ATL

ATL-A-108
INFORMAL AEC RESEARCH
AND DEVELOPMENT REPORT
UC-81, Reactors-Power
SPECIAL DISTRIBUTION

Quarterly Progress Report No. 4
VARIABLE MODERATOR REACTOR
DEVELOPMENT PROGRAM

ATL Job 45003

31 May 1960

Prepared Under
Contract No. AT(04-3)-250
Project Agreement No. 3
for the U. S. Atomic Energy Commission
Division of Reactor Development

ADVANCED TECHNOLOGY LABORATORIES
A Division of American-Standard
369 Whisman Road
Mountain View, California

463 - 001

ADVANCED *Technology* LABORATORIES
A DIVISION OF AMERICAN-Standard

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

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.

DO NOT PHOTOCOPY

CONTENTS

Forward	1
Program Objectives	2
Status of Program	3
A. Core Designs and Parametric Studies	3
B. Kinetics	3
C. Physics	3
D. Mechanical and System Design	3
E. Economic Evaluation	3
F. Critical Experiment	3
Summary of Results	4
A. Core Designs, Parametric Studies, and Economic Evaluation	4
B. Kinetics	4
C. Physics	4
D. Critical Experiment	4
I. Core Designs and Fuel-Cycle Cost Analysis	5
A. Core Designs and Parametric Studies	5
B. Results of VMR Fuel-Cycle Cost Analysis	7
C. Effect of Control Requirements on Fuel-Cycle Costs	10
1. Reactivity Effects	10
2. Moderator-Level Variation with Load	13
D. Comparison of Fuel-Cycle Costs for "Conventional" Boiling-Water Reactors and the VMR	14
1. Prototype Boiling Water Reactor	15
2. 50-mw BWR Designed by General Nuclear Engineering Corporation	17
3. 40-mw Process-Steam BWR	18
4. General Discussion of VMR and Conventional BWR Fuel-Cycle-Cost Differences	19
E. Method of Computing Fuel-Cycle Costs	20
1. Reactivity	20
2. Beginning- and End-of-Life Uranium and Plutonium Concentrations	21
3. Reactor Power Output	21
4. Fabrication Costs	21
5. Reprocessing Fuel to Nitrates	21
6. Burnup Cost	21
7. Pu Credit	21
8. Conversion Nitrates to UF_6	21
9. Losses	22
10. Use Charges	22
11. Shipping	22
12. Interest on Fabrication Capital	22

CONTENTS

II. MSBU Fuel-Burnup Code	27
A. General Description of Code	27
B. "Batch" Loading Calculation Procedure	31
C. "Three Region" Loading Calculation Procedure	31
D. Discussion of "Batch" Loading Versus "Three Region" Fuel Loading	32
III. Kinetics	34
Nomenclature	39
IV. PUREE' Physics Analysis, Thermal Group	53
A. Blackness Computation	53
B. Spectral Shift Between Coolant and Fuel	53
C. Conclusion	54
V. VMR Mechanical and Systems Design	55
A. Reactor Arrangement	55
1. Fuel	55
2. Moderator Barrel	55
3. Coolant Riser	56
4. Pressure Vessel	56
B. Reactor Component Stress Analysis	57
1. Core Support Plate	57
2. Shrouds	57
3. Moderator Inlet Pipe	58
4. Pressure Vessel	58
C. Economic Evaluation	58
D. Cleanup System For VMR Moderator Region	59
VI. Thermal Design of Fuel Assemblies	60
VII. Critical Experiment	63
A. Initial Critical Experiments	63
B. Current and Future Program	65
C. Deviations of the PUREE' Physics Code from Experimental Data	66
Appendix A - Materials Balance	69
Appendix B - Heat Balance	72
Appendix C - Equipment Sizes	73
Distribution	

FOREWORD

Development of the Variable Moderator Reactor (VMR) is conducted under Contract No. AT(04-3)-250, Project Agreement No. 3, by Advanced Technology Laboratories, a Division of American-Standard, for the U. S. Atomic Energy Commission. This fourth quarterly technical progress report covers the continuation of work under Phase I of the contract, "Physics and Kinetics Analysis and Initial Evaluation," and the continuation of work under Phase II, "Critical Experiment and Analysis of Results."

The information contained in this progress report is preliminary and subject to change as the program evolves.

PROGRAM OBJECTIVES

The objectives of the work currently being performed, and of the work to be performed during the next quarter (16 May 1960 through 15 August 1960), in accordance with the agreed upon scope,¹ is as follows:

1. To compare the core physics model PUREE'² with the VMR critical experiment and to modify or apply corrections if the experimental data indicate that adjustments should be made.
2. To use the hydrodynamic code BOCH³ and the physics codes PUREE' and VAL PROD⁴ to complete the detailed analysis of core designs determined to be attractive by the completed parametric study.
3. To use the analog representation of the VMR core kinetics to study specific core designs found to be attractive by the completed parametric study, and to complete the comparison of the analog representation with existing reactor transient response data.
4. To complete the mechanical and system design and cost analysis of specific VMR cores that have been examined for nuclear and hydrodynamic feasibility.
5. To continue with the critical experiments.
6. To prepare an initial technical and economic feasibility report and thus complete Phase I of the contract.

-
1. "Program for Evaluation of the Variable Moderator Reactor under the New Reactor Concepts Program for the Atomic Energy Commission," Atomic Energy Division of American-Standard, Proposal P-470A, December 17, 1958.
 2. PUREE' is a non-dimensional physics code which computes homogenized constants for use in neutron-balance equations (see Quarterly Progress Report No. 3 for description).
 3. BOCH is a two-dimensional hydrodynamic code which computes the non-uniform void distribution in the core (see Quarterly Progress Report No. 3 for description).
 4. R. H. Stark and C. H. White, "VAL PROD, a Revision of the PROD II 630 Multigroup," General Electric, Atomic Power Equipment Division, GEAP-0952, January 9, 1957 (a one-dimensional physics code for solving criticality and reactivity).

STATUS OF PROGRAM

A. Core Designs and Parametric Studies

The hydrodynamics code BOCH, the physics code PUREE', and the fuel burnup code MSBU⁵ have been used in the analysis of VMR cores for the parametric study. This study is largely complete, and based upon its results, attractive core designs have been selected for more detailed analysis. In particular, control requirements on these attractive designs are being investigated.

B. Kinetics

The analog representation of the VMR kinetics has been applied to the analysis of the VMR over a range of parameters for both positive and negative void coefficients of reactivity. It is presently being applied to reactor experimental data.

C. Physics

The blackness method of computing the PUREE' thermal group constants has been checked against experimental data to determine the effect of the change in fuel-pin blackness due to incident neutron distortion by neighboring fuel pins.

D. Mechanical and System Design

Design of the fuel assembly has been completed, and specifications and drawings have been prepared for release to vendors for quotation. Cost data obtained from these quotations will be used in the economic evaluation of the VMR. Stress analyses of the pressure vessel and core structure have been completed, and drawings and specifications are being prepared. A moderator cleanup system has been designed.

E. Economic Evaluation

The fuel-cycle cost analysis is nearly complete. Modifications to the analysis will be made as required by the results of the critical experiment and vendor quotations.

F. Critical Experiment

The first of a series of core arrangements has achieved criticality and macroscopic and microscopic measurement of its nuclear properties are being made.

5. See section II for description of MSBU.

SUMMARY OF RESULTS

A. Core Designs, Parametric Studies, and Economic Evaluation

Work to date on the parametric studies, coupled with the partially completed economic evaluation, has shown the following:

1. A moderator-to-coolant volume ratio of at least 0.45 is required in order to obtain sufficient reactivity worth in the moderator-reflector region of the VMR to control the reactor.
2. Stainless-steel-clad fuel appears to be slightly more attractive economically than Zircaloy-clad fuel, based upon present day fabrication costs for the VMR core.
3. Fuel-cycle costs are considerably more sensitive to reductions in specific power brought about by reductions in coolant-to- UO_2 ratios than they are to changes in conversion ratio brought about by changes in total water-to- UO_2 ratios.

B. Kinetics

The VMR analytical model of the kinetics of the reactor system shows that the reactor can be made to be stable and load-following for both positive and negative void reactivities with a simple control system.

C. Physics

The blackness method of computing the PUREE' thermal group constants produces negligible differences for lattices of interest, as compared to those determined by the P_3 -POP method described in the last quarterly report.⁶

D. Critical Experiment

The value of $k_{\text{effective}}$ computed by the present PUREE' techniques appears to be approximately 3% higher than the experimental values obtained on VMR-type lattices.

6. "Variable Moderator Reactor Development Program," Advanced Technology Laboratories, a Division of American-Standard, Quarterly Progress Report No. 3, ATL-A-107, 29 February 1960.

I. CORE DESIGNS AND FUEL-CYCLE COST ANALYSIS

A. Core Designs and Parametric Studies

The data for various core designs were tabulated in the previous quarterly progress report.⁶ The analysis of most of these core designs has been completed, and the majority of the results are presented herein. Table I shows the significant core design parameters, and some of the computed power and thermal flux "peaking" factors. Figures 1 through 6 illustrate various relationships among the computed parameters. Some of the reported information differs from that reported previously.⁶ These differences represent corrections to the earlier preliminary data.

Referring to Table I and to Figures 1, 2, and 3, it will be found that the radial peaking factors (maximum flux/average flux and maximum flux/minimum flux) are sensitive to changes in moderator/coolant volume ratios. An increase in the moderator/coolant ratio by a factor of 3, for instance, results in an increase in the large cell peak/minimum thermal neutron flux ratio by approximately a factor of 2. ("Small Cell" is defined as a single fuel pin and its associated coolant. "Large Cell" is defined as a complete fuel assembly with its associated coolant, shroud, and moderator.) Figure 4 shows that increasing the moderator/coolant ratios decreases the distortion in the axial power-distribution curve produced by the axial void distribution, and hence the peak/average power ratio. However, the reduction in the axial power-peaking factor is not as sensitive to changes in moderator/coolant ratios as is the radial peaking factor (and is in the opposite direction); hence, there is a slight increase in the over-all flux peaking factor as the moderator/coolant ratio is increased. The effect of the variation in moderator/coolant ratios on flux and power-peaking factors is an important parameter in the VMR design, since a certain minimum moderator/coolant ratio must be maintained in order to properly control the reactor (see section I. C, "Effect of Control Requirements on Fuel-Cycle Costs").

Figure 5 shows the variation in $k_{\text{effective}}$ with "average" coolant density for various ratios of coolant and moderator to UO_2 . As the moderator/ UO_2 ratio is increased, the slope of the reactivity-density curve becomes less positive with increasing coolant density (decreasing steam voids). However, within the range investigated, it does not become negative. The VMR kinetics analysis (section III) includes an investigation of the transient operation of the reactor with both positive and negative reactivity-void curves.

TABLE I
VMR CORE DESIGN DATA

	Core Design Number				
	1	2	3	4	5
Cladding thickness, in.	0.030	→			
Cladding material	Zr	→			
Fuel pin diametral gap, in.	0.005	→			
Shroud thickness, in.	0.050	→			
* Coolant volume/ UO_2 volume	1.61	→		1.24	→
Moderator volume/ UO_2 volume	0.892	2.0	3.0	3.0	1.0
* Water volume/ UO_2 volume	2.502	3.61	4.61	4.24	2.24
* Moderator volume/coolant volume	0.553	1.24	1.86	2.42	0.806
No. of fuel pins/element	61	→			
No. of elements/core	121	→			
UO_2 in core, kg	12,560	→			
Density of UO_2 , gm/cc	10.41	→			
Fuel-pellet diameter, in.	0.420	→			
Fuel-pin diameter, in.	0.485	→			
Fuel-pin centerline spacing, in.	0.676	→		0.631	→
Fuel-pin spacing/diameter (S/D)	1.393	→		1.30	→
Fuel-element centerline spacing, in.	6.20	7.01	7.67	7.44	5.98
Moderator gap between shrouds, in.	0.75	1.56	2.12	2.34	0.88
Shroud internal distance between flats, in.	5.35	→		5.00	→
Active fuel height, in.	72	→			
Core axial peak power/avg. power	1.50	1.45			
Core radial peak power/ avg. power (volume-weighted)	1.82	1.88			
Small-cell peak thermal neutron flux/ minimum thermal neutron flux	1.233	1.241	1.260	1.265	
Large-cell peak thermal neutron flux/ minimum thermal neutron flux	1.963	3.122	4.09	4.61	

* Coolant volume computed with zero voids.

RADIAL POWER DENSITY DISTRIBUTION

FIGURE 1

SMALL CELL FLUXES
THERMAL GROUP

FIGURE 2

463 012

LARGE CELL FLUXES
THERMAL GROUP

FIGURE 3

97

AXIAL POWER DENSITY DISTRIBUTION

FIGURE 4

k_{eff} VERSUS COOLANT DENSITY

FIGURE 5

463 014

k_{eff} VERSUS ENRICHMENT

FIGURE 6

B. Results of VMR Fuel-Cycle Cost Analysis

In order to determine the economic attractiveness of the VMR, it is necessary that three items of the reactor plant be cost estimated and compared with "conventional" control-rod boiling-water reactors. These three items are:

- 1) Reactor-plant capital costs.
- 2) Reactor-plant maintenance and operating costs.
- 3) Fuel-cycle cost.

The preliminary results of the fuel-cycle cost analysis are given in this section of the report.

The data used to develop the fuel-cycle costs were obtained from AEC-published cost schedules and from preliminary cost estimates of fuel manufacturers. The VMR design reflects current design techniques. No design credit was taken for items considered to be unusually developmental in nature. A significant item of this nature would be fuel-element shrouds constructed of Zircaloy instead of stainless steel. Since the integrity of the VMR moderator region is important, the stainless steel joints at the grid plates must be welded or brazed. To date, large grid plates have not been constructed of Zircaloy because of the expense, and Zircaloy to stainless steel joints are still in the developmental stage. It is recognized, however, that the Commission is supporting a Zircaloy development program, which could successfully solve these particular problems by the time a full-scale VMR is ready for construction. For this reason, a cost estimate will probably be attempted (factoring out developmental costs) to determine the effect of using Zircaloy shrouds in the VMR. Cost estimates will thus be available for a VMR based on current technology and the potential reduction in costs obtained by using advanced metallurgical techniques.

Three separate studies of "conventional" reactors were used to compare fuel-cycle costs with those of the VMR. Two of these studies were successfully reduced to an approximately common base with the VMR, but the uncertainties are still rather large. To reduce these uncertainties, additional work is in progress. All of the cost data shown in this and in other sections of the report may be modified as the final design data is firmed up.

Table II includes the significant core design data used to compute fuel-cycle costs. The methods used to compute these costs are described in section I. E, "Methods of Computing Fuel Cycle Costs," and in section II, "MSBU Fuel Burnup Code."

TABLE II

VMR FUEL-CYCLE CORE DESIGN DATA

	Core Design No.													
	23	26	28	29	30	31	32	33	35	36	37	38	39	40
Cladding thickness, in.	0.030	0.014							0.030	0.014		0.016	0.012	0.014
Cladding material	Zr	SS							Zr	SS				SS
Fuel pin diametral gap, in.	0.005													
Shroud thickness, in.	0.050													
Coolant volume/VO ₂ volume	2.79	2.35	1.49	1.00	2.35		1.49	2.35	1.61	2.35				3.00
Moderator volume/VO ₂ volume	1.075	1.00	0.652	0.650		0	1.00	2.00	1.075	1.00				0.65
Water volume/VO ₂ volume	3.87	3.35	2.14	1.65	3.00	2.35	2.49	4.35	2.68	3.35				3.65
Moderator volume/coolant volume	0.300	0.426	0.437	0.650	0.277	0	0.672	0.852	0.668	0.426				0.217
No. of fuel pins/element	37									61	19	37		
No. of elements/core	163	151	181	187	151		175	151	163	97	295	97	265	151
VO ₂ in core, kg	4980	4620	4590	4660	4620		4630	4620	4980	4620		4650	4600	4600
Density of VO ₂ , gm/cc	10.41													
Fuel-pellet diameter, in.	0.320											0.420	0.250	0.320
Fuel-pin diameter, in.	0.385	0.353							0.385	0.353		0.457	0.279	0.353
Fuel-pin centerline spacing, in.	0.616	0.565	0.494	0.445	0.565		0.494	0.565	0.524	0.567	0.562	0.737	0.443	0.624
Fuel-pin spacing/diameter (S/D)	1.60	1.60	1.40	1.26	1.60		1.40	1.60	1.36	1.60	1.59	1.61	1.59	1.77
Fuel-element centerline spacing, in.	4.37	4.055	3.49	3.23	3.91	3.605	3.66	4.45	3.86	5.17	2.93	5.26	3.20	4.19
Moderator gap between shrouds, in.	0.45	0.45	0.335	0.370	0.305	0	0.505	0.800	0.51	0.58	0.32	0.59	0.35	0.28
Shroud internal distance between flats, in.	3.82	3.505	3.055	2.76	3.505		3.055	3.505	3.25	4.49	2.51	4.57	2.75	3.81
Active fuel height, in.	60		50	49	60		52	60		57	60	55	56	60
Coolant height, in.	>60		>50	>49	>60		>52	>60		>57	>60	>55	>56	>60
Moderator height, in.	60		50	49	60		52	60		57	60	55	56	60
Coolant density, gm/cc	0.56													
Moderator density, gm/cc	0.75													

Figure 7 shows how fuel-cycle costs vary with coolant/ UO_2 ratio and moderator/ UO_2 ratio. The effects of natural and forced circulation are also shown in the graph, as are the differences between stainless-steel-clad and Zircaloy-clad fuel pins. It is clear that, for natural-circulation cores, minimum fuel-cycle costs occur at coolant/ UO_2 ratios between 2.0 and 2.5. Increasing moderator/ UO_2 ratios results in increasing costs for constant coolant/ UO_2 ratios. At coolant/ UO_2 ratios above 2.5, the power density becomes limited by central fuel-element temperature requirements and hence remains constant at a maximum of 10 kw per foot of fuel pin. This results in increased fuel costs, because of the approximately constant fuel-use charges and decreasing conversion ratios. As coolant/ UO_2 ratios are decreased below 2.0, the area available for coolant flow in the fuel channel becomes smaller, and, for natural-circulation reactors, the power density decreases. Despite slight increases in conversion ratios at low coolant/ UO_2 ratios, the fuel-cycle costs increase because of the very marked dependence of fuel-use charges on power density.

Figure 7 also illustrates that at the minimum fuel-cost point, Zircaloy-clad fuel is approximately 1.3 cents per million Btu more expensive than stainless-steel-clad fuel. If the fabrication costs of the Zircaloy-clad elements are decreased by approximately \$17/kg U, the cost differential is zero. Due to control requirements (discussed in section I. C, "Effect of Control Requirements on Fuel Cycle Costs"), an additional 2 to 3 dollars per kg U is required for Zircaloy, resulting in a total requirement of approximately \$19/kg U to reduce the cost differential to zero. As Zircaloy fabricators gain experience, it is likely that Zircaloy will become more attractive than stainless steel as a cladding for the VMR fuel elements. The fabrication costs for both Zircaloy- and stainless-steel-clad fuel elements are based upon preliminary quotations received from fuel vendors. These quotations will be firmed-up and the costs adjusted accordingly for the initial technical and economic feasibility report.

Figure 8 shows the variation in fuel-cycle costs with moderator/coolant ratio. This graph is discussed more fully in section I. C in connection with the control requirements of the reactor.

Figure 9 shows the effect of fuel-pin diameter on fuel-cycle costs. The diameter selected for Core Design No. 26 (0.353 inch) is seen to be an optimum for stainless-steel-clad pins. In addition to the 1 cent per million Btu difference between Core Design No. 26

CONDITIONS:

NO. OF FUEL PINS PER ASSEMBLY
FUEL PIN OD, IN..
CLADDING THICKNESS, IN.
BURNUP, MWD/SHORT TON U
KG UO₂ PER CORE
OXIDE PELLET OD, IN.

STAINLESS STEEL CLADDING

37
0.353
0.014
15,000
4590 ≥ KG UO₂ ≥ 4660
0.320

ZIRCALOY CLADDING

37
0.385
0.030
15,000
4980
0.320

LEGEND: — STN. STL. CLAD, NATURAL CIRCULATION
- - - STN. STL. CLAD, FORCED CIRCULATION
- - - ZIRCALOY CLAD, NATURAL CIRCULATION
(30) CORE DESIGN NO. 30 (TYP.)

VMR FUEL COST VERSUS MODERATOR/UO₂ AND
COOLANT/UO₂ RATIOS

FIGURE 7

CONDITIONS:

NO. OF FUEL PINS PER ASSEMBLY = 37
 FUEL-PIN CLADDING = 0.014" THICK STN. STL.
 FUEL-PIN OD, IN. = 0.353
 OXIDE PELLET, OD, IN. = 0.320
 BURNUP, MWD/SHORT TON U = 15,000
 KG UO_2 PER CORE = BETWEEN 4590 AND 4660

LEGEND:

— STN. STL. CLADDING, NATURAL CIRCULATION
 - - - STN. STL. CLADDING, FORCED CIRCULATION
 (29) CORE DESIGN NO. 29 (TYP.)

VMR FUEL COSTS VERSUS MODERATOR/COOLANT
 AND COOLANT/ UO_2 RATIOS

FIGURE 8

463 020

CONDITIONS:

FUEL-PIN CLADDING = STN. STL.
 COOLANT VOLUME/ UO_2 VOLUME = 2.35
 MODERATOR VOLUME/ UO_2 VOLUME = 1.00
 BURNUP = 15,000 MWD/SHORT TON U
 KG UO_2 PER CORE = 4630

LEGEND:

(39) CORE DESIGN NO. 39 (TYP.)

VMR FUEL COSTS VERSUS FUEL-PIN DIAMETER

FIGURE 9

463 021

ADVANCED Technology LABORATORIES
 A DIVISION OF AMERICAN Standard

FTL

and No. 39, the latter design requires approximately 114 additional fuel-channel shrouds for the same quantity of fuel. This results in an estimated \$20,000 increase in the capital cost of the core structure, (an amount not included in the fuel-cycle cost analysis). Core Design No. 39 is also considered unattractive because of control problems, which are discussed in section I.C. It is noted that the 4 cents per million Btu differential between Core Design No. 38 and No. 26 is considerably in excess of the equivalent decreased capital costs resulting from fewer shrouds in Core Design No. 38.

Figure 10 shows the effect of the number of fuel pins per assembly on fuel-cycle costs. Core Design No. 36 with 61 pins per assembly and Core Design No. 37 with 19 pins per assembly are both slightly less expensive than Core Design No. 26 with 37 pins per assembly. Core Design No. 37 accrues a penalty which offsets the slight fuel-cycle advantage, however, because it requires approximately twice as many fuel shrouds for a given quantity of fuel as Design No. 26. As discussed in section I.C, Design No. 36 has approximately the same control characteristics as No. 26 and in addition requires approximately 50% fewer shrouds.

Because of reactivity control requirements (see section I.C), moderator/ UO_2 ratios less than 1.0 are not attractive. From the above discussion it appears that designs 26 and 36 are near the minimum for natural-circulation VMR cores. A discussion of a forced-circulation core is included in section C below.

C. Effect of Control Requirements on Fuel-Cycle Costs

1. Reactivity Effects

The following table of reactivity requirements has been developed from calculations and preliminary estimates. These data may undergo modifications as final calculations of parameters are completed.

Table III

Estimate of VMR Reactivity Effects for Core Design No. 26

<u>Item</u>	<u>%Δk</u>
Doppler	1.2
Temperature	1.0
Voids	1.8
Xe and Sm	3.3
Burnup (for ss-clad fuel)	5.8
Shutdown margin	2.0
Total	<u>15.1</u>

463 022

CONDITIONS:

FUEL PIN CLADDING	= 0.014" THICK STN. STL.
FUEL PIN OD	= 0.353"
OXIDE PELLET OD	= 0.320"
BURNUP	= 15,000 MWD/SHORT TON U
KG UO ₂ PER CORE	= 4620
COOLANT VOLUME/UO ₂ VOLUME	= 2.35
MODERATOR VOLUME/UO ₂ VOLUME	= 1.0

LEGEND:

(37) CORE DESIGN NO. 37 (TYP.)

VMR FUEL COSTS VERSUS NUMBER OF PINS PER ASSEMBLY

FIGURE 10

463 023

It can be seen from the above that to have a satisfactory design, moderator must control 15.1% Δk , or $\frac{15.1}{1.151} = 13.1\% \Delta k/k$. Figures 11, 12, and 13 present the $\Delta k/k$ available from the moderator-reflector region. Since the curves were computed using the axial "average" density of the coolant, they do not reflect the effects of non-uniform density distribution in the fuel channels, nor do they reflect the control requirements with the reactor in the "cold" condition. These effects, and others, are presently being determined and will be included in the initial VMR feasibility report.

Figure 11 indicates that the $\Delta k/k$ available from the moderator-reflector region is a sensitive function of moderator volume/coolant volume ratio. Therefore, for a given coolant/ UO_2 ratio (determined by hydrodynamic considerations), it is necessary to select a shroud spacing which results in the moderator/coolant ratio required for control. For the reactivity effects shown in Table III, this moderator/coolant ratio is approximately equal to 0.45. It is noted, however, that the control requirements with the reactor in the cold condition (e. g. , zero voids and ambient temperature) are likely to necessitate a larger moderator/coolant ratio. If so, this will have the effect of increasing the economic attractiveness of a forced-circulation reactor (see discussion below).

Figure 8 in section I. B also can be used to compare forced- and natural-circulation reactors. At low coolant/fuel ratios, forced circulation permits high power densities to be retained. Thus, for a given moderator/coolant ratio, it is possible to select a forced-circulation core with a coolant/ UO_2 ratio smaller than a natural-circulation core and to obtain lower fuel costs. At a moderator/coolant ratio of 0.45, a forced-circulation core (with $C/UO_2 = 1.0$) has a fuel cost approximately 1 cent per million Btu lower than the optimum natural-circulation core. This represents approximately \$18,000 per year. However, the forced-circulation system is estimated to cost \$200,000 or at 0.15 capitalization, \$30,000 per year. The smaller pressure vessel for forced circulation is estimated to save \$40,000 or \$6,000 per year. Thus, a forced-circulation system appears to show a loss of \$6,000 per year over the selected natural-circulation system (exclusive of building and shielding cost differentials between natural- and forced-circulation systems). If the detailed core design analysis indicates that a significantly higher moderator/coolant ratio is required for control, Figure 8 shows that these requirements can be met without a significant economic penalty by using a forced-circulation system.

$\Delta k/k$ FOR MODERATOR AND REFLECTOR REMOVAL VERSUS
 MODERATOR/COOLANT RATIO

FIGURE 11

$\Delta k/k$ FOR MODERATOR AND REFLECTOR REMOVAL VERSUS
 NUMBER OF PINS PER ASSEMBLY

FIGURE 12

$\Delta k/k$ FOR MODERATOR AND REFLECTOR REMOVAL VERSUS
FUEL-PIN DIAMETER

FIGURE 13

Figure 12 shows that the $\Delta k/k$ available from the moderator-reflector region does not change rapidly as the number of pins per fuel assembly is changed.

Figure 13 indicates that the moderator-reflector reactivity worth increases markedly with increasing pin diameter in the range of 0.28 and 0.35 inch OD for the same moderator/coolant ratio. This is probably due to the increasing effectiveness of the moderator region compared to the coolant region as the fuel-pin size increases and the number of stainless steel shrouds in the core decreases. It would be expected that the curve would go through a maximum and decrease as the pin diameter continued to increase and the number of shrouds continued to decrease.

It is noted that for Zircaloy-clad fuel, the reactivity required for burnup (shown in Table III) increases by an estimated 2% over that for stainless-steel-clad fuel. Zircaloy-clad fuel cores thus require higher moderator/coolant ratios than stainless-steel-clad fuel. This increased reactivity required for burnup of the Zircaloy-clad fuel is principally due to the increased enrichment required for the stainless steel core. (Since there is more U 235 in the stainless-steel-clad core than in the Zircaloy-clad core, for a given burnup the change in U 235 will produce a more marked change in k in the Zircaloy-clad than in the stainless-steel-clad core.)

All fuel-cycle costs discussed in section I. B are based upon the assumption that the moderator-reflector height is retained near the top of the active region of the core throughout the life of the core. The power densities achievable over the core life are thus quite high. In order to make this possible, it is necessary to provide for control of the excess reactivity available in the moderator-reflector region when the core is initially put in service. This is accomplished by adding boron in the form of boric acid to the moderator-reflector water in order to reduce the worth of the water. Sufficient boric acid is added so that the moderator height can be maintained near the top of the active region of the fuel at full power. Load changes are accommodated by varying the level of the moderator-reflector. Long-term reactivity changes (e.g., burnup, Xe, Sm) are accommodated by varying the concentration of boric acid poison.

With regard to the proposed boric acid system, such systems have been considered for use in other reactors but usually not for extended service. A significant disadvantage of their use in most reactors results from the requirement that boric acid fill the reactor

primary system. Boric acid adjacent to the hot fuel assemblies may plate-out on the assemblies, the rapidly circulating primary water may have a rapid change in boric acid concentration as a slug of feedwater enters the core, and undesirable void-reactivity effects may occur as a result of adding boric acid to the primary system. These disadvantages do not exist in the VMR in which the relatively small moderator-reflector region is completely separate from the reactor primary system. The moderator-reflector cleanup system and boric acid concentration control system are described in section V.D, "Cleanup System for Moderator-Reflector Region."

A thorough investigation has not yet been made of the effect of borating the moderator region on the moderator-level coefficient of reactivity. It is possible that some core configurations could result in a positive level coefficient for the lower portion of the core but a negative level coefficient for the upper portions, especially with a highly poisoned moderator. This point is currently being investigated. If the level coefficient should change sign as moderator height increases, it would be necessary to operate only in the positive level coefficient region. This requirement could result in decreased permissible power densities and increased fuel-cycle costs. Preliminary calculations indicate that the level coefficient does not change sign for the core designs which are economically attractive. It is noted that this effect is important only for full power operation with the reactor at operating temperature and with non-uniform voids in the fuel channels. A changing level coefficient with uniform coolant density and with the reactor at low temperatures is unimportant, since high power density is not necessary at low-power operation.

2. Moderator-Level Variation with Load

The moderator-level change required to accommodate a core thermal-output change from one steady-state to another steady-state level of operation at the same pressure can be simply computed by noting that the net reactivity change must be zero. The reactivity changes, R , accompanying the change in core thermal output are postulated to be due to a) moderator-level change, R_L ; b) void change, R_ρ ; and c) fuel temperature, R_T . Thus

$$R_L = -(R_\rho + R_T) ,$$

or

$$\frac{\partial R}{\partial L} \Delta L = - \left(\frac{\partial R_\rho}{\partial \rho} \frac{\partial \rho}{\partial \Delta' Q} + \frac{\partial R_T}{\partial \Delta' Q} \right) \Delta' Q ,$$

463 029

in which ρ is the average coolant density, T the fuel temperature, L the moderator level, and $\Delta'Q$ the fractional change in core thermal output. (Changes in coolant mass rate through the core and the consequent void changes are small and therefore ignored.) The reactivity derivatives $\partial R_{\rho} / \partial \rho$ and $\partial R / \partial L$ have not been calculated but can be estimated for preliminary purposes (from EBWR data in the case of $\partial R_{\rho} / \partial \rho$, and from moderator control requirements for the VMR in the case of the $\partial R / \partial L$ derivative). For the EBWR operating at 20 mw and 550 psi the derivative $\partial R_{\rho} / \partial \rho$ is $-1.2 \text{ } \$/(\text{lb}/\text{ft}^3)$. For the VMR, the derivative $\partial R_L / \partial L$ is estimated to be of the order of $0.05 \text{ } \$/\text{in.}$ The values of the derivatives with respect to changes in the core thermal output are obtained from Attachment I to the Quarterly Progress Report No. 3. Substituting these values in the foregoing equation gives as a preliminary estimate,

$$\Delta L = 80 \Delta'Q.$$

Thus, if the thermal output is increased 1% ($\Delta'Q = 0.01$), the consequent moderator-level change must be 0.8 inch. It will be noted that the reactivity controlled by the moderator will change during the life of the core. As a result, the factor (80 in the present calculation) will vary during core life.

D. Comparison of Fuel-Cycle Costs for "Conventional" Boiling-Water Reactors and the VMR

Three separate studies of small boiling-water reactors have been used to compare the fuel costs of "conventional" boiling-water reactors with the VMR. Since each of the studies used different criteria for establishing fuel-cycle costs, these costs were recomputed using the techniques described in section I. E. The values that were used without modification from the three reports were: initial enrichment, final enrichment, plutonium concentration, fuel burnup, quantity of fuel in reactor, and total power output from reactor. Since these values are dependent upon the physics techniques employed by the investigators and upon the thermal design of the reactor, a discussion of these items and their effect on fuel-cycle costs is included below. Table IV compares the computed fuel-cycle costs of these three reactors with the VMR; VMR_1 and VMR_2 are similar except that VMR_1 has stainless-steel-clad fuel and VMR_2 Zircaloy-clad fuel. To be meaningful, the data should be considered in conjunction with the following discussion.

1. Prototype Boiling Water Reactor⁷

Two boiling-water-reactor designs are described in ANL-6019. A "reference" reactor design having a thermal output of 165 mw was selected, and the data developed for this reference reactor were extended by Argonne to include a "full-scale" reactor of 825-mw thermal capacity. The reference reactor was designed as a prototype for the larger reactor, and therefore certain design compromises were effected to provide favorable economics for the full-scale reactor. Two unique schemes for shuffling the fuel during reloading cycles were developed, the most attractive of which has been selected for comparison with the VMR. The stated objective of the study was "to design a nuclear power plant of 50 mw(e) capacity which could be used to demonstrate advanced performance concepts for boiling water reactors."

Examination of Table IV will show that the reference design selected by Argonne is a very "dry" lattice, high-specific-power core employing forced circulation. These factors tend to produce high conversion ratios (hence low burnup costs) and low inventory costs. However, several other factors tend to result in higher cycle costs than those of an "optimum" design. These factors are:

- a. High neutron leakage due to small core. As pointed out by ANL, the tight lattice core is probably optimum for the larger reactor, not for the small prototype.
- b. Conservative simplified techniques employed by Argonne for determining power distribution.
- c. Burnup of 11,000 mwd/T instead of 15,000 mwd/T.

It is estimated that the above factors might add 3 cents per million Btu to the calculated fuel-cycle costs.

To decrease the neutron leakage in the prototype reactor design, it would be necessary to increase the core size by either:

- 1) Adding additional fuel and decreasing specific power, or
- 2) Increasing the water/ UO_2 ratio.

7. J. M. Harrer, "Prototype Boiling Water Reactor," Argonne National Laboratory Report ANL-6019, October 1959.

TABLE IV

COMPARISON OF "CONVENTIONAL" BWR AND VMR FUEL-COST DATA

	<u>VMR₁</u>	<u>VMR₂</u>	<u>ANL-6019 BWR</u>	<u>TID-8510 BWR</u>	<u>ANL-6009 BWR</u>
Thermal output, mw	76	83	165	47	40.6
Fuel cladding material	SS	Zr	Zr	Zr	Zr
Operating pressure, psia	1,000	1,000	1,000	1,465	333
Water volume/UO ₂ volume	3.4	3.4	1.1	3.3	0.8
Avg. specific power, kw/kg U	18.7	19.1	14.1	12.4	3.9
Approx. avg. power density, kw/liter of coolant	73	-	117	45	30 (est.)
Max. heat flux, Btu/hr-ft ²	370,000	-	369,000	312,000	130,800
Burnup, mwd/T	15,000	15,000	11,000	15,000	11,000
Kg U/core	4,070	4,380	11,700	3,796	10,300
Forced or natural circulation	Natural	Natural	Forced	Natural	Natural
Calculated fuel-cycle costs, ¢/10 ⁶ Btu	32.5	33.8	33.8	26.1	31.9
Adjusted fuel-cycle costs, ¢/10 ⁶ Btu	32.5	33.8	30.8-32.8	-	29-31

Alternative "1" results in additional inventory charges, and alternative "2" results in increased burnup costs. It is noted also that a forced-circulation system is used. Even considering the decreased pressure drop with core-size increase, the existing lattice is so tight it is doubtful that the present specific power could be maintained without a pumping system. It is estimated that the increased costs resulting from "1" or "2" above and the reduction in specific power obtained from a natural-circulation system would be approximately 1 cent per million Btu.

The net decrease in fuel costs, considering all of the effects outlined above, is estimated at 2 cents per million Btu. Since the calculated costs were 33.8 cents per 10^6 Btu, the adjusted costs are 31.8 cents per 10^6 Btu. To allow for uncertainties, these adjusted costs are estimated at between 30.8 and 32.8 cents per million Btu. This would represent a decrease in fuel-cycle costs of 3 to 10% over the VMR Zircaloy-clad core. The VMR stainless-steel-clad core is between 0 to 5% more expensive than the conventional reactor Zircaloy-clad core.

2. 50-mw BWR Designed by General Nuclear Engineering Corporation⁸

The reactor data shown in Table IV under TID-8510 BWR were extracted from reference 8. The general objectives of the study were:

- a. To review the state of the art of boiling water reactors in the 5- to 40-emw range.
- b. To summarize anticipated problems having to do with boiling-water reactors in the 5- to 40-emw range.
- c. To present economic data in the form of capital and operating cost breakdowns.

It is noted that a rather large discrepancy exists between the fuel-cycle costs shown for the reactor design obtained from TID-8510 and the fuel-cycle costs shown for the other reactors in Table IV. The source of this discrepancy is difficult to determine because of a lack of information in TID-8510 concerning the methods used to obtain the nuclear constants in the calculations and the method of operating the reactor. While this report describes the numerical results of the burnup calculations in considerable detail, it is not clear whether the

8. "A Brief Study of Boiling Water Reactors in the 5 to 40 emw Range," TID-8510, October 1959.

reactor operates under a continuous discharge, batch discharge, or some intermediate scheme, or what the power distribution is. Also, the source of the values, and the values used for relative-absorption cross sections, slowing-down cross sections, water densities, and age are not specified. In addition, for the water/ UO_2 ratio at which these reactors are designed, and for the U 235 consumption and the Pu production obtained, there is some question as to whether the one-group burnup procedure described is adequate. For these reasons, it does not appear reasonable to compare the fuel costs obtained by GNEC with VMR fuel costs.

3. 40-mw Process-Steam BWR⁹

This study was undertaken by Argonne National Laboratory for the Atomic Energy Commission to determine the "feasibility of using nuclear reactors for process steam" and "to determine if the PWR, OMR, or BWR are particularly suitable for generation of 380°F saturated steam." It follows, from these objectives, that an attempt was probably made by Argonne to optimize each reactor type with regard to over-all economics. If this objective had been realized, it is apparent from examination of Table IV (discounting differences due to burnup) that the VMR fuel-cycle costs for the Zircaloy-clad core and the stainless-steel-clad core are, respectively, 5% and 2% more expensive than the Argonne conventional BWR fuel-cycle costs.

Unfortunately, the BWR selected by Argonne may not really represent an optimized design with regard to economics. Although the extremely dry lattice (water volume/ $\text{UO}_2 = 0.8$), coupled with the relatively large core (56-in. diameter) and the Zircaloy cladding, result in a high conversion ratio, the specific power is very low (3.9 kw/kg U). This low specific power is obtained for two reasons: a) the tight lattice selected by Argonne imposes hydrodynamic restrictions on the core; and b) the low operating pressure (333 psia) imposes power-density restrictions because of stability considerations. It is difficult to ascertain the magnitude of these various effects, but the VMR has been shown to be more sensitive to specific power limitations brought about by hydrodynamic restrictions than it is to changes in fuel-cycle costs resulting from water/ UO_2 volume changes.

9. L. Link, "Study of 40-mw Pressurized Water, Boiling Water, and Organic Moderated Reactors for Production of Process Steam," Argonne National Laboratory Report, ANL 6009, June 1959.

If the above effects (and the difference in burnup times) resulted in a net cost increase of 1.9 cents per million Btu, the "optimum" fuel-cycle cost would be 30 cents per 10^6 Btu. Allowing ± 1 cent per 10^6 Btu for uncertainties, the Zircaloy-clad fuel for the VMR would show a fuel-cycle cost between 9 and 16% higher than the conventional BWR. The stainless-steel-clad fuel for the VMR would have a fuel-cycle cost between 5 and 12% higher than the conventional reactor.

4. General Discussion of VMR and Conventional BWR Fuel-Cycle-Cost Differences

Table V summarizes the differences in fuel-cycle costs obtained by different methods.

Table V

Summary of VMR and Conventional BWR Fuel-Cycle-Cost Differences

	<u>165-mw BWR</u> <u>ANL-6019</u>	<u>40-mw BWR</u> <u>ANL-6009</u>
"Base" VMR Zircaloy fuel costs, ¢/ 10^6 Btu	33.8	33.8
Range % less than base of conventional costs	3-10	9-16
Average % less than base of conventional costs	6.5	12.5

From Table V, it would appear that an optimized conventional BWR will have fuel-cycle costs between 7 and 12% less than the VMR. As noted in previous discussions, however, there is considerable uncertainty in the 7 to 12% difference between the VMR fuel-cycle costs and conventional BWR costs. The reason for this uncertainty is that it is difficult to reconstruct the work done by others. To reduce the uncertainty, a conventional BWR is being analyzed by the same hydrodynamic and physics procedures as those used on the VMR.

Those factors which tend to make the VMR fuel costs higher than a conventional BWR are:

a. Stainless Steel Shrouds

The additional poison introduced by the stainless steel shrouds (compared with conventional reactors with Zircaloy structural materials) reduces the conversion ratio.

Zircaloy shrouds may be feasible on the VMR if the technology in Zircaloy welding advances sufficiently.

b. High Water/ UO_2 Ratio

The use of a separate moderator region results in a water/ UO_2 ratio which is higher than normally used in a conventional BWR, resulting in slightly lower conversion ratios. However, this effect is not as significant as the costs associated with "a".

Tending to offset the VMR fuel-cycle cost disadvantages outlined above is the improved peak-to-average power ratio obtained as a result of the elimination of control rods. Although the VMR has a slightly higher fuel-cell peak-to-average power ratio than a conventional BWR, it has a relatively low axial peak-to-average power ratio. This low axial peak-to-average power ratio is possible because the moderator is maintained at a uniform height throughout the fuel life by the addition of a soluble poison. A conventional BWR, on the other hand, may have a relatively high axial peak-to-average power ratio because of the flux distortion introduced by the control rods.

In the initial technical and economic feasibility report on the VMR, the net fuel-cycle advantage or disadvantage will be determined with greater accuracy than in this report. If the fuel costs are significantly higher in the VMR than in a conventional BWR, these increased fuel costs must be offset by the decreased capital and maintenance costs in order for the VMR to be attractive. The capital and maintenance costs for the VMR will be lower than those for a conventional BWR because of the elimination of control rods.

E. Method of Computing Fuel-Cycle Costs

Table VI includes all of the data used to compute fuel-cycle costs and is largely self explanatory. A description of the method is outlined below.

1. Reactivity

The value of $k_{\text{effective}}$ is determined at the beginning and end of life (as determined from the MSBU burnup computer code) by means of a VAE PROD IBM-650 calculation. The end-of-life k_{eff} is adjusted to retain 5.5% surplus reactivity corresponding to Xe, Sm, and shutdown reactivity margin. Xe and Sm reactivities are not included in the MSBU calculations.

2. Beginning- and End-of-Life Uranium and Plutonium Concentrations

The beginning-of-life concentrations are first estimated, and the end-of-life concentrations are then obtained (for 15,000 mwd/T burnup) from the MSBU code. The beginning- and end-of-life concentrations are then adjusted to result in the desired 5.5% excess k_{eff} .

3. Reactor Power Output

Calculations are made to determine whether the specific core design being considered is limited by hydrodynamic considerations, or by heat flux or center temperature. The limiting power is used.

4. Fabrication Costs

The fabrication costs are based upon preliminary data received from fuel fabricators and is subject to change as final quotations are received. Losses are included but use charges are not.

5. Reprocessing Fuel to Nitrates

The reprocessing costs are based upon the AEC estimate¹⁰ of costs for a typical reprocessing plant and are computed at \$15,300 per 1000 kg U plus \$15,300 per cleanup day. The number of days of cleanup time are estimated at one day per 1000 kg U, with a minimum of 3 days and a maximum of 8 days. Because of the minimum charge of \$45,900 associated with reprocessing, the fuel is assumed to be held in storage until a minimum of 3000 kg is available for processing.

6. Burnup Cost

The burnup cost is based upon the AEC-published values of uranium as a function of U 235 concentration.

7. Pu Credit

Plutonium credit is computed at \$12/gm of Pu. A separate calculation is made to determine the cost of producing plutonium buttons at the rate of \$1.50/gm of Pu.

8. Conversion Nitrates to UF₆

Conversion costs are computed at the AEC-published value of \$5.60/kg U.

10: "Notice of AEC Services in Chemical Processing of Spent Reactor Fuels," 22 FR 1591, March 12, 1957.

ATL

9. Losses

Processing losses of 1% are assumed. Uranium values are calculated at the end-of-life enrichments, and plutonium losses are calculated at \$12/gm.

10. Use Charges

All use charges are computed at 4% per annum of the value at the beginning-of-life enrichment. In addition to the fuel in the reactor, a fabrication time of 7 months and a cooling-off and shipping period of 7.2 months is assumed for each fuel cycle. During the cooling-off period, if less than 3000 kg of fuel are available, use charges are paid on the discharged fuel until 3000 kg are accumulated.

11. Shipping

Shipping charges are assumed to be \$3/kg U.

12. Interest on Fabrication Capital

Interest on the fabrication capital is computed at 6% per annum for the length of the fuel cycle.

TABLE VI
VMR FUEL-CYCLE ANALYSIS

	CORE DESIGN NO.																Prototype BWR ANL-6019		5-40 emw BWR TID- 8510	BWR ANL- 6009
	23	26	26A	28	29	29A	30	31	32	33	35	36	37	38	39	40	825-mw Divided Core	165-mw Divided Core		
1) k_{eff} at 0 mwd/T	1.1884	1.1967	1.1303	1.1844	1.1559	1.1559	1.1883	1.1537	1.1954	1.2376	1.1782	1.2070	1.1507	1.2250	1.1362	1.2113				
2) k_{eff} at 15,000 mwd/T	0.9843	1.0379	1.0721	1.0787	1.0704	1.0704	1.0521	1.1012	1.0706	1.0784	1.0092	1.0578	0.9929	1.0772	0.9908	1.0626				
3) $\% \Delta k$ 0-15,000 mwd/T	17.172	13.3	5.15	8.93	7.39	7.39	11.47	4.55	10.44	12.86	14.23	12.42	13.70	12.07	12.79	12.268				
4) ② - 1.055 = surplus (or deficit) k	-0.0707	-0.017	0.017	0.0237	0.0154	0.0154	-0.003	0.046	0.0156	0.0234	-0.0458	0.0028	-0.0621	0.0222	-0.0642	0.0076				
5) Natural or forced circulation	Nat.					Forced	Nat.										Forced	Nat.		
6) Beginning-of-life concentrations $\times 10^4$																				
a) 235	1.2569	1.8041	1.8041	2.7655	3.4607	3.4607	2.0008	2.4702	2.3960	1.7335	1.6128	1.8710	1.6770	1.9805	1.6715	1.8244				
b) 238	40.639	45.6730	45.6730	63.0791	73.4434	73.4434	50.6525	59.2845	57.5041	38.5799	52.1469	47.3662	46.2410	47.5330	46.0860	43.7844				
c) Total = ⑥a + ⑥b	41.8959	47.4771	47.4771	65.8446	76.9041	76.9041	52.6533	61.7547	59.9001	40.3134	53.7597	49.2372	47.9180	49.5135	47.7575	45.609				
d) $\%235 = 100 \times \text{⑥a} / \text{⑥c}$	3.00	3.80	3.80	4.20	4.50	4.50	3.80	4.00	4.00	4.30	3.00	3.80	3.50	4.00	3.50	4.00				
7) End-of-life concentrations $\times 10^4$																				
a) 235	0.5569	0.9461	0.9580	1.6528	2.1708	2.1708	1.1044	1.4354	1.3714	0.9997	0.7487	1.0190	0.8631	1.1144	0.8768	1.02				
b) 238	40.2675	45.2728	45.2894	62.4424	72.5924	72.5924	50.1905	58.6724	56.9683	38.3172	51.6148	46.9668	45.8184	47.1537	45.6506	43.424				
c) Total Pu	0.2081	0.2379	0.2308	0.3903	0.5341	0.5341	0.2772	0.3728	0.3236	0.1620	0.2988	0.2385	0.2473	0.2283	0.2586	0.2188				
8) Burnup $\times 10^4 = \text{⑥a} - \text{⑦a}$	0.7001	0.8580	0.8461	1.1127	1.2899	1.2899	0.8964	1.0348	1.0246	0.7338	0.8641	0.8520	0.8139	0.8661	0.7947	0.8045				
9) Adjustment to ⑥d from curve k vs %E	+0.56	+0.12	-0.12	-0.18	-0.11	-0.11	+0.02	-0.36	-0.12	-0.17	+0.36	-0.02	+0.49	-0.16	+0.50	-0.06				
10) Adjusted $\%235$ at 0 mwd/T	3.56	3.92	3.68	4.02	4.39	4.39	3.82	3.64	3.88	4.13	3.36	3.78	3.99	3.84	4.00	3.94	2.50	2.65	2.00	1.50
11) Adjusted beginning-of-life concentration $\times 10^4$																				
a) $235 = \text{⑥c} \times \text{⑩} / 100$	1.492	1.861	1.746	2.648	3.375	3.375	2.011	2.248	2.324	1.664	1.806	1.860	1.912	1.902	1.910	1.797				
b) $238 = \text{⑥c} - \text{⑪a}$	40.404	45.616	45.731	63.197	73.529	73.529	50.642	59.507	57.576	38.649	51.954	47.377	46.006	47.612	45.848	44.812				
12) Adjusted end-of-life concentration $\times 10^4$																				
a) $235 = \text{⑪a} - \text{⑧}$	0.7915	1.003	0.900	1.535	2.085	2.085	1.115	1.135	1.299	0.930	0.942	1.008	1.098	1.036	1.115	0.9925				
b) $238 = \text{⑪b} - [\text{⑥b} - \text{⑦b}]$	40.033	45.216	45.347	62.560	72.678	72.678	50.180	58.895	57.040	38.386	51.422	46.978	45.583	47.233	45.413	44.452				
c) Total Pu = ⑦c	0.2081	0.238	0.231	0.390	0.534	0.534	0.277	0.373	0.324	0.162	0.299	0.239	0.247	0.228	0.259	0.2188				
d) Total = ⑫a + ⑫b + ⑫c	41.032	46.457	46.478	64.485	75.297	75.297	51.572	60.403	58.663	39.478	52.663	48.225	46.928	48.497	46.787	45.663				
e) Total U = ⑫a + ⑫b	40.824	46.219	46.247	64.095	74.763	74.763	51.295	60.030	58.339	39.316	52.364	47.986	46.681	48.269	46.528	45.444				
f) $\%U 235 = 100 \times \text{⑫a} / \text{⑫e}$	1.939	2.17	1.95	2.40	2.79	2.79	2.18	1.89	2.23	2.37	1.80	2.10	2.36	2.15	2.40	2.184	0.96	1.60	0.578	0.495
g) $\%Pu = 100 \times \text{⑫c} / \text{⑫d}$	0.507	0.512	0.497	0.605	0.710	0.710	0.537	0.617	0.552	0.410	0.567	0.496	0.527	0.470	0.557	0.479	0.82	0.64	0.894	0.573
13) Mwd/short ton U	15,250	15,399	15,067	15,115	15,202	15,202	15,033	15,064	15,105	15,081	15,111	15,131	15,207	15,108	15,015	15,228	16,360	10,940	15,000	11,180
14) Kg UO_2 /core	4,980	4,620	4,620	4,590	4,660	4,660	4,620	4,620	4,630	4,620	4,980	4,620	4,620	4,650	4,600	4,600	67,050	13,410		
15) Kg UO_2 /cycle	4,980	4,620	1,540	4,590	4,660	4,660	4,620	4,620	4,630	4,620	4,980	4,620	4,620	4,650	4,600	4,600	13,410	2,680		
16) Kg U/core	4,383	4,070	4,070	4,040	4,100	4,100	4,070	4,070	4,080	4,070	4,380	4,070	4,070	4,090	4,050	4,048	58,500	11,700	3,796	10,300
17) Kg U/cycle	4,383	4,070	1,356	4,040	4,100	4,100	4,070	4,070	4,080	4,070	4,380	4,070	4,070	4,090	4,050	4,048	11,700	2,340	3,796	1,030
18) Short tons U/cycle	4.821	4.48	1.49	4.44	4.51	4.51	4.48	4.48	4.49	4.48	4.82	4.48	4.48	4.50	4.50	4.454	12.86	2.57	4.18	1.134
* 19) Peak power/foot pin (kw/ft)	10	9.82	9.82	5.33	2.93	9.03	9.86	9.86	6.28	9.82	5.15	9.86	9.58	10	1.69	10				

* Power per foot values less than 10 kw are pressure-drop-limited with natural circulation, rather than centerline-temperature- or heat-flux-limited.

TABLE VI
(page 2)

	CORE DESIGN NO.																Prototype BWR ANL-6019		5-40 emw BWR TID- 8510	BWR ANL- 6009
	23	26	26A	28	29	29A	30	31	32	33	35	36	37	38	39	40	825-mw Divided Core	165-mw Divided Core		
20) Hot-spot factor, over-all	3.6																		3.5	3.0
21) Avg. power/foot pin = $(19) / (20)$	2.78	2.73	2.73	1.48	0.814	2.51	2.74	2.74	1.75	2.73	1.43	2.74	2.66	2.78	0.468	2.78				
22) No. fuel pins/element	37											61	19	37						
23) No. elements/core	163	151	151	181	187	187	151	151	175	151	163	97	295	97	265	151				
24) Active fuel height, in.	60	60	0	50	49	49	60	60	52	60	60	57	60	55	56	60				
25) Mw/core = $(21) \times (22) \times (23) \times (24) / 1000 \times 12$	83.8	76.2	76.2	41.2	16.4	70.7	76.4	76.4	41.5	76.2	43.0	76.8	74.5	45.6	68.2	77.6	825	165	47.0	40.6
26) Plant capacity factor	0.8																0.8			
27) Cycle length, yr = $(13) \times (18) / (25) \times (26) \times 365$	3.0046	3.11	1.01	5.58	14.24	3.32	3.01	3.02	5.60	3.04	5.80	3.02	3.14	5.11	3.38	2.9933	0.873	0.584	4.57	1.07
28) Fabrication of fuel/cycle																				
a) Conversion $UF_6 \rightarrow UO_2$																				
a_1) \$3.00/lb-% E $\times (10) \times 2.2 \times (17)$, \$	102,983	105,100	32,900	107,000	119,000	119,000	102,600	97,800	104,400	111,000	97,200	111,400	107,200	103,500	106,800	105,282	193,000	37,400	45,600	10,200
a_2) Cleanup, \$	3,000																3,000			
a_3) Total = $(a_1) + (a_2)$, \$	105,983	108,100	35,900	110,000	122,000	122,000	105,600	100,800	107,400	114,000	100,200	114,400	110,200	106,500	109,800	108,282	196,000	40,400	48,600	13,200
a_4) \$/kg U = $(28a_3) / (17)$	24.18	26.60	26.50	27.30	29.80	29.80	26.00	24.10	26.30	28.00	22.90	28.10	27.10	26.00	27.10	26.75	16.80	17.50	12.80	12.80
b) Pelleting, incl. losses, \$/kg U	15.00														20.00	15.00	15.00			
c) Materials, fabrication, & shipment																				
c_1) Material	Zr	SS										Zr	SS				Zr			
c_2) Pin OD, in.	0.385	0.353										0.385	0.353	0.457	0.279	0.353	0.470	0.510	0.555	
c_3) \$/kg U	100.00	40.00										100.00	40.00	38.00	50.00	40.00	100.00			
d) Total Fab., \$/kg U = $(28a_4) + (28b) + (28c_3)$	139.18	82.00	82.00	82.00	85.00	85.00	81.00	79.00	81.00	83.00	138.00	83.00	82.00	79.00	97.00	81.75	132.00	133.00	128.00	128.00
29) Reprocessing/cycle																				
a) Reprocessing to nitrates																				
a_1) 3000/(17), to nearest whole no. = no. of cycles/batch processed	1	1	2	1													1	2	1	3
a_2) Tonnes U processed = $(29a_1) \times (17) / 1000$	4.383	4.07	2.71	4.04	4.10	4.10	4.07	4.07	4.08	4.07	4.38	4.07	4.07	4.09	4.05	4.049	11.70	4.68	3.80	3.09
** a_3) Process & cleanup time = $2 \times (29a_2)$, days	8.766	8.14	5.42	8.08	8.20	8.20	8.14	8.14	8.16	8.14	8.76	8.14	8.14	8.18	8.10	8.097	19.70	9.36	7.60	6.18
a_4) Total cost = \$15,300/day $\times (29a_3)$	134,120	124,500	82,900	123,500	125,500	125,500	124,500	124,500	124,800	124,500	134,000	124,600	124,600	125,100	124,000	123,890	301,000	143,300	116,300	94,500

** If calculated value of $(29a_3) < 6$, adjust so that $(29a_3) = (29a_2) + 3$; if calculated value of $(29a_3) > 16$, adjust so that $(29a_3) = (29a_2) + 8$.

TABLE VI
(page 3)

	CORE DESIGN NO.																Prototype BWR ANL-6019		5-40 emw BWR TID- 8510	BWR ANL- 6009
																	825-mw Divided Core	165-mw Divided Core		
	23	26	26A	28	29	29A	30	31	32	33	35	36	37	38	39	40				
29) a) Continued																				
a ₅) Avg. cycle cost, \$/kg U = $\frac{29a_4}{29a_2} \times 10^3$	30.60																25.70	30.60		
b) Burnup cost, \$/kg U																				
b ₁) Fuel at initial U 235 enrichment	464.66	522.60	484.00	538.74	598.60	598.60	506.60	477.50	516.20	556.60	432.70	500.10	533.90	509.70	535.50	525.84	297.00	320.60	220.00	145.50
b ₂) Fuel at final U 235 enrichment	210.85	246.20	212.50	281.60	342.50	342.50	247.70	203.60	255.40	276.90	196.20	235.40	275.40	243.10	281.60	248.34	70.50	160.40	24.70	16.20
b ₃) Burnup cost = $\frac{29b_1}{29b_2}$	253.81	276.40	271.50	257.14	256.10	256.10	258.90	273.90	260.80	279.70	236.50	264.70	258.50	266.60	253.90	277.50	226.50	160.20	195.30	129.30
c) Pu credit, \$/kg U (at \$12/gm)																				
c ₁) Gm Pu/cycle = $\frac{12g}{17} \times 10$	22,226	20,840	6,740	24,400	29,100	29,100	21,900	25,100	22,500	16,700	24,800	20,200	21,400	19,200	22,600	19,393	95,900	15,000	34,000	5,900
c ₂) \$/kg U = $\frac{29c_1}{17} \times 12$	60.852	61.40	59.60	72.40	85.20	85.20	64.70	74.00	66.20	49.30	67.90	59.60	63.10	56.40	67.00	57.48	98.40	76.80	107.30	68.70
d) Chemical processing loss of U 235 = 0.01 $\times \frac{29b_2}{17}$, \$/kg U	2.11	2.46	2.13	2.82	3.43	3.43	2.48	2.04	2.55	2.77	1.96	2.35	2.75	2.43	2.82	2.48	0.71	1.60	0.25	0.16
e) Pu loss = 0.01 $\times \frac{29c_2}{17}$, \$/kg U	0.61	0.61	0.60	0.72	0.85	0.85	0.65	0.74	0.66	0.49	0.68	0.60	0.63	0.56	0.67	0.57	0.98	0.77	1.10	0.69
f) Conversion nitrates to UF ₆ , \$/kg U	5.60																5.60			
g) Conversion Pu nitrate to buttons = \$1.50/gm $\times \frac{29c_1}{17}$	7.607	7.70	7.40	9.00	10.60	10.60	8.10	9.30	8.30	6.20	8.50	7.50	7.90	7.80	8.40	7.19	12.30	9.60	13.40	8.60
h) Use charge during fabrication and shipping, \$/kg U = $\frac{7}{12} \times 0.04 \times \frac{29b_1}{17}$	10.84	12.30	11.30	12.60	14.00	14.00	11.80	11.10	12.10	13.00	10.10	11.80	12.50	11.90	12.50	12.27	6.90	7.50	5.10	3.40
i) Use charge on fuel in reactor, \$/kg U = $\frac{16}{17} \times \frac{29b_1}{17} \times 1 \text{ core} \times \frac{27}{17} \times 0.04$	55.845	65.00	58.50	120.30	341.00	79.50	61.10	57.80	115.60	67.60	99.30	60.40	66.90	104.20	72.20	62.96	51.80	37.50	40.20	60.40
j) Use charge on irradiated fuel																				
j ₁) Storage & shipping time, yr	0.6																0.6			
j ₂) nth-cycle storage time = $\frac{27}{17} [(\frac{29a_1}{17} - 1) + 0.6]$, yr	0.6	0.6	1.61	0.6													0.6	1.184	0.6	2.74
j ₃) (n - 1) time = $\frac{27}{17} [(\frac{29a_1}{17} - 2) + 0.6]$			0.60															0.600		1.67
j ₄) $\frac{27}{17} [(\frac{29a_1}{17} - 3) + 0.6]$ (for positive nos.)																				0.60

TABLE VI
(page 4)

	CORE DESIGN NO.																Prototype BWR ANL-6019		5-40 emw BWR TID- 8510	BWR ANL- 6009
	23	26	26A	28	29	29A	30	31	32	33	35	36	37	38	39	40	825-mw Divided Core	165-mw Divided Core		
29) j) Continued																				
j_5) Total time = $j_2 + j_3 + j_4$	0.6	0.6	2.21	0.6													0.6	1.784	0.6	5.01
j_6) Avg. time/cycle = $j_5 / 29a_1$	0.6	0.6	1.10	0.6													0.6	0.892	0.6	1.67
j_7) \$/kg U = $j_6 \times 0.04 \times 29b_1$	11.15	12.50	21.20	12.90	14.40	14.40	12.20	11.50	12.40	13.40	10.40	12.00	12.80	12.20	12.90	12.62	7.10	11.50	5.30	9.70
k) Shipping, \$/kg U	3.00																3.00			
30) Interest on fabrication capital at 6%/yr, \$/kg U = $28d \times 0.06 \times 27$	25.10	15.30	4.96	28.50	73.70	16.90	14.70	14.30	27.20	15.10	48.10	15.00	15.40	24.20	19.60	14.68	6.90	4.70	32.10	8.20
31) Summary of costs, \$/kg U-cycle																				
a) Reprocess to nitrates = $29a_5$	30.60																25.70	30.60		
b) Conversion nitrates to UF_6 = $29f$	5.60																5.60			
c) Pu nitrates to buttons = $29g$	7.61	7.70	7.40	9.00	10.60	10.60	8.10	9.30	8.30	6.20	8.50	7.50	7.90	7.80	8.40	7.19	12.30	9.60	13.40	8.60
d) Shipping = $29k$	3.00																3.00			
e) Losses = $29d + 29e$	2.72	3.10	2.70	3.50	4.30	4.30	3.10	2.80	3.20	3.30	2.70	3.00	3.40	3.00	3.50	3.05	1.70	2.40	1.40	0.90
f) Burnup = $29b_3$	253.81	276.40	271.50	257.10	256.10	256.10	258.90	273.90	260.80	279.70	236.50	264.70	258.50	266.60	253.90	277.50	226.50	160.20	195.30	129.30
g) Inventory = $29h + 29i + 29j_7$	77.84	89.80	91.00	145.80	369.50	107.90	85.10	80.40	140.10	94.00	119.80	84.20	92.20	128.30	97.60	87.85	65.80	58.50	50.60	73.50
h) Total reprocessing = $\Sigma 31a$ thru $31g$	381.18	416.20	411.80	454.60	679.70	418.10	394.40	405.60	451.60	422.40	406.70	398.60	401.20	444.90	402.60	414.80	340.60	267.90	291.90	253.50
i) Pu credit = $29c_2$	60.85	61.40	59.60	72.40	85.20	85.20	64.70	74.00	66.20	49.30	67.90	59.60	63.10	56.40	67.00	57.48	98.40	76.80	107.30	68.70
j) Total net process = $31h - 31i$	320.33	354.80	352.20	382.20	594.50	332.80	329.70	331.60	385.40	373.10	338.80	339.00	338.10	388.50	335.60	357.32	242.20	191.10	192.60	184.80
k) Fabrication = $28d$	139.18	82.00	82.00	82.00	85.00	85.00	81.00	79.00	81.00	83.00	138.00	83.00	82.00	79.00	97.00	81.75	132.00	133.00	128.00	128.00
l) Total direct costs = $31j + 31k$	459.51	436.80	434.20	464.20	679.50	417.80	410.70	410.60	466.40	456.10	476.80	422.00	420.10	467.50	432.60	439.07	377.90	327.80	320.60	312.80
m) Interest on fab. = 30	25.10	15.30	4.90	28.50	73.70	16.90	14.70	14.30	27.20	15.10	48.10	15.00	15.40	24.20	19.60	14.68	6.90	4.70	32.10	8.20
n) Total costs = $31l + 31m$	484.61	452.11	439.10	492.70	653.20	434.70	425.40	424.90	493.60	471.20	524.90	437.00	435.50	491.70	452.20	453.75	384.80	332.50	352.70	321.00
32) Cycle costs, $\$ \times 10^{-3} = 31n \times 17 \times 10^{-3}$	2,124	1,840	596	1,990	2,680	1,782	1,730	1,730	2,010	1,920	2,300	1,776	1,770	2,010	1,830	1,837	4,500	778	1,337	330
33) Annual costs, $\$/yr = 32 \times 27$	706,931	592,000	590,000	357,000	188,000	536,000	574,000	573,000	360,000	632,000	397,000	588,000	564,000	394,000	542,000	613,000	5,150,000	1,330,000	293,000	309,000
34) $4/10^6$ Btu = $33 \times 4.19 \times 10^{-3} / 25$	35.35	32.5	32.4	36.3	46.9	31.8	31.5	31.4	36.3	34.7	38.6	32.1	31.7	36.2	33.3	33.14	26.2	33.8	26.1	31.9
35) Assumed plant net efficiency	27																30	27		
36) Mills/kw-hr = $34 \times 3.413 / 35$	4.47	4.11	4.09	4.59	5.93	4.02	3.98	3.97	4.59	4.39	4.88	4.06	4.02	4.58	4.21	4.19	2.98	4.28	3.30	4.03
37) Power density, kw/kg U = $\frac{1000 \times 25}{16}$	19.1	18.7	18.7	10.2	4.01	17.3	18.9	18.9	10.2	18.7	9.82	18.9	18.3	11.2	16.9	19.2	14.1	14.1	12.4	3.94

II. MSBU FUEL-BURNUP CODE

A. General Description of Code

The method used for computing isotopic concentrations and effective multiplication as a function of specific exposure is an IBM-650 computer code, MSBU, which has been designed to provide convenient treatment of non-uniform burnup and fuel and control poisoning, and to have general validity over a wide range of conversion ratios and specific exposures. Although the burnup calculation itself is essentially a zero-space-dimensional computation, it provides direct facilities for inserting local energy-dependent leakages and control poisons in the input, and it yields output directly usable in four-group diffusion calculations of flux and power distribution and effective multiplication. The computation is performed in four energy groups, using, for each group i , the following cross section data:

- χ_i , the fraction of fission neutrons born in group i .
- Σ_{sl_i} , the cross section for neutron slowing down by elastic and inelastic scattering from group i to groups $i+1$.
- $(\nu\Sigma_f)_i$, the cross section for fission neutron production by collision of neutrons in group i .
- Σ_{a_i} , the cross section for removal of neutrons from group i by absorption collisions and leakage (the leakage component may be negative).
- Σ_{f_i} , the fission cross section for neutrons in group i .

The first two quantities are specified as input. The others are computed at each time step from the concentration of U 235, U 238, Pu 239, Pu 240, Pu 241, fission products, and from the input values of the microscopic cross section and Σ_{as_i} , the macroscopic component of Σ_{a_i} due to other materials present and to leakage. From these cross sections, the four group fluxes are computed at each time step from the equation

$$\phi_i = \frac{F\chi_i + \Sigma_{sl_{i-1}}\phi_{i-1}}{\Sigma_{a_i} + \Sigma_{sl_i}},$$

where F is an input constant equal to the approximate number of fission neutrons produced per second per cm^3 .

The ϕ_i are then used to obtain the effective multiplication, the change in concentration of each isotope, and the increase in specific exposure during the time step, from the following differential equations. These equations are solved by means of an iterative predictor-correction technique.

$$\begin{aligned}
 k &= 1 + \sum_i \left[(\nu \Sigma_f)_i - \Sigma_{a_i} \right] \phi_i \\
 \frac{dN^{25}}{dt} &= -N^{25} \sum_i \phi_i \sigma_{a_i}^{25} \\
 \frac{dN^{28}}{dt} &= -N^{28} \sum_i \phi_i \sigma_{a_i}^{28} \\
 \frac{dN^{49}}{dt} &= N^{28} \sum_i \phi_i (\sigma_{a_i}^{28} - \sigma_{f_i}^{28}) - N^{49} \sum_i \phi_i \sigma_{a_i}^{49} \\
 \frac{dN^{40}}{dt} &= N^{49} \sum_i \phi_i (\sigma_{a_i}^{49} - \sigma_{f_i}^{49}) - N^{40} \sum_i \phi_i \sigma_{a_i}^{40} \\
 \frac{dN^{41}}{dt} &= N^{40} \sum_i \phi_i (\sigma_{a_i}^{40} - \sigma_{f_i}^{40}) - N^{41} \sum_i \phi_i \sigma_{a_i}^{41} \\
 \frac{dE}{dt} &= C \sum_i \phi_i \Sigma_{f_i} \\
 \frac{dN^{FP}}{dt} &= \sum_i \Sigma_{f_i} \phi_i - N^{FP} \sum_i \phi_i \sigma_{a_i}^{FP}
 \end{aligned}$$

C is an input constant equal to the number of mwd/T per fission per cm^3 . At each time step, the information printed out by the program consists of the exposure and each of the concentrations computed above. In addition, at each time step, the macroscopic cross sections, including input values of the diffusion coefficients, are punched out in a form directly usable in the multigroup diffusion code. Since the input values of the Σ_{as_i} , the Σ_{sl_i} , and the D_i may be changed at any time step, MSBU can include the effects of changes in the local leakage and poison cross sections and coolant and power densities during operation. Since, further, the output cross sections are directly usable for each region of a multigroup diffusion calculation, the values of these changes may conveniently be computed, and similarly, control-rod programming calculations may be performed.

Some detailed calculations on Core Design No. 26 are described in sections C and D below. As the description shows, the inclusion of control poisons and leakage variation has only a slight effect on fuel-cycle costs for a well thermalized low-conversion reactor like the VMR. It is possible, however, that this effect, which is the sum of several opposing factors, is significant for some VMR design cases. In Table VII, the magnitude of some other effects is illustrated by calculations on Core Design No. 36. Case A was computed at zero exposure with no control poisons inserted to hold down the burnup reactivity change. In case B, sufficient $1/v$ poison was inserted to hold down half the burnup reactivity change. Cases C and D were computed at 15,000 mwd/T, but in C the thermal microscopic cross sections were recomputed for a Wigner-Wilkins spectrum corresponding to the isotopic composition at that exposure.

Examination of the table shows that, although the thermal and non-thermal conversion ratios are somewhat different in each case, the maximum difference between the total conversion ratio of any two cases is 4%. Further, the values of k obtained for cases C and D are only 1% different. Since the core designs that appear attractive for the VMR are Nos. 26 and 36, and since consideration of various effects does not appear to change significantly the results of the fuel-cycle cost analysis obtained by the "batch" loading calculational procedure described in section B below, this simplified burnup code is considered to represent adequately the VMR fuel-cycle analysis. If other attractive core designs are considered besides 26 and 36, further review will be made of the nuclear effects on the simplified burnup code as applied to these cases.

TABLE VII

SPECIAL NUCLEAR EFFECTS IN VMR CORE DESIGN NO. 36

Case	A	B	C	D
Fraction of thermal neutrons absorbed in U 238	0.07916	0.06566		0.08562
Fraction of thermal neutrons absorbed in U 235	0.7242	0.6698	0.4312	0.4302
Fraction of thermal neutrons absorbed in FP	0	0	0.03401	0.03378
Fraction of thermal neutrons absorbed in Pu	0	0	0.2308	0.2356
Fraction of thermal neutrons absorbed in others	0.1968	0.2645		0.2147
Number of Pu 239 atoms formed by thermal capture in U 238 per U 235 atoms destroyed	0.09465	0.09362	0.1764	0.1744
Number of Pu 239 atoms formed by capture in U 238 per U 235 atoms destroyed	0.3257	0.3411	0.5619	0.5608
*C thermal	0.09465	0.09362	0.1032	0.1023
C non-thermal	0.2311	0.2475	0.2257	0.2266
Fraction of neutrons reaching thermal	0.7203	0.7171	0.7441	0.7441
Reactivity, k	1.298	1.211	1.129	1.137

* C = conversion ratio.

B. "Batch" Loading Computational Procedure

The MSBU code described above was used in conjunction with a four-group diffusion-theory calculation, using VAL PROD, to evaluate the changes in flux, power density, and reactivity for a uniformly burned core. This batch-loaded core was evaluated at 0 mwd/T and 15,000 mwd/T to simulate a beginning-and end-of-life condition for the uniform core. The changes in flux and power density are shown in Figures 14 and 15. The reduction in reactivity for Core Design No. 26 was found to be 13.2%.

The microscopic parameters for the MSBU code were determined from the particular specifications of this design and from information obtained from the PUREE' code and a P_3 approximation for the thermal group, using the Wigner-Wilkins spectrum via Amster's Table.¹¹ This same procedure was followed for all the water lattices under consideration and the resulting macroscopic parameters used in the beginning- and end-of-life four-group diffusion-theory calculations.

As seen from the description of MSBU (section A above), the concentrations and macroscopic diffusion-theory parameters are produced as functions of mwd/T. The output at 15,000 mwd/T was used to perform the end-of-life diffusion calculation and produced the results mentioned above.

C. "Three Region" Loading Computational Procedure

To gain a more realistic picture of the operational reactor, a three-region core was evaluated, each region having an average burnup of 5000 mwd/T.

The cylindrical core was divided into three equal-volume regions, and the burnup in each region was treated as follows:

R_I 10,000 - 15,000 mwd/T

R_{II} 5,000 - 10,000 mwd/T

R_{III} 0 - 5,000 mwd/T

Three separate calculations using the MSBU code were performed. From a set of similar three-region core calculations, the leakage cross sections for the three regions and four groups were evaluated and used in conjunction with four-group flux-weighted cross sections

11. Harvey J. Amster, "A Compendium of Thermal Neutron Cross Sections Averaged Over the Spectra of Wigner and Wilkins," WAPD-185, January 1958.

463 048

RADIAL FLUX (THERMAL) AND POWER DISTRIBUTION
BEGINNING OF LIFE
CORE DESIGN NO. 26, SINGLE-REGION CORE

FIGURE 14

FTL

263 049

RADIAL FLUX (THERMAL) AND POWER DISTRIBUTION
END OF LIFE
CORE DESIGN NO. 26, SINGLE-REGION CORE

FIGURE 15

PT

for a $1/v$ absorber (Boron), and these two quantities were included in the structure cross sections in the MSBU input. The burnup code (MSBU) was then run for R_{III} from 0 to 5000 mwd/T, with the initial concentrations of U 235 and U 238 and the appropriate structure cross sections. The concentrations given at 5000 mwd/T were then used in a burnup calculation for R_{II} (5,000 - 10,000 mwd/T), with the appropriate structure cross sections. A similar procedure was used for R_I (10,000 - 15,000 mwd/T).

The macroscopic parameters generated by the MSBU code were used in performing the two four-group diffusion-theory calculations for the beginning- and end-of-cycle configurations. The change in power-density distribution is shown in Figure 16, and the reduction in peak-to-average power ratio in going from a batch-loaded to a three-region core can be seen by comparing Figures 14, 15, and 16. The reduction in reactivity from beginning to end of cycle was found to be 5.15%. Figure 17 shows radial-flux distribution at the beginning and end of cycle for the three-region core.

Figure 18 shows the thermal-flux distribution in the large cell (a complete fuel assembly with its associated fuel, cladding, coolant, shroud, and moderator). It is noted that the peak-to-average of importance with regard to fuel-center-temperature calculations is that within the fuel region and is less than the 1.27 shown for the complete cell. This peak-to-average across the large cell is lower than was anticipated and will result in lower fuel-cycle costs than those shown elsewhere in the report for center-temperature-limited cores.

Figure 19 shows the thermal-flux distribution for the small cell (pellet, clad, and coolant).

D. Discussion of "Batch" Loading Versus "Three Region" Fuel Loading

It is recognized that a multizone fuel-loading scheme is required in order to reduce the radial-power peaking factors in the core and thus obtain an attractive fuel cycle. It is also recognized that it is desirable to obtain a fuel cycle which requires plant shutdown in integral increments of years. Therefore, for an average burnup of 15,000 mwd/short ton U (selected because it appears to be an achievable maximum based on present day technology), a fuel cycle is selected which results in the discharge of one-third of the core per year for an 0.8 plant capacity factor.

In order to simplify the calculations for the parametric study, the "batch" loading calculational technique described in section B is used, together with the expected three-region

403 051

RADIAL POWER DISTRIBUTION
BEGINNING AND END OF CYCLE, CORE DESIGN NO. 26
THREE-REGION CORE

FIGURE 16

417

RADIAL FLUX (THERMAL) DISTRIBUTION
 BEGINNING AND END OF CYCLE, CORE DESIGN NO. 26
 THREE-REGION CORE

FIGURE 17

THERMAL FLUX IN THE LARGE CELL
CORE DESIGN NO. 26

FIGURE 18

THERMAL FLUX IN PELLET, CLAD, COOLANT
CORE DESIGN NO. 26

FIGURE 19

ATL

peak-to-average power ratios for total-core power determination. For Core Design No. 26, the more detailed "three region" calculational technique described in section C is also used. From Table VI in section I. E, the "batch" loading technique results in a fuel-cycle cost of 32.5 cents/ 10^6 Btu, while the "three region" technique results in a cycle cost of 32.4 cents/ 10^6 Btu for Core Design No. 26.

III. KINETICS

The reactor kinetics model presented in the last quarterly report¹² has been represented on an analog computer for both positive and negative void coefficients of reactivity. The results of the analog investigations are shown in Figures 21 through 31. The reference values of the model parameters are listed in Table VIII, and the flow diagram for the model is shown in Figure 20.

The purpose of this preliminary analog work was to evaluate the relative importance and effect of various system parameters and to determine a control system for stable, load-following reactor operation. Consequently, the general procedure in the analog work was to set all parameters at their reference values and vary, from run to run, only one parameter. Thus, it is to be understood that all parameters have reference values except those specifically mentioned.

Inspection of Figures 29, 30 and 31 reveals that a control system using reactor pressure as the input signal appears to provide stable operation of the model with positive, negative, or zero void coefficients of reactivity.

Kinetics work presently being done is directed toward demonstrating that the kinetics model can be used to predict the measured reactivity feedback transfer function of the EBWR. Preliminary calculations are encouraging in this respect. Subsequent work will be restricted to application of the kinetics model to the VMR reactor design selected for its economic potential.

12. "Kinetics Model for Boiling Water Reactors," ATL-226 Preliminary, 29 February 1960, Attachment I to Quarterly Progress Report No. 3, ATL-A-107.

PAGES 35 THROUGH 38 WERE ELIMINATED.

463 056

ADVANCED *Technology* LABORATORIES
A DIVISION OF AMERICAN-Standard

Nomenclature

c_p	Unit heat capacity of liquid water
$\Delta'D$	Fractional change in steam demand
$\Delta'G$	Fractional change in steam-generation rate
G_o	Reference steam-generation rate
Δh	Heat of vaporization of water
L	Moderator level
L_p	Length of fuel pin
m_o	Mass rate of water entering core
N	Number of fuel pins in core
P	Reactor pressure
$\Delta'Q_c$	Fractional change in heat rate to coolant
$\Delta'Q_g$	Fractional change in core heat-generation rate
Q_o	Steady-state heat-generation rate
R	Excess multiplication
ΔR_T	Change in excess multiplication due to change in fuel temperature
ΔR_ρ	Change in excess multiplication due to change in coolant density
ΔR_L	Change in excess multiplication due to change in moderator level
r_o	Radius of UO_2 fuel pellet
s	Laplace transform variable
T_f	Fuel temperature
T_{in}	Temperature of water entering core
T_{sat}	Saturation temperature of water
T_{feed}	Temperature of feedwater delivered to reactor
W_o	Mass of water in two-phase region above core
ρ	Density of coolant
ρc	Heat capacity per unit volume of fuel

463 058

FIGURE 20

717

TABLE VIII

DEFINITIONS OF CONSTANTS FOR FIGURE 20

Constant	Definition	Units	Reference Value
α_1	$-\partial R / \partial T_f$	$$/^{\circ}\text{F}$	0.00133
α_2	$\partial R / \partial L$	$$/\text{ft}$	7.42
α_3	$\partial R / \partial \rho$	$$/\text{lb}/\text{ft}^3$	-0.0553
α_6	$\partial \rho / \partial P$	$(\text{lb}/\text{ft}^3)/\text{psi}$	0.06
α_7	$\partial \rho / \partial \Delta T_{Q_c}$	lb/ft^3	-10
α_8	$\partial \rho / \partial T_{in}$	$(\text{lb}/\text{ft}^3)/^{\circ}\text{F}$	-0.45
α_{10}	$\left[1 - \frac{G_o}{m_o} \right] \frac{dT_{sat}}{dP}$	$^{\circ}\text{F}/\text{psi}$	0.113
α_{11}	G_o / m_o	--	0.07
α_{13}	$1 + \frac{m_o}{G_o} \frac{c_p}{\Delta h} (T_{sat} - T_{in})_o$	--	1.171
α_{15}	$\frac{c_p m_o}{\Delta h G_o}$	$1/^{\circ}\text{F}$	0.0284
α_{16}	$\frac{\Delta h G_o}{3600 c_p W_o (dT_{sat}/dP)}$	psi/sec	.46
α_{19}	$\frac{Q_o}{\pi r_o^2 \rho c L N \times 3600}$	$^{\circ}\text{F}/\text{sec}$	31.2
α_{20}	$\frac{dT_{sat}}{dP}$	$^{\circ}\text{F}/\text{psi}$	0.1215
τ_1	(see eq. 20)*	sec	0.4
τ_2	(see eq. 20)*	sec	0.4
τ_4	(see eq. 43)*	sec	3.0

TABLE VIII
(concl.)

<u>Constant</u>	<u>Definition</u>	<u>Units</u>	<u>Reference Value</u>
τ_5	ζ_1 (eq. 30)*	sec	9.5
τ_6	ζ_2 (eq. 30)*	sec	1.52
C_3	$-(dL/dt)/(P - P_o)$	ft/sec-psi	1×10^{-6}
K	Downcomer transport delay	sec	8

* Reference 12.

FIGURE 21

FIGURE 21
(Positive Void Coefficient)

The traces show the response of the system with positive void coefficient to step changes in steam demand. Response to step increases of 5, 10, and 20% are shown in Figures 21a, 21b, and 21c, respectively. Figures 21d and 21b are identical except that Figure 21d was obtained using a higher tape speed in order to show the initial response with greater resolution. Figure 21b is regarded in the following as a reference curve for the case of a positive void coefficient, because all parameters have their reference values (Table VIII) and because a 10% step increase in steam demand is the excitation used in most of the system investigations.

Examination of Figures 21a, 21b, and 21c shows that the system behaves in an essentially linear manner, inasmuch as the magnitude of the response is proportional to the magnitude of the excitation. This linearity is consistent with the fact that, even with a 20% step increase in steam demand, the maximum resultant excess multiplication is only 0.10. It is therefore probable that elimination of the product term, RQ_g , in the nuclear kinetics equations would have little effect on system behavior. Stability and transient behavior can thus be analytically investigated by linear constant-coefficient methods over the range of system parameters covered in this study.

FIGURE 22

FIGURE 22
(Positive Void Coefficient)

Changes in feedwater temperature, flow rate, reactor pressure, and recirculation ratio produce changes (dT_d) in the temperature of the water entering the downcomer (after mixing with the feedwater). The subsequent change, dT_{in} , in the core inlet-water temperature is equal to dT_d but is delayed by the transport time associated with flow from the top of the downcomer to the core inlet. The relation between dT_{in} and dT_d has, for the reference model, the form

$$dT_{in} = \frac{e^{-Ks}}{1 + \tau_4 s} dT_d$$

in which, for the reference case, K and τ_4 are, respectively, 8 and 3 seconds. Figure 22 shows, by comparison with Figure 21b, the effect of replacing the time delay, K , by a simple time constant τ_d . With this substitution, the above equation is altered to the form

$$dT_{in} = \frac{dT_d}{(1 + \tau_4 s)(1 + \tau_d s)}$$

In Figure 22, τ_4 has the reference value of 3 seconds, and τ_d the value of 0, 4, 8 and 16 seconds in Figures 22a, 22b, 22c, and 22d, respectively. Comparison of Figures 21b and 22c shows that replacing the 8-second time delay by an 8-second time constant has little effect on the transient response of the model.

FIGURE 23

FIGURE 23
(Positive Void Coefficient)

For the reference case, the fuel-element time response is represented by the relation

$$\frac{\Delta'' Q_c}{\Delta' Q_g} = \frac{0.922}{1 + \tau_5 s} + \frac{0.078}{1 + \tau_6 s}$$

in which τ_5 and τ_6 are, respectively, 9.5 and 1.52 seconds. The effect of replacing this second-order representation of the fuel-element time response by a first-order approximation:

$$\frac{\Delta'' Q_c}{\Delta' Q_g} = \frac{1}{1 + 9.5s}$$

can be obtained by comparing Figures 21b and 23c. The difference between the two sets of traces is detectable but not large. Figures 23a through 23e show the effect of different (first-order) fuel-element time constants of 2.4, 4.8, 9.5, 19, and 38 seconds. The fuel-element time constant has a large effect on the temperature change of the fuel in response to a change in steam demand (and thus thermal output of the core). The relation, for a first-order time-constant representation, is given by the equation

$$\frac{\bar{T}(s)}{Q_g(s)} = \frac{1}{\pi r_o^2 \rho c} \left(\frac{\tau_5}{1 + \tau_5 s} \right)$$

(The product $\pi r_o^2 \rho c$ is a constant in Figure 23.) Thus, the temperature change, $\bar{T}$, of the fuel (and therefore the reactivity change) is directly proportional to the time constant.

FIGURE 24

FIGURE 24
(Positive Void Coefficient)

The rate of change of reactor pressure is related to steam demand and steam generation by the equation

$$\frac{dP}{dt} = \alpha_{16} (\Delta^v G - \Delta^v D)$$

For the reference case, α_{16} has the value 46 psi/sec. Figures 24a, 24b, and 24c show the reactor response to a 10% step increase in steam demand for values of α_{16} of 5.75, 11.5, and 92 psi/sec, respectively. It will be observed that a value of 5.75 psi/sec (Figure 24a) produces unstable operation with the reference control system defined by Figure 20 and Table VIII. No attempt was made to alter control-system gain to produce stable operation with $\alpha_{16} = 5.75$ psi/sec, since the objective of this analysis is to study the effects of varying parameters on reactor stability, and not to provide stable systems for all possible reactor designs. The specific reactor designs considered attractive will of course be designed so as to be stable systems.

FIGURE 25

FIGURE 25
(Positive Void Coefficient)

Figure 25 shows the effect of different values for the positive void coefficient of reactivity. The reference value (cf. Figure 21b) is $-0.0553 \text{ } \$/(\text{lb}/\text{ft}^3)$. Equivalent values, expressed in different units are, $+0.0244 \text{ } \$/(\% \text{ voids})$ and $+0.000156 \Delta k/(\% \text{ voids})$. It will be noted that the absolute magnitude of the positive void coefficient is considerably less than is characteristic for a negative void coefficient, a fact which is believed consistent with the physical limitations encountered in achieving a positive coefficient.

Figures 25a, 25b, 25c, and 25d show system response when the positive void coefficient is 0, $1/2$, $1-3/4$, and 2 times the reference value. In Figures 25a and 25d, the system excitation is a 5% step increase in steam demand rather than the "reference" value of 10%. For the case of a zero void coefficient (Figure 25a), the system is neutrally stable. For a negative void coefficient the model, with the reference control system, is unstable. Again, for the same reasons outlined in Figure 24, no attempt was made to obtain a stable model with the reference control system and the negative void curve.

FIGURE 26

FIGURE 26
(Positive Void Coefficient)

The reference value of the fuel temperature coefficient of reactivity (α_1) is -0.00133 $\$/^\circ\text{F}$ or $-0.85 \times 10^{-5} \Delta k/^\circ\text{F}$ ($-1.5 \times 10^{-5} \Delta k/^\circ\text{C}$). Figures 26a, 26b, 26c, and 26d show the system response when the coefficient is 0, 1/2, 2, and 4 times the reference value. It will be noted that Figure 26a ($\alpha_1 = 0$) shows the response to a 5% step increase in steam demand. For the higher values of the coefficient, the reactivity decrease due to the increase in fuel temperature is larger than the reactivity increase due to the increased voids. The moderator level consequently increases with an increase in steam demand.

FIGURE 27

FIGURE 27
(Positive Void Coefficient)

Figure 27a shows the transient response of the model to a step decrease in reactor feedwater temperature of 15°F. Steam demand is constant.

As shown in the discussion of Figure 22, the relation between a change, dT_d , in down-comer water temperature and the subsequent change in core inlet temperature is given by the relation

$$dT_{in} = \frac{e^{-Ks}}{1 + \tau_4 s} dT_d$$

in which, for the reference case, $K = 8$ sec and $\tau_4 = 3$ sec. Figure 27b shows the system response to a 10% step increase in steam demand when τ_4 is 1-1/2 seconds. Comparison with Figure 21b shows that there is little effect due to this change in the time constant.

Following step changes in heat input rate to the coolant and in coolant inlet temperature to the core, the subsequent changes in coolant density follow with time constants τ_1 and τ_2 . For the reference case, τ_1 and τ_2 are equal and have the value 0.4 second. Figure 27c shows the system response when $\tau_1 = \tau_2 = 1.6$ sec. Comparison of Figures 27c and 21b reveals no detectable effect due to this change in the two time constants.

923 826

FIGURE 28

FIGURE 28
(Positive Void Coefficient)

Control of the reference model is effected by making the rate of change of moderator level proportional to the departure of the pressure from a set pressure, P_o :

$$\frac{dL}{dt} = C_3 (P - P_o)$$

Noting that $\partial R / \partial L = -\alpha_2$, the reactivity change due to changes in moderator level is given by

$$\frac{dR}{dt} = -C_3 \alpha_2 (P - P_o)$$

in which, for the reference conditions, $C_3 = 10^{-6}$ and $\alpha_2 = 7.42$ \$/ft, giving $C_3 \alpha_2 = 7.42 \times 10^{-6}$ \$/sec-psi. Figures 28a and 28b show model behavior when $C_3 \alpha_2$ is 0 and 10 times the reference value. For the case of no control ($C_3 = 0$), it will be observed that the pressure continues to increase following a 10% increase in steam demand. The rate of pressure increase, after the initial transient is low, being less than 0.2 psi/sec.

FIGURE 29

FIGURE 29
(Positive Void Coefficient)

The reference control system was replaced with a control system described by the following equation:

$$d_2 \Delta P + d_3 \Delta P + d_4 \int \Delta P dt = -\Delta R_L$$

With d_2 and d_3 set to zero, it will be noted that the original reference control system is obtained. The selected reference values of the coefficients are $d_2 = 0.125$ \$/(psi/sec), $d_3 = 0.005$ \$/psi, and $d_4 = 7.42 \times 10^{-6}$ \$/psi-sec.

Figure 29a shows model response to a 10% increase in steam demand with $d_2 = 0$ and with d_3 and d_4 at reference values. Figure 29b shows the same response but with d_2 (as well as d_3 and d_4) at reference values. The large damping effect attributable to the pressure-derivative control (i. e., to d_2) is notable. Figure 29c shows model response with a void coefficient of zero ($\alpha_3 = 0$) and $d_2 = 0$. By comparison with Figure 25a, it will be seen that addition of the d_3 term in the control equation permits stable operation with a zero void coefficient. Figures 30 and 31 show that the revised control system also permits stable operation with a negative void coefficient of reactivity.

FIGURE 30

FIGURE 30
(Negative Void Coefficient)

For a reactor having a positive void coefficient of reactivity, the reactivity-void relation is likely to be similar to that shown in the sketch.

For sufficiently large voids, the derivative of reactivity with respect to voids will be zero and, for a larger void fraction, negative. Thus, a control system which gives stable operation with positive, zero, and negative void coefficients of reactivity is required. Figures 30 and 31 demonstrate that the control equations described opposite Figure 29 also permit stable operation with a negative void coefficient of reactivity. The magnitude of the reference negative void coefficient is the same as that of the reference positive coefficient.

The conditions applying to each set of traces is summarized by the following table.

Coefficient	d_2	d_3	d_4	α_3 (void coeff.)
Figure				
30a	$d_{2\text{ref}}$	$d_{3\text{ref}}$	$d_{4\text{ref}}$	$\alpha_{3\text{ref}}$
30b	$d_{2\text{ref}}$	$2d_{3\text{ref}}$	$d_{4\text{ref}}$	$\alpha_{3\text{ref}}$
30c	0	$1/2d_{3\text{ref}}$	0	$\alpha_{3\text{ref}}$
30d	0	$1/2d_{3\text{ref}}$	$d_{4\text{ref}}$	$\alpha_{3\text{ref}}$

FIGURE 31

FIGURE 31
(Negative Void Coefficient)

The conditions applying to Figure 31 are summarized in the following table.

Coefficient	d_2	d_3	d_4	α_1	α_3
Figure					
31a	0	$d_{3\text{ref}}$	$2d_{4\text{ref}}$	$\alpha_{1\text{ref}}$	$\alpha_{3\text{ref}}$
31b	0	$d_{3\text{ref}}$	$d_{4\text{ref}}$	$\alpha_{1\text{ref}}$	$2\alpha_{3\text{ref}}$
31c	$d_{2\text{ref}}$	$d_{3\text{ref}}$	$d_{4\text{ref}}$	0	$\alpha_{3\text{ref}}$
31d	0	$d_{3\text{ref}}$	$d_{4\text{ref}}$	0	$\alpha_{3\text{ref}}$

In Figure 31b, it will be observed that the reactivity change due to fuel temperature (and thus fuel temperature) does not become constant after the initial transient. The drift is caused by an analog computer difficulty and has no analog in the system represented. Examination of Figure 10 of reference 6 shows that the fuel temperature is the time integral of the difference between two signals. Thus, unless the two signals are exactly balanced in the steady state, the analog fuel temperature will continue to drift. The drift which can be noted in most of the preceding figures is slow and does not influence the transient performance indicated.

IV. PUREE' PHYSICS ANALYSIS, THERMAL GROUP

A. Blackness Computation

Using the blackness iteration procedure described in the first quarterly progress report,¹³ a series of cases was evaluated to demonstrate the effect of lattice pitch on blackness. The fuel rods used for these cases were one of those described by Kouts, et al¹⁴; i.e. aluminum-clad UO_2 rods having a diameter of 0.493 in. (fuel only) and 1.3% enrichment. Blackness was computed as a function of neutron velocity over the thermal energy range (20 groups) for four different lattice pitches. The results are plotted in Figure 32 along with the blackness value determined by the method of Stuart¹⁵ which neglects the effects of neighbor rods.

It is seen that no significant variation from the Stuart blackness value occurs except for very close lattices (i.e., pitch/diameter $\cong 1.02$).

A significant reference parameter for evaluating the effect of neighboring rods on blackness is the number of mean-free-paths between the adjacent surfaces, or more conveniently, the ratio (pitch-minus-diameter)/(mean-free-path). The curve of Figure 33 depicts the ratio of blackness (β) involved by this method to that predicted by Stuart's method (β_0). This curve may be assumed valid for a small rod (~ 0.5 cm) at low enrichments. No perceptible deviation from the Stuart value occurs until $(p-d)/\lambda_c$ drops below 0.3. The three lattices proposed for the VMR critical experiment all have ratios $(p-d)/\lambda_c$ greater than 0.6; hence, even with a water density of 0.5, the effect of neighbor rods on blackness would be negligible.

B. Spectral Shift Between Coolant and Fuel

The matrix iteration program for the IBM 650 which computes the coolant spectrum by modifying the Wigner-Wilkins analysis¹⁶ to account for the presence of absorbing rods has been found to be in error. However, the expression for neutron current into the rod is valid;

-
13. "Variable Moderator Reactor Development Program," Advanced Technology Laboratories, a Division of American-Standard, Quarterly Progress Report No. 1, ATL-A-100, 31 August 1959.
 14. H. Kouts, et al, "Physics of Slightly Enriched, Normal Water Lattices (Theory and Experiment)." Proceedings of the Second United Nations International Conference on Peaceful Uses of Atomic Energy, 12, Reactor Physics, (1958), P/466. (Specific case referred to is in table on page 451.)
 15. G. W. Stuart, "Multiple Scattering of Neutrons," Nuclear Science and Engineering, 2, (1957), p 617.
 16. E. P. Wigner and J. E. Wilkins, Jr., AECD-2275, "Effect of the Moderator on the Velocity Distribution of Neutrons with Numerical Calculations for H as Moderator," September, 1944.

BLACKNESS VERSUS NEUTRON VELOCITY FOR VARIOUS LATTICES

FIGURE 32

47

BLACKNESS AS A FUNCTION OF
LATTICE PITCH AND COOLANT MEAN FREE PATH

FIGURE 33

hence, the ratio of fuel flux to coolant flux by energy group is attainable with the following relation:

$$\frac{\bar{\phi}_f}{\bar{\phi}_c} = \frac{J_{in} \beta}{\sum_{a_f} N_c v_c} \cdot \frac{2}{r},$$

where $\bar{\phi}_f$, $\bar{\phi}_c$ = average flux in fuel, coolant, respectively ($\frac{n}{cm^2 - sec}$)

J_{in} = current into rod ($\frac{n}{cm^2 - sec}$)

β = blackness

$\sum_{a_f}$ = absorption cross section for fuel (cm^{-1})

N_c = neutron density in coolant ($\frac{n}{cm^3}$)

v_c = neutron velocity for each group in coolant (cm/sec)

$2/r$ = surface-to-volume ratio for an infinite cylinder (cm^{-1})

Figure 34 depicts the thermal neutron spectrum in the coolant and fuel for the Kouts¹⁴ test lattice with an assumed Wigner-Wilkins distribution in the coolant. Despite the fact that the true spectrum in the coolant may not be represented in Figure 34, the relative shift from coolant to fuel is evident.

The value measured for $\bar{\phi}_c/\bar{\phi}_f$ in the Kouts lattice is 1.0 ± 0.01 .¹³ The spectra of Figure 34 indicate a value of $\bar{\phi}_c/\bar{\phi}_f$ of 1.13, correcting for the $1/v$ detector used for the experimental determination. This is a reasonable check on the analytical determination of spectral shift.

C. Conclusion

The methods of obtaining properly weighted cross sections for the PUREE' thermal group¹³ have been adequately checked against experimental data and found to be valid. However, as seen by the negligible effect of neighbor rods on blackness (Figures 32 and 33) and by the small spectral shift between coolant and fuel for the VMR studies, it does not seem warranted at the present time to replace the "P-3" and "POP"¹⁷ codes now in use for obtaining thermal constants by the more rigorous methods (the PUREE' thermal code - Reference 13).

17. "Variable Moderator Reactor Development Program," Advanced Technology Laboratories, a Division of American-Standard, Quarterly Progress Report No. 2, ATL-A-103, 30 November 1959 (section III. B).

COOLANT AND FUEL SPECTRA

FIGURE 34

463 088

V. VMR MECHANICAL AND SYSTEMS DESIGN

A. Reactor Arrangement

1. Fuel

The fuel pins, consisting of UO_2 pellets clad with type 348 stainless steel, are arranged in a triangular lattice to form a fuel element. The elements, of hexagonal form, are in turn arranged in a triangular pitch to form a roughly circular core. The triangular pin pitch and hexagonal fuel element were selected to minimize the disadvantage factor across a fuel cell, and to obtain a constant moderator gap between fuel cells. This arrangement also results in the maximum distance between fuel pins for a given water to UO_2 volume ratio.

2. Moderator Barrel

The moderator barrel, consisting of a cylindrical core barrel, shrouds, grid plates, and miscellaneous piping, welded to form a single unit, performs the following functions:

- a. Provides completely separate moderator region for control purposes.
- b. Provides support for fuel elements and for a natural circulation riser.
- c. Provides fuel element flow channels.

The shrouds are hexagonal and sized slightly larger than the fuel elements to permit refueling. A transition from hexagonal to circular section is made in the shrouds immediately above the lower grid plate. Thus the holes in the lower grid plate are circular, providing a small ledge at each shroud corner for fuel element support.

An annular reflector region is provided between the outer row of shrouds and the core barrel. The resulting solid annular section of the lower grid plate is used for making moderator inlet, outlet, and level-indicating pipe connections to the moderator barrel. The annular section of the upper grid plate is used for connection of pressure equalizing pipes, which extend above the water level in the steam generating portion of the pressure vessel.

The moderator inlet, outlet and level-indicating pipes are extended through pressure vessel nozzles to a point below the shielding beneath the pressure vessel, where they are welded to the vessel nozzles. As this is the only point at which the moderator barrel assembly is fastened to the vessel, the assembly may be removed from the vessel at any time during the life of the plant.

There are four moderator outlet pipes distributed equally about the moderator barrel reflector region. These serve to allow for rapid moderator dump when scram action is required. The pipes have a sufficiently heavy wall thickness to permit their being used as core supports. This arrangement simplifies the pressure vessel and substantially eliminates problems caused by differential expansion of the moderator region pipes with respect to the pressure vessel.

The moderator barrel contains a moderator inlet flow distribution header and flow baffles to assure symmetrical moderator flow.

Attached to the outside of the moderator barrel is a thin boron-containing stainless steel cylindrical shell which serves to prevent reflection of neutrons to the core by the downcomer region water. This component is designed to allow its removal and replacement.

All components of the moderator barrel assembly are of type 304 stainless steel. The welded joints are designed with particular care to insure moderator region integrity throughout plant life.

3. Coolant Riser

A riser assembly, attached to the top surface of the upper grid plate, provides the increased driving head necessary for natural circulation. It is fabricated from type 304 stainless steel sheets arranged in a pattern providing individual channels above each fuel assembly to prevent cross-flow in the riser. The design also permits the riser to remain in place during the refueling operation.

4. Pressure Vessel

The reactor pressure vessel is a carbon steel, stainless steel-clad cylindrical shell with ellipsoidal heads. The upper head is removable. A feedwater distribution ring and a steam collecting ring are attached to the inside of the vessel. The inside diameter of the vessel is such that a large annular space is provided for the downcomer region, which results in low velocity flow in the downcomer, thereby minimizing entrainment of steam. This water space, together with the metal of the core barrel and the water in the reflector region of the moderator, provides sufficient shielding to avoid the need of additional neutron shielding for vessel wall protection.

The vessel shell may be tapered to a smaller diameter above the core region in order to reduce costs. The reduction in diameter is possible because of the smaller diameter of

the riser as compared to the moderator barrel and also because of reduced shielding requirements above the core. The minimum pressure vessel diameter is that which permits field installation of the moderator barrel.

B. Reactor Component Stress Analysis

1. Core Support Plate

For various core configurations core support plate thicknesses have been determined. Since the plate is quite similar to a heat exchanger tube sheet, bending stresses have been treated in the manner recommended by Malkin¹⁸ and Horvay¹⁹. This method treats plates perforated uniformly with circular (or hexagonal) holes as solid plates with fictitious values of Young's modulus and Poisson's ratio which are functions of the plate geometry. Bending moments are calculated for this "equivalent plate" and applied to the individual ligaments for the actual plate. However, for the core configurations presently under study, it appears that a close approximation of maximum ligament stresses may be made by application of the maximum radial bending moment found from standard plate formulae to a radial ligament. This observation is true only when a very large portion of the original plate has been removed, as is the case with the VMR grid plates.

Effects of gamma heating of VMR grid plates have been calculated and found to be negligible.

2. Shrouds

Calculations of stresses in the VMR shrouds have been made for the following:

- a. Thermal stresses due to shroud gamma heating.
- b. Thermal stresses due to core barrel gamma heating.
- c. Thermal stresses due to moderator and coolant temperature transients.
- d. Bending stresses due to grid plate deflections.
- e. Membrane stresses due to differential pressures in coolant and moderator which would result from a pipe rupture accident.
- f. Column and local buckling assuming fuel elements supported by upper grid plate.

18. "Notes on a Theoretical Basis for Design of Tube Sheets of Triangular Layout," Trans. ASME, 74 (1952), pp 382-396.

19. "Bending of Honeycombs and Perforated Plates," Journal of Applied Mechanics, 19, No. 1 (1952), p 122.

ATL

Of the above the only stress which appears to be a potential problem is (e), the membrane stress resulting from an accident such as complete rupture of a steam or moderator outlet line. Because of the hexagonal form of the shrouds they have little resistance to deformation when differential pressures exist between the inner and outer surfaces, that is, between the coolant and moderator regions. In order to avoid deformation which might cause difficulty in removing fuel elements, the pressure equalizing pipes have been sized to prevent deformation of the shrouds under the worst possible conditions which could result from pipe rupture.

3. Moderator Inlet Pipe

Because of the differential expansion resulting from the dissimilar materials of the pressure vessel and the moderator inlet pipe and from the unequal temperatures of coolant and moderator, it appears that, with very conservative assumptions, the moderator inlet piping will experience stresses above yield. However, treating such stresses as transient stresses in accordance with the reference below,²⁰ indicates no possibility of failure over a projected 30-year life. For example, temperature swings of 180°F in moderator inlet temperature correspond to an alternating stress of 24,000 psi as compared with a "yield" stress of 19,500 psi. The design curve on page 34 of reference 20 below indicates a life of 10^6 cycles or, approximately 100 cycles per day, for the above condition. Since the moderator region inlet pipe will be subjected to far fewer cycles than this, the design is satisfactory.

4. Pressure Vessel

The basic geometry and dimensions of the pressure vessel walls, heads, flanges, and nozzle reinforcements have been determined using references 21 and 20 below.

C. Economic Evaluation

Drawings for the VMR fuel elements Dwg. F-1712, and the pressure vessel, Dwg. F-1716, have been prepared for purposes of obtaining quotations as part of the VMR economic evaluation. Drawings for the reduced diameter pressure vessel described in Section V-A and for the moderator barrel will be prepared in the near future for the same purpose.

-
20. "Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components," U. S. Dept. of Commerce; Office of Technical Services, P. B. 151987, dated December, 1958 - as amended 27 February 1959, pp. 29-33.
 21. "Unified Pressure Vessel," ASME Boiler and Pressure Vessel Code, Section VIII, 1959 Edition, and Pertinent Case Rulings Applying to Nuclear Vessels.

D. Cleanup System for VMR Moderator Region

It has been proposed that from 1 to 2 grams per liter of boric acid be used in the VMR moderator as a variable poison.

This will provide an acid medium with a pH of approximately 5.8 to 6.2, and all corrosion is therefore likely to be acid corrosion.

At corrosion rates calculated to be 2.3×10^{-3} lb/hr. of stainless steel, the blowdown rate for a 1 ppm impurity in the moderator water is 2300 lb/hr. Assuming uniform acid corrosion of 18-8 stainless steel, the blowdown will produce 0.187 grams of chromium, 0.083 grams of nickel, and 0.883 grams of iron per hour in solution. This is equivalent to 0.0413 gram-equivalents of metal ion per hour, assuming trivalent chromium and divalent nickel and iron from corrosion in an acid medium. Since these materials exist as metal ions in acid solution and each equivalent of metal liberates one equivalent of molecular hydrogen in the corrosion process, the corrosion products can be removed and the acid balance of the solution can be restored to its original state by passage of the solution through a simple cation exchange in the hydrogen cycle.

The proposed system utilizes an evaporator to remove the excess water. After cooling, the residue from this evaporator is passed through a filter, through the deionizing bed, and then returned to the reactor moderator region.

The blowdown being at 510°F will be partially flashed on expanding to atmospheric pressure in the evaporator. The evaporator is surmounted by a small demister supplied with a cold finger to produce approximately 10% reflux, thus eliminating the possibility of acid carryover in the steam. Steam issuing from the demister is then passed directly to the feedwater heaters of the power cycle. Condensate from the evaporator coils is also usable in the feedwater heaters.

A materials flow diagram of the system is shown in Figure 35, and heat flow is shown in Figure 36. Calculations by which the data were obtained are given in appendices A and B. Equipment sizing is shown in Appendix C.

CLEANUP SYSTEM MATERIALS FLOW

FIGURE 35

CLEANUP SYSTEM HEAT FLOW

FIGURE 36

VI. THERMAL DESIGN OF FUEL ASSEMBLIES

Certain data pertinent to the thermal design of the VMR fuel assemblies are tabulated in Tables IX and X.

The "hot spot" factors shown in Table IX are based upon preliminary calculations and are subject to change as the design progresses. The fuel pin tolerances shown in Table X are those tolerances which can be obtained from fuel fabricators without severe penalty to the cost of the fuel assemblies.

Calculations of fuel pin temperature distribution have been made for the "maximum gap" and "minimum gap" cases shown in Table X. The results of these calculations are shown in Figure 37. Two conservative assumptions were made in the calculations. They are listed below.

- 1) The thermal conductivity of the UO_2 was assumed to be invariant at 1.0 Btu/hr-ft-°F.
- 2) The thermal conductivity of the gap was assumed to correspond to a 5/1 mixture of Xe and Kr with the following properties:
 k (Btu/hr-°F-ft) = 0.0065 at 600°F
 = 0.008 at 900°F
 = 0.0095 at 1200°F

The diametral gaps shown in Table IX correspond to the oxide-to-cladding clearances with the fuel pin in the cold condition. The actual clearances used in the calculations of the temperature distribution were determined for the hot condition of the fuel pins by an iterative process, balancing temperature distribution against gap thickness.

TABLE IX

FUEL ASSEMBLY THERMAL DESIGN DATA

Power per core, tmw (102% of rated power)	77.5
No. of fuel pins/fuel assembly	39
No. of fuel assemblies/core	151
Fuel pin cladding material	Stainless steel
Fuel active length, inches	60
"Hot Spot" factors	
Eccentricity, gap variation, spacers, etc.	1.32
Large cell neutron flux (local flux factor)	1.30
Axial neutron flux	1.50
Radial neutron flux	<u>1.40</u>
Total "hot spot" factor	3.60
Average power density, kw per foot of fuel pin	2.78
Maximum power density, kw per foot of fuel pin	10.0
Average surface heat flux, Btu/hr-sq ft	103,000
Maximum surface heat flux, Btu/hr-sq ft	370,000
Estimated burnout heat flux, Btu/hr-sq ft	1,000,000

TABLE X

FUEL CLADDING AND PELLET DIMENSIONS
(Cold Dimensions, inch)

Pellet	Tube ID	Tube Wall Thickness	Tube OD
Nominal D = 0.3200	Max. pellet = 0.3203	Nominal wall = 0.014	Max. tube ID = 0.3248
+ Tolerance = 0.0003	Min. gap = 0.0030	+ Tolerance = 0.001	2 × max. wall = 0.030
Max. D = 0.3203	Min. tube ID = 0.3233	Max. wall = 0.015	Max. tube OD = 0.3548
	Tolerance = 0.00075		
Nominal D = 0.3200	Nominal tube ID = 0.32405	Nominal wall = 0.014	Min. tube ID = 0.3233
- Tolerance = 0.0003	Tolerance = 0.00075	- Tolerance = 0.001	2 × min. wall = 0.026
Min. D = 0.3197	Max. tube ID = 0.3248	Min. wall = 0.013	Min. tube OD = 0.3493
	Min. pellet = 0.3197		
	Max. gap = 0.0051		
			Nominal tube OD = $\frac{0.3493 + 0.3548}{2} = 0.352$

TOLERANCE DIMENSIONS

NOMINAL DIMENSIONS

TEMPERATURE DISTRIBUTION IN
HOTTEST FUEL PIN FOR MAXIMUM
DIAMETRAL GAP

TEMPERATURE DISTRIBUTION IN
HOTTEST FUEL PIN FOR MINIMUM
DIAMETRAL GAP

TEMPERATURE DISTRIBUTION IN VMR FUEL PIN

FIGURE 37

463 101

VII. CRITICAL EXPERIMENT

A. Initial Critical Experiments

Table XI contains a description of the proposed critical experiment configurations, and indicates the present status of these experiments. To date two core configurations have been tested in critical experiments. The first of these (case 104) did not become critical, but did achieve k_{eff} of 0.99 or more with the moderator and coolant levels at the top of the active portion of the fuel pins. Measurements were made on this core of k_{eff} versus height of coolant and moderator for four coolant-moderator heights. For heights of 48, 46, 40 and 30 inches, the assembly was within approximately \$0.40, \$0.60, \$1.80 and \$5.50, respectively, of being critical.

Calculated critical height for this assembly was 29 inches. Thus, it appears that PUREE' results are approximately 3% k_{eff} greater than measured.

The second assembly (case 120) became critical with water moderator and coolant at a height of 27 inches. Detailed measurements of flux distributions; and of cadmium ratios for fission in U 235 and U 238, and capture in U 238 have been completed; but analysis of the results has not yet been completed. The assembly did not go critical with furfuryl alcohol or furfural in the coolant region. Attempts are being made to measure flux distributions in an assembly in which the seven inner elements contain furfuryl alcohol and the outer 28 contain water.

Moderator and coolant reactivity worths per inch of height were measured at the just-critical heights of the water coolant assembly, giving results for the moderator of \$0.144/in., for the coolant \$0.216/in., and for both simultaneously of \$0.335/in.

The calculations performed so far on the critical experiments have assumed that the thermal neutron spectrum is essentially the same in all regions of the reactor. Checks of this method against results of other critical experiments in which the moderator and fuel are more homogeneous than in the VMR experiments (i.e. in which the moderator was not separated into coolant and moderator regions) have been reasonably successful, (within approximately 1% k_{eff}). For some VMR reactor cores, calculations have been made assuming two different spectra in the cell, that in the fuel coolant region being a Wigner-Wilkins spectrum, and that in the moderator region being a Maxwellian. When the two spectra are used, the result is a decrease in thermal utilization (consequently in k_{eff}) of up to 3%

TABLE XI

PROPOSED CRITICAL EXPERIMENT CONFIGURATIONS

Case No.	Has Been Run	Will Be Run	Did Not Go Critical	Deleted	Config.	Element			Assembly		Moderator		Liquids	
						Shroud ID (in.)	Pin Pitch	No. of Pins	Element Pitch (in.)	No. of Elements	Volume C/UO ₂	Ratios M/UO ₂	Moderator	Coolant*
104			x		1	4.944	.633	61	5.95	37	1.2	1.05	H ₂ O	H ₂ O
105				x	"	"	"	"	"	"	"	"	"	FA
106				x	"	"	"	"	"	"	"	"	"	F
107		x			2	5.490	.703	61	6.34	37	1.8	0.95	H ₂ O	H ₂ O
108		x			"	"	"	"	"	"	"	"	"	FA
109		x			"	"	"	"	"	"	"	"	"	F
120	x				3	4.944	.633	61	5.54	37	1.2	0.55	H ₂ O	H ₂ O
121			x		"	"	"	"	"	"	"	"	"	FA
122			x		"	"	"	"	"	"	"	"	"	F
123		x			4	4.571	.582	61	5.54	37	0.8	0.95	H ₂ O	H ₂ O
124		x			"	"	"	"	"	"	"	"	"	FA
125		x			"	"	"	"	"	"	"	"	"	F
126		x			5	5.490	.703	61	5.95	37	1.8	0.45	H ₂ O	H ₂ O
127		x			"	"	"	"	"	"	"	"	"	FA
128		x			"	"	"	"	"	"	"	"	"	F
				x	6	4.944	.633	61	6.34	37	1.2	1.55	H ₂ O	H ₂ O
				x	"	"	"	"	"	"	"	"	"	FA
				x	"	"	"	"	"	"	"	"	"	F
				x	7	4.571	.582	61	5.95	37	0.8	1.45	H ₂ O	H ₂ O
				x	"	"	"	"	"	"	"	"	"	FA
				x	"	"	"	"	"	"	"	"	"	F
				x	8	4.571	.582	61	6.34	37	0.8	1.95	H ₂ O	H ₂ O
				x	"	"	"	"	"	"	"	"	"	FA
				x	"	"	"	"	"	"	"	"	"	F

* F = Furfural.

FA = Furfuryl alcohol.

compared with the result obtained by using only one spectrum. Currently, calculations of the experiments that have been run are being performed assuming the two spectra.

B. Current and Future Program

Recent calculations performed for VMR reactor cores have indicated that, for a fixed overall water-to- UO_2 ratio, configurations with relatively large coolant/ UO_2 ratios were found to be more reactive than had been previously estimated. When the first assembly (case 104) did not become critical, it was theorized that a more reactive configuration could be obtained if either the amount of water within the element could be increased or the amount exterior to the element decreased. Hence, in the next experiment, the same element was retained but the interelement spacing was decreased, thus decreasing the amount of moderator exterior to the element. Since the assembly went critical the calculated results were at least qualitatively supported. The implications of this effect are that the assemblies in which the shroud diameter is 4.94 inches or less will not go critical for element spacings of either 5.95 inches or 6.34 inches.

It has become apparent that in order to check the PUREE' code, a considerable effort should be applied to determining microscopic reactor parameters such as disadvantage factors and epithermal to thermal capture and fission ratios; and that an effort should be made to determine, if possible, the gross thermal spectrum differences between coolant-fuel and moderator regions. In view of this, and considering that the mechanical handling associated with changing of configurations requires more time than anticipated, it has been decided to perform a more detailed experimental program on those assemblies which have a high probability of being made critical. Table XI summarizes the eight proposed configurations of which one (configuration 3) has been partially investigated, and another (configuration 1) did not go critical. Two or three others (configurations 2, 4, and 5) will be investigated in detail, and the remaining ones (configurations 6, 7, and 8) are being deleted.

The measurements to be performed consist of two types, 1) reactivity and worth experiments, and 2) parameter measurements. The first of these consists of determination of critical heights and moderator and coolant worths for maximum coolant and moderator heights, and equal coolant and moderator heights; and if possible, sub-critical measurements of k_{eff} for several moderator heights. The parameter experiments consist of measurements of thermal flux distributions and regional average thermal fluxes; and of cadmium ratios for

403 104

thermal capture in U 238, and fission in U 235 and U 238; and also axial and radial power distributions. The thermal flux measurements will be performed in some cases with both manganese and natural uranium foils. In a few selected cases, plutonium foils will also be used. The objective of such duplicate measurements will be to determine, if possible, any differences in the thermal neutron spectrum between fuel-coolant and moderator regions.

Figures 38 through 41 are photographs of the VMR critical experiment facilities.

C. Deviations of the PUREE' Physics Code from Experimental Data

As mentioned in section VII.A, the data available from VMR critical experiments at this writing indicates that the predicted effective multiplication for case 104 was about 3% higher than the experimental value. Four approaches are presently being followed to determine the source or sources of this error.

1. As a portion of an ATL-sponsored project the method by which the VMR criticals are computed is being used to calculate parameters measured in a number of other critical experiments. Among these are the Yankee criticals in which the water-to- UO_2 ratio varies from 2.2 to 3.9, and in which a number of measures of spectrums, microscopic and macroscopic spatial flux, control and fuel rod worth, etc., have been experimentally determined. Among the measurements are gold foil transits of the flux distribution in the core and reflector. These measurements should provide a test of the ability of our model to predict the flux distribution in the vicinity of the moderator of the VMR. Other experiments providing similar data are those on metal and oxide lattices performed at Brookhaven and Westinghouse. There are, in addition, Yankee measurements on two-region cores and on lattices in which the fuel elements contain some plutonium. The critical experiments on the N. S. Savannah are also of value. The tentative conclusions from the calculations performed so far are that the analytical model yields excellent agreement with Yankee, Brookhaven, and Westinghouse experiments, and that the source of the error appears to be a phenomenon which is emphasized in, and is unique to, the heterogeneous VMR core. Table XII indicates some of the results obtained in this study.

2. In another ATL-sponsored project, the effects on multiplication of some special phenomena are being estimated by approximate methods. These effects include the changes in the thermal neutron spectrum and hence in effective thermal microscopic cross sections between regions of different average composition. The application to the VMR is in analysis

TABLE XII
RESULTS OF COMPARISON OF CALCULATIONS
WITH YANKEE CRITICAL EXPERIMENTS

Experiment		Data			
Parameter	Water-to-Uranium Ratio Utilized	Experimental Value	Calculated Value	(Calculated Value-Experimental Value) Experimental Error	Discrepancy (%)
Fission Cadmium Ratio Minus 1 (2.7% Foil) in Fuel Region	2.2	3.00 ± 0.04	3.08	+2.0	+ 3
	2.9	3.7 ± 0.4	4.12	+1.1	+11
Fission Cadmium Ratio Minus 1 (2.7% Foil) in Coolant	2.2	3.44 ± 0.04	3.50	+1.0	+ 2
	2.9	4.3 ± 0.4	4.68	+0.9	+ 9
U 238 Capture Cadmium Ratio Minus 1 in Fuel Region	2.2	0.35 ± 0.026	0.21	-5.8	-42
	3.9	0.55 ± 0.05	0.38	-3.4	-31
Gold Capture Cadmium Ratio Minus 1 in Coolant Region	2.2*	0.8*	0.8*	--	--
	2.9				
ϕ_w/ϕ_F (U 235-Al Foils)	2.2	1.100 ± 0.02	1.113	+0.6	+ 1.2
	2.9	1.111 ± 0.02	1.117	+0.35	+ 0.5
	3.9	1.127 ± 0.01	1.123	-0.4	- 0.4
k_{eff}	2.2	1.00	1.023	--	+ 2.3
	2.9	1.00	1.012	--	+ 1.2
	3.9	1.00	1.004	--	+ 0.4
Peripheral Fuel Rod Worth	2.2	3.3×10^{-5}	3.35×10^{-5}	+0.5	+ 1.5
Height of Reflector Flux Peak	2.2	515	624	--	+ 22

* Used to determine epithermal gold cross section.

47

VMR CRITICAL EXPERIMENT CORE SHOWING
PARTIALLY WITHDRAWN FUEL ELEMENT

FIGURE 38

463 107

ADVANCED *Technology* LABORATORIES
A DIVISION OF AMERICAN Standard

PT

VMR CRITICAL ASSEMBLY CORE WITH
LIQUID LEVEL MEASURING DEVICES MOUNTED ON TANK WALL

FIGURE 39

463
108

CONTROL ROD DRIVE MECHANISM

FIGURE 40

SPLIT FUEL PINS,
TWO ASSEMBLED WITH
CADMIUM COVERS OVER JOINT

FIGURE 41

of the difference of thermal spectrum between the moderator region and the fuel-coolant region. Calculations performed so far indicate that this effect probably causes a decrease in multiplication of between 1 and 3% in a typical VMR design. Another phenomenon being investigated is the spatial variation in the intensity of the source into the thermal group due to the analogous variation in the epithermal flux. Again the application to the VMR is in the difference between moderator and coolant and the indications are that this may cause a decrease in k of between 1/4 and 1% for a VMR lattice.

Other effects which will be studied as part of this project include the approximations made in obtaining effective cylindrical cells for our microscopic calculations on hexagonal lattices.

Some investigations of the effect of local variation of void density in the horizontal plane and of the effect of the difference between the binding energy of hydrogen in water and in organics are also being made.

3. Another approach to the discrepancy in case 104 is a direct examination of the nuclear data and the model used. A possible improvement in reflector (pure water) cross sections is presently being examined, and the resonance integrals of U 238 and U 235 fission and capture will again be compared with existing data. The oxygen cross sections also are open to question.

4. The last approach is the obvious one of performing calculations on the additional VMR critical experiment data in as much detail as possible. Such calculations on cases 120 and 121 are presently being made.

ATL

APPENDIX A

MATERIALS BALANCE

1. Blowdown

1045.5 l

Assume 18-8 SS; $2.3 \times 10^{-3} \text{ lb} = 1.043 \text{ g}$

$$0.18 \times 1.043 = 0.187 \text{ g Cr}^{+++}$$

$$0.08 \times 1.043 = 0.083 \text{ g Ni}^{++}$$

$$0.74 \times 1.043 = 0.773 \text{ g Fe}^{++}$$

$$\frac{0.187 \times 3}{52.01} = 0.0108 \text{ eq Cr}$$

$$\frac{0.083 \times 2}{58.71} = 0.0028 \text{ eq Ni}$$

$$\frac{0.773 \times 2}{55.85} = 0.0277 \text{ eq Fe}$$

$$= 0.0413 \text{ eq M}^+$$

$$\frac{2091}{61.84} = 33.8131 \text{ eq H}_3\text{BO}_3$$

$$K = 5.8 \times 10^{-10} \text{ (International Critical Tables)}$$

$$\text{at } 2\text{g/l, } M = \frac{2}{61.84} = 0.0323$$

33.8131 eq Total Boric Acid

0.0413 eq H₂BO₃⁻ due to M⁺

33.7718 eq

First Approximation

$$\frac{(\text{H}^+) (\text{H}_2\text{BO}_3^-)}{(\text{H}_3\text{BO}_3)} = 5.8 \times 10^{-10}$$

$$(\text{H}^+) (\text{H}_2\text{BO}_3^-) = 5.8 \times 10^{-10} \times 0.0323 = 18.7 \times 10^{-12}$$

$$(\text{H}^+) = (\text{H}_2\text{BO}_3^-) = \sqrt{18.7 \times 10^{-12}} = 4.33 \times 10^{-6}$$

$$4.33 \times 10^{-6} \times 1045.5 = 0.0045 \text{ eq H}_2\text{BO}_3^- \text{ due to ionization}$$

0.0413 eq H₂BO₃⁻ from neutralization

0.0045 eq from ionization

0.0458 eq H₂BO₃⁻ Total First Approximation

Note: (H⁺) indicates Hydrogen ion concentration.

H⁺ indicates Total Hydrogen ion in system.

ATL

$$\frac{0.0458}{1045.5} = 4.38 \times 10^{-5} (\text{H}_2\text{BO}_3^-)$$

$$(\text{H}^+) = \frac{18.7 \times 10^{-12}}{4.38 \times 10^{-5}} = 4.27 \times 10^{-7}$$

Further approximations would give a pH of approximately 6

$$1045.5 \times 10^{-6} = 0.0010 \text{ H}^+$$

Summation

$$\text{M}^+ = 0.0413 \text{ eq}$$

$$\text{H}_3\text{BO}_3 = 33.8131 - (2 \times 0.0413) - (2 \times 0.0010) = 33.7285 \text{ eq}$$

$$\text{H}^+ = 0.0010 \text{ eq}$$

$$\text{H}_2\text{BO}_3^- = 0.0423 \text{ eq}$$

2. Distillate - Neglect Acid Carryover

$$2000 \text{ lb H}_2\text{O}$$

3. Bottoms to Cation Exchanger (De-ionizer)

$$2300 \text{ lb} - 2000 \text{ lb} = 300 \text{ lb H}_2\text{O} = 136.38 \text{ l}$$

$$\text{M} = \frac{2300}{300} \times 0.0323 = 0.248$$

First Approximation

$$(\text{H}_2\text{BO}_3^-) = \frac{0.0423}{136.38} = 3.10 \times 10^{-4}$$

$$(\text{H}_3\text{BO}_3) = \frac{33.77}{136.38} = 0.2476$$

$$(\text{H}^+) = \frac{5.8 \times 10^{-10} \times 0.2476}{3.1 \times 10^{-4}} = 4.63 \times 10^{-7}$$

Summation

$$\text{pH} = \text{approximately } 6.5$$

$$\text{H}^+ = 4.63 \times 10^{-7} \times 136.38 = 0.0001 \text{ eq}$$

$$\text{H}_3\text{BO}_3 = 33.8131 - (2 \times 0.0413) - (2 \times 0.0001) = 33.7654 \text{ eq}$$

$$\text{H}_2\text{BO}_3^- = 0.0414 \text{ eq}$$

$$\text{M}^+ = 0.0413 \text{ eq}$$

4. Effluent from Cation Exchanger (De-ionizer)

$$300 \text{ lb H}_2\text{O} = 136.38 \text{ l}$$

$$\text{M}^+ = 0.0000 \text{ eq}$$

$$\text{H}_3\text{BO}_3 + \text{H}_2\text{BO}_3^- + \text{H}^+ = 33.8131 \text{ eq}$$

$$\text{M} = 0.248$$

$$(\text{H}^+) (\text{H}_2\text{BO}_3^-) = 5.8 \times 10^{-10} \times 0.248 = 1.438 \times 10^{-10}$$

$$(\text{H}^+) = (\text{H}_2\text{BO}_3^-) = \sqrt{1.438 \times 10^{-10}} = 1.2 \times 10^{-5}$$

pH = approximately 5

$$\text{H}^+ = \text{H}_2\text{BO}_3^- = 1.2 \times 10^{-5} \times 136.38 = 163.66 \times 10^{-5} = 0.0016 \text{ eq}$$

Resin bed must accommodate flow of 300 lb/hr = 0.6 gpm, and must absorb 0.0413 eq/hr

Resin consumption at 50 eq/cf

$$\frac{0.0413}{50} = 0.000826 \text{ cf/hr} = 0.0198 \text{ cf/d} = 7.24 \text{ cf/yr}$$

Use 6" pipe = 5 ft/cf

Use 1 cf beds and change 8 times/yr

$$\text{Flow rate} = \frac{0.6}{0.19635} = 3 \text{ gpm/ft}^2$$

APPENDIX B

HEAT BALANCE

Blowdown	2300 lb/hr, $h_f = 500$ Btu/lb	
	$2300 \times 500 =$	+ 1,150,000 Btu/hr
Steam out	2000 lb/hr, $h_g = 1150$ Btu/lb	
	$2000 \times 1150.4 =$	- 2,300,800
Reflux	10%	- 230,080
Bottoms out	300 lb/hr, $h_f = 180$ Btu/lb	
	$300 \times 180 =$	- 54,000
Losses	3%	- <u>46,000</u>
Required for evaporation		+ 1,480,880 Btu/hr
Steam available 1000 psia dry and saturated		
	$h_g = 1192$ Btu/lb	
	$h_f = 542$	
	$h_v = 650$	
	$1,480,880/650 = 2278$ lb/hr	
	$2278 \times 1192 =$	+ 2,715,400
	$2278 \times 542 =$	- <u>1,234,520</u>
		+ 1,480,880 Btu/hr

APPENDIX C

EQUIPMENT SIZES

1. Evaporator Surface

Assume $U = 300$

$$\Delta T = 544 - 212 = 332^{\circ}\text{F}$$

$$A = \frac{q}{U\Delta T} = \frac{1,480,880}{300 \times 332} = 15 \text{ ft}^2$$

2. Tower

Assume 3 plates or equivalent packing

3. Cooling Finger

10% reflux of 2000 lb H_2O , $h_g = 1150$

$$2000 \times 1150 = 2,300,800 \text{ Btu}$$

Assume $U = 300$

Assume cooling H_2O at $75^{\circ} \rightarrow 154^{\circ}$

$$\Delta T_1 = 137^{\circ}, \Delta T_2 = 58^{\circ}, \text{lm } \Delta T = 92^{\circ}\text{F}$$

$$A = \frac{2,300,800 \times .1}{300 \times 92} = 8.4 \text{ ft}^2$$

$$\text{H}_2\text{O required} = 230,080/79 = 2920 \text{ lb/hr}$$

4. Bottoms Cooler

300 lb/hr H_2O from 212°F to 100°F

$$h_{f1} = 180$$

$$h_{f2} = 69$$

$$\Delta h_f = 111$$

$$300 \times 111 = 33,300 \text{ Btu}$$

Assume cooling H_2O at $75^{\circ} \rightarrow 90^{\circ}$

$$\left. \begin{array}{l} \Delta T_1 = 137^{\circ}\text{F} \\ \Delta T_2 = 10^{\circ}\text{F} \end{array} \right\} \text{lm } \Delta T = 49^{\circ}\text{F}$$

Assume $U = 200$

$$A = \frac{33,300}{200 \times 49} = 3.4 \text{ ft}^2$$

$$\text{H}_2\text{O required} = \frac{33,300}{15} = 2220 \text{ lb/hr}$$

5. Filter

$$\frac{300}{8.345 \times 60} = 0.6 \text{ gpm}$$

6. Feed Water Pump

0.6 gpm - 1000 psia delivery

7. Deionizer Feed Pump

0.6 gpm 20 lb head

ATL

DISTRIBUTION

	No. of Copies
Chief, Water Reactors Branch U. S. Atomic Energy Commission	8
Contracts Division San Francisco Operations Office Attn: Walter H. Brummett Jr., Director	2
Technical Services Division San Francisco Operations Office Attn: R. W. Hughey, Director	1
Battelle Memorial Institute Attn: David Dingee, Supervisor Critical Assembly Lab.	1
Chicago Operations Office U. S. Atomic Energy Commission Attn: Claude Pursell	1
Technical Information Service Extension U. S. Atomic Energy Commission	2*
ATL Files	39

* Plus offset duplicator masters.

DO NOT REPRODUCE