1		150,000		
production units 36		288,000	72,000	510,000
instrument drives 12		66,000	12,000	78,000

TABLE 7-4

REFUELING EQUIPMENT

<u>item</u>	<u>number required</u>	<u>procurement cost, dollars</u>	<u>erection cost, dollars</u>	<u>total dollars</u>
fuel removal cask 1		160,000	30,000	190,000
spare cask 1		110,000	--	111,000
fuel handling cask 1		36,000	3,000	39,000
plug handling cask 1		18,000	--	18,000
access port valves 4		48,000	2,000	50,000
blanket shuffling				
equipment 1 set		90,000	6,000	96,000
wash cells 2		140,000	50,000	190,000

8. RESEARCH AND DEVELOPMENT PROGRAM

8.1 OBJECTIVES OF THE STUDY

The overall objective of the AEC's Fast Reactor Program is the development of technology necessary for economical electric-power generation from fast breeder reactors. Plants of this type, with a capacity of about 1000 Mwe, are planned for construction in the mid-1970's; and, a long-term nuclear power economy, based on the breeder reactor technology is foreseen. This study, related to the overall objective, has the following programmatic objectives:

(1) To determine the areas of fast-reactor technology in which R & D work must be done to permit the construction, in the mid-1970's, of a large plant of the generic type defined in the Contract Design Objectives. Specific program items in each area must be identified so that an overall program can be formulated and put into effect by the AEC.

(2) To define the short-range R & D program items which must be initiated immediately so that construction of a prototype reactor of about 200 Mwe can begin in 1968.

(3) To estimate the fuel-cycle cost for a 1000-Mwe plant of the type under study. The fueling cost should be at a minimum and consistent with the design characteristics specified by the Commission. The cost estimate will provide a basis for assigning emphasis to the various development programs, consistent with the contribution to the cost of power.

A conceptual design study of the entire power plant is required to meet all the programmatic and technical objectives. From the conceptual design of the large plant, those objectives which relate to the overall AEC Fast Reactor Program can be satisfied, the preliminary design criteria for a prototype plant deduced, and the short-range R & D requirements identified.

8.2 SHORT-RANGE R & D PROGRAM

From the study, many development areas have been delineated which are keys to the AEC's plan for a prototype construction project, beginning in 1968 or 1969. The start of specific development programs in these areas constitutes a critical item on the schedule of the prototype, and, hence, in the overall Fast Reactor Program.

8.2.1 Physics

In the area of physics, the major factor influencing the selection of the annular core was the large positive sodium void coefficient that is expected in the large oxide cores of conventional cylindrical designs. The theoretical prediction of the one and only measurement of a negative sodium danger coefficient (positive void coefficient)

disagreed by a factor of two to three. If calculations of the void coefficients in oxide cores are really this much in error, then it may not be necessary to resort to a high leakage core; and, a greater flexibility in design, such as higher fuel concentration to improve internal breeding and the Doppler coefficient, may be possible.

The gaps and uncertainties in physics design data and the R & D programs necessary for their measurement are summarized in the following subsections.

8.2.1.1 Sodium Danger Coefficient Measurements. More experiments are required on large zero power critical facilities, with dilute cores of various compositions, in order to provide an adequate range of tests for theory-measurement comparisons. If possible, these experimental cores should be simple to facilitate easy calculation models; e.g., a spherical core can be calculated more rigorously than a cylindrical one especially when zoned criticals are used. In addition, improved techniques for computing sodium coefficients should be sought. Experiments are also needed to determine the effectiveness of a sodium reflector as a safety device.

8.2.1.2 General Statics Measurements. Physics measurements on dilute plutonium-fueled cores are non-existent. Until large quantities of plutonium are available, zoned criticals and smaller oxygen and carbon plutonium criticals will provide interim information for theoretical experimental comparisons. The usual information such as critical mass data, spectral information, and danger coefficients is needed.

8.2.1.3 Doppler Coefficient Measurements. Doppler coefficient measurements of two different types are needed. The first are physics experiments with simple core configurations and compositions, where the effect is measured in only one isotope at a time in various neutron spectra. These short-range experiments should check the calculational methods and give confidence to the results. A large critical facility with a heated core region is essential to this program. The second type is the longer-range demonstration experiment in a reactor with the composition of the designed core. These experiments measure essentially the power coefficient and demonstrate its effectiveness for the safety of the designed core.

8.2.2 Heat Transfer and Fluid Flow

8.2.2.1 Sodium Boiling and Fuel-Assembly Voiding. The occurrence of bulk boiling in an assembly and the subsequent voiding of the assembly has a pronounced effect on the reactor kinetics. A better understanding of the time dependence of assembly voiding requires further theoretical investigation, and development of analytical and experimental techniques are required in this area. Experiments similar to the boiling sodium experiments, at AI, and the water-lead tests, performed at APDA, should be extended. An electrically heated element which would retain its integrity after coolant loss would be required, and then, monitoring of the void distribution by radioactive tracer or ultrasonic techniques might be possible.

8.2.2.2 Coolant Mixing. At the edges of the core, a considerable variation of power output exists across the cross section of a fuel assembly and results in unequal heating of the coolant in individual coolant paths. Knowledge of the anticipated effect of intra-assembly coolant mixing could be applied to reduce the hot-channel factor for coolant temperature rise. Other than theoretical investigation, experiments using tracer methods could be conducted to verify or determine the extent of coolant mixing.

8.2.2.3 Fuel-Element Pressure Drop and Flow Tests. Tests are necessary to verify the calculated pressure losses for pin bundles, complete fuel assemblies, and orifices required for flow tailoring. Tests should be conducted in sodium loops so that the long-time erosion resistance of the orifices and the susceptibility to plugging with typical coolant impurity levels can be evaluated. The performance of the Venturi hydraulic holddown nozzle for the inlet section of the fuel assembly must be investigated in the flow test program. Both the pressure loss and recovery characteristics and the effective holddown force should be evaluated in these tests.

8.2.3 Materials

Existing materials, such as austenitic stainless steels, are adequate for near-term objectives and even for far term objectives, if more suitable materials have not been developed. Continued support of the existing programs will lead to a better understanding of the behavior of stainless steels in sodium. Further property data should be obtained on 19-9 DL stainless steel in a sodium environment.

8.2.4 Fuel Cycle

Other than nuclear safety and efficient power removal, the most important requirement for nuclear power is low fuel-cycle cost. In the case of the Fast Breeder Reactor, high breeding gain and low doubling time is desired in addition to the economic requirements for the fuel cycle. Thus, development and demonstration of a fuel system and its cycle constitute a key area in the development of a large fast reactor.

8.2.4.1 Fuel-Assembly Tests. Programs already sponsored by the AEC have been reviewed and appear to be adequate to establish the basic irradiation behavior of the mixed oxide fuel, under typical fast reactor core conditions. However, many tests are required to confirm and refine the detailed fuel-assembly design and to demonstrate its transient behavior.

Prototype assemblies must be constructed to assure mechanical compatibility of the remote fabrication operations. These tests would be performed with mockups of the critical components of the fabrication machines. The design of the segmenting-disc rolling process and equipment must be developed and proof-tested. Alternate methods of fixing the segmenting discs within the cladding tube should be evaluated experimentally. It may be necessary to construct different types of fuel-pin spacer grids to assure that acceptable pressure drop characteristics will be obtained. These grids would be tested as a part of the fluid flow test program described previously.

The metallurgical life of the specific fuel design should be confirmed by long-term irradiation tests of full diameter pins, 3 to 4 ft long in special subassemblies in the Fermi or other suitable fuel test reactor. Individual pins would be periodically removed from these test assemblies for post-irradiation examination. It is very important to achieve in these tests the thermal and mechanical stress conditions anticipated in both the large scale and prototype reactor cores. The mechanical stability of the segmenting discs after many power cycles should also be evaluated in a test in the Fermi reactor, or if this is mechanically impractical, by thermal cycling tests in a reactor with good simulation of large fast reactor core thermal conditions. Supporting out-of-pile, thermal-mechanical cycling tests should be performed. If further design studies show that alternate cladding materials, such as 19-9 DL or other high temperature alloys, can contribute to improved fuel life, test assemblies of pins fabricated from these materials should be included in the irradiation test program.

Since the behavior of the oxide fuel pins under transient conditions is of key importance in establishing the reactor dynamic behavior and determining the failure threshold, a series of tests should be conducted in the TREAT reactor. The objectives of these tests would be to confirm the structural integrity of the segmented pin design and to assess the degree of fuel rearrangement which may occur within the fuel-pin compartments during severe power transients. The most promising segmenting approaches, established by further design and experimental work, should be evaluated by the initial TREAT tests. The selected segmenting concept should then be further proof-tested by irradiation to high burnup in the Fermi reactor, followed by transient irradiations in TREAT.

8.2.4.2 Integrated Reprocessing. The modified Purex process that uses only one cycle of extraction selected for core fuel fission product decontamination has been demonstrated to be satisfactory at low radiation levels. The effect of macro fission product concentrations on the behavior of the separation process must be investigated; this can be done by using inactive isotopes to simulate the actual concentration of fission products. The process must also be demonstrated on full-level feed. The behavior of iodine in the process should be determined because of possible future interest in shorter cooling of spent fuel. Satisfactory solvent recycle behavior should also be demonstrated.

The blanket plutonium recovery process selected was based on experimental plutonium-anion-exchange data obtained in applied chemical research in fission-product decontamination development, rather than in power-reactor fuel recycling. The stream concentrations were somewhat different from those required for the low-decontamination recovery process applicable to the blanket fuel cycle. A demonstration of the process must, therefore, be carried out to ensure a firm basis for the design of the recovery facility. The determination of permissible resin loading and eluate plutonium-concentration is an important objective of these studies.

Recovering the nitric acid from the highly acidic anion-exchange column waste stream may result in an appreciable saving in annual process operating costs. This additional process step significantly reduces the volume of waste which must be stored, since the low-acidity waste stream, after acid recovery, will have a high uranium concentration. The waste stream from the anion-exchange demonstration tests can be used as feed material for an investigation of the details of this acid-recovery waste treatment process.

8.2.4.3 Alternate Fuel Systems. Alternates to the mixed oxide fuel system should be investigated in case the next physics experiments (Sec. 8.2.1.1) and analytical studies, (Sec. 8.2.5) indicate that excessive economic penalties are incurred in assuring fast reactor safety with ceramic fuel systems. Thus, although most of the R&D recommendations in this section are directed toward the final demonstration of the reference oxide fuel system, a modest parallel effort on cermet fuels is also recommended. Cermets are of interest because their expansion characteristics are similar to metallic fuels and because their irradiation stability has been demonstrated.

The objective of this effort would be to demonstrate a plutonium-bearing cermet fuel through the irradiation of subassemblies. The specific system which is of particular interest and should be investigated is PuO_2 -thorium metal. Much of the work outlined for the mixed-oxide fuel is in some degree applicable to the cermet system.

8.2.5 Components

Some of the mechanical components for which conceptual designs have been prepared require prototype construction and proof-testing, rather than R&D programs. Specific test requirements can best be defined when the plant optimization of the large reactor has been completed and the prototype reactor requirements have been established. There is a growing trend to combine the cost of this type of development with the total cost of prototype units. Facilities would have to be provided in which to perform the proof-test operations.

(1) The sodium component test facility is an existing facility and is adequate to test prototype intermediate heat exchangers and steam generators.

(2) The pump test facility is essential to the orderly development of sodium-cooled reactor systems and is currently planned. An early start on construction is suggested.

(3) The primary system prototype facility is suggested for proof-testing reactor and fuel handling components not being considered for test in either of the two facilities listed above. It may be possible to provide this capability in the design of the pump test facility.

The components expected to be included in this program are:

- (1) control assembly drives,
- (2) primary system valves,
- (3) nuclear instrumentation for high temperature - high flux application,
- (4) intermediate heat exchangers and steam generators, and
- (5) fuel-handling and viewing equipment.

8.2.6 Engineering and Physics Studies

Engineering and physics studies of the annular core mixed oxide fast reactor must be performed concurrently with the experimental programs outlined in the foregoing sections. In each of the areas outlined and in the general area of overall system safety and performance, physics and engineering analyses are required to obtain a deeper understanding of the characteristics of the design and to identify problems. This second-stage analysis effort will divide the R&D program into specific tasks and experiments and will obtain the detail design criteria for the prototype plant.

The study should include major areas discussed in the following subsections.

8.2.6.1 Detail Analysis of the 1000 Mwe Plant Annular Core. As a continuation of the work already performed, it would be desirable to further optimize the core nuclear and thermal and hydraulic performance.

(1) The reactor statics calculations should be performed in further detail. Increased enrichment of the fuel at the core edge should be investigated to determine if such an approach would ensure that coolant boiling, when it occurs, would be initiated in the edge, rather than central, fuel assemblies. Another possible way of improving the safety characteristics which should be investigated further is the incorporation of moderating zones in the radial blanket.

(2) Improvement in core performance is possible by further optimization of the thermal and hydraulic design. A better balance between linear power generation, pressure drop and pin size appears possible.

(3) A systematic study of the effects of uncertainties in cross section data is required for the important neutronic characteristics like Doppler and sodium void coefficients.

(4) An improved dynamics code which can treat core orificing is required for safety studies. An integrated statics code consisting of an ELMOE-type routine and a resonance self-shielding routine that prepares input cross-section data for the main diffusion theory code, would greatly facilitate physics studies.

8.2.6.2 Energy Release and Containment Studies. The maximum energy release in a hypothesized meltdown accident would probably be much larger in a 1000-Mwe oxide core than in a small metallic system, because of the larger amount of fissionable material involved. However, there are several factors that help reduce the energy release or facilitate the containment problems. In meltdown studies, it is usually hypothesized that the core collapses under the influence of gravity and that a much more reactive configuration is reached at a high rate of reactivity insertion. It has been demonstrated that the explosion caused by this mechanism is much less severe if there is a large negative Doppler coefficient, but further investigations are needed to study this for a large oxide core. In an annular core, a considerable amount of the released kinetic energy would be carried by material moving toward the center of the annulus, which can be much easier slowed down than material moving outward. Certain design features, which simplify containment like the use of crushable material in the shield, should be investigated.

The energy release also depends on the equation of state of the oxide fuel, about which very little is known. Theoretical studies and experiments should, therefore, be conducted to obtain adequate knowledge about the equation of state. Specific areas include:

- (1) a more thorough safety study of both credible and incredible accidents and methods of reducing the probability of their occurrence and the resulting damage,
- (2) a detailed theoretical study, backed up by experiments (see Sec. 8.2.2), of the space-time behavior of void formation in channels when boiling starts, and
- (3) an estimation of the size of the energy release and the possibility of containing the energy release if the hypothetical accidents do occur.

8.2.6.3 System Studies. In addition to further core physics and thermal optimization studies, the power plant and auxiliary systems should be investigated. A thermal-cycle analysis should be made, utilizing presently available data on the performance of the intermediate heat exchanger, steam generator, and steam turbine. This study will establish the inlet temperatures and temperature rises for the primary and secondary coolant circuits and determine the core thermal power required to meet the 1000-Mwe plant rating. Primary and secondary system flowsheets should be prepared.

The control system should be defined after a review of several of the systems that will most likely control the appropriate amount of reactivity and yield the type of response necessary for stable control of the large fast reactors. A comprehensive survey made early in the Fermi project would be valuable in determining the best control system. The reactor, steam generator, and power plant instrumentation and control system components would be selected. In addition to the principal control and safety systems, the plant control center and the various auxiliary system instrumentation requirements should be considered.

The fuel-handling system design and operating schedule should be reviewed for compatibility with the various fuel management schemes that will be developed in further fuel cycle optimization studies. Better methods for purification and contamination detection of the sodium systems would be incorporated in the design.

8.2.6.4 Plant Layout and Cost Estimate After the plant system designs have been established, building layout and plant cost studies would be performed. Shield layouts and shield configuration, materials, and cost studies would be made. Arrangement concept studies, sketches, and layouts of the reactor vessel, equipment, and piping located within the reactor containment building would be prepared, based on the reactor vessel and piping layout design of this study.

Remote maintenance of the plant should be given considerable attention. Various remote maintenance schemes, some of which have been tried in the Fermi plant, would be reviewed and incorporated in the design. Drawings for the selected reactor building layout would be prepared. Preliminary criteria for structures, major equipment, subsystems, piping, and auxiliaries would also be prepared. The type and size of the turbine-generator plant equipment would be established. Drawings and diagrams which reflect the equipment sizes and space requirements would be prepared.

The physical layout and arrangement of all major site buildings and facilities are required. Drawings and architectural concepts, showing locations of all structures within the area, would be prepared. Capital and operating cost estimates for the 1000-Mwe plant would be prepared. Material take-offs and equipment lists are also needed; from this information and the building drawings and criteria, cost information would be assembled, and a construction cost estimate could be prepared. Operating costs would be estimated on the basis of personnel lists and experience factors; fuel-cycle costs would be obtained and used in tabulating the total cost of power.

8.2.6.5 Fuel-Cycle Optimization. The fuel cycle should be optimized on the basis of the refined physics and core thermal analysis studies and plant power cycle analyses. The reprocessing plant design would be reviewed, plant layout drawings would be prepared, and operating and capital cost estimates would be reviewed. The blanket fuel cycle would also be optimized on the basis of the further physics and thermal analyses.

8.2.6.6 Prototype Studies. The geometry of the annular core allows for simple prototyping. A theoretical study would determine the optimum size of the annular core segment, required to adequately prototype the full annular core. A study should also be made to determine what modifications would be required to accommodate the prototype in the existing Enrico Fermi Plant vessel. Prototype requirements for such key mechanical components as control drives and fuel-handling and viewing mechanisms would also be established.

8.2.6.7 Definition of Detail Scope. The additional recommended design and analysis work performed on the 1000-Mwe target plant and the evolution of the design criteria of the prototype would refine the development tasks required for construction of the

prototype. This detail scope of work would be compared to the development work already under way, and a schedule and cost estimate would be prepared for the additional required tasks. Changes in emphasis and orientation of the programs, already under way, could be recommended where required by the results of the second phase work.

8.3 LONG-RANGE R & D PROGRAM

The overall or long-range AEC program for fast reactor development can be illustrated by the chart (Fig. 8.1). The individual projects and programs constitute a broad and integrated fabric of technological exploration of the sodium-cooled fast-breeder. It is this diverse, but coordinated, program which, over a period of two decades, will insure that fission energy can make a significant contribution to the energy supply of the world.

The program is illustrated in the chart mainly in terms of facilities. Two facilities on the chart, for which construction will begin in the next year or two, can make important contributions to the overall program. These facilities, FARET and SEFOR, will provide power and temperature coefficient data which will give an understanding of the dynamics of both large and small fast reactors, as well as provide facilities for the physics evaluation of various advanced fuel systems, both ceramic and metallic. FARET, in particular, will provide a flexible facility which will be useful in a myriad of fast reactor programs, some only roughly outlined now, over the next decade. The programs, carried out in the existing and new facilities, provide the data required for design and construction of new facilities and eventually economic power stations. Long-range programs, already under way but of special importance in support of the long-range development of a 1000-Mwe power station, are discussed in the following subsections.

8.3.1 Basic Cross-Section Data

Both improvements in the basic cross-section data in the fast region and improved methods for preparing this data for reactor calculations is required. In particular, more information is needed on the absorption cross sections and on the resonance parameters of the higher isotopes of plutonium and the inelastic cross sections of plutonium and sodium. An improved code to calculate the group constants of compositions containing resonance scatterers as a function of spatial position would also be useful.

8.3.2 Contaminated Primary Circuit

If it is feasible, the operation of a plant with contaminated primary circuit has important advantages. Fuel elements, vented to the coolant, would allow reduction of the volume fraction of structure on the core and would, therefore, lead not only to extremely long fuel lifetime, but also to an improved internal breeding ratio, because

of its important economic advantages. The problems of design and operation of such a system should be explored with a loop system installed in one of the fast reactor facilities. A program of this type would be a necessary adjunct to the development of fluid fuel and blanket systems.

8.3.3 Advanced Fuel Systems

More advanced fuel systems, such as nitrides, sulfides and other ceramics, should be investigated for potential advantages over the shorter range oxide and carbide systems. Improved high temperature strength cladding materials for both the advanced ceramic fuels and the near-term oxide and carbide systems should be developed. Fluid fuel systems, that have potential for improved breeding gain and for eliminating the problem of fuel loading should be considered as blanket systems and systems possible for core application. Advanced fuel systems of these types presently require laboratory R & D, followed, if they retain their attractiveness, by engineering study and development.

OVERALL FAST-REACTOR DEVELOPMENT PROGRAM

FIG. 8.1

9. CONCLUSIONS

As a result of the conceptual design study of a 1000-Mwe sodium-cooled fast-breeder reactor the following statements can be made:

(1) A large fast reactor can be designed with a small sodium-void coefficient. The reactor is self-regulating for the normally considered situations of control rod withdrawal and loss of flow, without control rod action.

(2) A small sodium-void coefficient is desirable to prevent abnormally large insertions of reactivity during unusual but credible accidents that cause boiling or other sodium voids in the core.

(3) The design chosen in this report has achieved the initial objectives but is not necessarily the only one that can achieve the desirably low sodium-void coefficient. Further, this geometry has not been optimized and additional improvements in void coefficient, Doppler coefficient, and fuel cycle cost may be possible through a change in

- (a) Core dimensions
- (b) Core composition
- (c) Power density
- (d) Core and blanket management

(4) The annular core configuration, in spite of its central hole, has not penalized the design or cost but has improved it. The annular design has basic mechanical simplicity, good fuel geometry, good reactivity and safety characteristics, and excellent power distribution.

(5) Fuel cycle costs for this plant are attractively low at 0.69 mills/kwhr. Further core and blanket optimization should result in further improvements in the fuel cycle cost.

(6) High burnup inherent in the ceramic oxide fuel and the ability to control large amounts of excess reactivity with a relatively small number of control assemblies and good neutron economy results in the ability to operate for long periods without refueling (over 11 mo.) thus providing for high plant availability.

(7) Study ground rules established the plant efficiency at 40 percent and suggested a design target of 1200 F coolant outlet temperature. A 40 percent efficiency would not require a 1200 F sodium outlet temperature. However, efficiencies over 43 percent are possible with conventional superheat/reheat cycles normal for this size plant. Further improvements in efficiency are possible with other thermodynamic cycles and working fluids. Consideration of the power conversion cycle is a requisite for further optimization studies.

(8) Minor extrapolation of existing technology has made possible a design which requires no new major research and development effort. Principal uncertainties rest in the physics of large dilute plutonium systems where more detailed information on basic cross sections and the effect of heavy element resonance interactions is required.

(9) This design, which is based on minor extrapolations of current technology should, with a modest well directed development effort, lead to a safe, economically attractive, marketable, large fast reactor power plant by the mid 1970's.

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APPENDIX A

SODIUM-VOID-COEFFICIENT STUDIES

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1. GENERAL

The approach to the solution of the positive sodium coefficient problem adapted in this design study is to design the reactor so that sodium loss from any subassembly or groups of subassemblies has a negative reactivity effect.

The coolant-void coefficient in a fast reactor is made up of three components:

(1) An absorption component - a decrease in coolant density due to thermal expansion reduces the neutron absorption by the coolant atoms and so causes an increase in reactivity.

(2) A leakage component - decrease in coolant density reduces the macroscopic transport cross section of the core mixture causing a decrease in reactivity. This component is strongly space dependent, being zero at the core center where the flux gradient is zero, and a maximum at the core edge.

(3) A spectral component - a decrease in coolant density hardens the neutron spectrum. The magnitude and sign of this component depends on the scattering properties of the coolant and on the variation with neutron energy of the capture cross sections and the ratios of the capture-to-fission cross sections of the core materials. In a sodium-cooled oxide core this component is positive.

Thus there are three approaches to the problem of negating the sodium-void coefficient; use a coolant with a low neutron-capture cross section, increase the core-leakage characteristics thereby increasing the negative leakage component, and adjust the energy dependence of the neutron importance by varying the core composition.

2. CHOICE OF CORE MATERIALS AND VOLUME FRACTIONS

The design study ground rules have fixed the core materials to be $\text{PuO}_2\text{-UO}_2$ fuel, stainless-steel structure and cladding, and sodium coolant.

Previous studies made at APDA and elsewhere have shown that of the liquid-metal coolants, only sodium-potassium and lead-bismuth show any significant improvement over sodium with respect to the temperature coefficient. The use of the latter alloy is precluded by the high pumping power required and the lower specific heat of the NaK would mean an increase in core size for a given power output, thus offsetting the effect of the decreased moderating properties.

The core coolant fraction would normally lie in the range 0.25 to 0.55 for a design having economic breeding, thermal, and hydraulic characteristics. Calculations were made to investigate the effect of coolant volume fractions in this range on the reactivity change due to coolant loss. The initial calculations were made on a 7800-liter cylindrical core. After the annular core was selected for the final design a further check was

made to see if the chosen coolant fraction (0.4) was near optimum. In varying the core composition, it was assumed that the high ratio of clad to fuel volume remained constant, so that the fuel can reliably withstand distortion. The results are shown in Table 1. In the case of the cylindrical core a coolant fraction of 0.5 reduces the positive reactivity by one-third, but the total breeding ratio would be well below the required 1.2. The change in the reactivity effect is small when the volume fraction is reduced from 0.4 to 0.3.

In the annular core the picture is somewhat different due to the influence of the high leakage geometry. There is a definite disadvantage in going from 0.4 to 0.3 and the advantage in going to 0.5 is less than in the cylindrical core.

To summarize, it appears that the selection of a coolant fraction of 0.4 appears to be a good optimum for the annular core from an overall point of view.

TABLE 1
COMPOSITION VARIATION EFFECTS ON SODIUM WORTH

	7800 ℓ annular core			7800 ℓ cylindrical core		
Fuel	0.27	0.22	0.17	0.27	0.22	0.17
Na	0.30	0.40	0.50	0.30	0.40	0.50
s.s.	0.36	0.31	0.26	0.36	0.31	0.26
Void	0.07	0.07	0.07	0.07	0.07	0.07
U/Pu	3.55	2.85	2.18	4.84	4.22	3.43
Critical Mass, kg:						
Pu-239 (61.4%)	2,776	2,655	2,462	2,131	1,933	1,743
Pu-240 (32.8%)	1,483	1,418	1,315	1,138	1,033	931
Pu-241 (5.8%)	262	251	233	201	183	165
Total Pu	4,521	4,324	4,010	3,470	3,149	2,839
U-238	16,029	12,326	8,731	16,800	13,272	9,732
IBR	0.65	0.52	0.43	0.95	0.85	0.71
BR	1.37	1.32	1.30	1.20	1.12	1.04
100% sodium loss, Δke	0.011	-0.005	-0.009	0.029	0.030	0.021

3. REPLACEMENT OF SOME UO_2 BY ThO_2

A part of the positive spectral component comes from the increase in fertile material fission when the spectrum hardens. Thorium has a higher effective fission threshold and lower fission cross section than U-238. Other investigators have found that in metal fueled cores the replacement of U-238 by Th-232 negates the positive sodium coefficient. However, in the large oxide core being studied, the improvement was not sufficient to negate the coefficient. This is due to the fact that thorium metal is about 40 percent less dense than uranium metal whereas for oxides the density difference is only 10 percent. The improvement found in the metal case was due to the substantially smaller fertile to fissile ratio, resulting from the large density difference, and to the fact that the core volume was only 2500 liters.

Table 2 shows the effect of replacing some or all of the UO_2 by ThO_2 in the 7800-liter oxide core. The reduction in the positive sodium void coefficient is insufficient. In view of the fact that only small improvements were obtained by thorium addition, this line of approach was dropped.

TABLE 2

COMPARISON U-238 AND Th-232 - FERTILE MATERIAL

YOM-16 Group Model - 7800 Liter Cylinder

	<u>U-238</u>	<u>U/Th=1</u>	<u>Th-232</u>
k_{eff}	1.00	1.00	1.00
$\frac{\text{U} + \text{Th}}{\text{Pu}}$	4.68	4.35	4.05
$\frac{\Delta k}{k}$ 10% loss	0.0020	0.00175	0.00131
pert. components:			
spectral and absorption	0.0046	0.0044	0.0042
leakage	-0.0010	-0.0010	-0.0011
total	0.0036	0.0034	0.0031
IBR	0.888	0.85	0.820
BR	1.19	1.15	1.12
Pu comp.:			
Pu-239	0.70	0.70	0.70
Pu-240	0.25	0.25	0.25
Pu-241	0.05	0.05	0.05
fuel V.F.	0.22	0.22	0.22
<u>productions U-238 + Th</u> total productions	0.145	0.108	0.071

4. INTERNAL ANNULAR BLANKETS

The leakage component predominates in small fast reactors, making the sodium temperature coefficient negative. Studies were carried out to see if sufficiently large sinks of neutrons could be created in the large oxide core by an arrangement of internal annular blankets. The configurations considered are described in Table 3. The basic annulus thickness was 4-1/2 in. corresponding to the thickness of the proposed subassemblies. The number of annular blankets varied from one to five and the thicknesses from 4-1/2 to 18 in.

At first sight, inspection of the results shown in Table 3 indicates that significant reductions in the positive sodium coefficients have been achieved in the cases of the 9- and 18-in. blankets. However, the active core volumes have decreased so much as to make it impossible to generate 2500 Mw of heat in these two cores. The thickness of the annular neutron sink can be reduced by having a moderating zone with a thin blanket on either side. A 4-1/2-in.-thick annular blanket having a 2-1/2-in. center zone of stoichiometric zirconium hydride reduces the sodium temperature coefficient by a factor of two with only a 10 percent reduction in the active core volume. The stability of ZrH_2 at high temperatures and the effects of rapid build-up of plutonium in the uranium section would be the two main problems. Furthermore, the almost complete decoupling of the inner and outer cores might lead to control problems.

An approach which is indicated by the results of the annular blanket study is to eliminate the center core zone, i.e., have an annular core of sufficient volume and of a certain thickness such that 2500 Mw of thermal power can be removed and the sodium temperature coefficient is zero or negative. Thus, as in the pancake-type core, the high leakage would be the main mechanism for negating the sodium coefficient.

Since a thin annular core of large internal diameter is effectively a rolled-up-pancake core, a study of the effect of core height on the sodium coefficient of the pancake core could be applied to the annular case for predicting the required thickness.

TABLE 3

SODIUM WORTH IN CYLINDRICAL CORE HAVING ANNULAR BLANKETS

core height - 50 in.
core diameter - 100 in.

core 0.40 Na, 0.31 oxide
blankets 0.30 Na, 0.51 oxide

	radial configuration (R in inches)	core volume - ℓ	U/Pu	full Na loss, % Δk		
				core	blankets	total
1		7740	6.61	3.81	--	3.81
2		7040	5.83	3.09	0.54	3.63
3		6220	5.15	2.66	0.94	3.60
4		4280	2.26	2.33	2.62	4.95
5		4720	3.96	1.53	1.27	2.80
6		4720	4.36	1.22	0.20	1.42

* C (core)

** BL (blanket)

5. THIN SLAB CORES

Calculations were made on cores, 1-, 2-, and 3-ft thick and of constant diameter, 100 in. The results, tabulated in Table 4, show that, for loss of sodium from the core only, the core height for zero reactivity change would be about 9 in. For loss from core and axial blankets the null height is about 15 in. The necessary diameters to achieve a power output of 2500 Mw from these two cores are about 25 and 19 ft respectively. Alternatively, the power output can be achieved by stacking the required number of smaller diameter cores above each other with a decoupling axial blanket between each core.

The consequences of a meltdown in such an arrangement preclude its serious consideration in a safe design. A vessel diameter of about 25 ft, the large number of subassemblies, and reduced blanket effectiveness or more complicated element design would be the major disadvantages of the single pancake core.

TABLE 4

SODIUM WORTH IN THIN SLAB CORES

<u>fuel thickness,</u> <u>ft</u>	<u>core</u> <u>volume, l</u>	<u>U/Pu</u>	<u>IBR</u>	<u>BR</u>	<u>Na in</u> <u>core, %</u>	<u>Na in</u> <u>blanket, %</u>	<u>Δk_e, %</u>
1	1570	2.26	0.29	0.97	90	100	0.03
					100	90	-0.06
					90	90	-0.03
2	3140	3.66	0.52	0.98	90	100	0.11
					100	90	-0.02
					90	90	0.09
3	4710	4.36	0.66	0.99	90	100	0.19
					100	90	-0.01
					90	90	0.18
					0	100	1.67
					100	0	-0.19
					0	0	1.48

6. THE USE OF AN AXIAL SODIUM REFLECTOR

An annular core 9-in. thick would require a very large diameter for a power output of 2500 Mwt. To achieve a negative sodium void coefficient with a reasonable core thickness, the assistance of another mechanism is required.

The most probable mechanism for the creation of sodium voiding in the core is the onset of sodium boiling. When this occurs, the hydraulic resistance of the channel increases, causing a sodium flow-rate drop and subsequent rapid ejection of sodium from either the whole or the top half of the channel. If a gap is left between the core and the top axial blanket region in the subassembly, then, under normal operating conditions, this gap would be filled with sodium which would act as a fairly efficient reflector. Sodium boiling in a subassembly would cause ejection of some core sodium followed rapidly by voiding of the sodium reflector region. By suitable choice of reflector dimensions, it might be possible for the loss in reflectivity to more than offset the gain in reactivity due to sodium loss from the core.

One-dimensional slabs with buckling factors applied to simulate transverse leakage were used in the first series of calculations. Table 5 shows the model and gives the results of these preliminary calculations for slabs 3-ft and 5-ft high. The reflectors consisted of 10 v/o stainless steel (subassembly walls) and 90 v/o sodium. In the 3-ft-high core, loss of sodium from the core and reflector has a negative-reactivity effect.

It was suspected that in the above pseudo two-dimensional calculations, the effectiveness of the reflector as a safety device would be calculated too optimistically, i.e., the CRAM code simulates the radial leakage by introducing a DB_F^2 as a capture cross section in every zone and since the D in the almost empty reflector region (10% s.s.) is large, there may be an excessive number of neutrons lost in the DB_F^2 . Table 6 compares the values of DB^2 and the effective macroscopic absorption cross sections of the mixtures in the three reactor zones in both the full and zero sodium cases. Generally, the value for DB^2 is only a few percent of the total Σ_a . The case of the reflector zone without sodium is an exception. There, the DB^2 is about 60 percent higher than the Σ_a because only 10-percent dense stainless steel is present. However, the effect on the reactivity of extra absorption in this region would be small; the main effect is still the loss in the transport effect.

On the other hand, the calculations tend to be too pessimistic since the 10-percent steel in the voided reflector region is assumed to be homogeneous whereas in practice there would be strong effects due to streaming. Studies in which the steel density in the reflector was reduced were made later in two-dimensional geometry to estimate the effect of streaming on the effectiveness of the sodium reflector as a safety feature.

Another feature which might enhance the effectiveness of the reactivity loss caused by reflector voiding is the replacement of the upper axial blanket by a neutron absorber region. The penalty in breeding is negligible and the effect on the reactivity when sodium is present is small.

TABLE 5

REACTIVITY EFFECTS OF AXIAL SODIUM REFLECTOR

<u>region losing 100% sodium</u>	<u>resulting Δk_{eff}, %</u>	
	<u>3-ft core</u>	<u>5-ft core</u>
core	+1.17	+2.42
core, 2 reflectors	-6.38	-0.47
core, 2 reflectors, 2 blankets	-6.53	-0.52
core, 1 reflector	-2.61	+0.98
core, 1 reflector, 1 blanket	-2.69	+0.96

TABLE 6

COMPARISON OF REAL AND PSEUDO ABSORPTION CROSS SECTIONS

	<u>real absorption</u>	<u>diffusion coefficient</u>	<u>pseudo absorption*</u>
core	0.0265	1.54	0.0004
core (no sodium)	0.0210	1.92	0.0006
reflector	0.0124	2.62	0.0008
reflector (no sodium)	0.0022	12.53	0.0036

*5-ft high slab simulating 100-in. diameter core ($B^2 = 0.000287$)

It was decided that in the (R, Z) two-dimensional calculation of the chosen design a 12-in.-thick sodium reflector and a 6-in.-thick B₄C absorber region would be used. Later calculations in (XY) two-dimensional geometry showed that 18 in. of sodium was as good as 12 in. of sodium and 6 in. of absorber. The actual arrangement can be left optional until some studies have been made to see how much reflector coolant can be expected to be blown out by the expanding sodium vapor from the core. Another possible innovation is the interconnection of the sodium in the reflector region by holes in the subassembly walls. This could lead to sodium boiling in several channels expelling sodium from the whole reflector region.

As mentioned above, the effects of streaming in the 90 percent voided reflector region could be important. To investigate this effect, some two-dimensional calculations were performed in XY geometry. The large annular core can be simulated fairly well by an infinitely long core of rectangular section. Economy on mesh points (for reducing the running time of the two-dimensional problem) can now be gained in the horizontal direction by assuming symmetry about the core center. For this side study, symmetry in the axial direction was also assumed. The running time of the calculation on the IBM-7094 was thus reduced from about 35 min for the RZ case to about 5 min for the XY case.

The results of the XY studies are shown in Table 7. Three cases were studied:

(1) 10-percent stainless steel; 90-percent sodium reflector, 12-in. thick; a 6-in. absorber region containing natural boron in the form of B₄C.

(2) 10-percent stainless steel; 90-percent sodium reflector, 18-in. thick; no absorber region.

(3) Same as (1) but with 1-percent stainless steel and 99-percent sodium.

If the assumption is made that when the reflector contains sodium, the 10-percent steel acts as a homogeneous 10-percent steel, but when sodium is lost the 10-percent steel acts, because of streaming, more like 1-percent homogeneous steel. Then the reactivity change on loss of sodium from core and reflectors is $0.9665 - 0.9987 = -0.0322$. This is a factor of two greater than the case where the steel is assumed to be a homogeneous 10-percent under all conditions. This is probably an upper limit that can be prescribed to the correction effect for neutron streaming. The steel concentration in the reflector region could probably be reduced by perforating the sub-assembly walls without too much loss of strength.

Other calculations have shown that the absorber is not effective as was expected, even when enriched boron was used, because the albedo of the absorber region is about 0.8 to 0.9 for the fast neutrons leaking from the core. It is possible that the presence of a hydride moderator layer above a thin enriched boron carbide region would reduce the albedo.

TABLE 7

SODIUM REFLECTOR STUDIES ON ANNULAR CORE (XY GEOMETRY)

	<u>1% steel</u>	<u>10% steel</u>	
reflector thickness	12 in.	12 in.	18 in.
absorber thickness	6 in.	6 in.	0 in.
k_{eff} (no loss of sodium)	0.9955	0.9987	0.9939
k_{eff} (loss of sodium from core)	0.9976	1.0002	0.9985
k_{eff} (loss of sodium from core and reflectors)*	0.9665	0.9823	0.9752
$\Delta k/k$ (loss of core sodium)	+0.0021	+0.0035	+0.0046
$\Delta k/k$ (loss of core and reflector sodium)	-0.0290	-0.0164	-0.0187

*For this XY Study reflectors were above and below core since an axially symmetric model is more economic on mesh points in the diffusion theory calculation.

7. ADDITION OF MODERATOR TO FUEL

Some calculations were made to see if the addition of some moderating material such as BeO could significantly reduce the positive sodium coefficient. Since the moderator must displace some core material, it was decided that for a clearer interpretation of the effect some of the core steel should be displaced. A change in fuel or sodium concentration would tend to obscure the reasons behind any observed changes in the coefficient.

The results of a 10-percent BeO addition are given in Tables 8, 9, and 10. There appears to be no significant change in the effect on reactivity of core sodium loss compared with the reference case. The neutron importance curves and the spectral and absorption components for the various groups are compared with the reference case in Figs. A.1, A.2, and A.3 of this Appendix. The slope of the neutron importance above 10 Kev is not as steep; hence the spectral component is reduced, but this is offset by the increased capture in the 2.8 Kev sodium resonance (groups 15, 16, and 17). Also, there is a reduction in the core leakage which would offset the gain.

The similarity of the internal breeding ratios, in view of the change in Pu-239 and Pu-241 α with moderation, is at first surprising. Since the BeO replaced steel in the core, parasitic captures in the steel, which normally account for as much as 12 percent of total captures in the core, were reduced over 30 percent.

The above results were based upon calculations which used the YOM-BNL 325 data for the 2.8 Kev sodium-capture resonance. When the Schmidt data was applied to the results as a correction factor, the sodium-loss effect in the moderated core was about 25 percent lower than in the reference case.

With a core plus radial-blanket breeding ratio of only 1.02, it is unlikely that a total breeding ratio of 1.2 could be achieved. Replacement of some fuel by the moderator would reduce the positive coefficient more, due mainly to the reduction in fertile-to-fissile atom ratio but this would reduce the breeding ratio even further.

TABLE 8

COMPARISON 7800-LITER REFERENCE AND 10-PERCENT MODERATED CYLINDER

	<u>reference cylinder</u>	<u>10% moderated cylinder</u>
k_{eff}	0.991	0.964
U/Pu*	4.60	4.40
$\Delta k/k$ 100% Na loss core	0.022	0.024
IBR*	0.880	0.870
BR*	1.14	1.08
Pu-239 + Pu-241 α	0.25	0.335
core composition - fuel	0.22	0.22
s.s.	0.306	0.206
Na	0.40	0.40
mod.	-	0.10
Pu composition - Pu-239	0.70	0.70
Pu-240	0.25	0.25
Pu-241	0.05	0.05

*Estimated at $k_{\text{eff}} = 1.0$ Assuming $\Delta m/m = 2 \Delta k/k$.

TABLE 9

NEUTRON BALANCE FOR A 7800-LITER CYLINDER*

(100% Na Loss in Core)

	neutron production		neutron loss					
	<u>$\nu(n, f)$</u>	<u>$\Delta\nu(n, f)$</u>	<u>(n, f)</u>	<u>$\Delta(n, f)$</u>	<u>(n, γ)</u>	<u>$\Delta(n, \gamma)$</u>	<u>leakage from core to blanket</u>	<u>Δ leakage from core to blanket</u>
<u>core:</u>								
s.s.					5.830	(-0.359)		
O					0.114	+0.007		
Na					1.817	(-1.817)		
U-238	9.432	(+0.765)**	3.367	+0.192	29.053	(-3.207)		
U-235								
Pu-240	4.212	(+0.802)	1.295	+0.218	3.250	(-0.562)		
Pu-239	75.889	(+0.619)	26.056	(-0.415)	6.769	(-1.258)		
Pu-241	8.344	-0.287	2.797	(-0.160)	0.449	(-0.081)		
F.P.					1.136	(-0.214)		
totals	97.877	(+1.899)	33.516	(-0.166)	48.418	(-7.490)	15.943	+9.555
<u>blanket:</u>								
s.s.					0.242	+0.062		
O					0.008	+0.002		
Na					0.199	+0.049		
U-238	0.848	(+0.220)	0.303	+0.070	7.022	+1.516		
U-235	0.417	(+0.117)	0.173	+0.044	0.068	+0.016		
Pu-240								
Pu-239								
Pu-241								
F.P.								
totals	1.265	(+0.337)	0.476	+0.114	7.541	1.645	10.049	+5.898

*Volume: s.s. - 30.6%; Na - 40%; oxide - 22%.

**() indicates reactivity increase.

TABLE 10

NEUTRON BALANCE FOR A 7800-LITER CYLINDER*

(10% moderator in core and 100% Na Loss)

neutron production		neutron loss					
$\nu(n, f)$	$\Delta\nu(n, f)$	(n, f)	$\Delta(n, f)$	(n, γ)	$\Delta(n, \gamma)$	leakage from core to blanket	Δ leakage from core to blanket
<u>core:</u>							
s.s.				4.0328	(-0.0472)		
O				0.1214	+0.0074		
Na				2.3058	(-2.3058)		
U-238	9.2972	(+0.7420)**	3.4044	+0.1845	32.4253	(-1.8313)	
U-235							
Pu-240	3.5556	(+0.4353)	1.1165	+0.1094	4.2442	(-0.4598)	
Pu-239	72.6215	(+0.6556)	25.6771	(-0.4016)	8.9793	(-0.8978)	
Pu-241	8.5757	-0.0442	2.9401	(-0.0856)	0.6173	(-0.0712)	
F.P.				1.5596	(-0.1840)		
totals	93.9900	(1.7888)	33.1382	(-0.1934)	54.2857	(-5.7897)	6.5661 +7.7719
<u>blanket:</u>							
s.s.				0.2101	+0.0417		
O				0.0109	+0.0021		
Na				0.1765	+0.0421		
U-238	1.0032	(+0.2126)	0.3673	+0.0675	6.1144	+1.0551	
U-235	0.3582	(+0.0787)	0.1530	+0.0292	0.0615	+0.0111	
Pu-240							
Pu-239	1.0585	(+0.2194)	0.3749	+0.0670	0.1213	+0.0161	
Pu-241							
F.P.							
totals	2.4199	(+0.5108)	0.8951	+0.1637	6.6948	+1.1682	4.9862 +4.6512

*Volume: s.s. - 20.6%; Na - 40%; oxide - 22%; mod. - 10%.

**() indicates reactivity increase.

8. RADIAL POWER FLATTENING BY MULTIPLE ENRICHMENT

Radial power flattening by variable enrichment allows a smaller cylindrical core for a given power output. Thus, four zones of different enrichment enable the same power to be generated in a 5000-liter core as in a uniformly loaded 7800-liter core. The sodium-void coefficient is reduced because of the increased leakage. Table 11 shows the results of k_{eff} and perturbation calculations on the flattened cylindrical design. The same core volume fractions as in the reference cylinder and annular core were used and the enrichment in the four zones was adjusted to make the peak power the same in each zone. Loss of sodium from the center zone causes a reactivity increase of about \$2.5 and from the whole core, about \$5. This is an improvement over the 7800-liter cylinder, but the breeding-ratio requirement of 1.2 would not be met. An increase in fuel-volume fraction would be necessary to achieve this but the sodium-void coefficient would also increase. Also, burnup in this type of core will shift the power and sodium-worth distribution toward the normal unflattened design due to greater breeding at the center.

TABLE 11
EFFECT OF ZONE LOADING ON SODIUM WORTH

zone	R, cm	volume, ℓ	U/Pu	BR	P, Mwt	100% Na loss, ($\Delta k/k$) PERT
1	54.6	1238	5.61	0.17	591	0.0098
2	77.2	1238	4.87	0.16	576	0.0084
3	94.6	1236	4.02	0.13	551	0.0054
4	109.2	1238	2.75	0.10	510	0.0046
blanket	154.9	5009		0.54	2228	0.0282
				1.10		

region losing 100% sodium

zone 1

zones 1 - 4

Δk_e

0.0075

0.0151*

core volume fractions

fuel	0.22
Na	0.40
s.s.	0.306
void	0.074

Pu composition

239	0.70
240	0.25
241	0.05

*Total Na worth by PERT calculation is higher than worth by Δk_e calculation due to central plane flux values used in PERT method.

9. SPECIFIC EFFECTS

Several one-dimensional, radial CRAM calculations have been made to determine the sodium coefficient dependence upon the presence, in the reference design, of specific materials. Perturbation theory has been used to estimate the effect of full sodium loss. The k_{eff} values obtained in this work have also been collected.

Reactivity losses resulting from full sodium voiding have been investigated for blanket-plutonium content variation, core fission product and Pu-240 presence, for a 5-percent fuel-for-sodium swap, and for an initial (equilibrium cycle) core with full boron shim control inserted. The results of these investigations are presented in Table 12.

TABLE 12
EFFECT OF CORE MATERIALS ON THE Na COEFFICIENT

<u>reactor condition</u>	<u>k_{eff}</u>	<u>$\Delta k/k$</u> <u>100% Na loss core only</u>	<u>blanket exposure</u>
reference case	1.0062	+0.0217	nominal 50k (core)
high blanket Pu-239	1.0175	+0.0226	nominal 70k
reference case - fission products omitted	1.0377	+0.0173	nominal 50k
reference case - Pu-240 omitted	0.9832	+0.0148	nominal 50k
reference case - fuel for sodium swap in 5% of core volume	1.0071	+0.0203 (+0.0232 on 40% Na vol. basis)	nominal 50k
start of cycle core with 7% of boron control inserted	1.0 (search)	+0.0264	nominal 50k

An examination of the results shows that the plutonium buildup in the blanket does not present a serious sodium coefficient problem. The fission product and control cases indicate that the sodium coefficient will decline slightly during a fuel cycle because the boron withdrawal overrides fission-product buildup (the decrease by reducing control is $0.0264 - 0.0217 = 0.0047$, while the comparable increase from FP addition is $2/3 (0.0217 - 0.0173) = 0.0029$).

Isolating the Pu-240 contribution to the sodium reactivity defect shows the importance of the large fission cross section above the 0.2-Mev threshold. A serious problem could arise from this phenomenon, if a negative sodium coefficient is proved to be imperative, as the fuel-cycle investigations which have been done for this study indicate that a large amount of Pu-240 accumulates in these oxide reactors.

A very small penalty in terms of the sodium coefficient is shown for the fuel-for-sodium-swap case. If such a change can be made feasible from an engineering standpoint, a very significant improvement in the fuel cycle and control requirements can be realized. The calculation involved shows a relative internal breeding ratio gain from 0.54 to 0.70. This is a qualitative result because no recalculation of resonance scattering and resonance absorption appropriate for the new composition was made.

ADJOINT FLUX DISTRIBUTION

FIG. A.1

7800-liter oxide core - 22% fuel (70% Pu-239;
25% Pu-240; 40% sodium)

24-group set

ELMOE and resonance self-shielding corrections

ENERGY DEPENDENCE OF SODIUM WORTH

FIG. A.2

ENERGY DEPENDENCE OF SODIUM WORTH WITH 10 PERCENT MODERATOR

FIG. A.3

APPENDIX B

COMPARISON OF ANNULAR AND CYLINDRICAL CORES

For comparison with the reference annular core, a cylindrical core was set up which also satisfies the requirements of the study, with the possible exception of an inability to produce 2500 Mwt because of the high peaking factor. The core is a right cylinder 60 in. high and 100 in. in diameter. A diagram of the core is shown in Fig. B 1.

Since the study specifies a breeding ratio of at least 1.2, it became necessary to increase the internal breeding ratio of the cylindrical core used in the preliminary calculations. As a result, the effective oxide density was increased from 79.4 to 91.3 percent of theoretical. As this includes voids due to the gap between cladding and fuel, the actual cylindrical core pellet density is somewhat higher and in fact is greater than theoretical density. Obviously, in actual practice the extra fuel would replace either sodium or structure or, perhaps, both. A tabulation of comparable results based on two-dimensional calculation of the annular cylindrical cores is given in Table B-1. A comparison of reactivity effects as the hottest subassemblies become void is given in Fig. B.2.

The annular core meets or exceeds all requirements specified for the study and is superior to the cylindrical core in all but three of the major areas of comparison. These areas are over-all core and hence vessel dimension, critical mass, and internal breeding ratio.

If a negative sodium coefficient is set as the defining design criterion, the advantages of the cylindrical core are lost. Side studies described in Appendix A indicate that a pancake design is necessary for a negative coefficient in a cylindrical oxide core. In such a high leakage configuration the critical mass would increase and the internal breeding rates would decrease. In addition the core diameter would become very large because of heat transport considerations.

The superiority of the annular core is especially evident in terms of sodium worth. If the entire core plus the upper reflector loses all sodium, the reactivity change is -0.0074 . A corresponding sodium loss in the cylindrical core results in a reactivity change of $+0.031$. Similarly, sodium boiling and hence voiding in the hottest subassemblies is less dangerous in the annular core. Although at a given fraction of maximum power, e.g., 0.95, many more subassemblies are void in the annular core, the total reactivity change is still less than that for the cylindrical core. This comparison is demonstrated in Fig. B.2. Here voiding is considered for the core only.

In a core design having a sodium void effect of more than a couple of dollars, it is unlikely that the full reactivity can be released before the core blows itself apart. The essential approach appears to be to prevent super-prompt criticality which is much easier in a core having an inherently low positive sodium-void coefficient.

TABLE B-1

COMPARISON OF ANNULAR AND CYLINDRICAL CORES

	<u>annulus</u>	<u>cylinder</u>
Core volume, liters	7828	7722
Critical mass - Pu-239 and Pu-241, kg	2914	2222
U/Pu	2.85	4.81
Fuel volume fraction	0.235	0.272
IBR	0.54	0.93
BR.	1.34	1.19
$\frac{\Delta k/k}{\Delta m/m}$ edge		
inner.	0.36	-
outer.	0.25	0.14
total.	0.52	0.55
$\Delta k/k$ 100% Na loss, core PERT . . .	+ 0.0102	+ 0.0331
$\Delta k/k$ 100% Na loss, core & U.R. Δk	- 0.0074	+ 0.0309
$\frac{P_{max}}{P}$	1.52	2.36
Blanket volume, liters	1.71×10^4	1.28×10^4

An additional advantage of the annular core lies in the greater plutonium worth at the core boundaries. The ratio $\Delta k/k/M_{pU}/M_{pU}$ is 0.36 and 0.25 at the inner and outer core blanket interface, respectively, whereas the same ratio at the core blanket interface of the cylindrical core is only 0.14. Thus, a reasonable uncertainty of 5 percent in k would mean an uncertainty of 36 percent in critical loading (edge addition) for the cylindrical core and one of 17.4 percent in the annular core.

MODEL FOR CYLINDRICAL CORE CALCULATIONS

FIG. B.1

FIG. B.2

REACTIVITY INSERTION WITH SODIUM VOIDING

APPENDIX C

MECHANICAL CONSIDERATIONS IN THE SELECTION OF THE CORE DESIGN

In the selection of the core for the 1000-Mwe breeder reactor the initial approach was that of a right-cylindrical core. As discussed in Sec. 4.1 the existence of a large positive sodium voiding coefficient led to the consideration of alternate geometries. Initially, an annular blanket was considered as a method of increasing the leakage of the core to reduce the sodium voiding coefficient. When this approach was unsuccessful and a very thin core showed promise of achieving the desired results both the annular core and a sandwich core were considered (see Fig. C.1). The sandwich core had the apparent disadvantages of two distinct core regions, since, in order to retain the characteristics of the thin core it would be necessary to essentially decouple each of the core regions. Problems with the control of this arrangement seemed rather severe. This led ultimately to the choice of the annular core as the reference.

Once the basic core geometry had been chosen it was necessary to determine the manner in which it would be cooled. Various methods of flowing coolant through the core were considered and are shown in Fig. C.2. Experience with EBR-I showed the possibility of dynamic nuclear instabilities with a series flow arrangement, although this arrangement simplified the cooling of a large blanket region that does not generate much heat. Radial flow of the coolant has advantages when pressure drop and temperature rise of the coolant are considered although it imposes severe mechanical restrictions on the design of the core and core supporting structure. The choice of parallel upflow was made on the basis of mechanical simplicity and the achieving of a thermal arrangement that is best suited to natural convection cooling in the event of loss of pumping power.

In the selection of fuel assembly geometry the various arrangements shown in Fig. C.3 were considered. The most desirable assembly geometry would minimize pressure drop and structural material while providing maximum rigidity against fuel movement. The ability to remotely fabricate the fuel assembly was also a factor in the selection of the fuel assembly geometry. The arrangement of fuel in short pins perpendicular to the direction of flow appeared to be a promising concept to minimize the effects of fuel slumping. However, the pressure drop with this arrangement was considered abnormally high. The selection of the parallel flow integral fuel and axial blanket assembly with spacer discs between pellets and spacer grids between pins was made as the reference assembly. This geometry minimizes fabrication cost, allows remote handling, results in a low pressure drop, and minimizes the possibility of fuel movement during operation of the reactor. A hexagonal geometry for the fuel assembly was chosen as it was felt that assembly handling during reactor fueling would be simplified and that the required thickness of the fuel assembly shroud could be less than for a square assembly.

The annular core was compared to the more conventional cylindrical core in order to assess whether or not a mechanical or economic penalty was being paid for this design. A comparison of the annular core with a typical cylindrical geometry and a "pancake" assembly can be seen in Fig. C.4. The pancake has the same high leakage characteristics as the annulus but a penalty in assembly length is incurred with the thicker low efficiency axial blanket. It is apparent that for similar thermal and hydraulic design

criteria the annular core is superior to either the cylindrical or pancake because of improved power distribution. The mechanical arrangement of the annulus provides a maximum space of high worth for control rod assemblies thus minimizing the number required and further provides for a blanket management program which is essentially independent of the core fuel cycle management.

It is concluded that the annular core provides a means of achieving a small positive sodium void-coefficient without penalizing the mechanical design of the reactor.

1. Cylindrical core

2. Annular blanket

3. Annular core

4. Sandwich core

BASIC CORE CONFIGURATIONS

FIG. C.1

BASIC CORE FLOW PATTERNS

FIG. C.2

ELEMENT ARRANGEMENT

parallel flow
separate fuel and blanket

parallel flow
integral fuel and blanket

cross flow-fuel,
parallel-flow blanket

cross flow-fuel
and blanket

ELEMENT SPACERS

spiral wire

ferrules

grids

FUEL SPACERS

unsegmented

spacer disc

cylinder

$$L/D = 0.6$$

$$P_{\max}/P_{\text{avg}} = 2.4$$

annulus

$$P_{\max}/P_{\text{avg}} = 1.5$$

pancake

$$L/D = 0.13$$

$$P_{\max}/P_{\text{avg}} = 2.1$$

COMPARISON OF CORE GEOMETRIES

FIG. C.4

APPENDIX D
PHYSICS DATA

CONTENTS

1.	<u>GENERAL</u>	D-1
2.	<u>NUCLEAR DATA</u>	D-1
2.1	LIGHT-ELEMENT DATA	D-1
2.2	HEAVY-ELEMENT DATA	D-1
3.	<u>MATERIALS DATA</u>	D-2

1. GENERAL

A description of the nuclear and materials data used in the physics analysis described in Sec. 4.1.3 follows. The nuclear data pertains to reactor statics, and the materials data was used when calculating temperature coefficients.

2. NUCLEAR DATA

2.1 LIGHT-ELEMENT DATA (Sodium, Oxygen, Stainless-Steel Components)

Elastic removal and transport group cross sections are computed by the ELMOE code for a particular mixture. The total cross sections in the form of Legendre coefficients are on the ANL Legendre coefficient library. Values for μ and hence the elastic transport and total transport are calculated from the library. The library cross-section data is mainly BNL-325 data but with Hibden's measurements included for sodium.

Inelastic scattering - the YOM values were used.

Capture cross-sections - the YOM values were used for groups 1 to 14.

For groups 15 to 24 the group capture cross sections were calculated using BNL-325 data and spectrum of a typical oxide core. As mentioned above Schmidt's data for sodium was later accepted as being the best data available.

2.2 HEAVY-ELEMENT DATA (Uranium, Plutonium, and Fission Products)

1. Elastic removal and transport cross section - In groups 1 to 14, the YOM values were used, groups 14 to 24 were calculated from BNL-325 data and a typical oxide spectrum.

2. Inelastic scattering cross sections - The YOM data were used.

3. Neutron yield per fission - YOM data with Davey's modification for U-235.

4. Capture and fission cross section - In the nonresonance region the YOM data for the fission and most of the capture cross sections were used.

For Pu-239 the Hopkins and Diven data on alpha were used. In the resonance region the group capture and fission cross-section curves of Fischer were normalized to the infinite dilution value evaluated from BNL-325 data. For fission products the capture cross sections were taken from Greebler's 18-group set. The scattering and transport cross sections were assumed (arbitrarily) to be the same as iron. Considering the small quantities of fission products involved, the error introduced by this last approximation is negligible.

3. MATERIALS DATA

The materials data need only be discussed for the fuel, because for all other materials there are standard values available. The thermal conductivity of the mixed oxide was assumed equal to that of UO_2 . The formula used here for thermal conductivity is

$$k = \frac{30}{T + 10} \frac{\rho}{\rho_o} + 2.55 \times 10^{-12} T^3$$

where: T = temperature in K,
 k = thermal conductivity in watts/cm- $^{\circ}\text{C}$,
 ρ = actual oxide density, and
 ρ_o = theoretical oxide density.

The increase with the third power of T at high temperature was calculated from extrapolated optical properties of UO_2 and does not seem well confirmed, although it is compatible with experiments where melting occurs. There is, however, a wide divergence in the experimental data. The maximum fuel temperature at steady state for the pin with a center hole will now be calculated in different ways (the surface temperature is constant at 1950 F):

(a) For a solid pin with the same quantity of fuel as the hollow pin; thermal conductivity averaged over the range important for the average channel ($k = 1.6$ Btu/hr-ft- $^{\circ}\text{F}$)	4250 F
(b) Hollow pin, average k	3820 F
(c) Hollow pin, variable k	3720 F

As there is a difference of about 400 F between the two pin models, the hollow-pin model was used throughout. However, calculations were done in some cases with the constant average thermal conductivity.

The numerical values can be well approximated, by the following quadratic function of the temperature for use in the FORE code:

$$C(T) = 0.0686 + 6.9 \times 10^{-6} T + 7 \times 10^{-10} T^2 \text{ (Btu/lb-}^{\circ}\text{F)}.$$

Due to lack of a better method, the melting point of the mixed oxide was calculated by linear interpolation between the melting temperature of UO_2 (5000 F) and of PuO_2 (4130 F). The result is 4770 F.

The data for the other materials were mostly taken from Tebo's compilation. For quick check on the temperature coefficients, the coefficients of thermal expansion used (1000 F) are listed:

(a)	Stainless steel (clad and structure)	= $8.9 \times 10^{-6}/^{\circ}\text{F}$
(b)	Mixed oxide $\text{PuO}_2\text{-UO}_2$	= $5.5 \times 10^{-6}/^{\circ}\text{F}$
(c)	Volume expansion of sodium	= $1.62 \times 10^{-4}/^{\circ}\text{F}$

APPENDIX E
SAFETY STUDIES

CONTENTS

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1. GENERAL

The safety studies carried out for the 1000-Mwe project cover the investigation of the important credible accidents and the definition and qualitative evaluation of the problems associated with hypothetical accidents. Major credible accidents can be classified as reactivity insertions, caused for example by accidental control-assembly runout, and loss of primary coolant flow, caused by pump failure. The resulting transients are normally terminated by scram of the reactor. There are a number of minor accidents such as plugging of one subassembly which deserve comment. It could be shown that none of the credible accidents leads to damage of a significant portion of the core, nor does any of them constitute a hazard to the operating staff or the environment.

A complete investigation of hypothetical accidents is beyond the scope of this study. The object of these accident studies is to obtain a first impression of the problems involved and a better understanding of the safety aspects of very large oxide systems.

The major requirement in designing a reactor is that no credible accident should lead to major damage of the core. It is difficult to say that, given a particular accident, major damage to the core has or has not occurred. Therefore, major damage has been defined in terms of certain conditions rather than in terms of accidents. The existence of any one of the following condition is assumed to indicate that major damage has occurred.

(1) Coolant boiling in more than one or two subassemblies of the core. The boiling point at the pressure of the primary system is 2000 F. Thus there is a wide difference between operating and boiling temperature and the onset of widespread boiling is thereby suppressed under normal conditions.

(2) 80 percent of the fuel pin cross section at the hottest point in the reactor reaches melting temperature. It should be noted that some fuel melting is tolerable as oxide fuel pins can be operating with part of the fuel melted. This criteria defines the point at which failure occurs.

(3) Clad temperature in excess of 2000 F. At this temperature, the strength of the clad material decreases markedly.

2. CREDIBLE ACCIDENTS

Credible accidents are defined as failure of a single component, such as runout of a control assembly at normal speed, unintentional insertion of a subassembly during core loading, or failure of the primary circulation pumps. The transients following such failures will be terminated by scram, caused by high neutron flux, short positive period or negative period. One can, however, go one step further and ask what happens if the scram also fails. These are hypothetical accidents, because they involve two independent but simultaneous equipment failures.

The slow reactivity insertion by control assembly runout at normal speed (about 1 c/sec) was investigated at full power and at source power. The first case does not constitute a true transient, because the deviation from the steady state is very small all the time. The problem is only whether the total inserted reactivity can be compensated by the steady-state power coefficient, without exceeding the failure limits. The important data are plotted in Figs. E.1 and E.2. Both abscissas are the power in percent of the normal power. Fig. E.1 shows the critical quantities, namely the fraction of the fuel-pin cross section melted at the hottest point, and the maximum temperature of clad and coolant. Fig. E.2 shows the feedback due to the Doppler coefficient and the expansion coefficients. The curves show that \$1 feedback is reached at 360 percent power, at which point 94 percent of the fuel cross section at the hottest point is molten and the clad temperature is 2000 F. The requirement that not more than 80 percent fuel melts leads to a maximum reactivity insertion of 83 ¢, a power of 310 percent and a maximum clad temperature of 1850 F, and coolant temperature of 1760 F. As one control assembly is worth about 65 ¢, and one subassembly 30 ¢, conditions would not exceed failure limits even for the accidental runout of a control subassembly at full power.

It was assumed for these calculations that the coolant inlet temperature stays at its normal value, or the heat sink has enough capacity to remove the heat generated without an appreciable rise in the primary coolant temperature at the core inlet.

It must be pointed out that the credible accident is described by the range of the curves between 100 and 125 percent, at which point scram due to high flux sets in. No fuel melting occurs.

The accidental runout of a control assembly or insertion of a fuel assembly at source power is terminated by a scram on short period. If scram is set for a period of 20 sec and the insertion rate is 1 ¢/sec, scram occurs after 25 sec (Fig. E.3). If the scram fails, the power rises on a rather short period before the power range is reached and heating of the core materials produces a compensating feedback reactivity. The run shown in Fig. E.3 starts at 10 ¢ subcritical with a source power of 45 w. The ramp of 1 ¢/sec ceases when the external reactivity reaches \$1. This is very conservative, because a control assembly or subassembly is worth much less than \$1.

The shortest period of about 1.3 sec is reached after 80 sec. After that, the feedback reactivity leads to a gradual leveling off of the power. The period is not short enough to produce an overshoot. The equilibrium power which will eventually be approached with a relaxation time determined by the delayed neutrons is about 2900 Mw, i.e., 30 percent over a reference power level of 2250 Mw. The fuel and coolant temperatures are shown in Fig. E.4. Substantial heating sets in after 80 sec and produces feedback reactivity, Fig. E.5. Thus the power levels off, and neither temperature reaches the failure value.

It should be mentioned that the time constants of the heat transfer are rather short because of the small pin diameter. The two constants which are characteristic for the transfer function from power oscillations to oscillations of the fuel and coolant temperatures are the fuel-time constant and a combination of the coolant flow time with the ratio of heat capacities of fuel and coolant. For the 1000-Mwe reactor these values are:

Fuel-time constant	2.1 sec
Coolant-time constant	<u>0.2 sec</u>
Total	2.3 sec

Thus, the changes in heat transfer are fast compared to the decay of the delayed neutrons, with an average lifetime of about 12 sec. Therefore, in all transients, the final approach to equilibrium is governed by the neutron kinetics alone.

The next credible accident examined was the decay of the primary coolant flow due to pump failure. The behavior of the flow due to the inertia of the coolant and the pumps is shown in Fig. E.6. The final constant flow of 12 percent of the full value is sustained by auxiliary pony motors. In this accident, the main effect of the flow decay is a heating up of the coolant, and also of the structure and clad, which produces through the negative expansion coefficient a decrease in power. Scram occurs due to a short negative period of 15 sec after about 2 sec. The case without scram failure is described as follows.

Although the coolant is heated up, decreasing the temperature difference between fuel and coolant which in turn determines the heat flux, the power goes down faster than the heat flux, so that more heat is removed than generated in the fuel. The maximum fuel temperature decreases producing a positive reactivity effect through the Doppler coefficient. The coolant temperature rises considerably and reaches a maximum of 1800 F. The boiling temperature at the pressure of the primary system is 2000 F, so that there is an adequate safety margin against coolant boiling. Both the Doppler and expansion reactivity are plotted in Fig. E.7. During the transient the negative net reactivity due to sodium and structure heating exceeds the positive Doppler reactivity and keeps the reactor on a negative period until eventually equilibrium is reached. It is obvious that a large negative Doppler coefficient requires a large coolant temperature rise in order to balance the positive Doppler reactivity. For this run, the thermal conductivity was assumed to be constant which is conservative.

There are several minor credible accidents which do not require computer calculations because no significant changes in reactivity and power are involved. One of them, which deserves a brief comment is the plugging of the coolant flow in one of the hottest subassemblies. If the coolant flow in one channel is stopped by blockage, the channel will be voided by coolant boiling, and the fuel will melt due to loss of heat-removal ability. This will contaminate the primary circuit and lead to a cleanup problem, but there is no danger of a nuclear excursion because the reactivity changes are small.

Voiding of the channel gives 2¢ of positive reactivity. This occurs after about 0.5 sec, when the coolant reaches the boiling point. After about 5 sec, gross fuel melting in the subassembly occurs. Even if all the fuel were to accumulate in the center, the reactivity change would only be 20¢. This will not lead to any damage to the rest of the core.

3. HYPOTHETICAL ACCIDENTS

Hypothetical accidents can be generally defined as coincidence of two or more possible, but unlikely malfunctions. This definition implies that hypothetical accidents can be conceived that lead to the destruction of the core by any of the three critical failure values specified above. It is a matter of sound judgment to stay within reasonable limits in postulating such accidents.

Before hypothetical accidents are discussed in general, a step insertion of \$1 reactivity at full power will be described. This is a truly fast excursion compared to the rather mild transients of the last section. It must be emphasized that this is a hypothetical accident. It would require blowout of two control assemblies at a time or voiding of the center core channels without voiding of the reflector.

It should be noted that even a slow insertion of \$1 reactivity melts 94 percent of the fuel-pin cross section at the hottest point and thus exceeds the specified failure limits. A step insertion certainly leads to at least as severe a condition and thus cannot be tolerated. Therefore, the purpose of this discussion can only be to demonstrate the difference between slow insertion and step insertion and to show that the Doppler coefficient is large enough so that the latter does not in fact lead to any more severe conditions than the former, except perhaps for the occurrence of thermal shocks, which will not be investigated here. If this conclusion holds for a prompt critical excursion, it can be expected to hold for any smaller reactivity step, and this was indeed confirmed by computer runs.

Fig. E.8 shows the power trace for a step insertion of \$1. The prompt critical rise is so fast that the power reaches 60,000 Mw after about 0.1 sec, and then the Doppler effect decreases the reactivity and takes the power down below its final equilibrium value of 8000 Mw. The fast transient is then terminated, and the power rises to its equilibrium value with a relaxation time determined by the delayed neutrons. This is a slow transient of the same type as those discussed in the last section.

The behavior of the temperatures of the hottest fuel pellet is shown in Fig. E.9. During the fast excursion and rapid heating of the fuel, the center reaches the melting point after 0.2 sec, and after 0.4 sec half of the cross section is at the melting temperature. However, the temperature of the circumference encompassing 85 percent of the cross section does not reach the melting point during the fast excursion, but drops slightly after 1 sec and then gradually increases to the melting point which is reached after about 15 sec. This shows that the amount of melting fuel characteristic for the asymptotic steady state is approached gradually, and it is never reached during the transient. In this sense the step insertion is not any worse than the slow insertion.

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Of particular concern are those hypothetical accidents where sodium boiling is the primary cause for the failure. Coolant boiling in a channel leads to voiding of the channel which under some circumstances can take place in a time as short as 100 msec. The consequence is the loss of the heat-removal capacity, and if this occurs in a large number of subassemblies, then meltdown of the core would result.

Certain combinations of malfunctions can lead to coolant boiling in many channels. The primary system pressure of about 50 psig raises the boiling point of the coolant by about 400 F. Loss of the pressure would eliminate this 400° advantage and in combination with control-assembly runout at full power or flow decay at full power would lead to coolant boiling in the upper portion of a large number of channels if there is no scram. Another accident of this type is decay of the primary-coolant flow, runout of the control assembly at the same time, and failure to scram. The assembly runout prevents the power decrease that usually accompanies flow decay, and then the exit coolant temperature reaches the boiling point.

The hypothetical accidents outlined here lead to a partial damage of the core and high contamination of the primary circuit. This constitutes a commercial risk, which can be taken because of the extremely low probability that at least three independent malfunctions occur at the same time. However, though the loss of the core in such an accident seems a justifiable risk, any hazard to the personnel and to the environment should be prevented. In the design that was chosen, the negative sodium-loss coefficient tends to make the accident self-terminating rather than autocatalytic.

In all the cases discussed above, sodium boiling initiates at the top edge of the center channels. The first effect on reactivity is thus negative. Due to the high specific volume of the sodium vapor, the voiding of the channel over the core height occurs within a time of the order of 1 sec.

This estimate was obtained by equating the energy inserted into the evaporating coolant at the top to the energy necessary to evaporate enough sodium to void the channel plus the energy used to accelerate the fluid column in the channel against the pressure drop across the channel. From these considerations it is obvious that the sodium reflector is voided faster than the channel because the column to be accelerated is smaller, and the normal coolant pressure drop favors an upward motion. It can be seen from Fig. 4.2 that voiding of one-third of all the channels at the center of the core produces a negative reactivity of -0.15 if the reflector portion is also voided. Thus sodium boiling does not cause a power excursion.

In the case of coolant boiling initiated at the top of the core the sodium reflector is fully effective. This would not be so in accidents where boiling starts at the axial center of the core. It can be shown that boiling will not occur at the axial center if the coolant flow is at its full rate. This is because for the two possible causes of boiling, namely flow decay and power rise, the first inherently leads to boiling at the top, and in the second case the power rise would have to be faster than the coolant

flow through the core in order to cause center boiling. As the flow time from the center to the top is about 0.1 sec at full flow, the reactor must be on a period of the same order or shorter. Thus, in order to investigate the fast power excursion due to center boiling with a significant flow, one has to postulate a fast transient already in process to create center boiling. This cannot be produced by the hypothetical failure of equipment or control devices and so is in a different class of hypothetical accidents than those discussed above.

However, the situation is different at a low coolant flow rate where the flow time from the core center to the core top is of the order of 1 sec or longer. One can indeed postulate a hypothetical accident due to equipment failure by assuming the reactor at source power and the flow at a low rate. In this case a control assembly runout which brings the reactor on a 1 sec period would be likely to cause coolant boiling at the axial center of the core. The voiding would spread upward and downward about at the same rate, and the core portion of the channel would be voided before the reflector. The reflector is not effective in preventing a fast positive reactivity insertion.

The time sequence and reactivity effect of the voiding is rather involved and has not been studied in detail. The center channel will probably be voided after some hundred milliseconds causing a reactivity of $+ \$1.5$. But within that time the power is high enough to cause boiling also in the outer channels where the reactivity effect is negative, thus reducing the net positive reactivity.

The consideration of this accident puts the requirement on the final design that there must be enough reactivity available in the Doppler coefficient to prevent a severe power excursion even under the most unfavorable time sequence.

4. FUEL-SLUMPING PROBLEM

A segmented fuel element was chosen to minimize possible reactivity effects arising from horizontal fuel cracks or gaps. Three-in. segments are proposed for this reactor. The maximum positive reactivity effects obtainable have been found to be small.

Each fuel pin is divided internally into sixteen 3-in. segments, each of which consists of 2.85 in. of oxide fuel, a 0.09-in. spacer, and, nominally, a 0.06 in. void under cold conditions. This configuration will be just tight when operating.

The first case computed concerns the possibility of reactivity change caused by fuel pellets dropping into the 0.06-in. space in the top half of the most reactive 108 sub-assemblies. It is assumed that each space is initially beneath its associated pellet, and that all of these pellets move downward simultaneously. A reactivity increase of $+6\text{¢}$ was obtained.

As a second case, the calculation was repeated using the assumption that total slumping occurs, i.e., that each pellet increases in density from an assumed 83.5 percent to 100 percent, in the same core region as before. As all slumping occurs in the upper half of the core, material transport only toward the core midplane is involved.

Appropriately integrating worth times density change gives a reactivity change of +20 ¢ in this case.

A calculation which was based upon an arbitrary choice of 12-in. segments gives an input of +80 ¢. This suggests that the slumping effect is proportional to $\frac{1}{N}$, where N is the number of segments within the core half height. Algebraic checking shows that this is true so long as the total fuel stack length, total gap and spacer stack length, and density changes are constant, and the worth function is monotonic over the section of core length involved. All these conditions are true of the cases calculated.

Fuel segmenting is disadvantageous in that any reliable, negative fuel expansion coefficient which may be shown to exist is reduced. The reduction follows the $1/N$ behavior found for slumping.

The ratio of the oxide and clad expansion coefficients is 5.5/8.9, and, under power conditions, the oxide temperature increases about 8.5 degrees per degree of clad-temperature rise. Therefore, under isothermal conditions, fuel expansion in a whole-length oxide stack would be less effective than the clad-controlled expansion of the adopted design. If the fuel should expand reliably, however, a considerable prompt, negative power coefficient may be lost by segmenting. The above data show that fuel in any unsegmented stack would expand 5 times as much as it does when controlled by the clad. The axial power expansion coefficient (fuel + clad) is $-0.181 \times 10^{-6}/\text{Mw}$; because the greater fuel expansion requires slippage between fuel and clad, the resulting coefficient would be greater than $(5)(-0.181)(10^{-6})$. In the present 16-segment fuel design, this is reduced to something greater than $(5/8)(-0.181)(10^{-6})$.

The value for the unsegmented case represents an appreciable prompt negative coefficient. It would probably be at least as great as that due to the Doppler effect (-0.628×10^{-6}). When segmenting is used, an expansion coefficient very much less than the Doppler coefficient is obtained. This represents a small contribution to the overall feedback.

EFFECT OF POWER VARIATION ON CLAD AND COOLANT TEMPERATURE AND FUEL MELTING

FIG. E.1

EFFECT OF POWER VARIATION ON REACTIVITY COEFFICIENT

FIG. E.2

EFFECT OF REACTIVITY RAMP ON REACTOR POWER

FIG. E.3

EFFECT OF REACTIVITY RAMP ON FUEL AND COOLANT TEMPERATURE

FIG. E.4

EFFECT OF REACTIVITY RAMP ON TOTAL REACTIVITY

FIG. E.5

EFFECT OF REACTOR POWER WITH LOSS OF FLOW

FIG. E.6

REACTIVITY FEEDBACK DURING LOSS OF FLOW

FIG. E.7

EFFECT OF STEP REACTIVITY CHANGE ON REACTOR POWER
(maximum power = 60,000 Mw)

FIG. E.8

EFFECT OF STEP REACTIVITY CHANGE ON FUEL TEMPERATURE

FIG. E.9

FUEL AND COOLANT TEMPERATURES DURING LOSS OF FLOW

FIG. E.10

APPENDIX F

COMPARISON OF MEASURED AND CALCULATED
SODIUM REACTIVITY COEFFICIENTS IN ZPR-III ASSEMBLY NO. 43

ZPR-III Assembly No. 43 was a zoned critical which was designed to simulate the nuclear environment at the center of a typical 4000-liter carbide core. The core assembly consisted of a cylindrical dilute central zone (162 liters), an annular filter zone of depleted uranium and graphite, and an outer driver zone of high density fuel, see Fig. F.1.

ANL performed a number of reactivity coefficients in the assembly. These included core-center measurements and whole-channel measurements on sodium. Calculations were made to see how well the cross section sets being used in this 1000-Mwe study could predict the experimental results.

The initial calculations were performed using the 16-group YOM set (group collapsed to six groups) in real two-dimensional geometry. The reactivity coefficients of the heavy absorbers, U-235, U-238, etc., agreed very well with measurements. The central and average central channel reactivity coefficients for sodium were calculated to be -3.5 and -0.01 inh/kg (note that the negative sign here implies a positive sodium-void coefficient) compared with measured values of -1.3 ± 0.4 and $+1.06 \pm 0.05$ inh/kg, respectively. The calculations were based on perturbation theory since the difference in k_{eff} calculations would be too small for the coarse convergence that economy prescribes when making two-dimensional calculations.

The central reactivity coefficient has a negligible leakage component and hence the spectral plus absorption component is overestimated by a factor of 2.6. If the spectral plus absorption component in the whole channel calculation is reduced by a factor of 2.6 then the channel average for the total coefficient is $+0.69$ inh/kg which is only 36 percent less than the measured value of $+1.09$. This can be considered only as a qualitative indication of the area in which the errors lie.

To evaluate the effects of using the 24-group cross section set and refined cross section data the later calculations were done in spherical geometry and the central coefficients were normalized to the central coefficient for U-235. The results are summarized in Table F-1. Both perturbation and Δk calculations were possible in this geometry. The calculations were performed using YOM and the 24-group cross section sets, the latter incorporating ELMOE and resonance self-shielding corrections. Also calculated was the effect of using the Schmidt data for the 2.8 kev sodium absorption resonance. Using the refined techniques and cross section data the error in predicting the sodium reactivity coefficient at the center is 200 to 370 percent too large in magnitude but of the correct sign. The same discrepancy in the spectral plus absorption component of sodium void coefficient for the annular oxide core being studied would reduce the effect of loss of sodium from the central core channels from $+1.5$ dollars to the range -0.01 to $+0.42$ dollars.

TABLE F-1

COMPARISON OF CALCULATED AND MEASURED VALUES
FOR THE CENTRAL CORE SODIUM REACTIVITY COEFFICIENTS

(inh/kg)

<u>cross section set</u>	<u>geometry</u>	<u>measured value</u>	<u>perturbation calculation</u>	<u>k_{eff} calculation</u>
YOM 6*	cylindrical	-1.3 ± 0.4	-3.5	-
YOM 16	spherical		-3.67	-4.0
YOM 6*	spherical		-3.66	-
APDA 24-ELMOE Self-shielding	spherical		-4.35	-4.18
APDA 24-ELMOE Self-shielding	spherical		-3.41	-3.34

* Group collapsed from 16 groups.

Too much credence should not be given to this crude translation of errors from the ZPR-III measurements to the annular core calculations. The geometry and core compositions are quite different; one is oxide and the other carbide. Also effects on sodium reactivity measurements such as cell inhomogeneity should be investigated. The exercise does, however, indicate how inadequate the sodium coefficient calculations are in soft spectrum cores and how sensitive the void coefficients are to errors in individual components.

CORE MODEL FOR ZPR-III ASSEMBLY 43

FIG. F.1

APPENDIX G

FUEL CYCLE AND CONTROL REQUIREMENTS

The fuel cycle for the 1000-Mw annular core has been computed from the one-group cross sections and reactivity worths resulting from specifically applicable CRAM cases. The computations utilize the following set of equations:

$$C^{38}(t_b) = C_o^{38} - bf^{38} (1 + \alpha^{38})$$

$$C^{39}(t_b) = C_o^{39} - bf^{39} (1 + \alpha^{39}) + bf^{38} \alpha^{38}$$

$$C^{40}(t_b) = C_o^{40} - bf^{40} (1 + \alpha^{40}) + bf^{39} \alpha^{39}$$

$$C^{41}(t_b) = C_o^{41} - bf^{41} (1 + \alpha^{41}) + bf^{40} \alpha^{40}$$

$$C^{42}(t_b) = bf^{41} \alpha^{41}$$

$$P_u^{42} \equiv P_u^{40}$$

$$\sum_{i=38}^{41} w^i c^i(t_b) + w^{38} \Delta C^{38} + w^{39} \Delta C^{39} = \sum_{i=38}^{41} w^i c^i(t_o)$$

$$\sum_{i=38}^{41} C^i(t_b) + \Delta C^{38} + \Delta C^{39} = \sum_{i=38}^{41} C^i(t_o)$$

where

C^i = i^{th} isotope concentration/cc.

B = Number of fissions/cc giving the desired burnup level (100,000 Mwd/T for this reactor).

w^i = Reactivity worth of the i^{th} isotope.

Nb = B , where N is, the number of times the f 's are recomputed during a cycle length defined by B .

f^i = the fraction of core fissions contributed by the i^{th} isotope.

$C^{38, 39}$ = the makeup concentrations of U-238 and Pu-239 necessary to make the reclaimed fuel meet reactivity and mass requirements for the next fuel burnup cycle.

This system of equations has been programed for the IBM-1620. The practice in making a fuel cycle calculation has been to estimate an initial set of C's (these represent fresh fuel in the reactor) and run the 1620 code - "BURP" - over a series of zero-B cycles until equilibrium is attained. The output is examined, and if the fuel mixtures of the equilibrium cycle do not give the proper reactivity level through fuel life, the procedure is repeated with a new C set.

The code gives, as output, the fuel mixture at each irradiation interval defined by b , and always starts with fresh fuel. The history given by the series of points at b is regarded as that of a specific fuel lump as it is exposed from zero to 100 GWD/T. The points between 33 GWD/T and 67 GWD/T give the history of the average fuel mixture in the reactor during one refueling interval. It is not necessary to include fission products in the equations programed because fresh fuel is always used to start a sequence, and because the fission product reactivity penalty is included when determining the reactivity requirements upon the set of C's.

Initial fuel studies were made to get fuel compositions for input to the RZ CRAM run. One-group data was obtained from preliminary one-dimensional CRAM cases. It was assumed at this point in the analysis that U-238 control assemblies with fuel followers would be specified. The fuel composition obtained was accordingly a critical mixture of heavy metal and fission products corresponding to an average core irradiation of 50 GWD/T.

Estimates of control requirements made, in part, from information gained by the RZ calculation show that a more effective control agent than U-238 is required. Further, the fuel followers have been dropped from consideration because of difficulties in cooling the moving assemblies.

A fuel cycle consistent with B₄C control assemblies and U-238 followers has been computed. This was done by, firstly, using the RZ composition as a starting point and extrapolating the isotopic concentration back to the point representing 33 GWD/T. BURP output was used as a guide when extrapolating. Separate one-dimensional CRAM runs were made using the 33 and 50 GWD/T compositions to get the reactivity loss caused by burning the core over this range (the 50 GWD/T composition is that of the RZ). The result was taken to be half that caused by exposing the core, on average, from 33 to 67 GWD/T which is the span between the refueling points. The overall reactivity penalty obtained is $k = 0.06$. Secondly, the fuel cycle was recomputed, using one-group data from the RZ CRAM. Trial C sets were used until one giving an equilibrium cycle forcing $k_{\text{eff}} = 1.00$ at 67 GWD/T was obtained.

The characteristics of the fuel cycle for the boron-controlled core are listed in Tables 1, 2, and 3. All information pertains to the equilibrium case.

TABLE 1
FUEL-BATCH MASSES

<u>isotope</u>	<u>fresh batch*</u>	<u>discharge batch (100 GWD/T)</u>
U-238	4150 kg	3790
Pu-239	1140	780
Pu-240	540	510
Pu-241	92	95
F.P.P.	--	745

*Each batch is 1/3 core loading.

TABLE 2
REACTION RATES AT EQUILIBRIUM CYCLE MIDPOINT

<u>isotope</u>	<u>fission rate gm/Mwd</u>	<u>consumption rate</u>	<u>formation rate</u>
U-238	0.083	0.591	0
Pu-239	0.919	1.115	0.507
Pu-240	0.087	0.237	0.217
Pu-241	0.133	0.152	0.151

TABLE 3
INTERNAL BREEDING RATIO

<u>core average ratio</u>	<u>IBR</u>
33,000 Mwd/T	0.500
Average $\approx$ 50,000	0.524
67,000	0.548

The RZ CRAM gives an overall breeding ratio, at nominal conditions, with a correction for the subsequent addition of boron control, of 1.32.

The control assemblies are hexagonal units similar to the fuel bundles. There is structure additional to that of the static fuel elements in the control assemblies, so that each one contains 85 steel-clad B₄C pins of 0.30-in. OD. The boron is unenriched and the carbide density is assumed to be 2.25 g/cc. Depleted uranium is used to replace the carbide in the follower sections of the control elements.

Auxiliary calculations have been made which show that each of these elements is worth 0.2 percent k in the reference core. This value was obtained by computing the worth of a single element occupying the axis of a cylindrical core equivalent to the 1000-Mw annular design in composition, criticality, and neutron spectrum.

As the k_{eff} loss between adjacent refueling points is 6 percent, 30 control elements are required to compensate fuel burnup. In addition, about \$ 8 of negative Δk is desired for the safety control system, and less than \$ 1 is necessary for temperature override. These requirements are met by 12- and 6-element groups, respectively. The latter group need only be rods of much reduced density to account for the small temperature override (or they could be normal rods near the end of their lifetime).

Control assembly heat generation from neutron capture in boron and from U-238 and Pu-239 fission in the follower sections has been determined. The rate of boron depletion was also computed. Control element in-core cooling, and replacement rate requirements can be estimated from the information generated.

The heating rate in fresh control elements is 49 w/cc in an average core location, and has a peak of 75 w/cc. Heating in the control followers is assumed to be entirely from fission in U-238 and Pu-239. The results are tabulated in Table 4 as functions of core irradiation paralleling that of the followers.

TABLE 4
CONTROL FOLLOWER HEAT GENERATION, W/CC OF OXIDE

core irradiation		average power	peak power
0		120	180
20 k		180	300
40		240	440
60		300	550
80		350	640
100		390	700

Average power denotes the power level in the U-Pu mix arising from exposure at the average flux level when the mix is exposed to the average flux. Peak power is related to the flux peak in the same way.

The power level in the fuel pins is about 1030 w/cc.

Table 5 gives boron depletion information.

TABLE 5

B^{10} DEPLETION IN CONTROL ELEMENTS
- FRACTION OF INITIAL CONCENTRATION

<u>core irradiation</u>		<u>average irradiation</u>	<u>peak irradiation</u>
20		0.89	0.84
40		0.79	0.70
60		0.70	0.58
80		0.63	0.51

The power level in the B₄C should present no problems. On the other hand, a control follower located at the peak flux location for several refueling cycles can develop local heating rates approximately 70 percent as high as the fuel. Severe boron depletion is also evident. Frequent replacement of at least the control and follower elements occupying the high-flux regions of the core must be anticipated if loss-of-control and control-assembly-cooling problems are to be avoided.

The extensive nature of the large annular oxide core requires some consideration in overall control-system design. The calculations indicated before show that it should be relatively easy to build a B₄C control system, which gives enough control on a geometrically averaged basis. However, mutual decoupling between different sections of the large core requires careful planning of the spacing of control assemblies.

In addition, the decoupling problem necessitates instrumentation adequate to detect the power peaking which can result, in a large core, from normal variations in fuel and structure manufacturing and from other factors. The extra instrumentation must be accompanied by enough independence between individual control-assembly drives to enable correction of any localized power peaking that is detected.

Similar design problems have been encountered in large water reactors which have operated successfully. There is an inherent difference between large fast reactors and large water reactors which makes the problem more difficult. The annular geometry could render the associated detection and correction measures more straightforward.

APPENDIX H
ALTERNATE STEAM CLEANING FACILITY

CONTENTS

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1. GENERAL

An alternate cleaning facility to that discussed in Sec. 3.4.4(f) has been designed and is described below. The equipment and handling procedures are outlined.

In this concept (see Fig. H.1) the cleaning unit is contained in an outer vessel which is pressurized with nitrogen or argon to depress the water in the tunnel connecting it with the storage pool. Alternatively, the reactor building floor could be higher than the pool floor by about 16 ft. The carriage would then operate on an inclined track. However, there would be a short time when the fuel was not being cooled with this method, and it would have to be further examined from a safety standpoint.

The steam-cleaning unit will have a valve at the reactor building floor level similar to that used over the reactor. The fuel-removal cask will be lowered over it and sealed against it. Then this valve and the one on the cask will be opened. Argon circulation will have been maintained in both the cask and steam cleaner prior to this, so their atmospheres will be similar. The cask hoist lowers the element into the cleaning unit. After the element is seated on support rods at the bottom of the cleaning unit, the cask grapple is released and withdrawn into the cask, the valves are closed and the cask may be removed. The cleaning cycle is then carried out.

When cleaning is completed, a shield cart carrying a grapple and drive is brought over the cleaning unit and sealed against the valve. There is no need for a valve on the cart. The valve is opened and the grapple is lowered to pick up the element and support it temporarily. The cleaning unit is pressurized with nitrogen or argon to equal the pressure in the external vessel. Then the lower, or foot valve on the cleaning unit is opened by activating the air cylinder. The grapple is lowered, allowing the fuel element to move down into the transport tube. The grapple is released and retracted from the cleaning unit, the foot valve is closed and the transport tube brought through the tunnel to the pool.

The cleaning unit is then prepared for the next element. This consists of:

- (a) Closing the foot valve.
- (b) Reducing the internal pressure.
- (c) Closing the valve at the reactor floor.
- (d) Removing the grapple cart.
- (e) Pumping out the last amount of water remaining after cleaning. Some will be in the pumpout coil.
- (f) Closing the valve on the drain coil.

(g) Blowing hot argon or nitrogen through the unit, including a purge of the portion of the drain coil located between the valve and the cleaning chamber. This hot gas purge will continue until all water has been evaporated and removed.

2. EQUIPMENT

2.1 STEAM-CLEANING CHAMBER

The steam-cleaning chamber consists of two sections -- an upper fixed section and a lower movable section. The steam cleaning of the fuel element is accomplished in the upper chamber and the transfer cask must be sealed to it before the valves on the steam cleaning chamber and the fuel transfer cask can be opened. After cleaning, the element is lowered into the lower chamber which conveys it to the fuel storage pool.

Auxiliary services supplied to the steam cleaning chamber include argon, steam, and water. Power or pressurized fluid is also supplied for valve activation and the required handling of the fuel element.

2.1.1 Upper Chamber

The upper chamber of the steam-cleaning facility is a cylinder, whose top consists of a seal and a shielded valve and whose bottom consists of a valve to permit operation of the lower section of the steam cleaning facility. The interior of the chamber contains a fuel-element holder to receive and hold the fuel element during steam cleaning.

2.1.2 Lower Chamber

The lower chamber of the steam cleaning facility is essentially a carrier for the sodium-free, spent fuel element. Its design will permit rapid, safe transfer of the fuel element to the storage pool. This second chamber is in a pool and always exposed to the water. It is isolated from the upper chamber during the cleaning process to prevent sodium or fission products from entering it and thereby passing to the pool.

3. SODIUM CLEANING CYCLE

The sodium cleaning cycle consists of the following major steps:

(a) The fuel-element transfer cask is sealed to the upper cleaning chamber, similar to the method used at the reactor. The space between seal and valve is purged with hot (350 F) argon. The upper cleaning chamber is warmed and purged of air with steam and hot argon circulated through it.

(b) Valves on the upper chamber and fuel-transfer cask are opened. Argon circulation in the cask continues during this operation. The steam-cleaning facility argon-circulation system is in continuous operation. The sodium-drip pan in the cask is emptied into the upper chamber and the fuel element is lowered into the chamber.

(c) The loading valves are closed and the fuel-transfer cask is removed. Fuel element temperature is controlled by the argon recirculation system.

(d) Dry steam is gradually added to the argon and the mixture is passed through the cleaning chamber to react with sodium on the fuel element. When the reaction is complete, dry steam (100 percent) is introduced for a predetermined period. This is followed by a wet steam cycle. Finally the upper chamber is flooded with water.

(e) The water is drained from the chamber. A second water wash is used to ensure that no sodium hydroxide is transferred to the fuel storage pool.

(f) The second water wash is drained and the valve on the bottom of the upper chamber is opened.

(g) The element is lowered into the lower chamber, where it is cooled by water.

(h) The lower chamber is moved to the fuel-storage pool.

(i) The upper chamber valve is closed, washed with water, and drained. The chamber is dried with hot argon and is then ready for reuse.

4. CYCLE TIME

The steam-cleaning-cycle time is estimated to be about 40 min.

ENGINEERING COMPANY A DIVISION OF VITRO CORPORATION OF AMERICA NEW YORK, N. Y.	<u>LFRDS - FUEL HANDLING</u> <u>TRANSFER OF ELEMENTS</u> <u>INTO AND FROM STEAM</u> <u>CLEANING UNIT</u>		
						SCALE	1/2" = 1'-0"	APPD.	
NO.	DATE	DESCRIPTION	BY	CH	AP	DRAWN	P. D. 20	APPD.	
REVISIONS						CHKD.		APPD.	
								DWG. NO.	SK-1498-L-7

FORM NUMBER 1000.02-0

FIG. H.1

APPENDIX I
PROJECT PERSONNEL

The scope of work allowed separation into distinct areas of effort. Personnel devoting a major fraction of their time to the study and their principal area of contribution are listed below.

1. Project Management

C. Klotz Allis-Chalmers

2. Core Design and Fuel Cycle

R. Miller, Lead Engineer	Allis-Chalmers
A. Gage	Allis-Chalmers
E. Medal	Allis-Chalmers
G. Menzel	Allis-Chalmers
L. Phillips	Allis-Chalmers

3. Reactor Mechanisms and Fuel Handling

H. Barringer, Lead Engineer	Babcock & Wilcox
E. Brown	Babcock & Wilcox
J. Bunts	Babcock & Wilcox
G. Winders	Babcock & Wilcox

4. Physics and Safety

J. Nims, Lead Engineer	APDA
H. Atwater	Babcock & Wilcox
E. Fischer	APDA
S. Garg	APDA
D. Green	APDA
D. Hostetler	Babcock & Wilcox
J. Lewellen	Allis-Chalmers
R. Palmer	APDA
E. Volk	APDA

5. Building and Plant Layout

D. Rhoads United Engineers & Constructors

6. Fuel Cycle Services

R. Pitkin Vitro Engineering