**STEAM-COOLED FAST BREEDER POWER
REACTORS PARAMETRIC DESIGN STUDIES**

J. C. FOX, D. T. AASE,
K. R. WISE, S. J. ALTSCHULER,
G. J. BUSSELMAN AND R. H. HOLEMAN

APRIL 1968

**AEC RESEARCH &
DEVELOPMENT REPORT**

ROUTE TO	DATE	INITIALS	REMARKS

LEGAL NOTICE

This report was prepared as an account of Government sponsored work. Neither the United States, nor the Commission, nor any person acting on behalf of the Commission:

A. Makes any warranty or representation, expressed or implied, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or

B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of any information, apparatus, method, or process disclosed in this report.

As used in the above, "person acting on behalf of the Commission" includes any employee or contractor of the Commission, or employee of such contractor, to the extent that such employee or contractor of the Commission, or employee of such contractor prepares, disseminates, or provides access to, any information pursuant to his employment or contract with the Commission, or his employment with such contractor.

PACIFIC NORTHWEST LABORATORY

RICHLAND, WASHINGTON

operated by

BATTELLE MEMORIAL INSTITUTE

for the

UNITED STATES ATOMIC ENERGY COMMISSION UNDER CONTRACT AT(45-1)-1830

PRINTED BY/ FOR THE U. S. ATOMIC ENERGY COMMISSION

BNWL-713
UC-80,
Reactor Technology

STEAM-COOLED FAST BREEDER POWER REACTORS
PARAMETRIC DESIGN STUDIES

By

J. C. Fox
D. T. Aase
K. R. Wise

Engineering Analysis Section
Engineering Development Department

S. J. Altschuler
G. J. Busselman
R. H. Holeman

Applied Reactor Physics Section
Reactor Physics Department

April 1968

UNCLASSIFIED
DATE 11-11-01 BY 1043

APR 25 '68

PACIFIC NORTHWEST LABORATORY
RICHLAND, WASHINGTON

Printed in the United States of America
Available from
Clearinghouse for Federal Scientific and Technical Information
National Bureau of Standards, U.S. Department of Commerce
Springfield, Virginia 22151
Price: Printed Copy \$3.00; Microfiche \$0.65

TABLE OF CONTENTS

LIST OF FIGURES	v
LIST OF TABLES	vii
INTRODUCTION	1
Objectives	1
Scope of Study	1
General Methods	1
SUMMARY	3
Model Assumptions	3
Parameter Evaluations	3
Design Analyses	5
Conclusions	8
Future Study Areas	8
NEUTRONICS SURVEY	9
Scope of Survey	9
Assumptions	9
Methods of Calculation	10
Results	14
Conclusions	20
DESIGN AND ECONOMICS STUDIES	22
Methods	22
Assumptions	22
Operating Parameters Sensitivity Study	24
Geometry Sensitivity Studies	35
Design Comparison Study	42
APPENDIX A: SCFBR DESIGN AND ECONOMICS SIMULATION DESCRIPTION	A-1
Mathematical Model	A-1
Calculational Procedures	A-4
Summary of Equations for SCFBR Design and Economics Simulation	A-7
Nomenclature	A-12
APPENDIX B: VARIATIONS AND UNCERTAINTIES IN STEAM-COOLED FAST BREEDER REACTOR PHYSICS	B-1
Constants	B-1
Best Adjusted Results	B-6
REFERENCES	Ref-1
DISTRIBUTION	Distr-1

LIST OF FIGURES

1	Reference Designs Relative to Parametric Nuclear Analysis	3
2	Breeding Ratio Versus Reactor Outlet Pressure	5
3	Doubling Time Versus Reactor Outlet Pressure	6
4	Fuel Cycle Costs Versus Reactor Outlet Pressure	6
5	KEFF Versus Enrichment	11
6	Core Breeding Ratio Versus Enrichment	12
7	Total Breeding Ratio Versus Enrichment	12
8	Initial Reactivity	16
9	Reactivity Response	16
10	Reactivity Versus Steam Density 0.25 Steam Volume Fraction	17
11	Reactivity Versus Steam Density 0.45 Steam Volume Fraction	18
12	Reactivity Versus Steam Density 0.55 Steam Volume Fraction	18
13	Total Breeding Ratio Versus Blanket Thickness	19
14	Total Breeding Ratio and Voiding Reactivity Response	20
15	Allowed Combinations of Steam Density and Volume Fraction 8000 Liter	21
16	Allowed Combinations of Steam Density and Volume Fraction 6000 Liter	21
17	Core Thermal Power Versus Reactor Outlet Pressure	24
18	Hydraulic Parameters Versus Reactor Outlet Pressure	25
19	Average Coolant Velocity Versus Reactor Outlet Pressure	25
20	Fuel Pin Diameter Versus Reactor Outlet Pressure	26
21	Core Material Fractions Versus Reactor Outlet Pressure	26
22	Core Steam Inventory Versus Reactor Outlet Pressure	27
23	Total Breeding Ratio Versus Reactor Outlet Pressure	27
24	Critical Mass Versus Reactor Outlet Pressure	28
25	Specific Inventory Versus Reactor Outlet Pressure	28
26	Doubling Time Versus Reactor Outlet Pressure	29
27	Fuel Cycle Costs Versus Reactor Outlet Pressure	29
28	Core Thermal Power Versus Reactor Outlet Temperature	31
29	Hydraulic Parameters Versus Reactor Outlet Temperature	31

30	Fuel Pin Diameter Versus Reactor Outlet Temperature	32
31	Core Material Fractions Versus Reactor Outlet Temperature	32
32	Core Steam Inventory Versus Reactor Outlet Temperature	33
33	Total Breeding Ratio Versus Reactor Outlet Temperature	33
34	Critical Mass Versus Reactor Outlet Temperature	34
35	Doubling Time Versus Reactor Outlet Temperature	34
36	Fuel Cycle Costs Versus Reactor Outlet Temperature	35
37	Total Breeding Ratio Versus Core Height to Diameter Ratio	36
38	Doubling Time Versus Core Height to Diameter Ratio	37
39	Core Material Fractions Versus Core Height to Diameter Ratio	37
40	Fuel Cycle Costs Versus Core Height to Diameter Ratio	38
41	Total Breeding Ratio Versus Core Volume	38
42	Doubling Time Versus Core Volume	39
43	Critical Mass Versus Core Volume	39
44	Specific Inventory Versus Core Volume	40
45	Core Material Fractions Versus Core Volume	40
46	Total Number of Fuel Pins Versus Core Volume	41
47	Fuel Pin Diameter Versus Core Volume	41
48	Fuel Cycle Costs Versus Core Volume	42
49	Pumping Power Versus Reactor Outlet Pressure	43
50	Total Breeding Ratio Versus Reactor Outlet Pressure	44
51	Doubling Time Versus Reactor Outlet Pressure	44
52	Fuel Cycle Cost Versus Reactor Outlet Pressure	45
53	Volume Fraction Fuel Versus Reactor Outlet Pressure	45
54	Core Steam Inventory Versus Reactor Outlet Pressure	46
55	Core Thermal Power Versus Reactor Outlet Pressure	46
A-1	Steam Cycle Conversion Efficiency as a Function of Turbine Inlet Conditions	A-3
A-2	SCFBR - Schematic Flow Sheet for Fuel Cycle Economics Simulation	A-5
A-3	Integral Control Diagram for TCL and TCMAX Controllers	A-5
A-4	Three Possible Integral Control Circuits for Maintaining Volume Fraction Fuel Constant	A-6

B-1	Keff Versus Enrichment	B-7
B-2	Core Breeding Ratio Versus Enrichment	B-7
B-3	Total Breeding Ratio Versus Enrichment	B-8
B-4	Relationship Between BOL and Average Core Breeding Ratios	B-9
B-5	Relationship Between BOL Core Breeding Ratio and the BOL Keff Required for 50,000 MWD/tonne Batch Burnup	B-9

LIST OF TABLES

I	BNW SCFBR Parameter Survey	4
II	Comparison of Results for Two Sets of Cross Sections	5
III	Parametric Study Core Characteristics	9
IV	Parametric Study of Atom Densities	10
V	BNW SCFBR Parameter Survey	13
VI	Best Adjusted Results for 8000 Liter Core	15
B-I	Comparison of Results	B-2
B-II	Comparison Between FCC and HRG Calculations Using ORNL Cross Sections	B-3
B-III	Effect of Fuel Pin Size	B-4
B-IV	Comparison Between Homogenized and Unhomogenized ORNL Cross Sections in FCC	B-4
B-V	Comparison Between Russian and ORNL Cross-Section Sets in FCC	B-5
B-VI	Comparison Between Russian and ORNL Cross-Section Sets in FCC	B-5
B-VII	Best Adjusted Results, 8000 Liter Spherical Cores	B-10

STEAM-COOLED FAST BREEDER POWER REACTORS PARAMETRIC DESIGN STUDIES

INTRODUCTION

OBJECTIVES

To supplement an evaluation of three specific steam-cooled FBR designs being made by Oak Ridge National Laboratory during 1967, the Battelle Pacific Northwest Laboratory was asked to perform a parametric study of reactor design over the range of steam conditions of current interest. The purpose of this study was to identify, by consistent methods and assumptions, the major effects and the importance of steam pressure on reactor performance. Interest in this subject arises from the wide range of steam pressures evaluated in four recent design studies: 1200, 1500, 1800, and 2500 psi. This study is intended to supplement the ORNL evaluation by helping to isolate effects of the selected steam pressure from the effects of design approach and assumptions. Quantitatively, findings have not been normalized to any of the ORNL specific evaluations, and the results for a given reactor will differ somewhat from the ORNL results, since different methods of calculation were used where necessary for expediency.

SCOPE OF STUDY

The Battelle-Northwest parametric studies estimated the effect on breed-

ing ratio, doubling time, and fuel cycle cost of the following design and operating parameters:

- Steam conditions, turbine inlet pressure and temperature
- Core design, volume, shape (height-to-diameter ratio)

The sensitivity of reactor performance to parameters, varied over certain ranges to reflect changes in design philosophy, was also examined. The following ranges of parameters were investigated:

- Steam Pressure 1000-3000 psi
- Steam Temperature 900-1100 °F
- H/D Ratio 0.2-0.6
- Core Volume 5000-10,000
 liters
- Core Material Volume
 Fractions

Coolant	25-60%
Fuel	60-25%
Structural Material	15%

GENERAL METHODS

The general approach was to set up a matrix of nine cases for core physics calculations covering the range of steam inventories and core material volume fractions of interest. Results of these calculations could then be plotted in a form suitable for input to the parametric engineering core design and fuel cycle cost computations. Calculations were made using an expanded version of a digital-analog model developed at BNW

for FFTF core design tradeoff studies. This model permits the design of cores to specified limits (e.g. clad temperature, fuel centerline temperature, core pressure drop), thus allowing design parameters to be varied or fixed according to the cases being investigated.

The primary results presented in this report are for cases where core volume, core shape, power distribu-

tion, ex-core pressure drop, and steam temperature were held fixed over the full range of steam pressure, while material volume fraction and fuel rod size were varied. The effects of varying the other design parameters at one selected pressure are presented. Effects of varying these parameters at other pressures were not studied.

SUMMARY

MODEL ASSUMPTIONS

The nuclear survey was based on a set of nine combinations of steam inventory and core material volume fractions, designated as cases 0A through 2C. Figure 1 shows the characteristics of these cases and their relationship to the three specific designs evaluated by ORNL. The physics calculation geometric model was based on the Karlsruhe intermediate pressure design. In most calculations, 0.276 in. pins, spherical geometry from 1000 to 10,000 liters, an 18 in. depleted uranium blanket, and a Karlsruhe-type reflector were used. All cases assumed 15% clad-structure of Inconel-625. An equilibrium plutonium isotopic composition of 76.0,

21.0, 2.3, and 0.7% for ^{239}Pu , ^{240}Pu , ^{241}Pu , and ^{242}Pu , respectively, was used for all cases. The methods used in the physics parametric study were chosen to conform, as closely as possible, to those used by ORNL so that a reasonably valid comparison could be obtained. The ORNL cross section set was used to help eliminate discrepancies in the nuclear analysis. Details of the physics calculations appear under Neutronics Survey.

PARAMETER EVALUATIONS

Table I summarizes the physics results incorporated as input into the BNW design and economics simulation. Comparison of Table I results with the gross design parameters of

FIGURE 1. Reference Designs Relative to Parametric Nuclear Analysis

TABLE I. BNW SCFBR Parameter Survey

Liters	Case No.	k_0	Enrichment	Initial Breeding Ratio		K_{void}	ΔK	Pu Inventory, kg
				Core	Total			
6000	OA	1.01	11.70	1.170	1.385	1.003	-0.007	3479
8000			11.40	1.195	1.380	1.001	-0.009	4519
6000	OB	1.01	11.67	1.071	1.255	1.001	-0.009	3470
8000			11.30	1.11	1.258	0.995	-0.015	4479
6000	OC	1.024	12.00	0.920	1.045	1.018	-0.006	3569
8000			11.65	0.947	1.050	1.014	-0.010	4619
6000	1A	1.010	13.40	1.015	1.307	1.003	-0.007	2991
8000			12.95	1.055	1.292	1.003	-0.007	3853
6000	1B	1.022	13.80	0.900	1.115	1.020	-0.002	3080
8000			13.40	0.926	1.105	1.023	+0.001	3987
6000	1C	1.063	15.10	0.741	0.889	1.071	+0.008	3373
8000			15.00	0.748	0.870	1.088	+0.025	4467
6000	2A	1.053	18.80	0.700	1.019	1.060	+0.007	2804
8000			17.85	0.745	1.018	1.059	+0.006	3548
6000	2B	1.076	19.90	0.611	0.805	1.093	+0.017	2969
8000			18.90	0.645	0.840	1.093	+0.017	3759
6000	2C	1.092	21.10	0.532	0.7015	1.122	+0.030	3150
8000			20.30	0.565	0.695	1.1375	+0.046	4039

Figure 1 discloses cases with lower steam inventory and higher fuel fraction to have the best breeding ratio, as expected. Blanket breeding gains of modest value, even for the low steam inventory "A" cases, indicate that the blanket model used may not be the optimum.

The potential doubling time* of SCFBR's, however, is not obvious from the physics results alone, since the fissile inventory depends on the engineering design of the core and on

safety constraints. Although the void coefficients shown in Table I decrease for higher fuel fractions and lower steam inventory, there is considerable uncertainty in their magnitude as pointed out in Table II. The trends, however, were found to be the same for both sets of cross sections. Because of the uncertainties in specifying a limit and in calculating values, the void coefficient was not used as a constraint on the fuel volume fraction in the PNL parametric studies. Instead, the limit on the volume fraction fuel was arbitrarily set at 60 vol%. This limit represents a minimum allowable pitch-to-diameter ratio of about 1.05, somewhat lower than that normally taken as the minimum practical value.

* The definition of doubling time used here is

$$t_d = \frac{0.693}{\text{Fraction of annual increase}}$$

The out-of-core inventory for each design is based on process times for a 15,000 MW_e industry.

TABLE II. Comparison of Results for Two Sets of Cross Sections

8000 liter core; 40 vol% coolant, 15 vol% Inconel 625, 45 vol% fuel

Method 1: HRG - ORNL Cross Sections

Method 2: FCC - Russian Cross Sections

Core Steam Inventory, H_2O/ft^3	Method	Initial k_{eff}	Enrichment, % Pu	Core Breeding Ratio, BR_c	Void Reactivity Change, Δk_{void}
0.357	1	1.010	12.95	1.055	-0.007
	2		12.85	1.152	+0.016
1.18	1	1.022	13.40	0.926	+0.001
	2		13.34	1.053	+0.027
2.57	1	1.063	15.00	0.748	+0.025
	2		15.30	0.852	+0.079

In formulating the input for the parametric calculations, linear approximations over discrete ranges to describe the coolant properties and physics data, and linear interpolation for intermediate points were required. The fact that some perturbations or inflections in curves reflect the input and calculational methods rather than system characteristics should be borne in mind when interpreting the results presented.

DESIGN ANALYSES

Appendix A of this report discusses the mathematical model and calculational procedures used to perform the BNW parametric engineering and economics studies, while results are presented under Design and Economic Studies. Figures 2, 3, and 4 summarize these results in showing variations in breeder performance and fuel

FIGURE 2. Breeding Ratio Versus Reactor Outlet Pressure

FIGURE 3. Doubling Time Versus Reactor Outlet Pressure

FIGURE 4. Fuel Cycle Costs (mils/Kw-hr) Versus Reactor Outlet Pressure

cycle costs over a range of pressures for three general design approaches selected from the sensitivity studies. By adjusting the number and size of the fuel pins, a maximum design clad temperature of 1374 °F and a maximum fuel centerline temperature of 4850 °F is maintained for each design over the pressure range shown. Design 1 in these figures is similar in most respects to the Karlsruhe D-1 design. Design 2 is similar to the B&W low pressure design. Design 3 is similar to Design 2 with the exception of different hydraulic performance. The hydraulic performance assumed for each design is indicated by the out-of-core pressure drop and the friction factor multiplier, each representing different in-core structure designs. The streamlining of Designs 2 and 3 was necessary for the smaller volume cores, particularly at low system pressure, to obtain reasonable pumping powers.

The improved breeding ratio of Design 2, compared at a constant pressure with Design 1 (Figure 2), is due to the higher volume fraction fuel in Design 2 as well as to the lower steam inventory. The gain in breeding ratio at constant pressure is comparable to the gain in breeding ratio from varying the pressure over the entire range. The effect of redesigning at constant pressure is even more evident in the doubling time shown in Figure 3. The gain in doubling time is due partially to the improved breeding ratio, but also important is the lower fissile inventory of the smaller core volume.

Designs 2 and 3 are not optimized but they do show that, by altering the thermal and hydraulic assumptions, gains in the form of more compact cores with higher fuel fractions can be considered.

The doubling time curve for Design 1 in Figure 3 is distorted by an error in the integral control circuit which maintains the fuel centerline temperature at 4850 °F. Between 1950 and 2200 psia, the actual fuel centerline temperature changes from 4849 to 4854 °F. The temperature remains at 4854 °F from 2200 to 2400 psia, and then returns to 4850 °F above 2400 psia. This small error in fuel centerline temperature is the primary cause of small inflections in the fuel volume fraction and steam inventory. These inflections, in turn, cause small errors in both breeding ratio and fissile inventory. These errors compound, however, to cause a considerable distortion of the doubling time curve. The curve for Design 1 should have the same shape as for Designs 2 and 3. The controller error at 2850 psia is negligible. Hence, the doubling time shown for that point is correct. These errors could have been eliminated by proper selection of controller gains and phases. However, because the resulting distortions did not, in general, affect the conclusions drawn from the data, the studies were not rerun.

The fuel cycle costs for these three designs are shown in Figure 4. These costs appear to change so little between designs or over the

pressure range that, as a first approximation, they could be considered independent of steam pressure. At lower system pressure, the higher fabrication costs associated with smaller fuel pins offset the gain in plutonium revenue from better breeder performance. The trends for fuel cycle costs over the pressure range could be effected by more refined unit fabrication cost information.

CONCLUSIONS

From the results of this parametric study, the following conclusions can be drawn:

- Fuel cycle costs are very weakly affected by choice of steam pressure.
- Choice of steam pressure will depend on whether the chief objective in a given design is to minimize capital cost or to emphasize breeding ratios and doubling time.
- Performance, in terms of breeding ratio, doubling time, and fuel cycle cost, is affected as much by design approach and assumed limitations as by choice of steam pressure.
- Designs which reduce hydraulic resistance contribute significantly to the benefits of the breeding ratio potential in low steam pressure designs.

FUTURE STUDY AREAS

This study also identifies some key areas requiring additional infor-

mation at an early date to allow a more conclusive assessment of the potential for the steam-cooled fast breeder, and to help resolve the choice of steam pressure. One of the most important objectives of future work on steam-cooled fast reactors involves certain critical experimentation with a representative neutron spectrum. Such experiments, by exploring methods for the calibration of cross section sets would place steam-cooled fast reactor evaluations on a much firmer basis. In addition, dynamic system studies should be performed to define safety coefficients limits that must be obtained in the reactor design. Of particular importance is the defining of the necessary void coefficient. Design studies could then be performed over a suitable range of core volumes, H/D ratios, steam inventories, thermal limits, and hydraulic designs, to better define the limiting design conditions considering the required void coefficient as a criteria. Because of the economic attraction of employing higher core fuel volume fractions, data on hydraulics and on heat transfer at low pitch diameter ratios for pin arrays should be obtained to define practical limits as a function of steam conditions. With this information more rigorously evaluated, the potential performance of the SCFBR can be determined with much greater confidence.

NEUTRONICS SURVEY

SCOPE OF SURVEY

The scope of the reactor systems investigation was determined by physical characteristics of steam pressure, the principal parameter. At the outset, the pressure was to vary from 1000 psi to 3000 psi. It soon became evident, however, that since temperatures and pressure are separable in a reactor system, several combinations of the two should be examined. From a neutronics point of view, the main consideration is the atom density of the various components. With this in mind, three steam densities and three coolant fractions were chosen. Tables III and IV show the resulting nine cases and their characteristics.

The study included the determination of breeding ratios and reactivity coefficients for 1000 MW_e reactors using an average fuel exposure of 50,000 MWd/tonne of metal. The sensitivity of these parameters to changes in steam inventory and core size and shape was examined.

ASSUMPTIONS

The equilibrium plutonium composition is held constant for all cases and is from Kiefhaber and Ott.⁽¹⁾ It is in close agreement with specific cycle analysis reported by others.^(2,3) The depleted uranium used in the core and blanket is a composition suggested by E. A. Eschbach⁽⁴⁾ as typical of that

TABLE III. *Parametric Study Core Characteristics*

Core Steam Inventory, (lbs/ft ³)	Core Volume Fractions				Average Steam Temperature,* °F
	Fuel	0.60	0.45	0.30	
	Steam	0.25	0.40	0.55	
	Structure	0.15	0.15	0.15	
0.367	Case OA 1400 psi	Case IA 880 psi	Case 2A 650 psi		1200
1.18	Case OB 3400 psi	Case 1B 2300 psi	Case 2B 1700 psi		1000
2.83	Case OC 4100 psi	Case 1C 3400 psi	Case 2C 2800 psi		800

* Temperatures and pressures contained in the tables are typical values which result in a density that will yield the steam inventory shown in column one.

TABLE IV. Parametric Study of Atom Densities^(a)

Core Steam Inventory, lb/ft ³ of Core	Steam, vol%	25	40	55
	Fuel, vol%	60	45	30
	Structure, vol%	15	15	15
0.367	Case 0A	Case 1A	Case 2A	
	0 = 252.5	0 = 189.9	0 = 127.2	
	H = 3.9	H = 3.9	H = 3.9	
	U+Pu = 125.3	U+Pu = 94.0	U+Pu = 62.6	
	Struc = 125.5	Struc = 125.5	Struc = 125.5	
1.18	Case 0B	Case 1B	Case 2B	
	0 = 256.9	0 = 194.2	0 = 131.6	
	H = 12.6	H = 12.6	H = 12.6	
	U+Pu = 125.3	U+Pu = 94.0	U+Pu = 62.6	
	Struc = 125.5	Struc = 125.5	Struc = 125.5	
2.83	Case 0C	Case 1C	Case 2C	
	0 = 265.7	0 = 203.1	0 = 140.4	
	H = 30.3	H = 30.3	H = 30.3	
	U+Pu = 125.3	U+Pu = 94.0	U+Pu = 62.6	
	Struc = 125.5	Struc = 125.5	Struc = 125.5	

(a) Nuclei per cm³/10²⁰

available in 1980. The fuel pin diameters, as well as many other design specifics, are taken from the Karlsruhe D-1 design.⁽⁵⁾ Spherical geometry was assumed for ease of calculation; cylindrical cores with various H/D ratios were represented by relating them to spheres of equivalent buckling. All of the non-fuel material, both clad and structure, is Inconel 625 consisting of 3.0% Fe, 61.0% Ni, 22% Cr, 9.0% Mo, and 4.0% Ta.

The blanket is assumed to be geometrically identical to the core (in some specific designs it is more dense than the core and in others less dense). An 18 in. shell of

blanket material is used on all of the base calculations, although the results are not very sensitive to this dimension. Most calculations are at the beginning of life for the blanket and assume no plutonium is present. The reflector (6 vol% and 60 cm thickness) is patterned directly after the D-1 design. Other parametric assumption data are given in the Summary.

METHODS OF CALCULATION

The cross sections used in this study are from two sources. The primary cross section set was obtained from ORNL and is basically the

GAM II set from GA revised by L. L. Bennett of ORNL to more closely agree with new data. The plutonium constants in this set are the preliminary ENDFB values by Greebler. The other set is a version of the 26-group Russian Set⁽⁶⁾ as modified for use in FFTF calculations.⁽⁷⁾ These cross sections were used only for burnup calculations in the data presented in this Section. These two cross section sets are compared in detail in Appendix B.

The cross sections from ORNL were processed using the HRG⁽⁸⁾ code to obtain a seven-group set. The TEMPEST code⁽⁹⁾ was used to provide a thermal group. These eight-group cross sections, recalculated for each different composition or enrichment, were used in the 1-D diffusion theory code HFN.⁽¹⁰⁾

For each of the nine cases (Table III) and for selected core volumes, several enrichments were

used in the combination of codes HRG, TEMPEST, HFN, and CLERK.⁽¹¹⁾ The resulting reactivity and breeding ratios are plotted as shown in Figures 5, 6, and 7. This process was carried out for both normal steam conditions and the completely void situations.

The required initial reactivity, obtained in a manner described below in the next paragraph, was used with Figure 5 to define the initial enrichment. This enrichment and Figures 6 and 7 pinpoint the beginning of life breeding ratios for both core and blanket. This process allows the initial reactivity requirement estimate to be refined without invalidating the basic data. This method makes it inexpensive to examine the changes in core loading and breeding ratio caused by changes in various assumptions or different methods.

FIGURE 5. *KEFF Versus Enrichment*

FIGURE 6. Core Breeding Ratio
Versus Enrichment

FIGURE 7. Total Breeding Ratio
Versus Enrichment

The method used to determine the initial reactivity required to achieve a batch core exposure of 50,000 MWd/tonne, and denoted as k_0 in Table V, was as follows: The

lowest allowable k , 1.01, is to allow a reasonable amount of maneuvering room at the beginning of life and to account for nonuniformities at the end of life. A reflector plus

TABLE V. BNW SCFBR Parameter Survey

Liters	Case No.	k_0	Enrichment	Initial Breeding Ratio		K_{void}	ΔK	Pu Inventory, kg
				Core	Total			
1000	OA	1.01	15.15	0.890	1.420	0.999	-0.011	752
3000			12.55	1.067	1.390	1.000	-0.010	1867
6000			11.70	1.170	1.385	1.003	-0.007	3479
8000			11.40	1.195	1.380	1.001	-0.009	4519
10000			11.25	1.215	1.385	1.001	-0.009	5574
1000	OB	1.01	14.90	0.835	1.280	0.991	-0.019	739
3000			12.50	0.980	1.240	0.999	-0.011	1859
6000			11.67	1.071	1.255	1.001	-0.009	3470
8000			11.30	1.11	1.258	0.995	-0.015	4479
10000			11.20	1.115	1.245	0.999	-0.011	5549
1000	OC	1.024	15.15	0.755	1.080	0.999	-0.025	752
3000			12.75	0.855	1.043	1.009	-0.015	1896
6000			12.00	0.920	1.045	1.018	-0.006	3569
8000			11.65	0.947	1.050	1.014	-0.010	4619
10000			11.55	0.9485	1.0425	1.018	-0.006	5723
1000	1A	1.010	19.10	0.71	1.35			712
6000			13.40	1.015	1.307	1.003	-0.007	2991
8000			12.95	1.055	1.292	1.003	-0.007	3853
10000			12.90	1.050	1.262	1.014	+0.004	4797
1000	1B	1.022	19.4	0.64	1.12			724
6000			13.80	0.900	1.115	1.020	-0.002	3080
8000			13.40	0.926	1.105	1.023	+0.001	3987
10000			13.10	0.945	1.11	1.023	+0.001	4872
1000	1C	1.063	21.1	0.55	0.89			788
6000			15.10	0.741	0.889	1.071	+0.008	3373
8000			15.00	0.748	0.870	1.088	+0.025	4467
10000			14.50	0.770	0.775	1.082	+0.019	5396
1000	2A	1.053	30.00	0.45	0.93			749
6000			18.80	0.700	1.019	1.060	+0.007	2804
8000			17.85	0.745	1.018	1.059	+0.006	3548
10000			17.30	0.7695	1.019	1.062	+0.009	4297
1000	2B	1.076	31.2	0.39	0.80			780
6000			19.90	0.611	0.805	1.093	+0.017	2969
8000			18.90	0.645	0.840	1.093	+0.017	3759
10000			18.30	0.678	0.825	1.096	+0.020	4548
1000	2C	1.092	32.5	0.35	0.67			813
6000			21.10	0.532	0.7015	1.122	+0.030	3150
8000			20.30	0.565	0.695	1.1375	+0.046	4039
10000			19.70	0.578	0.700	1.141	+0.049	4899

blanket core savings of 35 cm based on preliminary 1-D statics calculations was assumed. A zero dimensional but fundamental mode burnup code, FCC,⁽¹²⁾ was used to trace the

reactivity versus exposure for the various cores. If an initial k of 1.01 was insufficient to provide 50,000 MWd, then the initial reactivity was raised by increasing the

initial enrichment. The data presented in this Section was based on using the modified Russian cross sections for this FCC calculation. This was expedient because the automatic code linkage already existed. Changes in the results were found to be minor when the FCC calculations were repeated later using the ORNL cross section set, as discussed in Appendix B. This method, by assuming no blanket contribution to the reactivity as plutonium builds in, causes slightly higher-than-actual enrichments to be calculated.

The loss of coolant or void calculations involve a complete recalculation using the code combination HRG, TEMPEST, and HFN, with the entire reactor, including the blanket, voided of steam. This situation is different from the familiar sodium voiding, which is caused by a local over-power condition. Loss of steam occurs only when an external pipe rupture allows depressurization of the entire system. Calculations were performed only for spherical cores. The two-dimensional calculations for cylindrical cores required for evaluation purposes is beyond the scope of this survey. The loss of coolant accident was calculated for one case, however, using transport theory as a check on the standard diffusion theory methods. The Δk on voiding was 10% higher using the transport theory. The amount of error in this coefficient does not affect the credibility of the overall calculation.

Calculations on water flooding cases were carried out utilizing the

methods described above except for the replacement of the TEMPEST thermalization code with the much more exact THERMOS⁽¹³⁾ code. Only the 8000 liter cores were examined, and total flooding of both core and blanket with 20 °C water was assumed.

The question of variable shape or height-to-diameter ratio was handled in a simplified manner. The method assumes, first, that the breeding ratio for a particular core composition is relatively insensitive to size (data in Table V show this to be reasonable) and, second, that any shape and size of reactor core may be characterized by a buckling, B^2 . The buckling of a large pancake type core is the same as that for some smaller sphere. For example, to determine the enrichment required for an 8000 liter reactor of Type OA with some H/D ratio, the buckling is calculated first. Then, looking at a plot of the enrichment versus B^2 provided by and calculated from Table V, the required enrichment is determined. The breeding ratio may be assumed constant for all reasonable size cores of OA composition, i.e., about 1.385. Thus, the large pancake reactor is identified by a breeding ratio, and an enrichment is determined from an equivalent volume spherical core having the same material composition.

RESULTS

The main results, summarized in Table V, disclose that rather high initial breeding ratios (~ 1.38) can be achieved in the reactors having

low steam inventory and very high fuel volume fraction (Case 0A). The breeding ratio is essentially independent of core volume within the range of reasonable sizes for most cases. Enrichment is insensitive to changes in core volume alone. For a given steam inventory, the breeding ratio is strongly related to fuel volume fraction in the core. For example, the breeding ratio is 1.38 for Case 0A having 60 vol% fuel, and falls to 1.02 for Case 2A, with 25 vol% fuel.

The breeding ratios appear to agree rather well with values calculated by others for specific reactor designs, considering differences in the methods employed. However, the conversion ratios for reactors with high steam inventories (Cases 2B and 2C) are considerably lower than were ratios calculated for specific designs in this range.

The information contained in Table V was used for the design and economics studies reported in the next section of this report. However, the burnup and voiding calculations were rechecked and the flooding calculations completed. Table VI contains the best adjusted physics results for an 8000 liter core.

Figure 8, showing the required initial reactivity is very similar in shape to Figure 9, showing the Δk on loss of coolant. The indication here is that systems requiring a high initial k have more of a voiding problem than the good breeders starting with a low initial reactivity. It is also generally concluded that the less steam present, the smaller the void problem for a constant core volume split. The amount of fuel present seems to be equally as important as the amount of steam. A combination of two effects accounts

TABLE VI. Best Adjusted Results for 8000 Liter Core

Case	Initial k_{eff} 50,000 MWd/T	% Pu	Initial BR _C	Initial BR _T	BR _C	BR _T	Δk_{void}	Δk_{flood}	$\frac{dk_{eff}}{d\bar{p}}$
0A	1.0100	11.30	1.203	1.390	1.125	1.312	-0.0165	-0.0060	0.0141
0B	1.0100	11.29	1.103	1.250	1.061	1.208	-0.0175	-0.0065	0.0020
0C	1.0260	11.63	0.948	1.055	0.943	1.050	-0.0155	-0.0120	0.0000
1A	1.0115	13.14	1.024	1.268	0.997	1.241	0.0000	0.0250	0.0009
1B	1.0350	13.75	0.900	1.080	0.905	1.085	0.0030	0.0110	-0.0023
1C	1.0600	14.65	0.766	0.870	0.790	0.894	0.0150	0.0010	-0.0068
2A	1.0685	18.32	0.721	0.992	0.750	1.021	0.0055	0.0395	-0.0111
2B	1.0830	19.13	0.645	0.858	0.680	0.893	0.0175	0.0310	-0.0131
2C	1.0985	20.53	0.563	0.693	0.607	0.737	0.0455	0.0265	-0.0182

FIGURE 8. Initial Reactivity

FIGURE 9. Reactivity Response

for this conclusion. First, for a given steam inventory, cores with more fuel have a harder neutron spectrum which thus brings the normal operating spectrum closer to the void spectrum and results in less spectral

change on voiding. Secondly, the more dense cores, by depending less on the steam to reflect the neutron back into the core, produce a smaller effect on the neutron current upon going from normal condition to void.

Some preliminary calculations have indicated that reactivity change upon loss of coolant increases with burnup for the 0A corner of the survey table, and is about constant with burnup for the 2C corner. However, these calculations neglected the blanket changes with burnup and are therefore not conclusive.

Figures 10, 11, and 12 show the effect of steam density on reactivity.

The circles indicate the cases corresponding to Table III. These curves, together with an analysis of steam density versus power demand, would determine whether or not the reactor would be load-following or need a control system to force it to be load-following.

Figure 13 shows the sensitivity of the total breeding ratio to the blanket thickness. All of the base

FIGURE 10. Reactivity Versus Steam Density 0.25 Steam Volume Fraction

FIGURE 11. Reactivity Versus Steam Density 0.45 Steam Volume Fraction

FIGURE 12. Reactivity Versus Steam Density 0.55 Steam Volume Fraction

FIGURE 13. Total Breeding Ratio
Versus Blanket Thickness

cases have assumed an 18 in. blanket. Calculations in Section 4 assume that the blanket can be varied with no change in breeding ratio, which appears reasonable from these data. The effect on k of going from a 24 to a 12 in. blanket is less than 0.001, which is insignificant in this survey.

Figure 14 shows a combination of total breeding ratio and loss of coolant Δk , and may be used to determine so-called allowed combinations of the variables in the study. Figures 15 and 16 show allowed "areas" for some possible operating limits for 8000 and 6000 liter core volumes. Any combination of steam density and volume which lies below the curves will satisfy the imposed restriction.

FIGURE 14. Total Breeding Ratio and Voiding Reactivity Response

The flooded case calculations indicate that water flooding in SCFBR's is not a severe problem. The driest corner of the nine parametric cases, Case 0A, shows a Δk on flooding of -0.008, while the opposite corner, Case 2C, yields a Δk of 0.026.

It is estimated that 0.2 atom % of Gadolinium added to the fuel would produce a net Δk on flooding of -0.010 and -0.024 for the 0A and 2C, respectively. It is further estimated that this amount of Gadolinium would reduce the breeding ratio by about 0.02 and is independent of steam conditions. The 1B case appears to lie midway between the 0A and the 2C cases in all aspects of flooding.

CONCLUSIONS

Neutronically, reactors in the 0A corner of the survey, that is those of low steam inventory and high fuel inventory, appear to be competitive

with many sodium-cooled fast reactor designs. Breeding ratios of over 1.3 seem possible with negative loss of coolant reactivity responses. The doubling time for an 8000 liter 0A sphere could be about 12 years, not counting out-of-reactor time. This assumes that a reactor of this composition and size can be designed within constraints imposed by thermal and material considerations, which is the main concern of the work reported in the following section.

It is believed that all assumptions and methods used are reasonable for comparison purposes and do not mask any trends. The assumption that the blanket worth, in terms of reactivity, is constant with burnup may be the source of some as yet unknown amount of conservatism. However, this effect should be reasonably constant for all cases and therefore would not favor one reactor type over another.

FIGURE 15. Allowed Combinations of Steam Density and Volume Fraction 8000 Liter

FIGURE 16. Allowed Combinations of Steam Density and Volume Fraction 6000 Liter

DESIGN AND ECONOMICS STUDIES

METHODS

The parametric study was performed using the analog simulation techniques developed by BNW for the preliminary study of potential FFTF core designs. The simulation consists of two general parts: a core design simulation and an economics simulation. Appendix A contains a condensed description of the mathematical model and calculational procedures used.

The design simulation consists of the normal thermal and hydraulic analysis, the results of the physics analysis expressed in parametric form, and calculations for powerplant cycle thermal efficiency.

The economics simulation was patterned after the ORNL treatment in order to obtain results that could be compared with their evaluation studies.

Because the simulation was large and algebraic in nature, the actual calculations were performed with the digital computer program, MIMIC. MIMIC is one of many available digital analog simulators.

Coolant properties were put into the simulation as functions of temperature and pressure. The coarse structure of the data input is responsible for some lack of smoothness in the curves of some of the parameters. The basic trends, however, are valid. Future studies could include finer structure of the data input, but it is not evident that enough benefit would be gained in the present study

to warrant redoing the calculations with this improvement.

The results of the nuclear analysis presented in Table V were incorporated into the design and economics simulation in the following manner. The enrichment was found to vary linearly with geometric buckling, while the curve slopes were found to be a function of fuel volume fraction only. The curve intercepts were functions of both fuel volume fraction and steam inventory. These slopes and intercepts were incorporated into the simulation using MIMIC function generators to allow linear interpolation between the calculated points for obtaining a specific design value. The total breeding ratio data were entered into the simulation separately as functions of fuel volume fraction and steam inventory. Average breeding ratios for a batch core burnup of 50,000 MWd/tonne were used.

ASSUMPTIONS

A number of assumptions were necessary in the design analysis. For all cases, the reactor coolant inlet temperature is the saturation temperature for the corresponding pressure. The coolant flow distribution is assumed to have the same shape as the radial power distribution. The hot channel therefore receives more coolant than the cooler channels, and changes in power distribution with burnup are not considered. The distribution of the coolant flow

is a variable which must be optimized in reaching any final reactor design. Since pumping power is quite important in a steam-cooled reactor the need for orificing to distribute the flow proportional to the radial peaking factor is not obvious. The flow distribution yielding the lowest power cost may be between the one used in this simulation and a distribution which puts an equal coolant flow through each channel.

The blanket material was distributed proportional to geometric buckling for this parametric study. For any given core volume, the blanket volume was determined as though the core had an H/D of one and a 25 cm blanket. This blanket volume between the axial and radial blankets was then distributed proportionally to the geometric buckling of the actual core. This distribution was considered adequate for parametric study purposes, although radial and axial blanket thickness are variables which should be optimized in a particular design.

A parametric study for a zoned core, fuel moving, and partial loading could not be considered. It is recognized that actual reactors would include such schemes to more fully utilize the fuel. In the thermal hydraulic analysis, the radial peaking factor was selected to reflect a zoned core inasmuch as study results would be grossly distorted by the single zone power profile.

Clad design problems, including failure criteria for designing the clad, are complex since more than one

mode of failure is possible. The clad must be thick enough, or back-filled with helium, to stand the external coolant pressure at beginning of life. The fuel pin must be strong enough at the end-of-life to withstand a loss of coolant accident without rupturing. The creep-rupture failure mechanism at end-of-life must also be considered. For the parametric study, prepressurized pins with 12 mil clad were assumed.

Fission gas plenum length was made proportional to system operating pressure to limit initial stress to $1/2$ the clad yield stress and final stress to not more than the yield stress.

The heat transfer area in the core, and linear power rating on the fuel were constrained by maximum clad temperature and maximum fuel centerline temperature. The number and diameter of pins in the core were simultaneously adjusted to maintain the maximum clad inner surface temperature at 1390 °F and the fuel centerline temperature at 4850 °F.

Unit fabrication costs were included as a function of pin diameter. The fabrication cost is a major factor in the sensitivity studies and, therefore, the shape and slope of the input curve of unit fabrication cost is important. The effect of number of pins per bundle and pin length was not included in estimating the unit fabrication costs for this study.

All unit costs were based on plants sized for a 15,000 MW_e industry.

OPERATING PARAMETERS SENSITIVITY STUDY

The results of the physics study in this report indicated that SCFBR breeder performance is improved by achieving low steam inventory and high fuel volume fractions in the design. The most direct way to vary the steam inventory is to vary the steam density. The first sensitivity studies to be discussed show the effect of system pressure and temperature on the important breeder performance parameters of a given design. The design selected as a base case for the sensitivity studies, while not optimum, was found in preliminary calculations to allow a sufficient selection range in the design and operating parameter studies.

Results of the pressure sensitivity study are shown in Figures 17 through 27. The reactor exit steam pressure is varied in the base design from 1000 psi to 3000 psi, with reactor exit temperature constant and inlet temperature at saturation. The fuel pin diameter and number of fuel pins are simultaneously varied to maintain a maximum clad inner surface temperature of 1390 °F and maximum fuel centerline temperature of 4850 °F.

Figure 17 shows the trend of core thermal power as a function of pressure. The high thermal power at lower pressures is caused by two factors. The first is the decrease in thermal efficiency of the power conversion system as pressure is decreased. The second, higher pumping power required for the greater

FIGURE 17. Core Thermal Power Versus Reactor Outlet Pressure

FIGURE 18. Hydraulic Parameters Versus Reactor Outlet Pressure

FIGURE 19. Average Coolant Velocity Versus Reactor Outlet Pressure

FIGURE 20. Fuel Pin Diameter Versus Reactor Outlet Pressure

FIGURE 21. Core Material Fractions Versus Reactor Outlet Pressure

FIGURE 22. Core Steam Inventory
Versus Reactor Outlet Pressure

FIGURE 23. Total Breeding Ratio
Versus Reactor Outlet Pressure

FIGURE 24. Critical Mass Versus Reactor Outlet Pressure

FIGURE 25. Specific Inventory Versus Reactor Outlet Pressure

FIGURE 26. Doubling Time Versus Reactor Outlet Pressure

FIGURE 27. Fuel Cycle Costs Versus Reactor Outlet Pressure

flow velocity in cores of higher thermal power (Figure 18), adds to the core thermal power requirements. The increased velocity in the core, indicated by Figure 19, actually compensates for the poor heat transfer properties of the lower density coolant. For 1260 and 2860 psi, the heat transfer rate is 1671 and 1559 Btu/(hr)(ft²)(°F/ft), respectively. The net result is a decrease in fuel pin diameter as shown in Figure 20. The fuel pins, however, are increased in number to enable the fuel volume fraction to remain approximately constant over the pressure range. This is shown in Figure 21.

Since the steam volume fraction does not change appreciably over the pressure range, the steam inventory variation shown in Figure 22 results primarily from the pressure change. The higher breeding ratio obtained at lower system pressures as shown in Figure 23 is thus due primarily to the different steam inventory. The fissile inventory and, therefore, the specific inventory (see Figures 24 and 25) does not vary greatly over the pressure range. Therefore, the doubling time* shown in Figure 26 is primarily a function of the changing breeding ratio.

The fuel cycle costs change little over the pressure range as shown in

Figure 27. Greater revenue from plutonium at low pressures is partially offset by the increased fabrication cost of smaller pins.

Another way of varying the steam density and thus the steam inventory in a reactor design is to vary the steam temperature. Figures 28 through 36 show, with inlet temperature at saturation, the sensitivity of the performance parameters to reactor outlet temperature in the range of 905 °F to 1035 °F. Due to the increased thermal efficiency of higher temperature operation, the core thermal power decreases as shown in Figure 28. The decreased coolant mass flow, due to lower thermal power and higher core temperature rise, maintains pumping power approximately constant over the temperature range, as shown in Figure 29. Increasing steam temperature causes larger pin diameters to be selected in order to maintain the specified thermal constraints. This is shown in Figure 30. The larger pins, although slightly fewer in number, cause an increase in the fuel volume fraction as shown in Figure 31. The decrease in steam inventory with increasing temperature (Figure 32) is partially due to the decrease in steam density and partially due to the decreased coolant fraction (Figure 31).

The combined effect of higher fuel fraction and lower steam inventory is to considerably increase the breeding ratio over the temperature range (in Figure 33), with changing fuel volume fraction as the most important contributor. The higher

* The definition of doubling time used here is

$$t_d = \frac{0.693}{\text{Fraction of annual increase}}$$

The out-of-core inventory for each design is based on process times for a 15,000 MW_e industry.

FIGURE 28. Core Thermal Power Versus Reactor Outlet Temperature

FIGURE 29. Hydraulic Parameters Versus Reactor Outlet Temperature

FIGURE 30. Fuel Pin Diameter Versus Reactor Outlet Temperature

FIGURE 31. Core Material Fractions Versus Reactor Outlet Temperature

FIGURE 32. Core Steam Inventory
Versus Reactor Outlet Temperature

FIGURE 33. Total Breeding Ratio
Versus Reactor Outlet Temperature

FIGURE 34. Critical Mass Versus Reactor Outlet Temperature

FIGURE 35. Doubling Time Versus Reactor Outlet Temperature

FIGURE 36. Fuel Cycle Costs Versus Reactor Outlet Temperature

fuel fraction and lower steam inventory result in a higher fissile inventory (Figure 34). The higher breeding ratio and a higher specific inventory combine, with increasing temperature, to reduce the doubling time (Figure 35). Fuel cycle costs are controlled by the plutonium revenue, as shown in Figure 36, since the higher breeding ratio obtained with increasing temperature results in a lower net plutonium cost.

In comparing these two studies, the pressure study better represents a change in steam density only. Results of the temperature study reflect the importance of changing core material volume fractions as well as varying steam density. The temperature study and subsequent core

geometry studies show that better breeder performance may be obtained other than by varying system pressure. It may, therefore, be desirable to first establish the system pressure on the basis of capital cost and then select a system temperature and a core and fuel design capable of achieving good breeder performance and economics.

GEOMETRY SENSITIVITY STUDIES

Steam-cooled fast reactor conceptual designs generally feature pancaked cores. Changes in reactor performance with change in core shapes were studied by holding the core volume constant and adjusting the number and sizes of pins in the core to meet temperature constraints, as

was done in all portions of the study. Performance appeared to be a strong function of reactor shape. As H/D increased, the breeding ratio decreased and the doubling time increased (Figures 37 and 38). This change in breeding performance was due to changes in the fuel volume fraction (Figure 39) and the steam inventory, which varied directly with the coolant volume fraction. The fuel cycle costs as a function of H/D are shown in Figure 40.

Since the more pancaked core has more of the blanket material located axially, the hydraulic characteristics are affected less by H/D changes than for a study assumption of constant blanket thickness.

The other independent geometry variable considered in this study was core volume. The effect of changing

the core volume between 5,000 and 10,000 liters for given reactor operating conditions is shown in Figure 41 to 48. Breeding ratio, shown in Figure 41, is relatively independent of core volume. However, the doubling time of the system (Figure 42) is quite dependent upon core volume, since the critical mass (Figure 43) changes considerably between the 5,000 and 10,000 liter cores. Consequently, the specific inventory shown in Figure 44 is also a strong function of core volume. The increasing fissile inventory has the effect of raising the inventory costs as volume increases. The material fractions over the volume sweep remain fairly constant as shown in Figure 45. The total number of pins in the core decreases as core

FIGURE 37. Total Breeding Ratio
Versus Core Height to Diameter Ratio

FIGURE 38. Doubling Time Versus Core Height to Diameter Ratio

FIGURE 39. Core Material Fractions Versus Core Height to Diameter Ratio

FIGURE 40. Fuel Cycle Costs Versus Core Height to Diameter Ratio

FIGURE 41. Total Breeding Ratio Versus Core Volume

FIGURE 42. Doubling Time Versus Core Volume

FIGURE 43. Critical Mass Versus Core Volume

FIGURE 44. Specific Inventory Versus Core Volume

FIGURE 45. Core Material Fractions Versus Core Volume

FIGURE 46. Total Number of Fuel Pins
Versus Core Volume

FIGURE 47. Fuel Pin Diameter Versus
Core Volume

FIGURE 48. Fuel Cycle Cost Versus Core Volume

volume increases, and the pin diameter increases as core volume increases (Figures 46 and 47). However, the overall change in fabrication costs is negligible as shown in Figure 48. The total fuel cycle cost is influenced almost entirely by the changing inventory costs.

The sensitivity of reactor performance to geometry variations may change at different steam conditions. There was insufficient time during this study to investigate other steam conditions, but it would be desirable to do so in the future, particularly at lower pressures.

DESIGN COMPARISON STUDY

The BNW design and economic simulations were originally set up with a set of values for some of the design

variables based on the Karlsruhe D-1 design. During the initial phases of the parametric study, it became apparent that these values (e.g. for fuel spacer pressure drop, out-of-core pressure drop, etc.) would not permit considering low pressure designs where better breeder performance could be expected. Redesigning the core by varying the core volume, the height-to-diameter ratio, and the fuel design to achieve lower pumping powers at the lower steam densities resulted in a net loss in breeder performance. The performance was reduced when each of these variables was changed either due to a lower breeding ratio or an increased doubling time due to a higher fissile inventory.

To obtain the potential breeder performance inherent in low steam

density design, it was necessary to streamline the fuel and power conversion system and assume a reduced engineering peaking factor. Streamlining of the power conversion system was represented in the BNW simulation by varying the out-of-core pressure drop. The range considered was 30 to 130 psi. Streamlining the fuel, which in reality would be changing the fuel pin spacer design and possibly the fuel pin design, was represented in the BNW simulation by varying the friction factor multiplier used to calculate the in-core pressure drop.

Figures 49 through 55 are a comparison of the major performance parameters for three different SCFBR designs over a range of pressures. Curve (1) in each figure represents the performance of a design resembling the Karlsruhe D-1 design in its thermal and hydraulic characteristics. It has a 130 psi out-of-core pressure drop, an engineering peaking factor of 1.34, and a friction factor multiplier of 2 applied to the fuel region of the core.

Curve (2) in each figure represents a design closely resembling the

FIGURE 49. Pumping Power Versus Reactor Outlet Pressure

FIGURE 50. Total Breeding Ratio Versus Reactor Outlet Pressure

FIGURE 51. Doubling Time Versus Reactor Outlet Pressure

FIGURE 52. Fuel Cycle Cost Versus Reactor Outlet Pressure

FIGURE 53. Volume Fraction Fuel Versus Reactor Outlet Pressure

FIGURE 54. Core Steam Inventory Versus Reactor Outlet Pressure

FIGURE 55. Core Thermal Power Versus Reactor Outlet Pressure

B&W low pressure design in its thermal and hydraulic assumptions. It has a 30 psi out-of-core pressure drop, an engineering peaking factor of 1.1, and a friction factor multiplier of 1 applied in the fuel region. The core geometry is also considerably different from that of Curve (1), having a smaller volume and a lower H/D ratio. The geometry variables for these two designs, while not necessarily optimum, were chosen because they were close to the two mentioned reference designs. Comparing Design (2) with Design (1), indicates the variation in geometry variables that can be achieved by reduced hydraulic and thermal restraints. The core outlet temperature is 1000 °F for both cases. The third curve in each figure represents a reactor very similar in design and thermal requirements to the second reactor. The hydraulic characteristics have been varied to obtain an out-of-core pressure drop of 80 psi and a friction factor multiplier of 1.4.

Figure 49 shows the pumping power of these three reactors over the pressure range considered. It is interesting to note the relative position of the three curves. The positions are more easily correlated with the hydraulic assumptions for out-of-core pressure drop and friction factor multiplier than with such geometry variables as volume and H/D ratio, or the engineering peaking factor representing reactor thermal characteristics. It should also be noted here when discussing thermal characteristics that each of the

three reactors are designed for a maximum fuel centerline temperature of 4850 °F and a peak inside clad temperature of 1390 °F. The trends are thus compared on a common set of thermal restraints and thermal performance data.

Figures 50, 51, and 52 compare the breeder performance and the fuel cycle cost of the three reactor designs. Since the reactors represented by Curves (2) and (3) differ only in their hydraulic characteristics, it can be concluded that the difference in breeder performance and fuel cycle costs existing among the three designs is due primarily to the selected core geometries and thermal performance requirements represented by the various engineering peaking factors.

The better breeding ratio of Designs (2) and (3) over (1), at a given pressure, is due largely to the different volume fraction of fuel in the designs as shown in Figure 53. A compounding effect in the increased fuel fraction causes a lower steam fraction. This effect is represented by the difference in the steam inventories shown in Figure 54. The combination of the higher fuel fraction and lower steam inventory results in the improved breeding ratio for Designs (2) and (3).

The improved doubling time shown in Figure 51 for Designs (2) and (3) is caused primarily by the increase in the breeding ratio. Actually, the specific inventory for Designs (2) and (3) is slightly higher than for Design (1). For instance, at 2000 psi, Design (2) has a specific

inventory of 5.34 kg Pu/kW_e, compared with 5.12 kg Pu/kW_e for Design (1). The increased inventory in Designs (2) and (3) results from the lower H/D ratio, the increased fuel fraction, and the lower steam inventory in the core. Although the smaller volume tends to decrease fissile inventory, the net effect is an increase.

Pumping power (Figure 39) as it affects reactor thermal power is shown in Figure 55. From these results, and the fuel cycle costs

shown in Figure 52, the pumping power limits can be seen as an important factor requiring resolution by design and capital cost studies. In general, it can be concluded that breeding characteristics and fuel cycle costs provide an incentive to design cores of small volume, high fuel volume fraction, low steam pressure, and low flow resistance in instances where such designs do not impose an excessive penalty in capital or core fabrication costs.

APPENDIX A

SCFBR DESIGN AND ECONOMICS SIMULATION DESCRIPTION

APPENDIX A

SCFBR DESIGN AND ECONOMICS SIMULATION DESCRIPTIONMATHEMATICAL MODEL

The purpose of this appendix is to explain, briefly, the analytical model and calculational procedures used to obtain the sensitivity study results of this report found in the Design and Economics Studies Section. A summary of the equations and a complete nomenclature list are found in this appendix.

The philosophy used in choosing the form for the equations was to put them in a straightforward form without any back solving. This results in some implicit relationships, such as that between heat transfer coefficient and film temperature, that must be resolved iteratively. This form of the equations also results in some quantities, such as maximum clad temperature, being calculated when normally the designer must specify them or at least set rigid limits on them. The procedures used to handle both the implicit functions and design constraints are discussed under Calculational Procedures in this section.

The simulation is intended to predict gross sensitivities; hence, certain areas of the mathematical model have been developed more than others, depending on their effect on the overall results.

Coolant properties for thermal hydraulic analysis found in Keenan and Keyes⁽¹⁴⁾ were incorporated into the simulation in tabular form. Linear interpolation between the tabular points was then used to find the coolant properties for specific cases.

To begin the calculations for a specific design, the independent parameters (shown in the Summary of Equations Section) must be specified. The independent parameters can be classified into two groups:

- Those representing the reactor description and steam system conditions
- Those representing the design philosophy and constraints to be considered.

The parameters in the first category (e.g. core volume, H/D ratio, etc.) are fairly straightforward. However, the second category contains assumptions made about the current status of steam-cooled reactor technology that are generally not quantified in a parametric analysis. For example, the out-of-core pressure drop is specified to represent the degree of compactness of the power conversion system. There is considerable variation among the SCFBR designs proposed. A friction factor multiplier is specified to predict the effect on the hydraulic performance

of the fuel pin spacer design. This factor simply multiplies the smooth channel Fanning friction factor as shown in Equation (B-15). The friction factor multiplier for various spacer designs can be estimated from several sources.^(15,16) The maximum clad and fuel temperature essentially limit the fuel design by limiting the heat flux and linear heat rating, respectively. The maximum fuel burnup and fuel density are also specified as values being proposed for SCFBR application.

To begin the iterative procedures in the simulation, initial values must be specified for the variables shown in the section on Summary of Equations. They are then adjusted internally so all specified parameters are maintained. These estimates are usually obtained from previous studies or existing designs.

To perform a set of calculations, the simulation first goes through the geometry calculations and determines: core diameter, height, effective height, number of power producing subassemblies, subassembly lattice spacing, and inside pin diameter. The fuel cross-sectional area, coolant flow area per subassembly, hydraulic diameter, number of feet of pin in the core, and the volume fractions for each of the types of materials in the core are also found in a straightforward manner. The assumptions made are:

- The core is a right circular hexagon, (made up of hexagonal subassemblies)
- The fuel cans have 0.10-in. walls
- The gap between subassemblies is 50 mils.

Equations (A-1 through A-13) summarize these calculations.

The axial and radial blankets are assumed to have a combined volume equal to that of a 25 cm blanket on a core with an $H/D = 1.0$. The total blanket volume is then distributed between axial and radial blankets by the ratio of axial-to-radial buckling for the core H/D being considered. From this, the blanket thicknesses are calculated. The blanket calculations are summarized in Equations (A-14 through A-28).

Equations (A-29 or A-30) show calculations of a fission gas plenum size. These equations assume a constant clad thickness, (0.012 in. was used for the studies in this report), and use the following criteria. At beginning of life and at full power, the helium backfull pressure is such that the pressure stress is equal to one-half the yield stress. At the end of life and in the event of a loss of coolant accident, the pressure stress would not exceed the yield stress. The fissioning process was assumed to yield 0.28 atoms of gas per fission, with 100% release from the fuel. A yield stress of 18,000 psi at a maximum cladding temperature of 1374 °F was used. The use of yield stress as a failure criterion is not as rigorous as the technique used by most SCFBR designers. There are several clad design techniques that can be employed over the range of pressures involved in this study. The simplified method used here agrees well with other analyses of prepressurized fuel pins, and it yields reasonable results over a wide

range of fuel pin diameters and system pressures. The end results of the parametric study are not strongly affected by the fission gas plenum design except in cases with very high pumping power.

By using the initial values for core pressure drop and thermal power, an iterative solution of Equations (B-1 through B-9) yields a new core thermal power and cycle efficiency. The turbine, feedwater heaters, and feedwater pumps were considered as a unit, and a "conversion efficiency" was determined from a more complete analysis of a similar cycle done by Atomics International.⁽¹⁷⁾ The values obtained from this report were corrected for lack of sodium reheat and extrapolated to include higher steam temperatures. The values used for conversion efficiency are shown in Figure A-1, as a function of steam cycle inlet temperature and pressure. These data were entered into the simulation in tabular form, and Equation (B-4) represents a table look-up for the steam conditions being considered.

Equations (B-5 through B-7) determine the ideal and actual pumping power. A constant mechanical efficiency for the circulator was used in Equation (B-7) so different types of circulators, axial or radial flow, could be simulated. All the studies reported here used 76% mechanical efficiency, which is typical of a 12 MW axial flow compressor.

The hydraulic calculations are encountered next. Mass flow rates and friction factors are found by using steam properties supplied by

FIGURE A-1. Steam Cycle Conversion Efficiency as a Function of Turbine Inlet Conditions

known tabular data and the power and geometry values. The ideal Fanning friction factor correlation of Equation (B-14) is given by Sutherland.⁽¹⁸⁾ This is modified in Equation (B-16) by the factor GFRFAC, the correction for the spacer design.

In the relationship for core pressure drop, Equation (B-16), the first term in the brackets is the change in flow cross section on leaving the inlet plenum, the next three terms account for the friction in going through the core, the term $1/\rho_{OH}$ is the loss due to cross section change on leaving the core, and the final two terms are losses due to the compressibility of the vapor.

The hot channel thermal analysis is performed next in Equations (C-1 through C-9). The distance to the point of maximum clad temperature is

first determined by using an assumed film coefficient and a chopped cosine axial power distribution, Equation (C-4). With this location known, the film temperature can be found and the film coefficient can be solved by using Equation (C-6) for the film Nusselt number.⁽¹⁹⁾ The maximum clad temperature can then be found also, but this involves another iterative solution since the film temperature is a function of the film coefficient.

The maximum fuel centerline temperature is assumed to occur at the midplane of the core. The fuel surface temperature is found from Equation (C-7). The heat transfer coefficient at the point of maximum clad temperature is used at the core midplane also. This was found to have a small effect and eliminated one iterative process. Equation (C-8) gives the fuel centerline temperature from linear curve fits of BNW $\int k dT$ data for ViPac fuel.⁽²⁰⁾ Linear curve fits are made of two regions of the data curve: one region centers on expected values of fuel surface temperature, the other on expected centerline temperatures. These are then combined in Equation (C-8).

The physics data for a specific design are determined in Equations (D-1 through D-23). The desired results of the physics survey, (described in the Neutronics Survey Section) were curve fitted for incorporation into the design and economics simulations. The coefficients of the curve fits were entered as tabular data in Equations (D-6, D-7, and D-9). These are used to determine the total heavy metal inventory and throughput rates

in the core and blankets, as well as the plutonium inventory in the core. One correlation yields the total breeding ratio (core and blanket), and the specific inventory and doubling time are also calculated.

The fuel cycle costs are estimated next, with Figure A-2 showing a schematic flow sheet of the fuel cycle considered. Unit costs were based on a 1.5 tonne/day reprocessing and fabrication complex. The ORNL cost estimates developed for the Alternate Coolant Task Force were used. The constants in the unit fabrication cost correlation [Equation (E-2)] were evaluated from the two specific designs estimated by ORNL. These were the Karlsruhe D-1 design and B&W 1200 psi design.

Although the fuel cycle costs are only approximate, they agree closely with specific designs and adequately predict the trends with changing design parameters.

CALCULATIONAL PROCEDURES

To perform the calculations on the mathematical model just described, the equations were programmed in MIMIC for the UNIVAC 1108 computer. MIMIC is one of several available digital-analog simulators. It is actually a computing language in itself, and the equations are programmed in a language similar to FORTRAN. However, the MIMIC program has built-in analog functions such as integration routines that can be called upon.

The built-in integration routines allow the use of integral control techniques to constrain calculated

quantities by varying a desired input parameter. The schematic diagram of Figure A-3 illustrates the use of this technique. At time zero, the design simulation using the specified parameters for a design and the initial values for number of subassemblies and fuel pin diameter performs the calculations previously discussed. Among other things, the fuel center-

line and maximum clad temperature are calculated. The differences between the calculated and desired values are calculated forming an error. This error is modified by the gain of the control circuit and integrated over the next time increment to estimate the correct value of number of subassemblies and fuel pin diameter. The corrected values are fed into the

FIGURE A-2. SCFBR - Schematic Flow Sheet for Fuel Cycle Economics Simulation

FIGURE A-3. Integral Control Diagram for TCL and TCMAX Controllers

design simulation and the calculations repeated. After several time steps, the desired values for maximum centerline and clad temperature are obtained. The stability and convergence of this control circuit is dependent on the selection of the circuit gains--GNSUB and GDO.

To perform the parametric studies reported here, one of the independent parameters, e.g. core volume, was changed with each time increment. By properly selecting the volume sweep rate and the control circuit gains, the controllers adjusted the number of subassemblies and fuel pin diameter so the desired fuel centerline and maximum clad temperatures were maintained. This type of study can be called a dynamic design simulation. The principle output parameters, breeding ratio, doubling time, fuel cycle costs, etc., are found for various core volumes with consistent design constraints.

The constraints can also be varied with time to obtain the sensitivity of the results to the constraints.

The integral control technique has its biggest advantage in the ver-

satility it affords. Figure A-4 illustrates three acceptable ways of maintaining volume fraction fuel constant. The three circuits say that volume fraction fuel can be changed in the reactor core by changing core volume, core outlet temperature or maximum allowable clad temperature. All that is required of the simulation to change from one circuit to another is to change the control equation and determine a new gain for the circuit. There is never any back solving necessary nor extensive program logic changes.

The iterative solution for such quantities as heat transfer coefficient are accomplished by an algebraic feedback technique in MIMIC called the implicit function. If the convergence criteria for this algebraic function are satisfied, the iteration completely converges for each time step in the study. This type of function is used to solve for heat transfer coefficient, core pressure drop, and core thermal power in the SCFBR design simulation.

FIGURE A-4. Three Possible Integral Control Circuits for Maintaining Volume Fraction Fuel Constant.

SUMMARY OF EQUATIONS FOR SCFBR DESIGN AND ECONOMICS SIMULATION

Independent Parameters

V	Core Volume (liters)
H/D	Core height to diameter ratio
(H'-H)/2	Reflector savings (ft)
NP	Number of fuel pins per subassembly
NCR	Number of control positions in the core
DPOC	Out-of-core pressure drop (psia)
P3	Core outlet pressure (psia)
T3	Core outlet temperature (°F)
PFE	Engineering power peaking factor
PFR	Radial nuclear power peaking factor
GFRFAC	Friction factor multiplier
TCSP	Maximum allowable clad temperature (°F)
TCLSET	Maximum allowable fuel centerline temperature (°F)
BUMAX	Maximum fuel exposure [MWd/tonne (u + Pu)]
ρ_f	Fuel density (g/cm ³)

Initial Values

d_o	Fuel pin diameter (ft)
NSUB1	Total number of subassemblies in the core
h	Heat transfer coefficient at point of maximum clad temperature (B/hr-ft ² -F)
DPCORE	Core pressure drop (psia)
PTT	Core thermal power (MW)

Geometry Calculations

I) Core

$$D = 0.355 \sqrt[3]{\frac{V}{H/D}} \quad (A-1)$$

$$H = (H/D) D \quad (A-2)$$

$$H^1 = H + 2 (H' - H) / 2 \quad (A-3)$$

$$NPR = NSUB1 - NCR \quad (A-4)$$

$$d_1 = \sqrt{\frac{0.04076 V}{NSUB1 H}} \quad (A-5)$$

$$d_o = \int_0^\infty GDO (TCMAX - TCSP) dt + d_o \text{ (initial)} \quad (A-6)$$

$$d_i = d_o - 0.001 \quad (A-7)$$

$$A_{fuel} = \pi d_i^2 / 4 \quad (A-8)$$

$$AC = 0.866 (d_1 - 0.02083)^2 - \frac{\pi d_o^2 NP}{4} \quad (A-9)$$

$$VFCOOL = \frac{4}{\pi D^2} NPR (AC) + 0.866 NSUB1 [d_1^2 - (d_1 - 0.00416)^2] \quad (A-10)$$

$$VFCLAD = (d_o^2 - d_i^2) NP NPR + 1.1026 NSUB1 [(d_1 - 0.00416)^2 - (d_1 - 0.02083)^2] \quad (A-11)$$

$$VFFUEL = \frac{4NP NPR A_{fuel}}{\pi D^2} \quad (A-12)$$

$$VFCON = \frac{1.1026 (d_1 - 0.02083)^2 NCR}{D^2} \quad (A-13)$$

II) Blankets

$$DHOD1 = 10.82 \sqrt[3]{V} \quad (A-14)$$

$$BVOL = \frac{\pi (DHOD1 + 50.)^3}{4000} - V \quad (A-15)$$

$$DMET = 30.48 D \quad (A-16)$$

$$HMET = (H/D) DMET \quad (A-17)$$

$$BR2 = 23.136 / (DMET)^2 \quad (A-18)$$

$$BA2 = 9.8697 / (HMET)^2 \quad (A-19)$$

$$BUCK = BR2 + BA2 \quad (A-20)$$

$$VA = BVOL \frac{(BA2)}{BUCK} \quad (A-21)$$

$$TA = 636.61 VA / (DMET)^2 \quad (A-22)$$

$$HT = 2 TA + HMET \quad (A-23)$$

$$VT = BVOL + V \quad (A-24)$$

$$DTOT = 35.7 VT / HT \quad (A-25)$$

$$TR = DTOT - DMET \quad (A-26)$$

$$VR = BVOL - VA \quad (A-27)$$

$$LBJ = TA / 30.48 \quad (A-28)$$

III Fission Gas Plenum

For: $P_2 < 1500$ psia

$$Lfg = \frac{[T2+460]}{3000} \frac{(14.7) (2.79 \times 10^{-5}) BUMAX \rho_f H}{530} \quad (A-29)$$

For: $P_2 > 1500$ psia

$$Lfg = \frac{[T_2+460]}{[4500-P_2]} \frac{(14.7) (2.79 \times 10^{-5}) \text{ BUMAX } \rho_f H}{530} \quad (\text{A-30})$$

Hydraulics Calculations

$$P_2 = P_3 + \text{DPCORE (Using initial value for DPCORE)} \quad (\text{B-1})$$

$$P_1 = P_3 - \text{DPOC} \quad (\text{B-2})$$

$$\text{WS} = 3.413 \times 10^6 (\text{PTT}) \text{ PFRAD/NPR } (H_3-H_2) \quad (\text{B-3})$$

(Using initial value for PTT)

$$\text{TEFF} = f(P_3, T_3) \quad (\text{B-4})$$

$$W = f\left(\frac{P_2+P_1}{2}, T_2\right) \quad (\text{B-5})$$

$$H_1 = H_2 + W (T_2+460) \ln\left(\frac{P_2}{P_1}\right) \quad (\text{B-6})$$

$$\text{PUMPP} = \text{NPR (WS) } (H_1-H_2)/3.413 \times 10^6 \text{ PFRAD(CEFF)} \quad (\text{B-7})$$

$$\text{PTT} = \left\{ \frac{1000}{\text{TEFF}} + \text{PUMPP} \right\} 0.95 \quad (\text{Iterative solution necessary because PUMPP and TEFF depend on PTT}) \quad (\text{B-8})$$

$$\text{EFFIC} = 950/\text{PTT} \quad (\text{B-9})$$

$$G = \text{WS/AC} \quad (\text{B-10})$$

$$\text{VC} = G/3600 \bar{\rho} \quad (\text{B-11})$$

$$dh = 4 \text{ AC}/3.464 (d_1 - 0.02083) - \pi (\text{NP}) d_o \quad (\text{B-12})$$

$$\text{Re} = G dh/\mu \quad (\text{B-13})$$

$$f = 0.112 \text{ Re}^{-0.2} \quad (\text{Re} > 10^5) \quad (\text{B-14})$$

$$\text{L1b=Lub} = \text{TA}/2. \quad (\text{B-15})$$

$$\begin{aligned} \text{DPCORE} = & \frac{G^2}{1.202 \times 10^{11}} \left\{ \frac{0.5}{\rho_{in}} + \frac{4 f \text{ GFRFAC}}{dh} \left[\frac{(Lfg+L1b)}{\rho_{in}} \right. \right. \\ & \left. \left. + \frac{H}{\rho_H} + \frac{\text{Lub}}{\rho_{OH}} \right] + \frac{1}{\rho_{OH}} + \frac{T_3-T_2}{\rho_H (460+T_{ave})} + \frac{P_2-P_3}{\rho_H P_{ave}} \right\} \\ & (\text{Iterative solution because } G, T_2, P_2 \text{ depend on DPCORE}) \end{aligned} \quad (\text{B-16})$$

Thermal Calculations

$$\text{PFAX} = \frac{\pi H}{H' \left[\cos \frac{\pi}{2} \frac{(H'-H)}{H'} - \cos \frac{\pi}{2} \frac{(H'+H)}{H'} \right]} \quad (\text{C-1})$$

$$q'''_{ave} = 3.413 \times 10^6 \text{ PTT}/A_{fuel} \text{ NP NPR } H \quad (\text{C-2})$$

$$q'''_{max} = q'''_{ave} (\text{PFAX}) (\text{PFR}) (\text{PFE}) \quad (\text{C-3})$$

$$XC = \frac{H'}{\pi} \tan^{-1} \left\{ \frac{CP \cdot WP}{H'} \left[\frac{1}{hdo} + \frac{\ln(do/di)}{2Kc} \right] \right\} \quad (C-4)$$

$$TCMAX = T2 + \frac{q'''_{\max} A_{fuel}}{\pi} \left\{ \frac{H'}{CP \cdot WP} \left[\cos \frac{\pi}{2} \frac{(H' - H)}{H} - \cos \frac{\pi XC}{H'} \right] + \left[\frac{1}{hdo} + \frac{\ln(do/di)}{2Kc} \right] \sin \frac{\pi XC}{H'} \right\} \quad (C-5)$$

$$Nuf = \frac{hdh}{Ks} = 0.0073 (Re_f)^{0.886} (Pr_f)^{0.61} \left(1 + \frac{2.76}{xc/dh} \right) \quad (C-6)$$

(Iterative solution because Re, Pr are evaluated at the film temperature)

$$Tfo = T2 + \frac{q'''_{\max} A_{fuel}}{\pi} \left\{ \frac{H'}{CP \cdot WP} \cos \frac{\pi (H' - H)}{2 H'} + \frac{1}{hdo} + \frac{\ln(do/di)}{2Kc} + \frac{1}{hgapi} \right\} \quad (C-7)$$

$$TCL = 1098 + \frac{q_{\max} A_{fuel}}{17.95} + 0.657 Tfo \quad (C-8)$$

($\int K dt$ data represented by two straight lines)

$$NSUB1 = \int_0^{\infty} GNSUB (TCL - TCLSET) dt + NSUB(Initial) \quad (C-9)$$

Physics Data

$$\rho = \bar{\rho} \cdot VFCOOL \quad (D-1)$$

$$REFSM = 30.48 \frac{(H' - H)}{2} \quad (D-2)$$

$$DPM = DMET + 2REFSM \quad (D-3)$$

$$HPM = HMET + 2REFSM \quad (D-4)$$

$$BUCK3 = \frac{23.136}{(DPM)^2} + \frac{9.870}{(HPM)^2} \quad (D-5)$$

$$SLP = f(VFFUEL) \quad (D-6)$$

$$INTCP = f(VFFUEL, \rho) \quad (D-7)$$

$$EF = SLP(BUCK3) + INTCP \quad (D-8)$$

$$BRT = f(VFFUEL, \rho) \quad (D-9)$$

$$VFP = 2.832 \times 10^4 (A_{fuel}) (H) (NPR) (NP) \quad (D-10)$$

$$VFPU = EF (VFP) \quad (D-11)$$

$$VFU = VFP - VFPU \quad (D-12)$$

$$WFPU = 0.01011 (TDF) (VFPU) \quad (D-13)$$

$$\text{WFUC} = 0.00967 (\text{TDF}) (\text{VFU}) \quad (\text{D-14})$$

$$\text{WHMC} = \text{WFUC} + \text{WFPU} \quad (\text{D-15})$$

$$\text{WHMA} = 10.102 (\text{VA}) (\text{VFFUEL}) (\text{TDF}) \quad (\text{D-16})$$

$$\text{WHMR} = 10.102 (\text{VR}) (\text{VFFUEL}) (\text{TDF}) \quad (\text{D-17})$$

$$\text{MC} = \text{WFPU} (\text{FF})$$

$$\text{BUAV} = \text{BUMAX}/\text{PFA}(\text{PFR}) \quad (\text{D-18})$$

$$\text{TPC} = 1/24 (\text{BUAV}) (\text{EFFIC}) \quad (\text{D-19})$$

$$\text{TPA} = \text{TPC} (\text{WHMA})/\text{WHMC} \quad (\text{D-20})$$

$$\text{TPR} = \text{TPC} (\text{WHMR}) (\text{RBF})/\text{WHMC} \quad (\text{D-21})$$

$$\text{SI} = \text{MC}(1 + \text{TXCORE}/\text{TICORE})/1000 \quad (\text{D-22})$$

$$\text{DUBT} = 1803.7 (\text{SI})/\text{PF}(\text{PTT}) (\text{BRT-1.}) \quad (\text{D-23})$$

Fuel Cycle Costs

$$\text{C1} = 1000(\text{Cu} + \text{C}_{\text{S1}}) \left\{ \frac{\text{TPC}(\text{FF}-\text{EF})}{\text{FF}} + \text{TPA} + \text{TPR} \right\} \quad (\text{E-1})$$

$$\text{CFAB} = 76.2 (\text{do})^{-0.304} \quad (\text{E-2})$$

$$\text{C2} = 1000 \frac{(\text{TPC} + \text{TPA} + \text{TPR})}{(1 - \text{LCF})} (\text{CFAB} + \text{CS2}) \quad (\text{E-3})$$

$$\text{C4} = 1000 (\text{TPC} + \text{TPA} + \text{TPR}) (\text{CRP} + \text{CS4}) \quad (\text{E-4})$$

$$\text{T1} = \text{T4} = [\text{WHMC} + \text{WHMA} + \text{WHMR} (\text{RBF})]/1000 \text{ PLCAP} \quad (\text{E-5})$$

$$\text{TICORE} = \text{BUAV} (\text{WHMC})/1000 (\text{PF}) (\text{PTT}) \quad (\text{E-6})$$

$$\text{TXCORE} = \text{TH1} + \text{T1} + \text{TS1} + \text{TH2} + \text{TD} + \text{TS4} + \text{T4} \quad (\text{E-7})$$

$$\text{CI} = 1000 \frac{(\text{TPC})(\text{EF})}{(1-\text{LCF})} (\text{CPU}) (\text{CIP}) (\text{TXCORE} + \text{TICORE}) \quad (\text{E-8})$$

$$\text{CUPU} = \text{PTT} (\text{CPU}) (1 - \text{BRT})/22.8 \times 10^6 \quad (\text{E-9})$$

$$\text{CUT} = \text{CI} + \text{CUPU} \quad (\text{E-10})$$

$$\text{CFC} = \text{C1} + \text{C2} + \text{C4} + \text{CUT} \quad (\text{E-11})$$

NOMENCLATURE

AC	Coolant flow area per subassembly (ft^2)
A_{fuel}	Cross section area of fuel per pin (ft^2)
BA2	Axial buckling for a bare core (cm^{-2})
BR2	Radial buckling for a bare core (cm^{-2})
BRT	Total breeding ratio $\left(\frac{\text{fissile atoms produced}}{\text{fissile atoms destroyed}}\right)$
BUAV	Average burnup of core fuel material (MWd/Tonne U+Pu)
BUCK	Total geometrical buckling for a bare core (cm^{-2})
BUCK3	Total geometrical buckling including the reflector savings (cm^{-2})
BUMAX	Maximum burnup of core fuel material (MWd/Tonne U+Pu)
BVOL	Total axial and radial blanket volume (liters)
C1	Cost of depleted uranium (mils/kW-hr)
C2	Total fabrication cost (mils/kW-hr)
C4	Total reprocessing and shipping costs (mils/kW-hr)
CEFF	Circulator efficiency
CFAB	Unit fabrication cost (\$/kg U+Pu)
CI	Total inventory charge for fissile material (mils/kW-hr)
CIP	Daily interest rate $\left(\frac{\%/ \text{day}}{100}\right)$
C_p	Average specific heat of steam in the hot channel (B/lb-°F)
CPU	Plutonium value (\$/kg total Pu)
CRP	Unit reprocessing cost (\$/kg U+Pu core and blanket)
CS1	Unit cost of shipping UF from diffusion to fabrication plant (\$/kg)
CS2	Unit cost of shipping fuel from the fabrication plant to the reactor (\$/kg)
CS4	Unit disassembly and shipping costs (\$/kg U+Pu)
CU	Unit core of depleted UF_6 (\$/kg)
CUPU	Net plutonium costs (mils/kW-hr)
CUT	Total fissile fuel use charges (mils/kW-hr)
D	Core diameter (ft)
dh	Hydraulic diameter (ft)

DHODI	Diameter of a core with the same core volume but an H/D of 1 (cm)
di	Cladding inside diameter (ft)
dl	Subassembly lattice spacing (ft)
DMET	Core diameter in metric units (cm)
do	Pin outside diameter (ft)
DPM	Effective core diameter (cm)
DPOC	Out-of-core pressure drop
DPCORE	Core pressure drop (psia)
DTOT	Outside diameter of the radial blanket (cm)
DUBT	Reactor doubling time (yr)
EF	Beginning of life plutonium enrichment (kg Pu/kg U+Pu)
EFFIC	Cycle efficiency including circulator
f	Fanning friction factor
FF	Fraction of total Pu that are fissile atoms
FTFR	Beginning of life fissile to fertile atom ratio
G	Mass velocity (lb/hr-ft ²)
GAMMA	Fissile atoms destroyed (fraction of initial fissile atoms)
GDO	Gain on the maximum clad temperature integral controller
GFRFAC	Fanning friction factor multiplier to account for spacer design
h	Local film coefficient (B/hr-ft ² -°F)
H	Core height (ft)
H'	Effective core height (ft)
H/D	Height-to-diameter ratio of core
$\frac{H'-H}{2}$	Reflector savings (ft)
hgap	Gap heat transfer coefficient (B/hr-ft ² -°F)
HPM	Effective core height (cm)
HMET	Core height in metric units (cm)
HT	Total core height including axial blankets (cm)
INTCP	Intercept in a linear correlation of enrichment versus buckling
Kc	Clad thermal conductivity (B/hr-ft-°F)
Ks	Steam thermal conductivity (B/hr-ft-°F)

LBJ	Axial blanket thickness (ft)
LCF	Loss in fabrication and conversion plant (fraction of thru put)
Lfg	Length of fission gas plenum (ft)
Llb	Length of lower blanket (ft)
LRP	Loss in reprocessing plant (fraction of thru put)
Lub	Length of upper blanket (ft)
MC	Critical mass (kg Pu ²³⁹⁺²⁴¹)
NCR	Number of control rods
NP	Number of pins per subassembly
NPR	Number of power producing subassemblies
NSUB1	Number of subassemblies
Nuf	Nusselt number at local film conditions
P ₁ , T ₁ , h ₁	Pressure temperature, and enthalpy entering circulator
P ₂ , T ₂ , h ₂	Steam conditions leaving circulator, entering core
P ₃ , T ₃ , h ₃	Steam conditions leaving core, entering steam cycle
Pave	Average coolant pressure (psia)
PF	Plant factor
PFAx	Axial power factor
PFE	Engineering power factor
PFR	Radial power factor
Pr _f	Prandtl number at film conditions
PTT	Total thermal power produced in the core (MW)
PUMPP	Circulator power (MW)
q''' _{ave}	Average volumetric heat generation rate (B/hr-ft ³)
q''' _{max}	Maximum volumetric heat generation rate (B/hr-ft ³)
RBF	Fraction of the radial blanket processed for each reloading
Re	Average Reynolds number
Re _f	Reynolds number at film conditions
REFSM	Reflector savings in metric units (cm)
SI	Specific inventory $\frac{\text{kg (Pu}^{239+241} \text{ in the cycle)}}{\text{MW}_e}$

SLP	Slope in a linear correlation of enrichment versus buckling
SRHOS	Smeared steam density (lb/ft ³ of core)
T4	Reprocessing time (days)
TA	Axial blanket thickness (cm)
Tave	Average coolant temperature (°F)
TCL	Maximum fuel temperature (°F)
TCLSET	Maximum allowable fuel centerline temperature (°F)
TCMAX	Maximum clad temperature (°F)
TCSP	Maximum allowable fuel clad temperature (°F)
TDF	Fuel density (fraction of theoretical density)
TEFF	Conversion efficiency of steam cycle, exclusive of steam circulator
T _f	Film temperature used in evaluating local heat transfer coefficient
Tfo	Fuel surface temperature (°F)
TH2	Hold up time at the reactor (days)
TH1	Preconversion and fabrication hold time (days)
T1	Time in conversion and fabrication plant (days)
TICORE	Time in the reactor (days)
TPC	Thru put of core material (kg U+Pu/kW-hr)
TPA	Thru put of axial blanket material (kg U/kW-hr)
TPR	Thru put of radial blanket material (kg U/kW-hr)
TR	Radial blanket thickness (cm)
TS1	Time to ship from fabrication plant to reactor (days)
TS4	Decay, disassembly and postreactor shipping time (days)
TXCORE	Total out-of-core time for each loading (days)
V	Core volume (liter)
VA	Axial blanket volume (liter)
VC	Average steam velocity (fps)
VF	Volume of fuel per pin (ft ³)
VFCLAD	Cladding volume fraction
VFCON	Control material volume fraction
VFCOOL	Coolant volume fraction

VFFUEL	Fuel volume fraction
VFP	Total volume of fuel in the core (liters)
VFPU	Total volume of PuO_2 in the core (liters)
VFU	Total volume of UO_2 in the core (liters)
VR	Volume of radial blanket (liters)
VT	Total core and radial and axial blanket volume (liters)
W	Average value of the universal gas constant for steam ($\text{ft}^2/\text{sec}^2\text{F}$)
WFPU	Total mass of plutonium in the core (kg Pu)
WFUC	Total mass of uranium in the core (kg U)
WHMA	Total mass of heavy metal in the axial blanket (kg U)
WHMC	Total mass of heavy metal in the core (kg U+Pu)
WHMR	Total mass of heavy metal in the radial blanket (kg U)
WP	Mass flow rate per pin (lb/hr)
WS	Mass flow rate per subassembly (lb/hr)
XC	Distance to point of maximum clad temperature when surface roughening is not used (ft)
μ	Average steam viscosity ($\text{lb sec}/\text{ft}^2$)
$\hat{\rho}$	Steam inventory in the core (lb/ft^3 of core)
$\bar{\rho}$	Average steam density in the core (lb/ft^3)
ρ_f	Heavy metal density of fuel (g/cm^3)
$\bar{\rho}_H$	Average steam density in the hot subassembly (lb/ft^3)
ρ_{in}	Steam density at the core inlet (lb/ft^3)
ρ_{OH}	Outlet steam density in the hot subassembly (lb/ft^3)

APPENDIX B

VARIATIONS AND UNCERTAINTIES IN STEAM-COOLED
FAST BREEDER REACTOR PHYSICS

APPENDIX B

VARIATIONS AND UNCERTAINTIES IN STEAM-COOLED
FAST BREEDER REACTOR PHYSICSCONSTANTS

The variations and uncertainties in Steam-Cooled Fast Breeder Reactor physics calculations affect both the economics and the safety of this concept. One of the primary objectives of a breeder reactor design is that it have a short doubling time. A second objective is that it be inherently safe to an acceptable but not, as yet, clearly defined degree. Because these objectives are often not compatible with core design, calculations of a high confidence level are required to achieve optimum design and adequate safety standards.

Doubling time is affected by the specific inventory and breeding gain (breeding ratio minus one). The specific inventory is the mass of fissionable material which must be committed to the reactor per unit electrical power output.

The void coefficient, Δk_{void} , is the change of reactivity due to the removal of the coolant steam. This condition is expected to occur as a result of a break in the coolant piping. While not directly analogous to sodium voids in a LMFBR, and therefore not necessarily suggestive of the same restrictions on reactivity coefficients, a Δk_{void} less than one dollar, or, better yet, negative would

be preferable. Unfortunately, the majority of SCFBR calculations have not indicated such coefficients in most designs.

The initial reactivity required to achieve a certain desired burnup is determined by the core breeding ratio. This initial k_{eff} , in turn, determines the enrichment, which affects the specific inventory, core breeding ratio, and the void coefficient.

That these important parameters can be greatly affected by the calculational procedure and cross section set used became apparent early in this study. It had been decided to use HRG,⁽⁸⁾ -TEMPEST,⁽⁹⁾ and -HFN⁽¹⁰⁾ with ORNL⁽²¹⁾ cross sections in order to favor comparisons with the ORNL work, but the necessity of using the previously mentioned modified Russian cross section set in early burnup calculations made it possible to compare some results using both sets. Table B-I gives a comparison of the results of HRG with ORNL cross sections, and FCC⁽¹²⁾ with Russian cross sections.⁽⁶⁾ The breeding ratios for the latter appear consistently higher. In addition, the enrichments do not vary to the same degree with changes of steam inventory for both methods. The void coefficients appear to vary in the

TABLE B-I. Comparison of Results

Case	Initial k_{eff}	Enrichment, % Pu	Core Breeding Ratio, BR_c	Void Reactivity Change, Δk_{void}
0A	1.010	11.40	1.195	-0.009
		11.23	1.347	+0.004
0B	1.010	11.30	1.110	-0.015
		11.55	1.240	+0.019
0C	1.024	11.65	0.947	-0.010
		12.51	1.053	+0.053
1A	1.010	12.95	1.055	-0.007
		12.85	1.152	+0.016
1B	1.022	13.40	0.926	+0.001
		13.34	1.053	+0.027
1C	1.063	15.00	0.748	+0.025
		15.30	0.862	+0.079
2A	1.053	17.85	0.745	+0.006
		17.11	0.852	+0.016
2B	1.076	18.90	0.645	+0.017
		18.81	0.748	+0.054
2C	1.092	20.30	0.565	+0.046
		20.77	0.652	+0.109

The first line for each case is from HRG-TEMPEST-HFN with ORNL cross sections. The second line for each case is from FCC with Russian cross sections. The ORNL cross sections have the appropriate resonance parameters for each case. The Russian cross sections are homogenized.

same direction, becoming more positive with increasing coolant volume and steam inventory. The HRG-ORNL and FCC-Russian methods differ in that, for the latter, the change of sign takes place at enrichments too low to be useful. Thus, all the values given in Table B-I are positive.

Many factors could account for these differences. FCC is a point calculation, while HRG and TEMPEST prepare fast and thermal cross sections for use in the one-dimensional

diffusion code HFN. Blankets and reflectors in FCC are treated as a reflector savings with the same value for all cases, while the treatment in HFN is considerably more detailed. Pin sizes enter into HRG calculations by way of the resonance parameters for those isotopes requiring them. These resonance parameters are different for each HRG case. The Russian cross-section set as used in FCC does not involve pin size

but adjusts the cross sections according to the composition of the homogenized cell. Also, the question of differences between the cross-section sets arises.

In an attempt to sort out these factors, a number of comparisons have been made. The SCFBR is somewhat unique among fast reactor concepts in that different coolant conditions will cause large variations in the neutron spectrum over a wider energy range. Calculations are thus made more difficult but a more rigorous test of calculational methods and cross sections is provided. In these comparisons, an enrichment close to that actually required is specified for each case. Thus, while certain comparisons are rendered more difficult, calculations are simplified.

To show the difference between the two methods of calculation used, a comparison was made using ORNL cross sections in both HRG and FCC (Table B-II). Case 1B should allow direct comparison since the resonance parameters are correct for the FCC calculations. The large differences are attributed to the effects of the approximate treatment of blanket and reflector by FCC.

Table B-III illustrates the effect of varying pin diameter for some cases. The effects are small and in opposite directions. Thus, an increase in k_{eff} will allow a decrease in enrichment to increase the breeding ratio, thus helping to compensate for any decrease. The net effect upon the neutronics is not of sufficient magnitude to require physics results as a function of pin size

TABLE B-II. Comparison Between FCC and HRG Calculations^(a)
Using ORNL Cross Sections

Case	Enrichment, % Pu	k_{eff}		Core Breeding Ratio, BR _C	
		FCC	HRG	FCC	HRG
0A	11.33	0.9985	1.006	1.2953	1.203
0B	11.80	0.9918	1.030	1.2388	1.063
0C	12.30	0.9319	1.049	1.1583	0.907
1A	13.00	1.0153	1.013	1.1181	1.047
1B	13.42	1.0080	1.023	1.0664	0.925
1C	14.70	1.0171	1.055	0.9586	0.760
2A	17.20	1.0605	1.033	0.8242	0.773
2B	18.80	1.0770	1.073	0.7558	0.650
2C	19.60	1.0660	1.078	0.7085	0.570

(a) HRG Calculations employ the appropriate resonance parameters for each case. FCC calculations use those for Case 1B throughout.

in supplying input data to the parametric design studies.

Table B-IV is a comparison of the homogenized and unhomogenized

ORNL cross sections used in FCC and, as such, is a good measure of the effects of inhomogeneities. As expected, this effect increases with

TABLE B-III. Effect of Fuel Pin Size

Case	Enrichment, % Pu	Pin Diameter d, in.	k_{eff}	BR_c
0A	10.	0.150	1.0770	1.366
		0.213	1.0784	1.364
		0.276	1.0798	1.360
		0.338	1.0809	1.358
		0.400	1.0820	1.355
1B	15.	0.150	1.0808	0.8396
		0.213	1.0841	0.8357
		0.276	1.0870	0.8313
		0.338	1.0896	0.8282
		0.400	1.0919	0.8251
2C	20.	0.150	0.9404	0.5883
		0.213	0.9411	0.5805
		0.276	0.9417	0.5735
		0.338	0.9423	0.5676
		0.400	0.9428	0.5624

TABLE B-IV. Comparison Between Homogenized and Unhomogenized* ORNL Cross Sections in FCC.

Case	Enrichment, % Pu	k_{eff}		Core Breeding Ratio, BR_c	
		Homogenized	Unhomogenized	Homogenized	Unhomogenized
0A	11.33	0.9977	0.9985	1.2996	1.2953
0B	11.80	0.9951	0.9918	1.2273	1.2388
0C	12.30	0.9929	0.9819	1.1215	1.1583
1A	13.00	1.0151	1.0153	1.1199	1.1181
1B	13.42	1.0126	1.0080	1.0518	1.0664
1C	14.70	1.0293	1.0171	0.9231	0.9586
2A	17.20	1.0610	1.0605	0.8238	0.8242
2B	18.80	1.0823	1.0770	1.7423	0.7558
2C	19.60	1.0733	1.0660	0.6787	0.7085

* The unhomogenized cross sections have resonance parameters corresponding to Case 1B.

steam inventory and does so in an expected manner. Breeding ratios decrease and k_{eff} 's increase due to self-shielding of resonance isotopes in the pins.

Table B-V provides the best comparison of the ORNL and Russian cross-section sets obtained. It does not appear to lend itself to

easy comparisons. To make this task somewhat easier, these results have been adjusted to the basis of a common k_{eff} rather than the common enrichment used in Tables B-II, B-IV, and B-V. This has been done by using Figures B-1 and B-2. The results are given in Table B-VI.

TABLE B-V. Comparison Between Russian and ORNL Cross-Section Sets in FCC (cross sections are homogenized over the cell)

Case	Enrichment, % Pu	k_{eff}		Core Breeding Ratio, BR_c	
		ORNL Cross Sections	Russian Cross Sections	ORNL Cross Sections	Russian Cross Sections
0A	11.33	0.9977	1.0151	1.2996	1.3328
0B	11.80	0.9951	1.0175	1.2273	1.2137
0C	12.30	0.9929	1.0166	1.1215	1.0671
1A	13.00	1.0151	1.0257	1.1199	1.1495
1B	13.42	1.0126	1.0255	1.0518	1.0449
1C	14.70	1.0293	1.0383	0.9231	0.8952
2A	17.20	1.0610	1.0561	0.8238	0.8467
2B	18.80	1.0823	1.0751	0.7423	0.7480
2C	19.60	1.0783	1.0621	0.6787	0.6820

TABLE-B-VI. Comparison Between Russian and ORNL Cross Section Sets in FCC (cross sections are homogenized over the cell)

Case	Initial k_{eff}	Enrichment, % Pu		Δ	Core Breeding Ratio		Δ	Difference in Doubling Time, %
		Russian	ORNL		Russian	ORNL		
0A	1.0151	11.33	11.71	0.38	1.3328	1.2636	0.0692	16.3
0B	1.0175	11.80	12.30	0.50	1.2137	1.1901	0.0236	10.4
0C	1.0166	12.30	12.84	0.54	1.0671	1.0909	-0.0238	-8.2
1A	1.0257	13.00	13.24	0.24	1.1495	1.1016	0.0479	13.9
1B	1.0255	13.42	13.74	0.32	1.0449	1.0353	0.0096	6.7
1C	1.0383	14.70	14.96	0.26	0.8952	0.9131	-0.0179	---
2A	1.0561	17.20	17.06	-0.14	0.8467	0.8303	0.0164	14.4
2B	1.0751	18.80	18.57	-0.23	0.7480	0.7504	-0.0024	---
2C	1.0621	19.60	19.01	-0.59	0.6820	0.6957	-0.0137	---

The enrichments calculated with the Russian cross-section set are lower than for the ORNL set in the cases where the volume fraction of fuel is highest. This difference decreases until, for the higher volume fractions of coolant and steam inventories (the wetter cases), the ORNL set gives the lower enrichments by an increasing amount. The behavior of the core breeding ratio is rather similar. The wetter the case, the greater would be the likelihood of the ORNL set yielding higher values than the Russian set. A major difference between these sets apparently is in the way hydrogen or one or more of the other important cross sections varies with energy.

The specific inventories for these cases were obtained by using the mass balance from a set of FCC cases and the ratios of the enrichments. The total breeding ratios were then computed by adding the blanket breeding ratios from Table B-VII. The doubling times for both ORNL and Russian cross section sets were then computed and compared. This comparison is given as the last column of Table B-VI for those cases of most interest.

The core breeding ratios for FCC calculations are all consistently higher than for HRG. Therefore, the total breeding ratios would be higher and the doubling times lower. To avoid misleading anyone, these over-optimistic values have not been listed. Doubling time comparisons are, if anything, conservative.

Lower doubling times will increase the differences over those given.

The most important difference between these cross section sets is their effect on the calculations of doubling time. The uncertainty in doubling time between these sets is about 15 to 20% in the cases of interest.

Comparison of experimental data with fast reactor calculation results using various cross section sets would be required to resolve present uncertainties. There is not now sufficient applicable data on a fast plutonium fueled system containing hydrogen.

BEST ADJUSTED RESULTS

An attempt has been made to obtain a set of consistent values using the results of both the HRG-TEMPEST-HFN calculations and the FCC burnup runs.

The HRG results for 8000 liter cores have been expressed as plots of k_{eff} , and core and total breeding ratios as a function of enrichment. These are similar to those previously described in this report. The results of the FCC burnup runs are given as correlations of the average core breeding ratio and the initial k_{eff} required to achieve 50,000 MWd/tonne batch burnup against the beginning-of-life (BOL) core breeding ratio. In the latter plot, the ORNL cross section set curve is used for consistency with all the other results calculated from this set. These relationships are given in Figures B-1 through B-5.

FIGURE B-1. K_{eff} Versus Enrichment
(8000 liter core)

FIGURE B-2. Core Breeding Ratio
Versus Enrichment (8000 liter core)

FIGURE B-3. Total Breeding Ratio
Versus Enrichment (8000 liter core)

An estimate is first made as to the initial k_{eff} required. The graphs are then employed to obtain, in turn, the corresponding enrichment, BOL core breeding ratio, and initial k_{eff} . Further iterations of this process are continued until the initial k_{eff} no longer changes appreciably. Then the BOL total and core breeding ratios are obtained.

Their difference is attributed to the blanket breeding ratio, assumed as a first approximation not to vary with burnup. The average core breeding ratio is obtained from its correlation with the BOL core breeding ratio and added to the blanket breeding ratio to obtain the total average breeding ratio. These results are given in Table B-VII.

FIGURE B-4. Relationship Between
BOL and Average Core Breeding Ratios

FIGURE B-5. Relationship Between
BOL Core Breeding Ratio and the BOL
 K_{eff} Required for 50,000 MWd/tonne
Batch Burnup

TABLE B-VII. Best Adjusted Results, 8000 Liter Spherical Cores

Case	Initial k_{eff}	Enrichment, % Pu	Initial Breeding Ratio			Average Breeding Ratio		Δk Void	Δk Flood
			Core BR_C	Total BR_T	Blanket BR_B	Core BR_C	Total BR_T		
0A	1.010	11.30	1.203	1.390	0.187	1.125	1.312	-0.0165	-0.0060
0B	1.010	11.29	1.103	1.250	0.147	1.061	1.208	-0.0175	-0.0065
0C	1.026	11.63	0.948	1.055	0.107	0.943	1.050	-0.0155	-0.0120
1A	1.012	13.14	1.024	1.268	0.244	0.997	1.241	0.0000	0.0250
1B	1.035	13.75	0.900	1.080	0.180	0.905	1.085	0.0030	0.0110
1C	1.060	14.65	0.766	0.870	0.104	0.790	0.894	0.0150	0.0010
2A	1.069	18.32	0.721	0.992	0.271	0.750	1.021	0.0055	0.0395
2B	1.083	19.13	0.645	0.858	0.213	0.680	0.893	0.0175	0.0310
2C	1.099	20.53	0.563	0.693	0.130	0.607	0.737	0.0455	0.0265

B-10

BNWL-713

REFERENCES

1. E. Kiefhaber and K. Ott. "Survey Parameter Study of Large Steam-Cooled Fast Power Reactors," Proceedings of the Conference on Safety, Fuels, and Core Design in Large Fast Power Reactors, ANL-7120, 1965.
2. Babcock and Wilcox. 1000 MWe - 3600 psi Steam-Cooled Breeder Reactor Design, BAW-1309, Lynchburg, Virginia, 1967.
3. D. C. Schluderberg and D. White. 1000 MWe - 1250 psi Steam-Cooled Breeder Reactor Design, BAW-1319, Babcock and Wilcox, Lynchburg, Virginia, 1967.
4. E. A. Eschbach. Unpublished Data (Personal Communication), 1967.
5. A. Muller et al. Reference Study for the 1000 MWe Steam-Cooled Fast Breeder (D-1), EURFNR-251, August, 1966.
6. L. P. Abagjan, N. O. Bazazjanc, J. J. Bondarenko, and M. N. Nikolaev. "Group Constants of Fast and Intermediate Neutrons for the Calculation of Nuclear Reactors," State Committee for the Use of Atomic Energy in the USSR (in Russian). Consultants Bureau, New York, 1964.
7. W. W. Little, Jr., R. W. Hardie, and L. L. Maas. Multigroup Analysis of Selected Fast Critical Assemblies, BNWL-347, Pacific Northwest Laboratory, Richland, Washington, December 1966.
8. J. L. Carter, Jr. "Computer C. Abstracts, Computer Code-HRG," Reactor Physics Department Technical Activities Quarterly Report, July, August, September 1966, USAEC Report BNWL-340, October 15, 1966.
9. R. H. Shuddle and J. Dyer. MAA Program Description, TEMPEST - A Neutron Thermalization Code, North American Aviation Corporation, Downey, California, September 1960.
10. J. R. Lilley. Computer Code HFN - Multigroup, Multiregion Neutron Diffusion Theory in One Space Dimension, HW-71545, General Electric Company, Richland, Washington, April 1961.
11. G. J. Busselman. CLERK - A Cross Section Averaging Routine, HW-75521, General Electric Company, Richland, Washington, November 1963.
12. W. W. Little, Jr., and R. W. Hardie. FCC - A Fundamental Mode Code for Fast Reactor Analysis, BNWL-234, Pacific Northwest Laboratory, Richland, Washington, March 1966.
13. H. C. Honeck. THERMOS - A Thermalization Transport Theory Code for Reactor Lattice Calculations, BNL-5826, Brookhaven National Laboratory, 1961.
14. J. H. Keenan and F. G. Keyes. Thermodynamic Properties of Steam, John Wiley & Sons, Inc., New York, 1936.
15. A. N. de Stordeur. "Drag Coefficients for Fuel Element Spacers," Nucleonics, vol. 19, no. 6, June 1961.
16. K. Rehme. Friction and Resistance Coefficients for Rod Clusters Parallel to Flow with Various Spacer Grids, EURFNR-223 P, Karlsruhe, July 29, 1966.
17. N. B. Hols and G. Schneider. 1000 MWe FBR Parametric Study of Secondary Sodium Steam Systems, NAA-SR-12060, North American Aviation, Inc., Downey, California, August 31, 1966.

18. W. A. Sutherland. Improved Heat Transfer Performance with Boundary Layer Turbulence Promotors, GEAP-5115, General Electric Company, June 1966.
19. A. A. Bishop, F. J. Krambeck, and R. O. Sandberg. High Temperature Supercritical Pressure Water Loop Part III Forced Convection Heat Transfer to Superheated Steam at High Pressure and Prandtl Numbers, WCAP-2056, Part III, Westinghouse Electric Company, Pittsburg, Pennsylvania, July 1964.
20. M. D. Freshley. Unpublished Data (Personal Communication), 1967.
21. L. Bennett. Unpublished Data (Private Communication), 1967.

DISTRIBUTION

<u>No. of Copies</u>		<u>No. of Copies</u>	
265	<u>AEC Division of Technical Information Extension</u>	2	<u>Atomics International</u> L. E. Glasgow S. Golan
2	<u>AEC, Chicago Patent Group</u> G. H. Lee R. K. Sharp	5	<u>Argonne National Laboratory</u> P. F. Gast (3) K. A. Hub LMFBR Program Office
36	<u>AEC, Washington, D.C.</u> M. Shaw, Director, RDT Asst Dir for Nuclear Safety Analysis & Evaluation Br, RDT:NS Environmental & Sanitary Engrg Br, RDT:NS Research & Development Br, RDT:NS Asst Dir for Plant Engrg, RDT Applications & Facilities Br, RDT:PE Components Br, RDT:PE Instrumentation & Control Br, RDT:PE Systems Engineering Br, RDT:PE Asst Dir for Program Analysis, RDT (3) Asst Dir for Project Mgmt, RDT Liquid Metals Projects Br, RDT:PM FFTF Project Manager, RDT:PM Asst Dir for Reactor Engrg Control Mechanisms Br, RDT:RE Core Design Br, RDT:RE Fuel Fabrication Br, RDT:RE Fuel Handling Br, RDT:RE Reactor Vessels Br, RDT:RE Asst Dir for Reactor Tech Chemistry & Chemical Separations Br, RDT:RT Fuels & Materials Br, RDT:RT Reactor Physics Br, RDT:RT Special Technology Br, RDT:RT G. M. Anderson (3) D. E. Erb (2) R. E. Pahler (2) S. Strauch N. E. Todreas	4	<u>Babcock & Wilcox</u> H. S. Barringer (3) D. C. Schluderberg
		3	<u>Battelle-Memorial Institute</u>
		3	<u>Brookhaven National Laboratory</u> H. J. C. Kouts C. J. Raseman W. E. Winsche
		5	<u>Douglas United Nuclear</u> L. W. Lang H. R. Kosmata G. F. Owsley DUN Files (2)
		47	<u>Fast Reactor Exchange</u> UK/US Fast Reactor Exchange (12) Euratom/US Fast Reactor Exchange (35)
		4	<u>General Atomics</u> D. P. Coburn P. Fortescue (3)
		5	<u>General Electric APO</u> K. Cohen F. Leitz P. M. Murphy (3)
		5	<u>Institut Karlsruhe, Germany</u> R. W. Albrecht A. Muller (3) D. Stegemann
2	<u>AEC Richland Operations Office</u> C. L. Robinson Technical Library	3	<u>Los Alamos Scientific Laboratory</u> J. H. Russell (3)
2	<u>AEC, RDT Site Representative</u> P. G. Holsted (2)		

No. of
Copies12 Oak Ridge National Laboratory

L. L. Bennett
R. S. Carlsmith
W. R. Gall (3)
J. A. Lane
R. C. Olson
J. T. Roberts
R. Salmons
J. P. Sanders
T. N. Washburn
F. G. Welfare

1 Tennessee Valley Authority

H. Falkenberry

1 University of Kentucky

O. J. Hahn

4 Westinghouse Electric Corporation

P. Cohen (3)
F. M. Heck

126 Battelle-Northwest

D. T. Aase (10)
F. W. Albaugh
S. J. Altschuler
C. W. Angle
E. R. Astley
W. J. Bailey
J. M. Batch
A. L. Bement
R. D. Benham
C. A. Bennett
T. K. Bierlein
J. B. Burnham, Jr.
S. H. Bush
G. J. Busselman (10)
J. J. Cadwell
T. T. Claudson
P. D. Cohn
D. L. Condotta
R. F. Corlett
J. M. Davidson
F. G. Dawson
D. R. de Halas
J. L. Diechman
D. E. Deonigi
R. L. Dillon
D. R. Doman
G. E. Driver
K. Drumheller
E. A. Eschbach
E. A. Evans
S. R. Fields
L. M. Finch
J. F. Fletcher
J. C. Fox (2)
M. D. Freshley
B. C. Fryer
L. H. Gerhardstein

Battelle-Northwest (contd)

S. Goldsmith
H. Harty
R. A. Harvey
R. J. Hennig
G. M. Hesson
R. M. Hiatt
V. R. Hill
P. L. Hofmann
R. H. Holeman (2)
M. T. Jakub
E. L. Kelley
W. C. Kinsel
R. L. Knecht
J. R. Kosorok
D. D. Lanning
G. A. Last
C. E. Leach
H. D. Lenkersdorfer
R. C. Liikala
W. W. Little
W. E. McClung
R. W. McKee
W. B. McDonald
M. A. McLaughlin
J. E. Minor
J. H. Nail
W. I. Neef
R. E. Nightingale
L. T. Pederson
R. E. Peterson
W. W. Porath
D. E. Rasmussen
W. E. Roake
C. A. Rohrmann
R. B. Shively
L. C. Schmid
G. L. Simmons
D. E. Simpson
D. H. Stewart
W. H. Swift
W. L. Thorne
K. G. Toyoda
R. E. Turley
L. D. Van Hees
F. W. Van Wormer
G. R. Waymire
R. G. Wheeler
O. J. Wick
R. D. Widrig
W. K. Winegardner
K. R. Wise (2)
N. G. Wittenbrock
F. W. Woodfield
J. R. Worden
D. C. Worlton
K. E. Yandon
J. M. Yatabe
J. R. Young
F. R. Zaloudek
Technical Information Files (5)
Technical Publications (2)