Volume II

MASTER

1000 MWE CLOSED CYCLE WATER REACTOR STUDY

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Prepared for
DIVISION OF REACTOR DEVELOPMENT
U.S. ATOMIC ENERGY COMMISSION

Contract Number AT(30-1)-3070

With New York Operations Office, U.S. AEC

March 1963

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GENERAL CONTENTS

Section Blue Tab

Volume I

I. Introduction	I
II. Summary	II
III. Conclusions	III
IV. Reference Plant Design	IV
V. Alternate Core Designs	V
VI. Power Generation Costs	VI

Volume II

I. Nuclear Evaluation of 1000 MWe Pressurized Water Reactor Core	I
II. Reactor Vessel Feasibility Study	II
III. Minimum Site Requirements	III
IV. Recommended Areas for Continued Study	IV
V. Proposed Research and Development Program	V

VOLUME II

TABLE OF CONTENTS

Description	Blue Tab
I. Nuclear Evaluation of 1000 MWe Pressurized Water	I
Reactor Core	
1. Objectives	Page I-1
2. Nuclear Hot Channel Factors Used in Study	I-1
3. Effect of Core Height, Enthalpy Rise and	I-2
Burnup on the Axial Power Distribution	
4. Study of Power Sensitivity and Code Convergence . .	I-7
Problems	
5. Power Oscillations	I-21
6. Fuel Cycle Studies	I-37
7. Acknowledgement	I-50
II. Reactor Vessel Feasibility Study	II
1. Introduction	Page II-1
2. Fabrication Feasibility of a 202" I.D. Reactor . . .	II-2
Vessel (Combustion Engineering, Inc. Report)	
3. Multi-Layer Construction	II-42
III. Minimum Site Requirements	III
1. General	Page III-1
2. Reactor Vessel Handling on Site	III-2
IV. Recommended Areas for Continued Study	IV
V. Proposed Research and Development Program	V
1. Program Administration	Page V-4
2. Core Physics Evaluation	V-5
3. Reactor Core Development	V-14
4. Chemical Development	V-26
5. Reactor Plant Development	V-28

FIGURES

Figure No.	Title	Follows Page
SECTION I		
I-1	Variation in Axial Power Distribution with Burnup	I-4
I-2	Variation in Axial Power Distribution with Burnup. No partially inserted control rods.	I-4
I-3	Variation in Axial Peaking Factor with Burnup	I-4
I-4	Variation in Radial Power Distribution with Burnup; Uniform Batch Core	I-4
I-5	Variation in Axial Peaking Factor with Burnup. Control Rods in Top 25% of Core ($\Delta h = 40$ BTU/lb)	I-4
I-6	Variation in Axial Peaking Factor with Burnup, Control Rods in Top 25% of Core ($\Delta h = 80$ BTU/lb)	I-4
I-7	Variation in Axial Peaking Factor with Burnup, Control Rods in Top 25% of Core	I-6
I-8	Variation in Axial Power Distribution with Soluble Poison	I-6
I-9	Axial Power Distribution Dependence on Poison Variation	I-8
I-10	Variation in Axial Peaking Factor with Poison Removal	I-10
I-11	Variation in Axial Peaking Factor with Control Rod Withdrawal	I-10
I-12	Axial Power Distributions in 1000 MWe PWR	I-10
I-13(a)	Illustration of Power Peak Sensitivity for Reactivity Transfer	I-10
I-13(b)	Reduced Variables Used in Power Peak Sensitivity Analysis	I-10
I-14	Illustration of Power Peak Sensitivity when Rod Bank is Withdrawn	I-16
I-15	Function Used in Xe Stability Analysis	I-24
I-16	Reactivity Coefficient at Threshold for Sustained Xe Oscillations	I-26

Figure No.	Title	Follows Page
I-17	Study of Partially Damped Xe Oscillations	I-26
I-18	Study of Partially Damped Xe Oscillations	I-26
I-19	Radial Power Distribution Uniform Batch Loading	I-38
I-20	Radial Power Distribution Out-in 3 Region - 3 Cycle Core	I-38
I-21	Radial Power Distribution Out-in 5 Region - 5 Cycle Core	I-40
I-22	Radial Power Distribution 3 Region 3-1-2 Cycle Core	I-40
I-23	Radial Power Distribution Roundelay 3 Batch Core	I-40
I-24	Radial Power Distribution Roundelay 5 Batch Core	I-40
I-25	Core Layout for 264 Element Core	I-44
I-26	Radial Power Distribution in 264 Element Core	I-44
I-27	Core Layout for 265 Element Core	I-44
I-28	Radial Power Distribution in 265 Element Core	I-46
I-29	Radial Power Distribution in 265 Element Core	I-46
I-30	Radial Power Distribution in 265 Element Core	I-46
I-31	PDQ Power Distribution in 265 Element Core	I-46
I-32	PDQ Power Distribution in 265 Element Core	I-46
I-33	Quarter Core Layout for 673 Element Core	I-48
I-34	Radial Power Distribution in 673 Element Core	I-48
I-35	Radial Power Distribution in 673 Element Core	I-48
I-36	Radial Power Distribution in 673 Element Core	I-48
I-37	PDQ Power Distribution in 673 Element Core	I-48
I-38	PDQ Power Distribution in 673 Element Core	I-48
I-39	PDQ Power Distribution in 673 Element Core	I-48

SECTION III

III-1	Design Cooling Water Temperature vs. Total Condenser Surface Area & Cooling Water Required	III-2
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Figure No.	Title	Follows Page
III-2	Reactor Installation	III-4
SECTION V		
V-1	Research and Development Program Schedule	V-2
V-2	Research and Development Program Cost Per Period	V-4
V-3	Cumulative Cost of Research and Development Program	V-4

TABLES

Table	Title	Page
I-1	Effect of Core Height, Enthalpy Rise and Burnup on the Axial Power Distribution	I-5
I-2	Parameters Needed to Calculate Flux Distribution	I-12
I-3	Reactivity Transfer Required to Shift the Flux Distribution	I-14
I-4	Reactivity Transfer Required in Small Region to Shift the Flux Distribution	I-16
I-5	Comparison of Various Radial Calculations for an Equilibrium Roundelay Fuel Cycle	I-20
I-6	Flux Threshold for Sustained Xe Oscillations if Temperature Coefficient is Zero	I-24
I-7	Margin of Safety to Prevent Sustained Oscillations	I-25
I-8	Minimum Damping for Xenon Oscillations etc.	I-27
I-9	Characteristics of Different Refueling Methods	I-41
I-10	Equilibrium Cycles in 265 Element Core	I-44
I-11	Discharge Burnup for the Equilibrium Cycles	I-45
I-12	Equilibrium Cycles in 673 Element Core	I-47
I-13	Effects of Xenon and Doppler on Radial Power Distribution	I-48
I-14	Approach to Equilibrium Roundelay Cycle	I-50
V-1	Total Research and Development Program Cost for 1000 MWe PWR	V-2

SECTION I - NUCLEAR EVALUATION OF 1000 MWE PRESSURIZED WATER REACTOR CORE

1. OBJECTIVES

The nuclear evaluation has been conducted for the purpose of studying those problem areas which are expected to increase in severity as the core size is increased to produce 1000 MWe. The main problems anticipated in the very large core are power distribution problems and instabilities.

The objective of this study is not to solve the problems associated with building a 1000 MWe PWR, but rather to define the problem areas and to demonstrate the possibility and reasonable probability of finding solutions to those problems through an R & D program. The proposed R & D program is included in Section V of this volume.

2. NUCLEAR HOT CHANNEL FACTORS USED IN STUDY

During a short study program, the nuclear evaluation must obviously be conducted concurrent with the thermal, hydraulic, and mechanical design studies. Consequently, the hot channel factors must be specified near the beginning of the study and without benefit of the nuclear analysis.

The hot channel factors specified were intentionally somewhat optimistic (compared to present-day technology) since the 1000 MWe PWR is presumed to be in operation not sooner than 1970. Considerable research and development on PWR's is anticipated before that time, for both smaller reactors and the 1000 MWe PWR.

The value of $F_{\Delta H}$ nuclear (ratio of maximum enthalpy rise to average enthalpy rise) was taken to be 1.50 multiplied by:

- (a) a local peaking factor to account for the presence of control rod followers, water slots, etc., and
- (b) a transient hot channel factor corresponding to the rod motion required for a 5% step at 95% power.

With Zircaloy followers, the local peaking factor was 1.15. For the design with short followers, the local peaking factor was taken from PDQ calculations. The transient hot channel factor was taken to be 1.02.

The axial hot channel factor (F_A) was 1.60, defined as the ratio of maximum to average axial power density in the hottest element.

The hot channel factors are based on the following assumptions:

- 1. Improvements in instrumentation will be made between now and 1970 so that in-core instrumentation can detect flux tilts in the time period required.

2. Research and development programs conducted before 1970 will result in improvement in the steady state nuclear hot channel factors.
3. The 1000 MWe PWR is a base load plant in that the system places no requirements on the method or time required to reach full power.

3. EFFECT OF CORE HEIGHT, ENTHALPY RISE AND BURNUP ON THE AXIAL POWER DISTRIBUTION

In designing very large reactor cores, it is important to understand the dependence of power distribution on core height, enthalpy rise, and burnup. To illustrate this point, consider the following hypothetical situation. Suppose a reactor with a core height of 10 feet has been designed to produce a given power output. If a new reactor design is required which has a 10% higher output, one might propose an increase in the core height to 11 feet, maintaining the same average power density. However, the axial hot channel factor might be greater in the 11 ft core; if the increase in hot channel factor is 10%, the power capability is not increased at all in going from a 10 ft to an 11 ft core, assuming a burnout heat flux limitation. The power distribution might be expected to become worse as the enthalpy rise is increased since the water density becomes more non-uniform.

The purpose of this study is to investigate effects such as those mentioned to determine what restraints must be placed on the core design due to nuclear effects. In studying the effects of core height, enthalpy rise, and burnup on the axial power distribution, the basic tool used was the CNCR Code, which is a modification of the CANDLE Code. CNCR is a one-dimensional diffusion theory code which treats burnup effects using four neutron energy groups. The non-uniform water density along the axis is calculated by the code by performing iterations on water density and power until a satisfactory convergence is obtained. The code performs this calculation based on input parameters consisting of the inlet and exit enthalpy and a correlation of water density versus enthalpy. The converged water density distribution is calculated after each burnup step, consequently changes in water density with burnup are taken in account.

One of the more interesting features of the code is a criticality search option in which criticality is obtained by adding a poison which has a macroscopic cross section everywhere proportional to the water density. This permits an excellent representation of chemical shim reactors. Another feature of the code is the pointwise correction for the Doppler effect. The resonance absorption at each point is corrected according to an input correlation which is dependent on the power. When the code iterates to determine the converged water density distribution, it simultaneously converges on the Doppler effect. The effect of partially or wholly inserted control rods can be represented by the addition of a poison cross section in the region of the core in which rods are presumed.

From the above considerations, it is seen that the CNCR code accounts for most of the effects which determine the axial power distribution. It has however certain limitations. Since it is a one-dimensional code, separability in the axial and radial directions is presumed. Since only the average channel is investigated, it is assumed that the shape of the axial power distribution is similar throughout the core and is determined by the average conditions. (The magnitude of the power will, of course, vary at different radial locations; only

the shape is presumed to be the same.) These limitations could only be eliminated by using a two-dimensional code similar to CNCR. Such a code does not now exist; even if it did exist the running time would be prohibitive for the type of survey calculations required in this study.

For purposes of this study, a reference core was selected which is considered representative of present PWR technology. A UO_2 pellet diameter of 0.375 inches was used. The stainless steel to UO_2 volume ratio was 0.235 and the H_2O to UO_2 ratio was 1.67. A core pressure of 2200 psia and an average water temperature of 575°F were used. The thermal rating of the core was 3220 MW and the power density was approximately 87 KW/liter of core. An enrichment of 3.4 a/o was arbitrarily used. These parameters do not represent a final reference design for the 1000 MWe PWR; the final reference design is specified in Section IV of Volume 1. The parameters were selected such that normal changes in any of them will exert only a minor effect on the conclusions of this present study.

The variable parameters in this study were enthalpy rise, core height and burnup. The enthalpy rise was considered in the range of 40 to 80 Btu/lb. The active core height was varied from 9 feet to 15 feet. Although the discharge burnup will probably be in excess of 20,000 megawatt days per metric ton of uranium (MWD/MTU), this study concentrated on relatively low burnups (less than 10,000 MWD/MTU). This is because the power distribution tends to burn to a flat condition after a few thousand MWD/MTU and the power distribution then becomes of little concern.

In actual operation, a chemical shim controlled reactor would maintain several control rods (having a total worth of a few percent if fully inserted) partially inserted so that transients can be controlled. Therefore, the cases normally considered in this study assumed that a bank of control rods worth 3% (if fully inserted) were inserted in the top 25% of the core. The actual worth of these rods in the clean core was approximately 0.3%. This rod bank distorted the power distribution and resulted in a more realistic condition.

In addition to the partially inserted rod bank, a uniform poison was distributed throughout the core in order to reduce k for the hot clean core to ≈ 1.10 . By using a special poison search, k was then reduced to unity at all times by a poison proportional to water density (to simulate chemical control). The worth of this poison varies from 10% in the hot clean core to zero at the end of life. Since the nonuniform poison affects the power distribution, it is necessary to use a realistic amount of this poison. With $k = 1.10$ in the hot clean core, k goes to unity and the nonuniform poison is entirely removed after approximately 7000 MWD/MTU. This is a typical burnup for one cycle if a three cycle core is presumed to result in discharge burnups in excess of 20,000 MWD/MTU.

The CNCR code maintains a constant flux throughout a burnup time step. If long time steps are used, the results will be inaccurate since the flattening effect of burnup during the time step will be neglected. Preliminary studies indicated a burnup time step of 1000 MWD/MTU was satisfactory. This value was maintained throughout the studies except that a 100 hour time step was used initially to determine the Xe effect. The preliminary study also indicated that one iteration on water density between each burnup step is satisfactory. Normally the change in the water density distribution with burnup is slight.

In Figure I-1 the axial power distribution is shown for various burnups for the 12 ft core with $\Delta h = 60$ Btu/lb. Partially inserted control rods appear in

the top quarter of the core. Figure I-2 represents a similar problem except that no control rods are partially inserted. The increase in peaking due to the control rods is evident. The maximum to average values for these two cases are plotted in Figure I-3 as a function of burnup.

An interesting item in Figures I-1 and I-2 is the fact that after several thousand MWD/MTU the power distribution develops a double peak -- a saddle effect with a dip near the center. This double peak was observed for all cases, regardless of core height, enthalpy rise, or presence of the partially inserted control rods. This effect can also be observed in the radial direction in a uniformly loaded batch core. Figure I-4 presents the results of a radial burnup study for a core diameter of 13.6 ft (corresponding to a 1000 MWe reactor with a 9 ft active height). After several thousand MWD/MTU the power is depressed at the center as compared to a region toward the outside.

A careful analysis of this phenomenon indicates it can be explained by the shape of the k vs. burnup curve. The value of $\Delta k/\Delta$ Burnup increases with burnup to approximately 5000 MWD/MTU. After a few burnup steps, the accumulated burnup is maximum near the center of the core. The change in the "local k " with burnup will then be greater at that point and a dip in the flux and power at that point is possible. The double peak becomes more predominate for large cores. As core height increases, the double peak occurs at lower burnups. The double peaks may not result in any major problems since, ordinarily, when the double peak is formed due to burnup effects, both peaks are lower than the single peak which existed at a lower burnup. However, if instabilities are proved to be a problem, the double peaks could tend to increase the severity of an axial power oscillation since a small reactivity effect could shift the power peak from one end of the core to the other.

To evaluate the dependence of the power distribution on enthalpy rise and core size, studies were made of the 9 ft core with $\Delta h = 40$ and 80 Btu/lb; the 12 ft core with $\Delta h = 60$ Btu/lb; and the 15 ft core with $\Delta h = 40$ and 80 Btu/lb. The changes between these cases appear to be small enough so that interpolation for the intermediate cases should be valid. In all cases a bank of control rods was partially inserted. The results of this analysis are quite dependent on burnup. The maximum-to-average values for several burnups are presented in Table I-1.

In Figure I-5, the ratio of maximum-to-average power is plotted as a function of burnup for the 9 and 15 ft cores with $\Delta h = 40$ Btu/lb. Figure I-6 shows the same case for $\Delta h = 80$ Btu/lb. In these cases, both peaks are plotted whenever the double peak occurs. The peak in the bottom of the core predominates until approximately 6000 MWD/MTU, when the upper peak becomes limiting.

Figures I-5 and I-6 indicate the power distribution in the larger cores is slightly worse for low burnup. However, for burnups greater than approximately 3000 MWD/MTU, the power distribution is actually better in the larger cores. If the axial power distribution is determined by the average burnup in the core, the beginning of the equilibrium cycle would have a burnup in the range of 5-10 thousand MWD/MTU (depending on discharge burnup). For burnups of this order, Figures I-5 and I-6 indicate the core size is not limited by the steady state axial power distribution. In the first core (with zero burnup), the axial power distribution is expected to be somewhat worse for the larger cores. For this case the calculations indicate the power peak increases by 10% without Xe and 5.5% with Xe in going from a 9 ft to a 15 ft core.

VARIATION IN AXIAL POWER DISTRIBUTION WITH BURNUP
CONTROL RODS IN TOP 25% OF CORE

FIG. I-1

VARIAION IN AXIAL POWER DISTRIBUTION WITH BURNUP
NO PARTIALLY INSERTED CONTROL RODS

FIG. I - 2

VARIATION IN AXIAL PEAKING FACTOR WITH BURNUP
FIG. I-3

VARIATION IN RADIAL POWER DISTRIBUTION
WITH BURNUP; UNIFORM BATCH CORE

FIG. I - 4

VARIATION IN AXIAL PEAKING FACTOR WITH BURNUP
CONTROL RODS IN TOP 25% OF CORE

FIG. I - 5

E. D. SK. 301434-B

VARIATION IN AXIAL PEAKING FACTOR WITH BURNUP
CONTROL RODS IN TOP 25% OF CORE

FIG. I - 6

E. D. SK. 301443-B

Table I-1

**EFFECTS OF CORE HEIGHT, ENTHALPY RISE, AND
BURNUP ON THE AXIAL POWER DISTRIBUTION IN
1000 MWE PWR**

A. 0 MWD/MTU Burnup

Height (ft) —————→	9	12	15
Δh (Btu/lb)			
40	1.468		1.619
60		1.574	
80	1.465		1.622

B. 2000 MWD/MTU Burnup

40	1.368		1.361
60		1.376	
80	1.379		1.386

C. 5000 MWD/MTU Burnup

40	1.221		1.175
60		1.192	
80	1.230		1.197

In Figure I-7 the maximum-to-average power ratio is plotted for the 15 ft core with $\Delta h = 40$ and $\Delta h = 80$ Btu/lb. The dependence of power distribution on enthalpy rise is practically negligible in this range. The effect of increasing the average temperature in the core, thereby pushing the exit temperature nearer saturation, was also investigated. It was found that, for the high pressures considered (≈ 2000 psia), the power distribution is insensitive to the average temperature as long as the exit temperature remains below saturation.

The results reported thus far have been based on problems which started with zero burnup and extended to burnups typical of one equilibrium cycle. At this point the nonuniform poison (representing boron chemical shim) has decreased to essentially zero and the power distribution is quite flat. In actual operation, reactivity would be added to the core at this time by inserting some fresh fuel. Boron would be added to the moderator to control the excess reactivity. Since the boron is proportional to water density (which is nonuniform), the previously flat power distribution will become distorted. The nonuniform poison will shift the power toward the top of the core. To investigate this effect, the pointwise isotopic number densities after 7000 MWD/MTU were taken from a previous problem (this is easily accomplished by saving an output tape and

running a "continuation" problem). The uniform poison, which was previously added to reduce k to 1.10, was removed and replaced by a nonuniform poison proportional to the water density. The bank of partially inserted rods was retained. Figure I-8 shows the axial power distribution after 7000 MWD/MTU with and without the nonuniform poison in the 12 ft core. Each case has a converged Xe, Doppler and water density distribution. The power peak increased by 5% (resulting in $P_{\max}/P_{\text{ave}} = 1.21$) when the nonuniform poison was added in the 12 ft core. This effect seems to be size dependent since an increase of 15% in the power peak (resulting in $P_{\max}/P_{\text{ave}} = 1.35$) was observed in the 15 ft core. These cases should be more representative of the equilibrium cycle than the previous cases which started with zero burnup.

The following conclusions can be drawn from the studies:

1. In the first core, which contains only fresh fuel, the axial peaking increases slightly with core size. At a burnup of a few thousand MWD/MTU, there is little variation in peaking factor with core size. Beyond 3000 MWD/MTU, the larger cores may have slightly less axial peaking than smaller cores.
2. In a cycle typical of equilibrium, the peaking factor appears to increase substantially with core size; however, burnup effects have reduced the equilibrium peaking factor to a low value in all cases so that the peaking is not serious, even in the larger cores.
3. All steady state power distributions which were studied had axial peaking factors less than 1.60, which is the design value for the 1000 MWe PWR.

It should be emphasized that these results are limited to the steady state condition. No control rod movement throughout life is assumed, although a group of rods are present in the upper quarter of the core. The problems associated with movement of control rods will be discussed in the next section.

VARIATION IN AXIAL PEAKING FACTOR WITH BURNUP
CONTROL RODS IN TOP 25% OF CORE

FIG. I - 7

E. D. SK. 301439-B

VARIATION IN AXIAL POWER DISTRIBUTION WITH SOLUBLE POISON
EACH CASE HAS CONVERGED X_e , DOPPLER AND WATER DENSITY

FIG. I - 8

E. D. SK. 301447-B

4. STUDY OF POWER SENSITIVITY AND CODE CONVERGENCE PROBLEMS

Throughout the course of the 1000 MWe study, a number of problems were encountered which are apparently limited to very large cores; similar problems have not been found for smaller pressurized water reactors.

In addition to power oscillation problems, which will be discussed in a later section, the problems could be categorized as analytical problems and physical problems. These two problem areas cannot readily be isolated and studied independently since the analytical problems apparently result from certain peculiar physical phenomena which exist only in the very large core.

In the 1000 MWe core, the dimensions are large enough that the coupling between distant parts of the core is very weak. Many critical masses of fuel are present. The power peak can sometimes exist in any one of several core regions; the eigenvalues associated with the different power peak positions can be essentially identical. Furthermore, if the power distribution is relatively flat, the higher harmonic flux modes become quite important; the eigenvalues associated with the higher harmonics can be practically as high as the fundamental eigenvalue. These effects combine to make the 1000 MWe core power distribution very sensitive to small local reactivity effects; this also results in great difficulty in calculating the converged flux and power distributions.

Throughout the study, it was difficult to determine whether the phenomena observed from the calculations were physical or were merely due to computational and convergence difficulties. The various problems are discussed in the following sections along with a few examples which illustrate the difficulties.

4.1 COMPUTATIONAL PROBLEMS

The following difficulties were encountered during the course of the study:

1. Using standard diffusion theory codes, the number of flux iterations required to satisfy a given convergence criteria is several times as great for the 1000 MWe core as are normally required.
2. If the convergence criteria is selected so that the power distribution is unaffected if a tighter convergence is used, a much tighter convergence is required in the 1000 MWe PWR than is normally used. The required convergence may be one to two orders of magnitude tighter than normal.
3. Both of the above effects increase the number of iterations. In order to complete the study, several codes had to be modified to allow a larger number of iterations.
4. The eigenvalues converged much more readily than the flux distribution. Cases were observed in which a tighter convergence criteria changed the eigenvalues by only a few parts in a million, while the power distribution changed by approximately 10%.

5. The results were found to be sensitive to the integration routine used. For example, a code using Simpson's rule gave satisfactory results when the mesh spacing was approximately 1.6 centimeters. Another code, which uses the trapezoidal rule for integration, was sensitive to mesh spacing unless a much tighter mesh was used.
6. Convergence criteria and mesh spacing which are satisfactory for axial calculations are not necessarily adequate for radial calculations.
7. Some difficulties were encountered with the CNCR code. For some types of problems, in performing iterations to converge on equilibrium Doppler and water density distributions, successive iterations indicated a large power peak oscillated between the top and the bottom of the core without converging. Apparently the code overcompensated at each iteration and, in the sensitive core, the effect was sufficient to cause gross changes in the power distribution.

4.2 AXIAL STUDIES OF POWER SENSITIVITY

To illustrate the degree of sensitivity of the power distribution to local reactivity effects in a large core, consider the following case. An axial burnup study was performed for a core having an active height of 13.3 feet. A group of control rods was assumed to be inserted in the upper quarter of the core throughout the burnup period. The worth of the group of rods would be approximately 3% $\Delta k/k$ if fully inserted. The rods were represented as a homogenized poison in the upper quarter of the core. The burnup study was performed with the CANDLE code. CANDLE is a one-dimensional four group diffusion theory code which treats pointwise burnup effects.

After an average burnup of approximately 8000 MWD/MTU, the axial power distribution had burned to a very flat condition, with a maximum-to-average power density ratio of 1.14. At this point, it was assumed that some of the control rods were removed from the upper quarter of the core; the poison cross section was reduced to simulate this effect. The power distribution was recalculated for several values of the poison cross section. The results are shown in Figure I-9. The nonuniform effects of Doppler and xenon were not included in the calculation. The results indicate the power distribution peaks very sharply as the control rods are withdrawn; when rods worth approximately 1% $\Delta k/k$ are removed from the core, the axial maximum-to-average power ratio increases to approximately 2.5. It is evident from Figure I-9 that, when control rods are withdrawn, the principal power production is in the upper quarter of the core and the remainder of the core serves as a multiplying reflector and produces little power.

In view of the foregoing results and their serious consequences, a more detailed study of power distribution sensitivity was undertaken.

In this study, the 1000 MWe reference design core height of 11 feet was used. As before, a group of rods worth approximately 3% $\Delta k/k$ (if fully inserted) was inserted in the upper quarter of the core. This amount of control rod insertion is typical of that required to provide the proper reactivity insertion rate to control transient effects. Using CANDLE, the core was burned to 8000 MWD/MTU to give a quite flat power distribution.

AXIAL POWER DISTRIBUTION DEPENDENCE ON POISON VARIATION IN 1/4 CORE
NO DOPPLER OR XENON EFFECTS INCLUDED

In order to investigate the effect on power distribution of withdrawing control rods, the FAB-II code was used. This is a few group diffusion theory code which includes options to calculate the effect of equilibrium xenon and/or equilibrium Doppler at each point. The nonuniform cross sections from CANDLER after 8000 MWD/MTU were used as input to FAB-II. The poison representing the control rods was then removed by two methods. In the first method, the poison was reduced uniformly throughout the quarter core, as in the preceding study. In the second method, the bank of control rods was withdrawn in steps until the rods were fully removed. For each poison concentration or control rod position, five FAB-II problems were run to determine the power distributions for the following cases:

1. No Doppler or xenon effects included.
2. Equilibrium xenon but no Doppler.
3. Doppler but no xenon.
4. Doppler but xenon corresponding to original power distribution.
5. Converged Doppler and xenon.

The ratio of maximum-to-average axial power (F_A) for all the cases is plotted in Figures I-10 and I-11. The results indicate the power distribution peaks very sharply as the control rods are removed, if Doppler and xenon are not considered. This is in agreement with the previously reported results. However, the results also indicate that the Doppler and xenon provide a major power flattening in the large core. For example, when the rod group is entirely withdrawn, $F_A \approx 3.0$ with no xenon or Doppler. The Doppler effect alone reduces the peak to less than 2.0; with equilibrium xenon added, the peak is further reduced to ≈ 1.75 . The power distributions for these cases with rods fully withdrawn are shown in Figure I-12. This study illustrates the point that, in reactor cores in which the power distribution is unusually sensitive to local reactivity effects, the compensating effects of Doppler and xenon are unusually great.

The importance of the Doppler and xenon can be illustrated by an example based on Figure I-11. If the design value of F_A is taken to be 1.6, then for the case on which Figure I-11 was based, the rod bank could be withdrawn only 12 cm if no Doppler or xenon were present. With Doppler considered, the bank could be withdrawn 34 cm before F_A exceeded 1.6. After equilibrium xenon and Doppler are established, the design value of F_A would permit 48 cm of withdrawal. These results indicate that the 1000 MWe power distributions are sufficiently insensitive that a control system could be designed to provide the required reactivity response while maintaining a reasonable power distribution.

To estimate the effect of core height on the power distribution sensitivity, Figures I-9 and I-10 can be compared. From the previous study (Figure I-9), with a core height of 13.3 feet, removing control rods worth approximately 1% $\Delta k/k$ increased F_A to approximately 2.5. From Figure I-10, the same rod worth removal increases F_A (no xenon or Doppler) to 1.9. Consequently, the power distribution sensitivity problem definitely becomes worse for larger core sizes. However, the Doppler flattening is more important in the larger cores and the overall effect including Doppler is probably less size dependent.

4.3 THEORETICAL ANALYSIS OF POWER SENSITIVITY IN VERY LARGE CORES

The purpose of this study is to attempt to find a simple model which reproduces the power sensitivity characteristics observed in the 1000 MWe core.

It will be shown that, in spite of the fact that the model used here is oversimplified, flux distortions of the right order of magnitude and qualitatively similar to those observed previously are readily obtained. Two separate problems are analyzed, corresponding to different methods of control rod removal. The two methods of control rod withdrawal were discussed in Section 4.2, along with the analytical results.

4.3.1 Case 1. Reduction of Control Rod Poisoning

Initially, the reactor is assumed to contain a group of control rods inserted in the upper 25% of the core. The overall reactor core is poisoned in such a way (e.g., chemical shim control) that the reactor is just critical.

A number of control rods are then assumed to be completely withdrawn, while the others are kept in place. This results in a net reduction in poisoning in the upper quarter of the core. In order to maintain the overall reactor just critical, some additional poison is added uniformly to the core. The net result of these actions is that the reactivity of the upper region is increased relative to the lower region.

To analyze this situation, a very simple one-dimensional, one-group model is employed. Before any control rods are removed, the flux is assumed to be maximum in the lower core region, as would normally be expected. After some control rods are withdrawn, the flux peak can shift to the upper core region. The conditions required to produce this shift in the flux peak are now investigated.

The situation which is under consideration is illustrated in Figure I-13(a). Initially, the sinusoidal flux mode exists in the lower region and the upper region serves simply as a multiplying reflector. Later, the situation is reversed. The extrapolated boundaries of the reactor (including reflector savings) are assumed to be at 0 and X. The boundary between the upper and lower regions (x_0) is taken to be 0.25 X.

Initially only the lower core region is assumed to have k_{∞} greater than one. The following equations are applicable:

In the lower core region

$$\phi_1(x) = A_1 \sin B_1 (X - x) \quad (1)$$

In the upper core region

$$\phi_2(x) = C_1 \sinh \kappa_1 x \quad (2)$$

The function itself and its derivative are continuous at $x = x_0$. We decide to simplify the model by assuming that the hyperbolic sine in eq. (2) can be assimilated to a linear function, which is the case if the k_{∞} of the upper region is very close to 1.

VARIATION IN AXIAL PEAKING FACTOR WITH POISON REMOVAL
POISON WAS INSERTED IN 1/4 CORE DURING 8000 MWD/MTU BURNUP

FIG. I - 10

E. D. SK. 301468-B

VARIATION IN AXIAL PEAKING FACTOR WITH CONTROL ROD WITHDRAWAL
CONTROL RODS ORIGINALLY INSERTED IN 1/4 CORE DURING 8000 MWD/MTU
BURNUP; ROD GROUP WORTH $\approx 3\% \Delta K$ IF FULLY INSERTED

AXIAL POWER DISTRIBUTIONS IN 1000 MWE PWR
CONTROL RODS FULLY WITHDRAWN FROM UPPER 1/4 CORE AFTER 8000 MWD/MTU BURNUP

FIG. I - 13 (a)

FIG. I - 13 (b)

We extrapolate the sine function toward the left and let x_1 be the point where it crosses the axis of abscissas. The conditions of criticality for the two regions together are the same as for a homogeneous slab of thickness $(X - x_1)$ (including reflector savings). Recall that in one-group theory the buckling is given by

$$B_1 = \frac{\sqrt{k_{\infty 1} - 1}}{M} \quad (3)$$

$k_{\infty 1}$ is the k_{∞} of the lower region initially.

The migration length M is calculated using typical PWR parameters:

$$M = \sqrt{L^2 + \tau} = \sqrt{53.7} = 7.33 \text{ cm.}$$

The migration length is assumed as being the same in both regions.

Due to the simplifications introduced in the model, the only calculation of importance that is needed is x_1 , the extrapolation distance for the sine function.

In order to calculate x_1 , we introduce the reduced variables z (z_1, z_0, Z corresponding respectively to x_1, x_0, X), with a change in the scale of the abscissa such that the calculations are as simple as possible. We use also a reduced flux ψ . A diagram defining the reduced variables is presented in Figure I-13(b). The following equations then apply:

$$\psi_1 = \sin (z - z_1) \quad (4)$$

$$\psi_1' = \cos (z - z_1) \quad (5)$$

ψ_1' has to be such that the tangent at $z = z_0$ crosses the axis of abscissas at the point $z = 0$. Hence:

$$\tan (z_0 - z_1) = z_0 \quad (6)$$

It is also evident that Z is such that

$$Z = z_1 + \pi \quad (7)$$

The only problem is to use eq. (6) in order to calculate z_1 (or better, z_1/z_0) as a function of z_0 (or better z_0/Z). Hence Table I-2 is constructed.

By interpolating from this table, it is found that the case $z_0/Z = 0.2500$ corresponds to the following set of parameters:

$$\begin{array}{lll} z_0 - z_1 = 0.685 & z_0 = 0.8183 & z_1 = 0.1333 \\ Z = 3.2751 & z_1/z_0 = 0.1629 & \end{array}$$

By using this set of parameters in conjunction with Figure I-13, the thickness of the homogeneous slab equivalent to the whole reactor is given by

$$H_1 = 0.9593 H \quad (8)$$

where H is the total height (including reflector savings) of the reactor.

Table I-2

PARAMETERS NEEDED TO CALCULATE FLUX DISTORTIONS

$z_0 - z_1$	$z_0 = \tan (z_0 - z_1)$	z_1 $= \tan (z_0 - z_1) - (z_0 - z_1)$	$Z = \pi + z_1$	z_1/z_0	z_0/Z
0	0	0	3.1416	0	0
0.10	0.10023	0.00033	3.1419	0.0033	0.0319
0.20	0.20271	0.00271	3.1443	0.0134	0.0644
0.30	0.30934	0.00934	3.1509	0.0302	0.0981
0.40	0.42279	0.02279	3.1644	0.0540	0.1336
0.50	0.54630	0.04630	3.1879	0.0847	0.1713
0.60	0.68414	0.08414	3.2257	0.1231	0.2143
0.70	0.84229	0.14229	3.2839	0.1695	0.2564
0.80	1.0296	0.2296	3.3712	0.2228	0.306
0.90	1.2602	0.3602	3.5018	0.286	0.360
1.00	1.5574	0.5574	3.6990	0.358	0.422
1.10	1.9648	0.8648	4.0064	0.440	0.491
1.20	2.5722	1.3722	4.5138	0.534	0.570
1.30	3.6021	2.3021	5.4437	0.640	0.663
1.35	4.4552	3.1052	6.2468	0.698	0.715
1.40	5.7979	4.3979	7.5395	0.759	0.770
1.45	8.2381	6.7881	9.9297	0.824	0.830
1.50	14.101	12.601	15.743	0.894	0.895
1.51	16.428	14.918	18.060	0.908	0.911
1.52	19.670	18.150	21.292	0.924	0.924
1.53	24.498	22.968	26.110	0.936	0.938
1.54	32.461	30.921	34.063	0.950	0.953
1.55	48.078	46.528	49.670	0.967	0.968
1.56	92.621	91.061	94.203	0.984	0.983
1.57	1255.8	1254.23	1257.37	0.998	0.998

We must now consider the second case illustrated in Figure I-13(a). An increase in reactivity in the upper core region and a simultaneous decrease in the lower core region could result in a flux shape illustrated by "later condition" in the diagram. This case is obtained by reversing the abscissa scale and setting $z_0/Z = 0.75$. From Table I-2 the following set of parameters is determined:

$$\begin{aligned} z_0 - z_1 &= 1.382 & z_0 &= 5.3102 & z_1 &= 3.9282 \\ Z &= 7.0698 & z_1/z_0 &= 0.737 \end{aligned}$$

By using this set of parameters in conjunction with Figure I-13, the thickness of the slab equivalent to the whole reactor (this time in the "later condition") is

$$H_2 = 0.448 H \quad (9)$$

Equations 8 and 9 express the requirements for the two flux conditions shown in Figure I-13(a). It remains to determine the reactivity transfer required to switch from one condition to the other. The following conditions must be fulfilled:

1. The buckling, hence the excess k_{∞} of the lower region has to go from the value corresponding to a slab thickness H_1 down to zero.
2. The buckling, hence the excess k_{∞} of the upper region has to go from zero to the value corresponding to a slab thickness H_2 .

If we call α the numerical coefficient of both eqs. (8) and (9), the relevant equation is:

$$k_{\infty} - 1 = \frac{\pi^2 M^2}{\alpha^2 H^2} \quad (10)$$

Taking into account the numerical values of α , and introducing a "reduced height" (reactor height in units of neutron migration lengths)

$$h = H/M \quad (11)$$

The equations then take the simple form:

$$k_{\infty 1} - 1 = 10.76/h^2 \quad (12)$$

$$k_{\infty 2} - 1 = 49.20/h^2 \quad (13)$$

Equation (12) corresponds to the lower core region in the initial condition and equation (13) to the upper core region in the later condition. (See Figure I-13(a))

Table I-3 gives numerical values corresponding to equations (11), (12), and (13), for $M = 7.33$ cm.

Table I-3

REACTIVITY TRANSFER REQUIRED TO SHIFT THE FLUX DISTRIBUTION

H (cm)	h	h ²	$\frac{10.76}{h^2}$	$\frac{49.20}{h^2}$
7.33	1	1	10.76	49.20
14.66	2	4	2.69	12.30
36.66	5	25	0.43	1.970
73.30	10	100	0.1076	0.4920
146.60	20	400	0.0269	0.1230
366.60	50	2,500	0.0043	0.0197
733.00	100	10,000	0.001076	0.00492
1466.00	200	40,000	0.000269	0.00123
3666.00	500	250,000	0.000043	0.000197
7330.00	1000	1,000,000	0.00001076	0.0000492

The 1000 MWe core corresponds roughly to $h = 50$. From Table I-3 we see that the flux peak can be transferred from the lower region to the upper region if the k_{∞} of the upper region is increased approximately 2% while the k_{∞} of the lower region is decreased approximately 0.4% to maintain criticality. Reactivity transfers of this order might result when reactivity in control rods is exchanged for reactivity in moderator poison.

The strong dependence of the power distribution sensitivity on core size is evident from Table I-3. For example, for $h = 20$ (corresponding to a core height of approximately 5 feet), the shift in the flux peak could only occur if k_{∞} of the upper region increased by 12% while k_{∞} of the lower region decreased by 3%. Such large transfers of reactivity would not ordinarily occur during the normal operation of a reactor. Equation (10) indicates the reactivity transfer required to shift the power peak is inversely proportional to the square of the height. Consequently, an 11 ft core is 50% more sensitive than a 9 ft core.

To estimate the increase in hot channel factor when the flux peak shifts from one position to the other, the peak to average ratio is calculated for both cases shown in Figure I-13(a).

From Figure I-13(b), we see that

$$\int_0^Z \psi \, dz = \int_0^{z_0} \psi \, dz + \int_{z_0}^Z \psi \, dz = \frac{z_0 \psi_0}{2} + 1 + \cos(z_0 - z_1) \quad (14)$$

ψ_0 is equal to $\sin(z_0 - z_1)$.

We now use the numerical values determined previously for $z_0/Z = 0.25$ and 0.75 . For the case $z_0/Z = 0.25$, the value given by eq. (14) is 2.049; in the case $z_0/Z = 0.75$, it is 3.793. The peak-to-average ratio is Z divided by these values. This gives

$$\text{for } z_0/Z = 0.25 \qquad \text{peak-to-average} = 1.598$$

(to be compared to 1.571 for a pure sine function)

$$\text{for } z_0/Z = 0.75 \qquad \text{peak-to-average} = 1.865$$

Hence, the peak-to-average ratio increases about 17%.

It should be pointed out that this value is probably an underestimate. If the straight line is replaced by a true hyperbolic sine in the case corresponding to the "initial condition", it will not alter the ratio very much, whereas a significant increase could be attained if the same thing occurred in the case corresponding to the "later condition".

Case 2. Small Withdrawal of Control Rod Bank

The second problem which has been considered corresponds to the case wherein the entire group of control rods is withdrawn a small amount (5 cm in the example chosen). Previously, machine calculations indicated a significant increase in the hot channel factor for this case.

The case is illustrated in Figure I-14. The "initial condition" is identical to Figure I-13(a); in the "later condition", the negligibly thin intermediate region is the only one with k_{∞} definitely greater than one and where the flux distribution is sinusoidal. The reactor should actually be divided into 3 regions instead of two, but the third region is considered to be negligibly small; a discontinuity is introduced to account for the influence of the intermediate region on the flux distribution of the other two regions. In both the upper and lower regions, the flux distribution is a hyperbolic sine, assumed to be indistinguishable from a straight line. (This is equivalent to stating that the upper and lower core regions have approximately the same k_{∞} . This could be the case if burnup effects in the lower region are balanced by control rod effects in the upper region).

Referring to Figure I-14, the point $x = x_0$ is characterized in the "later condition" by a definite change in slope, which is now calculated.

$$\left(\frac{d^2 \phi}{dx^2} \right)_{x_0} \simeq \frac{\left(\frac{d\phi}{dx} \right)_2 - \left(\frac{d\phi}{dx} \right)_1}{\Delta x} \quad (15)$$

The indices 1 and 2 refer to the upper and lower regions respectively, and Δx is the (negligibly small) thickness of the intermediate region.

Using the relation

$$\frac{1}{\phi} \frac{d^2 \phi}{dx^2} = -B^2,$$

we have

$$\frac{\left(\frac{d\phi}{dx}\right)_1 - \left(\frac{d\phi}{dx}\right)_2}{\phi_0} = B^2 \Delta x$$

or

$$\frac{1}{x_0} + \frac{1}{X - x_0} = B^2 \Delta x = \frac{k_{\infty 3} - 1}{M^2} \Delta x$$

$k_{\infty 3}$ is the k_{∞} in the intermediate region, in the case corresponding to "later condition".

Hence,

$$k_{\infty 3} - 1 = \frac{X}{\Delta x} \frac{X}{x_0} \frac{X}{X - x_0} \frac{M^2}{X^2} \quad (16)$$

Here, X has the same meaning as given previously to H , so that the last fraction is $1/h^2$.

If we consider $\Delta x = 5 \text{ cm} = 0.68 M$, with $x_0/X = 0.25$, we have the equation

$$k_{\infty 3} - 1 = \frac{16}{3} \frac{M}{\Delta x} \frac{1}{h} = \frac{7.85}{h} \quad (17)$$

Based on this equation, Table I-4 is constructed.

Table I-4

REACTIVITY TRANSFER REQUIRED IN SMALL REGION TO SHIFT THE FLUX DISTRIBUTION

<u>H (cm)</u>	<u>h = H/M</u>	<u>$\frac{7.85}{h}$</u>
7.33	1	7.85
14.66	2	3.92
36.66	5	1.57
73.30	10	0.785
146.60	20	0.392
366.60	50	0.157
733.00	100	0.0785
1466.00	200	0.0392
3666.00	500	0.0157
7330.00	1000	0.00785

ILLUSTRATION OF POWER PEAK SENSITIVITY
WHEN ROD BANK IS WITHDRAWN A SMALL INCREMENT

FIG. I - 14

E. D. SK. 301462-B

From Table I-4, for $h = 50$ (corresponding approximately to the 1000 MWe PWR), a k_{∞} of approximately 1.16 is required in the intermediate region to provoke the postulated shift in power. The k_{∞} of the lower region would decrease by 0.4% to maintain criticality, just as in the previous analysis.

The increase in the hot channel factor can be estimated from the following considerations. The case corresponding to the "initial condition" is identical to the previous analysis; hence, peak-to-average ratio is 1.598. Within the limits of the simplified model, the case corresponding to "later condition" obviously has a peak-to-average ratio of 2.00. Consequently, the hot channel factor increases approximately 25%.

Equation 17 indicates that, for the type of reactivity transfer assumed in this study, the sensitivity of the power distribution is inversely proportional to the height (in contrast to height squared, as found in the previous study).

The interesting aspect of these theoretical studies is that an extremely simple model predicts power distribution sensitivity rather similar to that observed from a multigroup calculation which considered pointwise burnup effects, etc. The model predicts that sensitivity is dependent on the size of the core (related to the height squared in one instance); therefore, it is not surprising that many difficulties related to power distribution sensitivity were encountered in the study of the 1000 MWe core.

4.4 STUDY OF CONVERGENCE DIFFICULTIES IN RADIAL PROBLEMS

The studies presented thus far have been limited to axial calculations. Many radial calculations were also performed throughout the study, particularly in analyzing various fuel cycles. The problems of code convergence and power distribution sensitivity were evident in the radial calculations also.

One particular radial case was examined in some detail to investigate convergence problems. The case corresponds to an equilibrium Roundelay fuel cycle in a core containing 673 fuel elements. The Roundelay fuel cycle is described in detail in Section 6.2. The active core can be approximated reasonably well in one dimension by using 20 radial regions. At the beginning of a cycle, every third region contains fresh fuel; the other regions have burnups which depend on the number of cycles the elements have been in the core and the power density at their location. The radial power distribution consequently contains "ripples", with peaks evident at each fresh fuel region. The radial power distribution for a typical case was calculated by a variety of one-dimensional codes as well as a two-dimensional PDQ code. In the PDQ, every fuel element was represented explicitly, so that the geometry approximation in the one-dimensional cases was eliminated.

The results of the various calculations are summarized in Table I-5. The following observations are pertinent:

1. CANDLER is a few group diffusion theory code which includes pointwise burnup effects. Since only the zero time step is included in Table I-5, the burnup option does not enter into this comparison. The results indicate CANDLER is not properly converged when the normal convergence criteria is used.
2. WANDA-III is a few group diffusion theory code, essentially identical to the diffusion code used in CANDLER. Consequently, it is not surprising

that WANDA-III results agree with CANDLE for the same convergence criteria. Comparing Cases 3 through 7, it is seen that the problem is well converged if the convergence criteria is 0.00005 or less. Comparing Cases 5 and 7, it is apparent that 135 mesh points are sufficient in this case, since 251 mesh points did not change the answer significantly. With 135 mesh points, the mesh spacing in the core is approximately 1.6 cm.

3. FAB-II is a few group diffusion theory code with options to calculate nonuniform effects of Doppler and/or xenon. A difference between FAB-II and WANDA is that FAB-II uses the trapezoidal integration rule rather than Simpson's rule. Comparing Cases 8 through 10, FAB-II appears to be well converged if the convergence criteria is 0.00005; this is the same value required in WANDA. However, the converged value of F_R is 1.46 in FAB (Case 10) compared to 1.80 (Case 6) in WANDA. The number of mesh points in the FAB problem was increased to the maximum allowed (215). The value of F_R increased to 1.58. One would expect the FAB power distribution to approach the WANDA distribution as the number of points are further increased: since, the trapezoidal rule approaches Simpson's rule in the limit of small mesh intervals. It is not presently known how many mesh points would be required in FAB to perform the calculation accurately. However, in the axial cases described in Section 4.2, the FAB-II results were insensitive to the mesh spacing, even when considerably fewer than the maximum allowable number of mesh points were used. This may indicate that the difficulty in the radial problem is due to the presence of the "ripples" in the flux distribution which greatly complicates the calculation.
4. PDQ is a two-dimensional few group diffusion theory code. Every fuel element in $1/4$ of the core was represented explicitly. The same 4-group cross sections were used as in the one-dimensional representations. No attempt was made to represent control rods, followers, water slots and cans in the PDQ. With 66 mesh points in each dimension, each fuel element contained three internal mesh lines in addition to mesh lines at each boundary. This appears to be an adequate representation since increasing the mesh points to 85 in each dimension has a small effect (compare Cases 19 and 21). Decreasing the convergence criteria has a substantial effect, as seen in Cases 20 through 24. Case 24 appears to be fairly well converged; however the converged value of F_R is considerably lower than the converged WANDA-III value. The difference might be due to geometry effects. It is interesting to note that in comparing Case 21 with Case 20, the eigenvalue changed by only one part in a million, while F_R increased from 1.478 to 1.537. In Case 22, the eigenvalue changed by three parts in a million and F_R increased to 1.599. This indicates the eigenvalue converges quickly as compared to the flux distribution.
5. The effects of xenon and Doppler on the radial power distribution can be estimated from Cases 14 through 17. Since the FAB-II code is inaccurate due to the limited number of mesh points, caution must be used in interpreting the results. FAB-II indicates the Doppler reduces the radial peaking by 8% and the Doppler plus xenon reduces the peaking by 12%. Since the Doppler and xenon make the power distribution more uniform, the FAB calculation should be more accurate than it was

with no Doppler or xenon included. This is evidenced by noting that the cases with Doppler and xenon are less sensitive to the number of mesh points. The values of F_R from Cases 12 and 16 are in much better agreement than Cases 10 and 14. Consequently, the accurate value of F_R including Doppler and xenon may not be greatly different from the FAB-II value, although FAB-II is seriously in error with no Doppler or xenon.

These results illustrate the difficulties encountered in calculating power distributions in the 1000 MWe core. It is apparent that considerable uncertainties still exist in the calculations and that further effort is needed to reduce the uncertainties. Furthermore, the results in Table I-5 indicate the computational costs in a 1000 MWe core design will greatly exceed the costs in a smaller reactor due to the convergence difficulties.

Table I-5

COMPARISON OF VARIOUS RADIAL CALCULATIONS FOR AN EQUILIBRIUM
ROUNDELAY FUEL CYCLE

Case No.	Comments	Code	# Mesh Points	Pointwise Convergence ϵ	K_{eff}	$F_R^{**} = P_{max}/P_{ave}$ (Radial)
1		CANDLE	135	.005*	1.1144	1.623
2		CANDLE	135	.0005	1.1147	1.791
3		WANDA-III	135	.005*	1.1144	1.619
4		WANDA-III	135	.0005	1.1148	1.784
5		WANDA-III	135	.00005	1.1148	1.802
6		WANDA-III	135	.000005	1.1148	1.803
7		WANDA-III	251	.00005	1.1149	1.803
8		FAB-II	135	.0005	1.1081	1.452
9		FAB-II	135	.00005	1.1082	1.456
10		FAB-II	135	.000005	1.1082	1.457
11	Doppler	FAB-II	135	.000005	1.1066	1.394
12	Doppler + Xenon	FAB-II	135	.000005	1.0779	1.356
13	Xenon	FAB-II	135	.000005	1.0800	1.400
14		FAB-II	215	.00005	1.1123	1.576
15	Doppler	FAB-II	215	.00005	1.1104	1.445
16	Doppler + Xenon	FAB-II	215	.00005	1.0824	1.388
17	Xenon	FAB-II	215	.00005	1.0839	1.462
18		PDQ-03	66 x 66	.05*	1.1141	1.482
19		PDQ-03	66 x 66	.04	1.1141	1.529
20		PDQ-03	85 x 85	.05*	1.1144	1.478
21		PDQ-03	85 x 85	.04	1.1144	1.537
22		PDQ-03	85 x 85	.03	1.1144	1.599
23		PDQ-03	85 x 85	.02	1.1144	1.634
24		PDQ-03	85 x 85	.01	1.1144	1.639

* These convergence criteria were previously found to be satisfactory for most calculations for typical PWR's.

** Does not include local peaking due to control rod followers, water slots, etc.

Note: The PDQ convergence criteria must be squared to be on a comparable basis with the others.

5. POWER OSCILLATIONS

A major concern in the design of very large reactors is that power oscillations may occur. In that event, the power distribution varies with time; unsafe core conditions may result, particularly if the oscillations are divergent.

In reactor cores in which the leakage is very small, the power distribution is quite sensitive to local reactivity effects, as discussed in the previous section. If the local reactivity is time dependent, the power distribution will vary with time also. If, in addition, the reactivity is a function of the power level, the feedback can result in oscillatory behavior.

The two feedback mechanisms which have been considered during the 1000 MWe study are xenon and Doppler. These mechanisms are discussed in the following sections.

5.1 XENON OSCILLATIONS

5.1.1 Summary

Studies have been conducted to determine whether xenon oscillations constitute a potential problem in the 1000 MWe PWR. Axial, radial, and aximuthal oscillations were studied. Both sustained and partially damped oscillations were considered since either type could lead to possible core damage. Calculations were performed for a nominal temperature coefficient and for a minimum coefficient. The minimum coefficient represents the lowest coefficient in the band of uncertainty. The following conclusions were reached:

1. The nominal temperature coefficient is sufficiently negative to prevent sustained Xe oscillations of constant or increasing amplitude, even with pessimistic assumptions for the degree of flux flattening.
2. With the minimum temperature coefficient and a pessimistic degree of flux flattening, the calculations predict the 1000 MWe core is near the threshold for sustained oscillations.
3. Damped oscillations are predicted in all directions for both the nominal and minimum temperature coefficients. Damped oscillations do not represent a serious operational problem if the degree of damping is sufficient to quickly reduce the amplitude of the oscillation. With the nominal temperature coefficient, the damping is probably sufficient to prevent any serious problem; however, with the minimum coefficient, very little damping is expected and operational problems could occur. These conclusions must be qualified since no definite standards have been established regarding the degree of damping required for satisfactory reactor operation. To establish such criteria extensive effort is required, using spatial codes in one and two dimensions and including the Xe and temperature coefficient effects. Studies of this type are recommended during the design stage of a very large PWR so that the control requirements can be clearly defined; this is particularly important if later work indicates the temperature coefficient may be significantly lower than the nominal value used in this study.

5.1.2 Introduction

The studies herein are based on the theory of spatial xenon oscillations as developed by Randall and St. John (Refs. 1 and 2). The reader is referred to these references for the details of the theory since only a brief description of the pertinent results are presented here. The present work differs from the previous work in the following respects:

1. Recent values have been used for the physical constants, such as decay constants, cross sections, and yields.
2. The reactivity coefficients are appropriate for a chemical shim PWR.
3. The effect of flux flattening is considered since the probability of Xe oscillations is increased when part or all of the core has a flat flux. For example, with a flat axial flux, the core height for sustained Xe oscillations is a factor $\sqrt{3}$ smaller than a core with the standard cosine flux distribution. Since the 1000 MWe core is expected to have a relatively flat flux during part of its lifetime, this effect must be considered.

The studies are divided into two parts:

1. Investigations of Sustained Oscillations.
2. Investigation of Damped Oscillations.

5.1.3 Investigation of Sustained Oscillations

In Ref. 1 it is shown that the behavior of a reactor under the influence of xenon can be described by a differential equation which has the auxiliary equation

$$\omega^2 + b\omega + c = 0 \quad (18)$$

where the coefficients b and c are functions of flux, geometrical buckling, cross sections, decay constants of Xe-135 and I-135, and reactivity coefficients of power and xenon. Delayed neutron effects are neglected since the delay period is extremely short compared to the periods for the buildup and decay of xenon.

The form of the general solution to (18) is

$$\varphi(\vec{r}, t) = \varphi(\vec{r}) \exp\left(-\frac{b}{2} t\right) \exp\left(\pm \sqrt{\frac{b^2}{4} - c} t\right) \quad (19)$$

For positive values of b , the first exponential term in (19) tends to damp any oscillation. For values of b which are zero or negative, no damping is

1. D. Randall and D. S. St. John, "Xenon Spatial Oscillations", NUCLEONICS 16, No. 3, 82 (1958).
2. D. Randall and D. S. St. John, "Xenon Spatial Oscillations", NUCLEAR SCIENCE AND ENGINEERING 14, No. 2, 204 (1962).

present and sustained oscillations are possible. The value $b = 0$ then represents the threshold for sustained oscillations. In Ref. 1 the following solution was found for the condition $b = 0$.

$$\left(\frac{M^2 B_g^2}{\alpha_x} \right) \Lambda_j - \frac{\alpha_T}{\alpha_x} \varphi = f(\varphi) \quad (20)$$

where the following definitions apply:

α_x is the reactivity held by xenon at infinite flux.

M^2 is the migration area.

B_g^2 is the geometrical buckling.

α_T is the temperature coefficient expressed in terms of reactivity per unit flux.

Λ_j is the dimensionless parameter depending on the shape of the flux distribution and is equal to the difference between the eigenvalue for the j -th mode and fundamental mode divided by the geometrical buckling.

$f(\varphi)$ depends on decay constants, xenon cross section and the fraction of Xe formed directly from fission. $f(\varphi)$ is plotted as a function of φ in Figure I-15.

Equation (20) can be solved by trial and error for values of φ which represent the threshold for sustained oscillations.

For reactors with height to diameter ratios of unity, xenon oscillations occur most readily in the axial direction, with decreasing probability for oscillations in the azimuthal and radial directions. In the 1000 MWe PWR reference design the dimensions are such that azimuthal oscillations are slightly more probable than axial oscillations.

The amount of flux flattening affects the value of Λ_j in (20). In the axial direction, Λ varies from 3.0 for a cosine to 1.0 for a flat flux. From previous axial burnup studies for the 1000 MWe PWR, it was estimated that the core could be $\approx 85\%$ flat (i.e., the flux distribution could be approximated as flat in the middle 85% of the core with a cosine distribution at the ends). For this case, $\Lambda = 1.1$ from Reference 1. A similar degree of flattening was assumed for the radial and azimuthal cases. This is undoubtedly a pessimistic assumption since it is unlikely that so much flattening will occur in the radial direction, regardless of the refueling scheme.

The flux coefficient of reactivity (α_T) at full power ($\varphi_{TH} = 2.5 \times 10^{13}$) was calculated to be

$$\alpha_T = \frac{\Delta k}{\Delta \varphi} = -6.4 \times 10^{-16} \text{ (nominal case)}$$

$$\alpha_T = -3.0 \times 10^{-16} \text{ (minimum case)}$$

These are based on the following coefficients for a chemical shim PWR:

$$\text{Fuel temperature } \frac{\Delta k}{\Delta T_F} = - 1.2 \times 10^{-5}/^{\circ}\text{F (nominal case)}$$

$$= - 0.5 \times 10^{-5}/^{\circ}\text{F (minimum case)}$$

$$\text{Moderator temperature } \frac{\Delta k}{\Delta T_M} = - 5. \times 10^{-5}/^{\circ}\text{F (both cases)}$$

For the nominal case, the fuel temperature coefficient contributes 90% of the total value of α_T .

It is of interest to determine the flux threshold for sustained oscillations for a core with zero coefficients ($\alpha_T = 0$). These are given in Table I-6 for the 1000 MWe PWR. The dimensions of the active reference core are:

Height = 11.0 ft.

Equivalent Diameter = 12.67 ft.

A reflector savings of 7.5 cm is used in all directions.

Table I-6

FLUX THRESHOLD FOR SUSTAINED XE OSCILLATIONS
IF TEMPERATURE COEFFICIENT IS ZERO

<u>Mode</u>	<u>Threshold (n/cm² - sec)</u>
Axial	1.2×10^{13}
Azimuthal	1.1×10^{13}
Radial	2.8×10^{13}

Except for the radial case, the above table indicates the flux threshold for sustained oscillations is below the reference design flux. Consequently, the reactor must depend on the negative coefficients to prevent xenon oscillations.

When the nominal calculated value of α_T is used in equation (20), no value of flux satisfies the equation. This is true even for an infinite core ($B_g^2 = 0$). Therefore, it can be concluded that no sustained xenon oscillations are possible in any reactor having a negative temperature coefficient as great as the nominal value of α_T .

In order to determine the margin of safety in the calculated temperature coefficient, we can solve equation (20) for the temperature coefficient:

$$- \alpha_T = \left[f(\varphi) - \frac{M^2 B_g^2 \Lambda_j}{\alpha_x} \right] \frac{\alpha_x}{\varphi} \quad (21)$$

For each value of φ , we can determine a temperature coefficient which corresponds to the threshold for sustained xenon oscillations. These calculations have been performed for values of $B_g^2 \Lambda_j$ which correspond to the first mode

FUNCTION USED IN Xe STABILITY ANALYSIS

FIG. I - 15

in the axial, azimuthal, and radial directions and also for an infinite core ($B_g^2 = 0$). The coefficients are plotted versus flux in Figure I-16. The region under each curve represents oscillatory behavior, whereas above each curve no sustained oscillations can occur. The nominal value of α_T (-6.4×10^{-16}) is well above the peak of all the curves, indicating a considerable margin of safety. However, with the minimum value of α_T (-3.0×10^{-16}), the peaks of the axial and azimuthal curves approach the value of α_T , indicating a small margin of safety. The margin of safety can be defined as the factor by which the design value of α_T can be reduced before sustained oscillations will occur. The margins of safety for both the nominal and minimum design coefficients are given in Table I-7.

Table I-7

MARGIN OF SAFETY TO PREVENT SUSTAINED OSCILLATIONS

Mode	$(\alpha_T)_{\max}$	α_T	α_T	$(\alpha_T)_{\text{design}}/(\alpha_T)_{\max}$	
	From Curve	Nominal Design	Minimum Design	Nominal Design	Minimum Design
	(10^{-16})	(10^{-16})	(10^{-16})		
Axial	- 2.9	- 6.4	- 3.0	2.2	1.0
Azimuthal	- 3.0	- 6.4	- 3.0	2.1	1.0
Radial	- 1.5	- 6.4	- 3.0	4.3	2.0
Infinite Core	- 4.4	- 6.4	- 3.0	1.5	0.7

The above results indicate the nominal temperature coefficient could be reduced by more than a factor of 2 before sustained xenon oscillations would be expected. However, the minimum temperature coefficient results in no margin of safety in the axial and azimuthal directions.

The above conclusions are probably conservative since the degree of power flattening assumed was quite severe. Such flattening would only occur near the end of a cycle; at that time, the boron concentration would be small and the moderator temperature coefficient would be much more negative than assumed. This would tend to make oscillations less probable than indicated above.

5.1.4 Investigation of Damped Oscillations

It may not be sufficient to insure that no sustained xenon oscillations can occur. Partially damped oscillations could lead to core damage due to local burnout if the amplitude of the oscillation diminishes too slowly.

In terms of equation (18), partially damped oscillations occur when $0 < b^2/c < 4$. When $b^2/c > 4$, the system is overdamped and no oscillations occur. For an underdamped system the solution for the flux takes the form

$$\varphi(\vec{r}, t) = \varphi(\vec{r}) \exp\left(-\frac{bt}{2}\right) \cos\left(\frac{\sqrt{4c - b^2}}{2} t\right). \quad (22)$$

The period of the oscillation is

$$T = \frac{4\pi}{\sqrt{4c - b^2}} \quad (23)$$

A measure of the amount of damping is given by the ratio of amplitude after 1/2 period compared to the original amplitude (ratio defined as $D_{T/2}$).

Then

$$\ln(D_{T/2}) = \frac{\pi}{\sqrt{\frac{4c}{b^2} - 1}} \quad (24)$$

The damping can be found immediately if the ratio b^2/c is known. Expressions for b and c are given in Reference 1. They are dependent on reactor size and geometry through a term which is the difference in eigenvalues between the fundamental and 1st harmonic modes. The differences can be expressed as:

$$\begin{aligned} \text{1st axial mode} \quad B_{001}^2 - B_{000}^2 &= \left(\frac{2\pi}{H}\right)^2 - \left(\frac{\pi}{H}\right)^2 \\ \text{1st azimuthal mode} \quad B_{010}^2 - B_{000}^2 &= \left(\frac{j_{01}}{R}\right)^2 - \left(\frac{j_{00}}{R}\right)^2 \\ \text{1st radial mode} \quad B_{100}^2 - B_{000}^2 &= \left(\frac{j_{10}}{R}\right)^2 - \left(\frac{j_{00}}{R}\right)^2 \end{aligned} \quad (25)$$

where R and H include reflector savings; j_{00} is the lowest zero of Bessel J_0 , j_{10} is the second zero of J_0 , and j_{01} is the lowest zero of J_1 . The above expressions do not account for any flux flattening which would tend to decrease the buckling differences. It was previously mentioned that the flux flattening in the axial direction resulted in a decrease of Λ from 3.0 to 1.1. Since Λ is directly proportional to the buckling difference, the values from equation (25) were reduced by the factor 1.1/3.0. (This assumes flux flattening in all dimensions.)

The ratio b^2/c is calculated as a function of flux for the cases corresponding to the first axial, azimuthal and radial modes of oscillation. The calculations are performed for both the nominal and minimum temperature coefficients. The results are plotted in Figures I-17 and I-18. The minimum value of b^2/c corresponds to the least damping. The minimum value is read from each curve and the damping is calculated from equation (24). The results are summarized in Table I-8.

The results in Table I-8 indicate that, with the nominal value of αT , the amplitude of the azimuthal Xe oscillation is reduced by a factor of 4 after one half period and a factor of 15 after a full period. The damping is greater for axial and radial oscillations. With the minimum reactivity coefficient, there is essentially no damping in the axial and azimuthal cases, as discussed previously. These results are probably conservative for the same reasons previously mentioned.

REACTIVITY COEFFICIENT AT THRESHOLD FOR SUSTAINED Xe OSCILLATIONS
 NO SUSTAINED OSCILLATIONS CAN OCCUR ABOVE THE CURVES

FIG I - 16

STUDY OF PARTIALLY DAMPED Xe OSCILLATIONS
1000 MWE PWR REFERENCE DESIGN

FIG. I - 17

STUDY OF PARTIALLY DAMPED Xe OSCILLATIONS
1000 MWE PWR REFERENCE DESIGN

FIG. I - 18

Table I-8

MINIMUM DAMPING FOR XENON OSCILLATIONS IN THE
1000 MWE PWR

	$(b^2/c)_{\min}$		$-D_{T/2}$	Damping Factor			
	Nominal α_T	Minimum α_T		$+D_T$		Nom. α_T	Min. α_T
				Nom. α_T	Min. α_T		
Axial	.68	≈ 0	.24	≈ 1	.058	≈ 1	
Azimuthal	.62	≈ 0	.26	≈ 1	.068	≈ 1	
Radial	1.22	.38	.12	.36	.016	.13	

Although the undamped Xe oscillation is generally conceded to be undesirable from an operating and control standpoint, no standards have been established to determine the degree of damping required in order to prevent operating difficulties. Consequently, it cannot be said with certainty that a reduction in amplitude by a factor of 4 after 1/2 period is sufficient to prevent difficulties, although in all likelihood, it is.

In order to pursue the problem further and establish damping criteria, spatial calculations in one and two dimensions are required. The calculations must include the effects of Xe and temperature coefficient. A 2-dimensional code is needed to study the azimuthal effect, which is most important. No adequate code is presently operable although some effort has been expended in this direction.

Since Xe oscillation problems cannot be ruled out entirely for the nominal temperature coefficient, and since the problems increase rapidly with decreasing temperature coefficient, it is recommended that the problem be investigated thoroughly during the design of any core comparable in size to the 1000 MWE PWR.

Until further evidence is available to the contrary, one should assume that xenon oscillations can occur in the 1000 MWe PWR. This does not affect the basic feasibility of the plant, since reactors have previously been operated successfully in the presence of xenon oscillations. However, a development program is required to design an adequate control system and to determine the restrictions on reactor operation which must be imposed due to the possibility of xenon oscillations.

5.2 DOPPLER OSCILLATIONS

5.2.1 Summary

The condition for Doppler oscillations is determined. It is shown that, for the conditions normally existing in a PWR, no Doppler oscillations are possible, even for a very large reactor. Moreover, even if the normal PWR parameters were modified greatly to result in Doppler oscillations, a strong damping would exist so long as the Doppler coefficient remains negative.

5.2.2 Introduction

Doppler oscillations, if they occur, could rule out feasibility of the 1000 MWe PWR. Unlike xenon oscillations, which have a long time constant and can be controlled, Doppler oscillations might have a time constant on the order of the delayed neutron time, which would make reactor control difficult, if not impossible. Consequently, an analysis of Doppler oscillations was undertaken. Simplifications in the physical model have been made to make the problem amenable to analysis, but are not believed to have affected the conclusions of the study.

5.2.3 Simplified Model for the Fuel Rod

It is assumed that the fuel rod is isothermal and that all the temperature drop is concentrated between the fuel and the coolant. Although such a model differs markedly from the true physical model, it appears sufficient to study the phenomena involved, provided the film coefficient is adjusted later to provide the correct order of magnitude for the time constant in the heat transfer phenomena. The power per unit volume of the rod is

$$\phi \Sigma_f \zeta$$

where ζ is the heat liberated per fission, expressed in a convenient unit.

The heat flux per unit of outer surface area of the rod is

$$q'' = K (T_F - T_C) \quad (26)$$

T_F is the fuel temperature, T_C the coolant temperature, K a "film" heat transfer coefficient, which is not the true value, but an adjusted value such that T_F is a good average value.

Per unit length of the rod, we have the following heat balance equation:

$$\pi r^2 \phi \Sigma_f \zeta = 2\pi r K (T_F - T_C) + C_s \pi r^2 \frac{\partial T_F}{\partial t} \quad (27)$$

where r is the fuel rod radius and C_s the specific heat of the fuel.

Equation (27) can be written in the form

$$\phi = a (T_F - T_C) + b \frac{\partial T_F}{\partial t} \quad (28)$$

with

$$a = \frac{2 K}{r \Sigma_f \zeta}, \quad b = \frac{C_s}{\Sigma_f \zeta} \quad (29)$$

5.2.4 Neutron Flux Equation

We use Glasstone and Edlund's equation (10.18.1), p. 296, written for one group of delayed neutrons only, as a neutron flux equation. Furthermore, since the lifetime of prompt neutrons is negligible with respect to other time constants involved in this study, we assume it is zero. Hence:

$$L^2 \nabla^2 \phi + \left[(1 - \beta) k_{\infty} e^{-B^2 \tau} - 1 \right] \phi + \frac{p e^{-B^2 \tau}}{\Sigma_a} \lambda C = 0 \quad (30)$$

$L^2 = D/\Sigma_a$ = square of the diffusion length.

β = fraction of the (one group of) delayed neutrons.

k_{∞} = infinite multiplication factor.

λ = time constant of the (one group of) delayed neutrons.

C = concentration of the precursor atoms.

All other symbols have the usual meaning.

This equation must be considered in conjunction with the one governing the concentration of the precursor atoms.

$$\frac{\partial C}{\partial t} = -\lambda C + \frac{k_{\infty} \beta}{p} \Sigma_a \phi \quad (31)$$

(loc. cit., eq. 10.15.1, p. 295, also written for one group of delayed neutrons.)

5.2.5 Coupling Between Thermal and Neutron Equations

The coupling between thermal and neutron flux equations enters through the Doppler effect, which is an effect on p . Through

$$k_{\infty} = \eta \epsilon p f \quad (32)$$

and

$$B^2 = (k_{\infty} - 1)/M^2, \quad (33)$$

we note an influence of p on both k_{∞} and B ; but the fraction k_{∞}/p is, as a first approximation, independent of any Doppler effect.

We note that, in equations (30) and (31), only two groups of parameters depend on the Doppler effect; they are

$$p e^{-B^2 \tau}$$

and

$$k_{\infty} e^{-B^2 \tau}$$

and, moreover, they vary proportionally to each other according to equation (32). If

$$(p e^{-B^2 \tau})^*$$

is the value in the steady state, we assume that

$$p e^{-B^2 \tau} = (p e^{-B^2 \tau})^* \left[1 - \alpha (T_F - T_F^*) \right]. \quad (34)$$

T_F^* is also a value in the steady state. α is normally positive, as p decreases with an increase in fuel temperature, and $e^{-B^2 \tau}$ is much less sensitive to fuel temperature than p .

Assuming that $\eta \in f$ is independent of any Doppler effect, we have also

$$k_{\infty} e^{-B^2 \tau} = (k_{\infty} e^{-B^2 \tau})^* \left[1 - \alpha (T_F - T_F^*) \right] \quad (35)$$

Hence, we write equation (30) in the form:

$$L^2 \nabla^2 \phi + \left[(1 - \beta)(k_{\infty} e^{-B^2 \tau})^* (1 - \alpha \theta) - 1 \right] \phi + \frac{(p e^{-B^2 \tau})^* (1 - \alpha \theta)}{\Sigma_a} \lambda C = 0 \quad (36)$$

θ is substituted for $T_F - T_F^*$.

5.2.6 Perturbation Equations

We consider that, in the course of the Doppler oscillation, the different variables undergo small variations around starred steady state values. Hence:

$$\phi = \phi^* + \phi \quad C = C^* + c \quad T_F = T_F^* + \Theta. \quad (37)$$

The steady state values must satisfy the steady state equations.

$$L^2 \nabla^2 \phi^* + \left[(1 - \beta)(k_{\infty} e^{-B^2 \tau})^* - 1 \right] \phi^* + \frac{(p e^{-B^2 \tau})^*}{\Sigma_a} \lambda C^* = 0 \quad (38)$$

$$0 = -\lambda C^* + \frac{k_{\infty}}{p} \beta \Sigma_a \phi^* \quad (39)$$

$$\phi^* = a (T_F^* - T_c). \quad (40)$$

If we introduce (37) into (31), (32) and (28), we have

$$\begin{aligned} L^2 \nabla^2 (\phi^* + \phi) + \left[(1 - \beta)(k_{\infty} e^{-B^2 \tau})^* (1 - \alpha \theta) - 1 \right] (\phi^* + \phi) \\ + \frac{(p e^{-B^2 \tau})^*}{\Sigma_a} \lambda (1 - \alpha \theta) (C^* + c) = 0 \end{aligned} \quad (41)$$

$$\frac{\partial c}{\partial t} = -\lambda (C^* + c) + \frac{k_{\infty}}{p} \beta \Sigma_a (\phi^* + \phi) \quad (42)$$

$$(\phi^* + \phi) = a (T_F^* + \theta - T_C) + b \frac{\partial \theta}{\partial t}. \quad (43)$$

(with the assumption that $\frac{\partial \phi^*}{\partial t}$, $\frac{\partial C}{\partial t}$ and $\frac{\partial T_F^*}{\partial t}$ are zero.)

By comparing (41) with (38), (42) with (39), (43) with (40) and neglecting second and higher order terms, we get

$$\begin{aligned} L^2 \nabla^2 \phi + \left[(1 - \beta)(k_{\infty} e^{-B^2 \tau})^* - 1 \right] \phi - (1 - \beta)(k_{\infty} e^{-B^2 \tau})^* \phi^* \alpha \theta \\ + \frac{(p e^{-B^2 \tau})^*}{\Sigma_a} \lambda c - \frac{(p e^{-B^2 \tau})^*}{\Sigma_a} \lambda C^* \alpha \theta = 0. \end{aligned} \quad (44)$$

$$\frac{\partial c}{\partial t} = -\lambda c + \frac{k_{\infty}}{p} \beta \Sigma_a \phi \quad (45)$$

$$\phi = a \theta + b \frac{\partial \theta}{\partial t}. \quad (46)$$

We introduce the following simplifications:

$$A = (1 - \beta)(k_{\infty} e^{-B^2 \tau})^* \quad (47)$$

$$A' = \lambda \frac{(p e^{-B^2 \tau})^*}{\Sigma_a} \quad (48)$$

$$E = \frac{k_{\infty}}{p} \beta \Sigma_a = \frac{k_{\infty}^*}{p^*} \beta \Sigma_a \quad (49)$$

$$\gamma = \alpha (A \phi^* + A' C^*) \quad (50)$$

hence, we rewrite (44), (45), and (46)

$$L^2 \nabla^2 \phi + (A - 1) \phi + A' c - \gamma \theta = 0 \quad (44')$$

$$\frac{\partial c}{\partial t} = -\lambda c + E \phi \quad (45')$$

$$\phi = a \theta + b \frac{\partial \theta}{\partial t} \quad (46')$$

Although γ is space dependent, it will be treated, as a rough approximation, as a constant.

5.2.7 Search for Particular Solutions

The most general solution of this system can be expanded as a sum of particular solutions, the time dependent part of which is an exponential of the form $e^{\omega t}$, and the space dependent part of which is one of the eigenfunctions ϕ_n of the operator ∇^2 for the chosen geometry. Hence, if we consider the particular solutions one by one, the time derivative operator $\frac{\partial}{\partial t}$ may be replaced by ω and ∇^2 may be replaced by $-B_n^2$, the n th eigenvalue for the buckling. Hence, rewriting equations (44'), (45'), and (46'),

$$L^2 B_n^2 \phi + (A - 1) \phi + A' c - \gamma \theta = 0 \quad (51)$$

$$(\omega + \lambda) c = E \phi \quad (52)$$

$$\phi = (a + b \omega) \theta \quad (53)$$

We calculate c and θ from (52) and (53) and introduce them in (51); after dividing by ϕ , assumed to be different from zero, we get:

$$-L^2 B_n^2 + A - 1 + \frac{A' E}{\omega + \lambda} - \frac{\gamma}{a + b \omega} = 0 \quad (54)$$

By rearranging the terms and writing

$$-L^2 B_n^2 + A - 1 = \mu \quad (55)$$

Equation (54) becomes

$$\mu b \omega^2 + (\mu a + \mu \lambda b + A' E b - \gamma) \omega + (\mu \lambda a + A' E a - \gamma \lambda) = 0. \quad (56)$$

The condition for a Doppler oscillation can be obtained by considering the roots of eq. (56)

We rewrite it in a more convenient form by dividing eq. (56) by $\mu b \lambda^2$. It becomes:

$$\left| \frac{\omega}{\lambda} \right|^2 + \left| \frac{a}{b\lambda} + 1 + \frac{A' E}{\mu \lambda} - \frac{\gamma}{\mu \lambda b} \right| \frac{\omega}{\lambda} + \left| \frac{a}{b\lambda} + \frac{A' E}{\mu \lambda} \frac{a}{b\lambda} - \frac{\gamma}{\mu \lambda b} \right| = 0 \quad (57)$$

We introduce the following notations:

$$\frac{\omega}{\lambda} = \Omega \quad \frac{a}{b} = \lambda_1 \quad (58)$$

(λ_1 is the "decay constant" for heat in the fuel rod.) Eq. (57) moreover comprises the following groups of parameters:

$$1 + \frac{A' E}{\mu \lambda} \text{ and } \frac{\gamma}{\mu \lambda b}$$

which we are going to calculate separately. By considering equations (47) through (50), and (55), we have

$$\frac{A' E}{\mu \lambda} = \frac{\beta (k_{\infty} e^{-B^2 \tau})^*}{-L^2 B_n^2 + (1 - \beta)(k_{\infty} e^{-B^2 \tau})^* - 1} \quad (59)$$

Since the reactor is critical in the steady state,

$$k_{\infty} e^{-B^2 \tau} = 1 + L^2 B^2 \quad (60)$$

B^2 is the buckling of the fundamental mode. Hence,

$$\frac{A' E}{\mu \lambda} = \frac{\beta (1 + L^2 B^2)}{-L^2 B_n^2 + (1 - \beta)(1 + L^2 B^2) - 1}$$

and

$$\begin{aligned} 1 + \frac{A' E}{\mu \lambda} &= \frac{-L^2 B_n^2 + (1 - \beta)(1 + L^2 B^2) - 1 + \beta (1 + L^2 B^2)}{-L^2 B_n^2 + (1 - \beta)(1 + L^2 B^2) - 1} \\ &= \frac{L^2 (B_n^2 - B^2)}{L^2 (B_n^2 - B^2) + \beta} \end{aligned} \quad (61)$$

In the last equation, the very small quantity $L^2 B^2 \beta$ has been neglected. Also,

$$\frac{\gamma}{\mu \lambda b} = \frac{\alpha (k_{\infty} e^{-B^2 \tau})^* \phi^* \Sigma_f \zeta}{\left[-L^2 B_n^2 + (1 - \beta)(k_{\infty} e^{-B^2 \tau})^* - 1 \right] C_s} \quad (62)$$

From eq. (28) in the steady state and eq. (29) and (58), we have

$$\frac{\phi^* \Sigma_f \zeta}{C_s} = \lambda_1 (T_F^* - T_c) \quad (63)$$

We now introduce

$$\delta = \alpha (T_F^* - T_c) \quad (64)$$

δ is the total reactivity controlled by Doppler between the coolant temperature and the (average) fuel temperature. δ is positive in the ordinary conditions, i.e., a decrease of reactivity when the fuel temperature increases (see the definition of α , eq. (34)).

Using again eq. (60), we finally write (62) in the form:

$$-\frac{\gamma}{\mu \lambda b} = \frac{\delta (1 + L^2 B^2) \frac{\lambda_1}{\lambda}}{L^2 (B_n^2 - B^2) + \beta} \quad (65)$$

Hence, the final form for eq. (57) is

$$\Omega^2 + \frac{\lambda_1}{\lambda} \left[1 + \frac{L^2 (B_n^2 - B^2) \frac{\lambda}{\lambda_1} + (1 + L^2 B^2) \delta}{L^2 (B_n^2 - B^2) + \beta} \right] \Omega + \frac{\lambda_1}{\lambda} \left[\frac{L^2 (B_n^2 - B^2) + (1 + L^2 B^2) \delta}{L^2 (B_n^2 - B^2) + \beta} \right] = 0 \quad (66)$$

5.2.8 Condition for Oscillations

Equation (66) is in the familiar form

$$\Omega^2 + b\Omega + c = 0 \quad (67)$$

which has a general solution in the form

$$\varphi(\vec{r}, t) = \varphi(\vec{r}) \exp\left(\frac{-b \pm \sqrt{b^2 - 4c}}{2} t\right) \quad (68)$$

Oscillations (either damped or undamped) can exist only if equation (67) has complex roots. This condition is determined by the sign of $b^2 - 4c \equiv \Delta$; negative values of Δ indicate the existence of oscillations. From equation (66) we have

$$\Delta = \frac{\lambda_1^2}{\lambda^2} \left[1 + \frac{L^2 (B_n^2 - B^2) \frac{\lambda}{\lambda_1} + (1 + L^2 B^2) \delta}{L^2 (B_n^2 - B^2) + \beta} \right]^2 - \frac{4\lambda_1}{\lambda} \left[\frac{L^2 (B_n^2 - B^2) + (1 + L^2 B^2) \delta}{L^2 (B_n^2 - B^2) + \beta} \right] \quad (69)$$

If we expand the first bracket, we note the double product is not very different from the last term in equation (69). We can rewrite the last term in the form

$$\frac{4\lambda_1^2}{\lambda^2} \left[\frac{L^2 (B_n^2 - B^2) \frac{\lambda}{\lambda_1} + (1 + L^2 B^2) \delta \frac{\lambda}{\lambda_1} + (1 + L^2 B^2) \delta - (1 + L^2 B^2) \delta}{L^2 (B_n^2 - B^2) + \beta} \right]$$

The sum of the first and third terms in this expression corresponds exactly, except for a factor of 2, to the double product. Consequently, Δ may be written as

$$\Delta = \frac{\lambda_1^2}{\lambda^2} \left[1 - \frac{L^2(B_n^2 - B^2) \frac{\lambda}{\lambda_1} + (1 + L^2 B^2)\delta}{L^2(B_n^2 - B^2) + \beta} \right]^2 - \frac{4\lambda_1^2}{\lambda^2} \left[\frac{(1 + L^2 B^2)\delta \left(\frac{\lambda}{\lambda_1} - 1\right)}{L^2(B_n^2 - B^2) + \beta} \right] \quad (70)$$

We investigate the conditions which will lead to negative values of Δ . We know that the decay constant for delayed neutron precursors (λ) is on the order of $1/12 \text{ sec}^{-1}$; the heat "decay constant" for the fuel (λ_1) is roughly $1/2 \text{ sec}^{-1}$. Hence the expression $(\lambda/\lambda_1 - 1)$ is generally negative. Since $B_n^2 - B^2$ is either positive or zero, the last term in equation (70) must be positive if δ is positive. (Recall that a positive δ corresponds to a negative Doppler coefficient.) Since the first term in equation (70) is squared, it obviously is positive for all conditions. Consequently, for the conditions normally existing in a PWR, regardless of size, the roots of equation (66) are real and no damped or undamped Doppler oscillations are predicted. In order to have Doppler oscillations, the decay constant for delayed neutron precursors would have to be greater than the heat "decay constant" or the Doppler coefficient would have to be sufficiently positive.

5.2.9 Sign of the Roots

We now investigate the sign of the roots of equation (66) to determine whether the solution is an increasing exponential (unstable condition) or a decreasing exponential (indicating stability). We can prove that both roots are negative by proving:

1. The product of the roots is positive, and
2. The sum of the roots is negative.

The product of the roots is given by c in equation (67). Hence, the product is

$$\frac{\lambda_1}{\lambda} \left[\frac{L^2(B_n^2 - B^2) + (1 + L^2 B^2)\delta}{L^2(B_n^2 - B^2) + \beta} \right]$$

This is positive for positive values of δ . In order for the expression to be negative, δ would have to be negative and its value greater than δ_1 .

$$-\delta_1 = \frac{L^2(B_n^2 - B^2)}{1 + L^2 B^2} \quad (71)$$

Since $L^2 B^2$ is small with respect to 1, the condition for instability may be expressed as

$$-\delta_1 > L^2(B_n^2 - B^2) \quad (72)$$

which states that the positive reactivity due to Doppler must be greater than the difference in thermal leakage between the fundamental and higher harmonic modes.

The sum of the roots of equation (67) has the opposite sign of b . From equation (66) we note that the coefficient b must be positive if δ is positive; hence, the sum of the roots is negative. We have proved that both roots of equation (66) are negative, which indicates a stable condition.

In order for b to be negative, it is necessary that δ be negative with a value greater than

$$-\delta_o = \frac{L^2 (B_n^2 - B^2)(1 + \frac{\lambda}{\lambda_1}) + \beta}{1 + L^2 B^2} \quad (73)$$

This condition requires an even greater positive Doppler coefficient than required by equation (71).

If the heat decay constant (λ_1) is somehow changed so that Δ given by (69) becomes negative, true oscillations could occur. In that case, the damping is given by $-b$. This is a negative quantity unless δ is negative with a value greater than δ_o given by (73). This requires a strongly positive Doppler coefficient, as previously discussed. Consequently, even if Doppler oscillations are incited in some unforeseen manner, a strong negative damping exists as long as the Doppler coefficient is negative.

5.2.10 Estimate of Time Constants

In order to crudely estimate the time constants of the flux solutions to equation (66), consider the fundamental mode of perturbation ($B_n^2 = B^2$). If we neglect $L^2 B^2$ with respect to 1 (very large PWR), equation (66) takes the form

$$\Omega^2 + \frac{\lambda_1}{\lambda} (1 + \frac{\delta}{\beta}) \Omega + \frac{\lambda_1}{\lambda} \frac{\delta}{\beta} = 0 \quad (74)$$

As a reasonable estimate, $\lambda_1/\lambda \approx 6$; $\delta/\beta \approx 3$.
Hence, the equation is approximately

$$\Omega^2 + 24 \Omega + 18 = 0 \quad (75)$$

The roots of this equation is approximately -1 and -23; consequently, one of the time constants is of the order of the average time constant for delayed neutrons, whereas the other one is considerably smaller.

6. FUEL CYCLE STUDIES

6.1 SELECTION OF REFERENCE METHOD OF FUEL MANAGEMENT

6.1.1 Summary

Several fuel cycling methods were studied to determine a satisfactory method of refueling the 1000 MWe PWR. For reactors of very large size and high burnup the standard regionwise cycling methods are relatively unattractive. A greater potential is indicated for refueling methods wherein the fresh and exposed fuel is mixed throughout the reactor.

6.1.2 Introduction

Fuel cycling problems are considerably different for a 1000 MWe core than for the conventional sized reactors designed previously. The very large size of the reactor (diameter approximately 12-13 ft. or roughly 50 migration lengths) results in little coupling between distant parts of the core and makes "local criticality" problems more severe. Relatively small local reactivity effects can cause a large change in flux and power distributions.

Another complexity results from the high design burnup. Average discharge burnups of approximately 24,000 MWD/MTU are postulated in the equilibrium cycle. With high burnup a large reactivity difference exists between fresh and burned fuel. Since the power distribution is very sensitive to the reactivity distribution, the high burnup may accentuate power distribution problems.

In order to analyze these effects several fuel cycling schemes were studied and the power distribution and burnup for a given enrichment were compared. In each case, emphasis was placed on the equilibrium cycle.

A reference core design was selected and used throughout the cycling studies. This reference design has a water to UO_2 volume ratio of 1.67 and a steel to UO_2 volume ratio of 0.16. An enrichment of 3.4 w/o U-235 was used.

These parameters do not necessarily correspond to the final design of the 1000 MWe core; nevertheless, the comparison between different fuel cycling schemes will be applicable to the final design. The reflector properties were based on present PWR designs. The reflector was approximated by a pure steel region 1.75 cm thick followed by a pure water region 10.8 cm thick. An active core diameter of 394 cm was used.

The CANDLE code was utilized for all cycling studies. CANDLE is a 4-group diffusion theory code which calculates burnup effects pointwise. No control rod effects were considered in the calculations. A special editing routine calculates the integrated and average isotopic number densities of each element regionwise. This assists in running successive cycles in a fuel cycling study. The non-uniform Doppler effect is not considered and consequently the power peaking from CANDLE should be conservative. However, CANDLE does not account for local peaking effects such as that caused by water slots and control rod followers.

The results of various refueling schemes are presented in the following sections.

6.1.3 Uniform Batch Loading

As a reference for later studies, a uniform batch core was investigated. The entire core was assumed to be loaded with fresh fuel at 3.4 w/o U-235. The core operates until it is no longer critical at full power, at which time the entire core is replaced by a fresh loading.

The average discharge burnup was 15,250 MWD/MTU (Megawatt days per metric ton of uranium). The radial power distribution at beginning and end of life is shown in Figure I-19. The ratio of maximum to average power in the radial direction (F_R) is 1.99 at the beginning of life. The power distribution becomes more uniform with burnup.

The uniform batch core is characterized by low burnup for a given enrichment, high peaking factors, and a large control requirement. Its main advantages are simplicity and a minimum downtime for refueling.

6.1.4 Out-In 3 Region-3 Cycle Core

With this method of refueling, the core is divided into 3 concentric regions of equal area. At the end of an equilibrium cycle, fuel is discharged from the central region for reprocessing; fuel in the other regions is moved inward one region and fresh fuel is added in the outer region.

The properties of the equilibrium cycle were determined as follows: The variation in the concentration of all isotopes was determined and plotted as a function of burnup from the results of a uniform batch core. Estimates were then made of the average discharge burnup and the fractional energy release in each region. From these estimates, the initial burnup in each region was determined and the isotopic number densities were determined from the graphs. After running the CANDL the cycle time and the fractional energy release in each region were compared with the estimates. Also, the isotopic number densities at the end of life in each region should coincide with the beginning of life number densities in the next inner region. After making these comparisons, a new set of estimates was made and another CANDL was run. This process was repeated until the results from CANDL were consistent with the estimates. Generally, excellent convergence can be obtained in 3-5 iterations. The final results should be representative of an equilibrium cycle. As a final check on the method, the converged case was actually cycled by shifting the number densities inward at the end of a cycle and placing fresh fuel in the outer region. Successive cycles were repetitive within a small error, indicating an equilibrium cycle had been determined.

For the out-in 3-region 3-cycle core, the average discharge burnup was 20,440 MWD/MTU or 1.34 times the burnup from a batch core. The radial power distribution is shown in Figure I-20. The radial maximum to average power density is 1.96 at the beginning of life. A large fraction of the power is generated in the outer region which contains the fresh fuel. The large reactivity difference between the fuel in the inner and outer regions plus the large distance between them results in very little power being generated in the central region. The coupling between the regions of high and low reactivity is so weak that the fresh fuel provides little support for the burned fuel.

The ratio of cycled burnup to batch burnup is considerably lower than that found for a smaller reactor. The relatively low burnup and the poor power

FIG. I - 19

RADIAL POWER DISTRIBUTION IN 1000 MWE PWR
OUT-IN 3 REGION - 3 CYCLE CORE; EQUILIBRIUM CYCLE

FIG. I - 20

distribution make this method of refueling relatively unattractive in the 1000 MWe PWR.

6.1.5 Out-In 5 Region-5 Cycle Core

This refueling concept is identical to the previous one except 5 regions are used rather than 3. The power distribution is shown in Figure I-21. With 5 cycles, the average discharge burnup is increased to 21,960 MWD/MTU, or a ratio of cycled burnup to batch burnup of 1.44. However, the power distribution is still unfavorable, with a radial maximum to average power ratio of 1.92.

6.1.6 3 Region 3-1-2 Cycle Core

Since the results of the out-in cycle studies indicated the coupling between the fresh and burned fuel must be increased to obtain higher burnups and satisfactory power distributions, another fuel cycle was studied which retains region-wise cycling while promising to offer better coupling between fresh and burned fuel.

Fresh fuel elements are inserted in the intermediate region; they are subsequently shifted to the outer region and finally to the inner region from which they are discharged for reprocessing. The region of fresh fuel is adjacent to the region of most highly burned fuel. This tends to increase the power in the region of most highly burned fuel and results in somewhat higher burnups than obtainable with the out-in cycle. The burnup in the equilibrium cycle is 21,740 MWD/MTU or 1.43 times the batch burnup. The radial power distribution is plotted in Figure I-22. The peaking factor is 2.28 at the beginning of life; this is even worse than the out-in cycle. The higher peaking factor is due to the increased importance of the fresh fuel when it is located nearer the center of the reactor.

If the fresh fuel were loaded in the central core region, the peaking would be even greater. These studies indicate that for a reactor having the size and burnup requirements of the 1000 MWe plant, any regionwise refueling scheme is relatively unattractive. Whenever a sizeable fraction of the core is replaced by fresh fuel, a large power peak always occurs in that region and the remainder of the core serves as a multiplying reflector.

6.1.7 Roundelay 3 Batch

Due to the difficulties inherent with regionwise cycling, it is of interest to consider methods of refueling which intimately mix the fresh and burned elements throughout the core. One such method which appears promising is designated "Roundelay Multi-Batch". At the end of an equilibrium cycle, fresh elements are inserted in a uniform distribution throughout the core (e. g., with Roundelay 3-Batch, every third element is replaced). The other elements are not moved. Since the elements which are replaced in any cycle have all been exposed for the same period of time, and since the power density is non-uniform throughout the reactor, the discharged elements will have a rather wide variation in burnup. The ratio of maximum to average burnup will be higher than for some other refueling schemes. This could be a potential disadvantage if the maximum burnup is limited due to physical or mechanical properties of the materials, and if the higher maximum burnup would cause this limit to be exceeded. However, this method has several potential advantages. One major advantage is that the gross distribution of burnup is proportional to the gross power distribution. Neglecting

local effects, the average burnup toward the center of the core is higher than that toward the outside. This is true throughout the entire equilibrium cycle. This results in power flattening similar to that in a non-uniformly loaded core. If the elements are allowed to remain in the core until a given burnup is attained, this power flattening advantage is lost, although the maximum to average burnup ratio is reduced. A second advantage of Roundelay Multi-Batch refueling is that the fuel elements are not handled between the time they are first inserted and the time they are removed for reprocessing. This may reduce refueling time since only a fraction of the elements are handled at each refueling instead of all the elements. A third advantage of this method of refueling is the significantly higher burnups which can be obtained. The mixing of fresh and burned elements results in excellent coupling and significant power can be produced from elements which are highly burned.

To estimate the properties of an equilibrium cycle, it was assumed that the fresh and burned elements in any region of the core could be homogenized and an average burnup used. This approximation eliminates the local peaks and dips which occur due to the presence of elements of different burnups. This local effect could be expected to be rather important since a fuel element dimension is on the order of three migration lengths. The local effect must be checked in detail in later calculations. Since the CANDLE code requires input parameters to be constant within a region, the core was arbitrarily divided into 5 regions. The fractional energy release in each region was estimated, along with the cycle burnup. These estimates determine the average burnup in each region at the beginning of an equilibrium cycle. The isotopic number densities are then determined from the set of curves which are plotted against burnup. After running the CANDLE, the fractional energy release in each region and the cycle burnup are calculated. On the basis of these results, new estimates are made and another CANDLE is run. This is repeated until the CANDLE results are consistent with the estimates. The final result represents an equilibrium cycle within the approximations.

With Roundelay 3-Batch refueling, the average discharge burnup was 23,290 MWD/MTU, or 1.53 times the batch burnup. The radial power distribution is shown in Figure I-23. A quite good power distribution is indicated, with a radial maximum to average ratio of 1.35. However, it must be remembered that the homogenization has eliminated the local ripples which could add a substantial peaking factor.

6.1.8 Roundelay 5 Batch

The refueling procedure is the same as Roundelay 3-Batch except that every fifth element is replaced after each cycle. This requires more shutdown time for refueling but improves the burnup and power distribution. The average discharge burnup is 25,830 MWD/MTU or 1.69 times the batch burnup. The power distribution is plotted in Figure I-24. The indicated maximum to average power ratio is 1.22, neglecting the local effects.

6.1.9 Comparison of Refueling Methods

The significant parameters for all the refueling methods are summarized in Table I-9.

RADIAL POWER DISTRIBUTION IN 1000 MWE PWR
OUT-IN 5 REGION-5 CYCLE CORE; EQUILIBRIUM CYCLE

FIG. I - 21

RADIAL POWER DISTRIBUTION IN 1000 MWE PWR
3 REGION 3-1-2 CYCLE CORE; EQUILIBRIUM CYCLE

FIG. I - 22

FIG. I - 23

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RADIAL POWER DISTRIBUTION IN 1000 MWE PWR
 ROUNDELAY 5 BATCH CORE; EQUILIBRIUM CYCLE

FIG. I - 24

Table I-9

CHARACTERISTICS OF DIFFERENT REFUELING METHODS FOR 1000 MWe PWR

FEED ENRICHMENT = 3.4 W/O IN ALL CASES

<u>Refueling Method</u>	<u>Discharge Burnup</u>	<u>Burnup Batch Burnup</u>	<u>P_{max} (Radial) P_{ave}</u>	<u>K_{initial} (No Xe)</u>
Uniform Batch	15,250	1.00	1.99	1.229
Out-In				
3 Region 3 Cycle	20,440	1.34	1.96	1.126
Out-In				
5 Region 5 Cycle	21,960	1.44	1.92	1.095
3 Region 3-1-2	21,740	1.43	2.28	1.158
Roundelay 3 Batch	23,290	1.53	1.35*	1.116
Roundelay 5 Batch	25,830	1.69	1.22*	1.081

*These values do not include local "ripple" effects.

The summary of all the fuel cycles studied indicates the Roundelay Multi-Batch refueling method has excellent potential and was selected for the reference design. Higher burnups, better power distributions and lower control requirements are indicated for that method. This suggests that additional work on the Roundelay Multi-Batch method is justified in order to further define its potential as well as its problem areas. Two such problems which must be considered are: 1) the effect of local non-uniformities; and 2) methods of approaching the equilibrium cycle. These problems are considered in detail in the next section of the report.

6.2 ANALYSIS OF ROUNDELAY REFUELING IN 1000 MWe PWR

6.2.1 Summary

Extensive calculations have been performed to study the effect of Roundelay Multi-Batch refueling in the 1000 MWe core. The results of this study confirm the conclusions of the previous simple analysis and indicate the Roundelay method is quite attractive since it results in higher burnups and improved radial power distributions over other refueling methods such as out-in cycling.

With Roundelay Multi-Batch refueling, the power distribution is improved when the fuel assembly size is reduced (less rods per assembly).

The reference design nuclear hot channel factor $F_{\Delta H}^N$ might be achievable with 673 fuel elements in the 1000 MWe PWR core. The reference design $F_{\Delta H}^N$ consists of an overall factor of 1.50 multiplied by a local peaking factor to account for the effect of water slots and control rod followers. This study indicates the overall factor might be attainable provided it is defined for the full power condition containing the effects of equilibrium xenon and Doppler.

It has been demonstrated that an equilibrium Roundelay fuel cycle can be reached relatively quickly and simply.

6.2.2 Introduction

Of the fuel management techniques considered in Section 6.1, the greatest potential is indicated for the Roundelay Multi-Batch method of refueling due to improved burnups and better radial power distributions. An additional advantage is that only a fraction of the elements are handled at each refueling, and the elements are not handled between the time they are first inserted and the time they are discharged for reprocessing.

To review the Roundelay refueling technique, the fresh fuel elements are distributed uniformly throughout the core, rather than being placed in a discrete region. At the beginning of an equilibrium cycle, with Roundelay 3-Batch refueling, a mixture of elements with 0, 1, and 2 cycles of exposure exists throughout the core. The mixing of the fresh and burned elements provides strong coupling between them and permits significant power production from the burned elements. This effect results in improved burnup. At equilibrium, the average burnup near the center of the reactor is higher than that near the outside, since the burnup distribution has the same shape as the power distribution. This nonuniform burnup distribution tends to flatten the power distribution; this effect can be quite significant and represents a principal advantage of Roundelay Multi-Batch refueling.

Since adjacent fuel elements can have greatly different burnups, their power production will also be different. This introduces a ripple on the overall power distribution. The ripple can be very substantial in amplitude, especially if the fuel element dimension is several diffusion lengths. The previous analysis (Sec. 6.1) did not consider the ripple, since the fresh and burned elements in any given core area were homogenized.

The purpose of this study is to analyze more thoroughly the properties of the Roundelay Multi-Batch refueling method including the effect of the local ripples, a more detailed analysis of the equilibrium cycle, and an analysis of a method to approach an equilibrium cycle.

The reference core design is the same as that used in the previous study. The water to UO_2 volume ratio is 1.67 and the steel to UO_2 volume ratio is 0.16. An enrichment of 3.4 w/o U-235 is used in the equilibrium cycle. These parameters do not necessarily correspond to the final reference design of the 1000 MWe core since the reference design was not determined at the start of the study.

The reference core uses chemical shim control. Ordinarily, control rods would be present in the core only to control transient effects including flux tilts and xenon effects. Although some of the power distributions calculated in this study might have been improved by assuming certain groups of control rods were inserted to shape the power, no credit for this effect was taken. It is felt that in a chemical shim reactor the number of control rods should be minimized and the control system should not be required to shape the power (except gross disturbances such as flux tilts). Other methods of shaping power which add complexity to the design (such as burnable poisons, multiple enrichments within an element, etc.) have not been considered in this study. Consequently, it can be seen that the Roundelay Multi-Batch refueling method, in itself, does not require any additional fuel element development; it relies simply on a different arrangement of the fuel elements in the core.

6.2.3 Studies for 264 Element Core

Using a basic 16 x 16 rod fuel element, the 1000 MWe core requires 264 elements. The core layout is shown in Figure I-25. One quadrant has been numbered to indicate which elements would be replaced during each refueling. For example, during one refueling, all #1 elements would be replaced; the next refueling would replace all #2's; then, all #3's would be replaced. The entire process is repetitive and the core quadrants are symmetric about the axes. The dashed lines in Figure I-25 indicate that various fuel regions can be approximated fairly well by concentric squares. With a minor additional approximation, the region boundaries can be made circular and each fuel region becomes an annulus of a circle. This approximation is particularly valuable, because it permits a one-dimensional analysis of the problem. The CANDLE code (a one-dimensional 4-group diffusion theory code with pointwise burnup) was used for all the burnup calculations. Each fuel annulus contains the same area as the number of fuel assemblies which it represents. The 264 element core then consists of 12 fuel regions in the CANDLE calculations. Beyond the core is a region of steel followed by the water reflector.

The burnup and corresponding number densities for each fuel region at equilibrium were estimated from the studies reported in Section 6.1. Fresh fuel was then introduced into the #1 positions in Figure I-25. The resulting power distribution was extremely poor, with a power spike in the center giving a maximum to average radial power ratio (F_R) of 3.77. The power spike results from having four unburned fuel elements together in the center of the core (one in each quadrant). This example is evidence of the futility of considering regionwise cycling in a very large core. Whenever several fuel elements of high reactivity are placed together, a large power peak occurs. The radial power distribution is shown in Figure I-26. It should be pointed out that the case presented is not a fully converged equilibrium cycle. Several succeeding cycles are required to prove that equilibrium has been reached. However, the results of this case are sufficiently poor that no further effort on this particular design is justified.

6.2.4 Studies for 265 Element Core

Based on the foregoing results, it is evident that a core design is needed in which the central group of 4 elements is eliminated. This is possible, providing the center of the core occurs at the center of an element, rather than at the corner of four elements. A core layout which meets these specifications is shown in Figure I-27. This design has 265 elements and is represented by 13 core regions in CANDLE.

The equilibrium cycle was determined for this core design by estimating the burnup and the corresponding fuel number densities in each region. This estimate can be made from an approximation of the power distribution and a knowledge of the number of cycles to which each region has been exposed. Based on these estimates, CANDLE is run and the cycle lifetime is determined. At the indicated end of cycle, all the #3 elements in Figure I-27 were replaced by fresh fuel. The average number densities in each of the other regions were continued to the next problem. At the end of the next cycle, the #2 elements were replaced. This sequence of cycling (#3, #2, #1 elements) was continued until an equilibrium condition was reached. With Roundelay 3-Batch refueling the core conditions are repetitive every three refuelings; consequently, when three cycles from CANDLE are essentially identical with the preceding three

cycles, the equilibrium condition has been reached. (Here "a cycle" is taken to be synonymous with "the time between consecutive refuelings", although in the strict meaning of "cycle", it would be corresponded to three refuelings.) To describe the equilibrium condition, three consecutive cycles must be considered. Some results for the equilibrium cycles in the 265 element core are given in Table I-10.

The values of F_R in Table I-10 and throughout this report do not include local peaking caused by water slots and control rod followers. This would increase the values by approximately 15%. The effects of non-uniform Doppler and xenon are not included in the quoted F_R values. These will tend to reduce F_R in all cases; the effects of Doppler and xenon are estimated later in this report. Most of the diffusion theory codes used in this study do not calculate a power distribution. Instead, the distribution of the source of fission neutrons is calculated. Quite often, the source distribution can be taken to be identical to the power distribution with negligible error. However, errors can result when the relative concentrations of fissionable elements vary throughout the core, since the number of neutrons per fission (ν) and the energy released per fission varies for each element. The CANDLER code calculates both a source and a power distribution. From those results, it was determined that F_R based on the power distribution was approximately 3% greater than F_R based on a source distribution for all cases in this study. In order to facilitate the comparison of different calculations in this report, all values of F_R quoted in the tables are based on the source distribution; they must be increased by 3% to obtain the values as normally defined (based on power distribution). However, the curves throughout this report show the actual power distributions, rather than the source distributions.

Table I-10

EQUILIBRIUM CYCLES IN 265 ELEMENT CORE

Fresh Fuel Position #'s (See Fig. I-27)	Cycle Burnup MWD/MTU	* F_R (from CANDLER)
3	7500	1.95
2	7850	1.83
1	7700	1.52

*Does not include local peaking due to control rods, followers, water slots, etc.

The average discharge burnup at equilibrium is the sum of the three cycle burnups in Table I-10, or 23,050 MWD/MTU. This number can be compared directly with the value calculated in Section 6.1.7, which was 23,290. This indicates that the previous calculation based on a homogenized burnup was satisfactory. However, as expected, the power distribution from the homogenized calculation was not adequate; F_R of 1.35 should be compared to the values in Table I-10. It would be expected that the values of F_R in Table I-10 would approach the homogenized value as the fuel elements become smaller (less rods per element). Smaller elements provide a better mixing of the fresh and

THE DASHED LINES INDICATE THE FUEL GEOMETRY COULD
BE APPROXIMATED REASONABLY WELL BY CIRCLES

ALL FUEL ELEMENTS WITH THE SAME NUMBER ARE
DISCHARGED AT THE SAME TIME

CORE LAYOUT FOR 264 ELEMENT CORE

FIG. I - 25

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RADIAL POWER DISTRIBUTION IN 264 ELEMENT 1000 MWE PWR CORE
ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #1

FIG. I - 26

THE DASHED LINES INDICATE THE FUEL GEOMETRY COULD
BE APPROXIMATED REASONABLY WELL BY CIRCLES

ALL FUEL ELEMENTS WITH THE SAME NUMBER ARE
DISCHARGED AT THE SAME TIME

CORE LAYOUT FOR 265 ELEMENT CORE

FIG. I - 27

E. D. SK. 301458-B

burned fuel and result in less local ripple in the power distribution. The power distributions at the beginning and end of each of the three equilibrium cycles are presented in Figures I-28, 29, and 30.

One disadvantage of Roundelay refueling is the difference in burnup of different discharged elements. Since all elements are in their same core positions for three cycles before being discharged, and since the power density is low near the outside of the core compared to the center, the outer elements will have a relatively low burnup. This effect is presented in Table I-11 for the three equilibrium cycles of the 265 element core.

Table I-11

DISCHARGE BURNUP FOR THE EQUILIBRIUM CYCLES

A. End of cycle in which fresh fuel was placed in #3 positions

<u>Region*</u>	<u>No. of Elements</u>	<u>Discharge Burnup MWD/MTU</u>	<u>Avg. BU MWD/MTU</u>	<u>Max BU Avg BU</u>
2	4	27,450		
5	16	27,400	24,120	1.14
8	28	26,800		
11	36	20,200		
TOTAL	84			

B. End of cycle in which fresh fuel was placed in #2 positions

1	1	27,600		
4	12	27,500		
7	24	27,300	22,730	1.21
10	36	24,100		
13	16	8,900		
TOTAL	89			

C. End of cycle in which fresh fuel was placed in #1 positions

3	8	27,500		
6	20	27,400	22,350	1.23
9	32	25,950		
12	32	14,300		
TOTAL	92			

*Region 1 is the central region and Region 13 the outermost core region.

For the three cycles, the overall average burnup is 23,050 MWD/MTU and the overall maximum to average burnup ratio is 1.20. This ratio is calculated on an element basis; i.e., axial effects and variations of rods within an element are not considered.

Further work may lead to modifications in the refueling method which will result in higher burnups for the outer assemblies. One might consider moving the outer assemblies to a more central location after one or more cycles. However, it must be cautioned that such modifications can readily lead to worse power distributions, since the overall power flattening with Roundelay Multi-Batch refueling depends on a non-uniform radial burnup distribution.

In order to determine the validity of the power distributions from CANDLE, which uses a one-dimensional geometry approximation, the two-dimensional PDQ code was run for comparison. In the PDQ every fuel element in a quarter core was represented explicitly as shown in Figure I-27. No attempt was made to represent control rods, control rod followers, or water slots. PDQ's were run for two consecutive cycles which corresponded to putting fresh fuel in positions #3 and #2 (see Fig. I-27). Table I-10 indicates these two cycles have the worst peaking at equilibrium. The PDQ power distributions cannot be directly compared to the equilibrium values in Table I-10 or Figures I-28 and 29 because the PDQ cases were not the converged equilibrium cycle. The PDQ cases must be compared to the similar CANDLE cases, which are slightly different from Figures I-28 and 29. The PDQ results are shown in Figures I-31 and 32. Each fuel element has two numbers associated with it. The upper number is the ratio of average power in that element to the average in the core. The lower number, in parentheses, is the ratio of maximum to average power within that element. Also shown is the point in the core which has the highest power (F_R).

With fresh fuel loaded in positions #3, the PDQ gives $F_R = 1.99$. The corresponding CANDLE indicated $F_R = 1.95$. With fresh fuel loaded in positions #2 the PDQ gives $F_R = 1.82$ compared to the CANDLE value of 1.84. The power peaks occur at approximately the same core locations with either PDQ or CANDLE. These results indicate that the CANDLE calculations are sufficiently accurate that two-dimensional burnup calculations are not warranted in this study.

The studies on the 265 element core for the 1000 MWe core may be summarized as follows: Roundelay 3-Batch refueling has a radial peaking factor of approximately 2.0, not including the local effect of control rods, followers, water slots, etc., which may increase the factor by approximately 15%, if zircaloy followers are used. This radial factor is essentially the same as that for Out-In cycling or the uniform batch core. The Roundelay 3-Batch average discharge burnup is 1.51 times the batch burnup; this is a considerable improvement over the 3 cycle Out-In core, which has a burnup of 1.34 times the batch burnup. (See Section 6.1)

6.2.5 Studies for 673 Element Core

The foregoing studies failed to determine a refueling method for the 1000 MWe PWR with an acceptable radial power distribution. With Roundelay refueling, the local ripples become very important when the fuel element is large. This importance is evident from Figures I-28, 29 and 30. The importance of the ripples can be decreased by more thoroughly mixing the fresh and burned fuel elements. This is accomplished by using smaller elements. To investigate

RADIAL POWER DISTRIBUTION IN 265 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #3

FIG. I - 28

RADIAL POWER DISTRIBUTION IN 265 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #2

FIG. I - 29

RADIAL POWER DISTRIBUTION IN 265 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #1

.351 (1.284)	.311 (1.282)	.312 (1.322)	.210 (1.512)						
.777 (1.143)	.691 (1.167)	.554 (1.158)	.441 (1.303)						
.879 (1.106)	1.021 (1.097)	.891 (1.137)	.661 (1.162)	.522 (1.286)	.298 (1.492)				
1.088 (1.070)	1.029 (1.111)	1.198 (1.082)	1.012 (1.130)	.746 (1.137)	.549 (1.287)				
1.490 (1.074)	1.315 (1.084)	1.150 (1.080)	1.299 (1.075)	1.066 (1.129)	.745 (1.157)				
1.368 (1.101)	1.583 (1.075)	1.396 (1.074)	1.202 (1.073)	1.329 (1.075)					
1.463 (1.040)	1.394 (1.127)	1.641 (1.075)	1.424 (1.075)						
1.836 (1.061)	1.622 (1.066)	1.430 (1.121)							
1.604 (1.126)	1.851 (1.074)	$F_R = 1.99$							
1.561 (1.023)									

FOR EACH ASSEMBLY

UPPER NUMBER IS (AVERAGE POWER IN ASSEMBLY) / (AVERAGE POWER IN CORE)

LOWER NUMBER IS (MAXIMUM POWER IN ASSEMBLY) / (AVERAGE POWER IN ASSEMBLY)

P D Q POWER DISTRIBUTION IN 265 ELEMENT
1000 MWE PWR CORE; ROUNDELAY 3-BATCH CYCLE
WITH FRESH FUEL IN POSITIONS #3

FIG I - 31

E. D. SK. 301419-B

.307 (1.349)	.378 (1.324)	.382 (1.302)	.242 (1.535)						
.670 (1.152)	.626 (1.180)	.723 (1.137)	.561 (1.287)						
1.093 (1.096)	.981 (1.111)	.840 (1.091)	.885 (1.118)	.672 (1.271)	.354 (1.515)				
1.058 (1.090)	1.253 (1.076)	1.136 (1.089)	.959 (1.067)	1.006 (1.095)	.711 (1.272)				
1.177 (1.047)	1.124 (1.140)	1.369 (1.076)	1.225 (1.082)	1.011 (1.035)	1.004 (1.111)				
1.504 (1.062)	1.334 (1.066)	1.194 (1.147)	1.427 (1.075)	1.251 (1.081)					
1.308 (1.132)	1.530 (1.075)	1.368 (1.058)	1.214 (1.150)						
1.357 (1.036)	1.292 (1.156)	1.545 (1.075)							
1.711 (1.064)	1.487 (1.074)								
1.592 (1.066)									

$F_R = 1.82$

FOR EACH ASSEMBLY

UPPER NUMBER IS (AVERAGE POWER IN ASSEMBLY) / (AVERAGE POWER IN CORE)

LOWER NUMBER IS (MAXIMUM POWER IN ASSEMBLY) / (AVERAGE POWER IN ASSEMBLY)

PDQ POWER DISTRIBUTION IN 265 ELEMENT
1000 MWE PWR CORE; ROUNDELAY 3-BATCH CYCLE
WITH FRESH FUEL IN POSITIONS #2

FIG. I - 32

this effect, a 10 x 10 rod element was used. In order to retain approximately the same total number of fuel rods, 673 assemblies are required. A core layout for this design is shown in Figure I-33. Note that a layout has again been selected which has a single central element (rather than four).

An equilibrium cycle was determined for this case, using the methods described previously for the 265 element core. The CANDLE problems have 20 core regions in this case. The equilibrium cycle conditions are presented in Table I-12.

Table I-12

EQUILIBRIUM CYCLES IN 673 ELEMENT CORE

<u>Fresh Fuel Position Nos. (See Fig. 9)</u>	<u>Cycle Burnup MWD/MTU</u>	<u>F_R^* (from CANDLE)</u>	<u>F_R^* (from PDQ)</u>
3	7700	1.62	1.48
2	7800	1.62	1.47
1	7800	1.50	1.44

*Does not include local peaking due to control rods, followers, water slots, etc.

The power distributions from CANDLE for these cases are shown in Figures I-34, 35, and 36. It is evident that the radial peaking has decreased significantly in the smaller assemblies.

PDQ problems were run for each of the equilibrium cycles. It is quite surprising that the PDQ power distributions are significantly flatter than the CANDLE results since the previous comparison indicated good agreement in F_R between PDQ and CANDLE. Further analysis has indicated that neither the CANDLE nor PDQ results were fully converged; i.e., by using a tighter convergence criteria, the values of F_R increased significantly, although the eigenvalues were essentially unchanged. This is typical of the problems encountered in the calculation of power distributions in the 1000 MWe core. (See Table I-5) Rather large uncertainties now exist in the power distribution calculations; an extensive development program has been recommended to reduce these uncertainties. It is felt that the values of F_R in this report are significant in that they indicate trends which exist when different refueling methods, different fuel element sizes, etc. are used. Parameters such as convergence criteria and mesh spacing were held constant throughout the study to minimize the relative uncertainties due to these effects. The PDQ power distributions are shown in Figures I-37, 38 and 39.

A study was made, using the FAB-II code, to estimate the effects of non-uniform Doppler and xenon on the radial power distribution. FAB-II is a one dimensional diffusion theory code which includes options for calculating the effects of equilibrium xenon and/or Doppler. The results for a typical equilibrium cycle with Roundelay refueling in a 673 element core are presented in Table I-13.

Table I-13

EFFECTS OF XENON AND DOPPLER ON RADIAL POWER DISTRIBUTION

<u>Condition</u>	<u>F_R^* (from FAB-II)</u>	<u>% Reduction in F_R</u>
No Xenon, no Doppler	1.58	Reference
Doppler but no Xenon	1.45	8.2
Xenon but no Doppler	1.46	7.6
Equilibrium Xenon and Doppler	1.39	12.0

*Does not include local peaking due to control rods, followers, water slots, etc.

The Doppler and xenon effects are seen to be quite important in the very large core. For core configurations which have more peaking than the case shown in Table I-13 the percentage reduction in F_R due to Doppler and xenon would be even greater. This effect is particularly important where convergence problems are encountered. In such cases, the power distribution is extremely sensitive; after many iterations, the power distribution converges to a high value of F_R . However, if the power distribution is extremely sensitive, the Doppler will have a very large effect in reducing F_R . Consequently, the value of F_R including Doppler can be quite reasonable. Since the Doppler will be fully effective whenever the reactor is operating at full power, the power distributions based on no Doppler are of little importance. Unfortunately, most of the presently available codes do not include the nonuniform Doppler effect. The development program for the 1000 MWe PWR will include effort to incorporate this effect in additional codes.

The main conclusion to be drawn from the study of the 673 element core is that significant improvements in the radial power distribution result when smaller fuel elements are used with Roundelay 3-Batch refueling. In reducing the size from a 16 x 16 rod array to a 10 x 10 rod array, a comparison of Tables I-10 and 12 indicates F_R is reduced approximately 17%. Further improvement could be expected for even smaller fuel elements. The smaller elements have the disadvantages of added fabrication costs and additional handling, which could be significant during refuelings. It is recommended that a study be performed to determine the optimum fuel element size considering all of these effects.

Based on the results in Tables I-12 and 13, it may be possible to achieve the reference design value of the nuclear hot channel factor by using Roundelay 3-Batch refueling in a 673 element core. The reference design nuclear enthalpy rise hot channel factor ($F_{\Delta H}^N$) was 1.50 multiplied by a factor to account for the local peaking due to control rod followers and water slots. With zircaloy followers, the local peaking factor was assumed to be 1.15. Assuming equilibrium xenon and Doppler the value of $F_{\Delta H}^N$ (not including local peaking) is calculated to be approximately 1.5 based on Tables I-12 and 13. The assumption that the hot channel factor is defined for the full power condition with equilibrium xenon and Doppler is considered reasonable since the 1000 MWe plant is a base load unit. The definition is important in the large plant since the effect of xenon and

RADIAL POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #3

FIG. I - 34

RADIAL POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #2

FIG. I - 35

E. D. SK. 301451-B

RADIAL POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE WITH FRESH FUEL IN POSITIONS #1

FIG. I - 36

THE NUMBER SHOWN IN EACH ASSEMBLY IS
 (AVERAGE POWER IN ASSEMBLY)/ (AVERAGE POWER IN CORE)
 PDQ POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
 EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE
 FRESH FUEL IN POSITIONS #3

THE NUMBER SHOWN IN EACH ASSEMBLY IS
 (AVERAGE POWER IN ASSEMBLY)/ (AVERAGE POWER IN CORE)
 PDQ POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
 EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE
 FRESH FUEL IN POSITIONS #2

FIG. I - 38

E. D. SK. 301453-B

THE NUMBER SHOWN IN EACH ASSEMBLY IS
 (AVERAGE POWER IN ASSEMBLY)/ (AVERAGE POWER IN CORE)
 PDQ POWER DISTRIBUTION IN 673 ELEMENT 1000 MWE PWR CORE
 EQUILIBRIUM ROUNDELAY 3-BATCH CYCLE
 FRESH FUEL IN POSITIONS #1

Doppler is quite substantial, according to Table I-13. A transient factor of undetermined magnitude is required to account for non-equilibrium xenon conditions. No control rod effects (except the followers) have been included; consequently, the assumption is made that the control rod insertion will not worsen the radial power distribution.

Due to the above conditions and the fact that a large uncertainty exists in calculating power distributions in the 1000 MWe core, it cannot be stated positively that the reference design hot channel factors can be achieved; however, the results obtained thus far in the study are encouraging enough to indicate that the reference design hot channel factors certainly represent a reasonable target for a research and development program.

6.2.6 Approach to an Equilibrium Cycle

In order for a fuel cycle to be attractive, it must be possible to reach the equilibrium condition in a reasonable time and without severe fuel cost penalties. A generally accepted criteria is that a core should be essentially at equilibrium after two full core loadings have been discharged. With Roundelay 3-Batch refueling, this corresponds to six cycles.

There are several possible ways to approach an equilibrium cycle. Only one method has been studied in detail. Using this method, the first core loading consists of three enrichments arranged in a checkerboard pattern throughout the core to simulate the equilibrium cycle with Roundelay refueling. Referring to Figure I-33, positions #1 used 2.3 a/o enrichment, positions #2 used 3.1 a/o and positions #3 used 3.9 a/o. The average enrichment was selected to provide an average burnup of approximately 12,000 MWD/MTU in the first cycle. The variations about the average value were selected to simulate the reactivity variations in the equilibrium Roundelay cycle. At the end of the first cycle (determined by $K = 1.0$ with xenon present) the fuel in positions #1 was replaced by the equilibrium cycle enrichment (3.44 a/o). After two cycles, the #2 fuel is replaced by the equilibrium enrichment. The cycling continues, with fresh fuel always replacing fuel which has spent three cycles in the core. Calculations were performed for ten cycling steps. The pertinent results are presented in Table I-14.

It can be seen that the equilibrium cycle is approached rather quickly; after six cycles, the core conditions are essentially those of the equilibrium cycle. If the last three cycles (8, 9, 10) are characteristic of equilibrium, then all cycles beyond the first have power distributions (F_R) within 4% of the corresponding equilibrium cycle. It should be noted that the equilibrium cycle indicated in Table I-14 has slightly different characteristics from the equilibrium cycle presented in Table I-12. This is because a different refueling sequence is used in the two cases. In Table I-12 the order of refueling was positions 3-2-1. In Table I-14 the sequence was 1-2-3. By rearranging the enrichments used in the initial core loading, a different refueling sequence could be used in approaching the equilibrium cycle.

In Table I-14 the first cycle has a very poor power distribution initially. The peak flattens rapidly with burnup and after approximately 3000 MWD/MTU, it is reduced to values typical of the equilibrium cycle. Operation at part power may be required during this initial period. This might be avoided, however, by using a different fuel enrichment distribution; for example, the use of more than three enrichments could improve the initial power distribution. No attempt has been made to optimize the approach to the equilibrium cycle. The purpose of

this study has been to demonstrate that it is possible to reach an equilibrium Roundelay cycle in a reasonably simple manner.

Table I-14

APPROACH TO EQUILIBRIUM ROUNDELAY CYCLE

Fuel Step	Fresh Fuel # Positions	K_{initial} (No Xe)	$F_R =$	Cycle Burnup (MWD/MTU)	Ave. Discharge Burnup (MWD/MTU)	Peak Discharge Burnup (MWD/MTU)
			$P_{\text{max}}/P_{\text{ave}}$			
1	All	1.197	2.51	12,600	11,100	13,600
2	1	1.105	1.44	7,200	18,800	23,000
3	2	1.110	1.51	7,500	28,800	33,100
4	3	1.119	1.67	8,100	22,800	27,400
5	1	1.113	1.49	7,600	22,500	27,600
6	2	1.115	1.57	7,800	24,100	28,000
7	3	1.115	1.64	7,750	23,100	27,700
8	1	1.114	1.50	7,700	22,700	27,800
9	2	1.115	1.57	7,800	23,900	27,800
10	3	1.115	1.63	7,750	23,100	27,700

7. ACKNOWLEDGMENT

Acknowledgment is made of the contributions of Dr. M. Hoyaux, formerly head of Development Dept., Nuclear Division, ACEC; Charleroi, Belgium for assistance in the stability analyses performed as part of the nuclear evaluation of this report.

SECTION II - REACTOR VESSEL FEASIBILITY STUDY

I. INTRODUCTION

The functional requirements of the reactor vessel with respect to inside diameter and clear inside height necessary to contain a reactor core capable of producing 3220 MW of heat were established at the outset of this study program. These requirements dictated, in part, a vessel having an inside diameter of 202 inches at a design pressure and temperature of 2500 psia and 650°F, respectively. On previous large plant design studies, the largest obtainable reactor vessel was restricted to an inside diameter of 170 inches. Fabrication of a 202 in. I.D. vessel, therefore, was regarded as a major potential limitation toward building a single reactor 1000 MWe all nuclear plant. Consequently, the Combustion Engineering Corporation was requested to undertake a feasibility study of the reactor vessel and to quote a firm price and delivery. Their report on the reactor vessel employing short followers is incorporated verbatim on pages II - 1 through II - 41 of this section. The following variances should be noted:

- 1) The Combustion Engineering text refers to 57 adapters for the control rod mechanisms whereas the reference plant design requires 109.

This has no effect on vessel feasibility.

- 2) An alternate vessel support arrangement was used which, again, does not alter feasibility. The merits of both arrangements can be deferred for future study.
- 3) The "Welding Research Council Bulletin No. 82" and "1962 Adams Lecture" attached to the Combustion Engineering report are not included herein since these reports are readily available and do not lend themselves to reproducibility.

A brief discussion on multi-layer construction of reactor vessels is also furnished.

2. FABRICATION FEASIBILITY OF A 202" I.D. REACTOR VESSEL FOR A PRESSURIZED WATER SYSTEM

VESSEL REQUIREMENTS

Seldom, if ever before, has a pressure vessel fabricator been faced with a request to quote price and delivery for the design, fabrication, and testing of a vessel the size, weight, and quality required by this plant. Only as a result of a series of investigations for the feasibility of fabricating larger, and yet larger, reactor vessels has Combustion Engineering been able to undertake this request. On the next page is a sketch indicating the dimensional requirements for this reactor vessel.

The principal functional requirement for the reactor vessel is a clear inside height of 384-1/2" for a cylinder having a diameter of 187-3/4" below the closure head and 156" within the closure head. To provide space, proper shielding, and nuclear moderation for the reactor core, an inside diameter of 186" is required of the 4" thick thermal shield which would have a height of 186" also. (The thermal shield is allowed to encroach on the clear diameter of 187-3/4" required for the vessel.) An annular space of 4" is desired for the coolant flow between the thermal shield and the reactor vessel wall, necessitating the inside diameter of the main pressure vessel to be 202".

The reactor plant designers have standardized the components of the primary coolant loops. The capacity of these standard components necessitates having seven coolant loops for the withdrawal of power from the reactor pressure vessel. The pressure vessel must therefore have seven inlet and seven outlet primary coolant nozzles. The piping of the standard loop has an inside diameter of 27-1/2"; therefore, the stainless steel safe end on the reactor vessel nozzles would match this internal diameter. The kinetic energy of the fluid flowing in the inlet piping system is lost upon entering the reactor vessel plenum. The pumping power required to overcome this loss can be minimized if the expansion takes place gradually. The reactor vessel supplier, therefore, has been requested to furnish tapered nozzles having an inside diameter of 31-1/2" min. at a distance of 101" from the vessel centerline. Although the pumping power lost in accelerating the fluid out of the reactor vessel is not nearly as large as that lost by the fluid entering the reactor vessel, the vessel designer has been requested to furnish outlet nozzles of the same contour as the inlet nozzles. Provision must be made in the reactor vessel for separating the plenum being supplied with cold inlet fluid from the plenum containing heated fluid leaving the reactor vessel.

For purposes of connecting to a standardized piping loop, it is desired to have both inlet and outlet nozzles alternating on the same plane of the reactor vessel. To meet this requirement, an internal extension of the outlet nozzle would be required to match with an opening in the reactor vessel internals.

The reactor vessel internals (to be supplied under a separate contract) must be clamped between the reactor vessel head and the vessel body. Provisions are required for alignment of the internals with the reactor vessel head, through which the control drives extend into the reactor core configuration. The

DIMENSIONAL REQUIREMENTS
202" PWR VESSEL INQUIRY

vessel and head flange geometries are left to the discretion of the reactor vessel fabricator.

Through the reactor vessel head extend fifty-seven (57) adapters for supporting control rod drive mechanisms. Great precision is required of the manufacturer to meet the tolerance limitations on locations of each of the control rod mechanism housings and to assure the verticality of these housings in relation to the plane in which the alignment of the reactor vessel internals occur. It is necessary that precision machining of these surfaces be performed so that no misalignment of control rod actuators and the rods within the reactor core occurs. The above items are the principal considerations defining the configuration of the reactor vessel.

This pressure vessel must be designed for a pressure of 2,500 psi, at 650°F. The vessel must be designed, manufactured, inspected and stamped in accordance with Section VIII of the ASME Boiler and Pressure Vessel Code, the Addenda and Special Case Rulings in effect on the date of the purchase order. The analysis of the reactor pressure vessel would be in accordance with Document PB151987, the Tentative Structural Design Basis for Reactor Pressure Vessels and Directly Associated Components.

DESIGN DISCUSSION

Can a vessel meeting these performance specifications be designed, fabricated, and tested to the present ASME Code? The design of the vessel presents only a routine problem. Furthermore, the adoption of the proposed Section III to the ASME Code will result in a design easier to fabricate than permitted by the present Code. The theoretical problem of meeting Section VIII of the present ASME Boiler and Pressure Vessel can be solved in innumerable ways. Solution of the practical problem, of meeting the Code requirements and BEING ABLE TO FABRICATE the design, utilizes the full ingenuity of the designer. This ingenuity was obtained from experience with material suppliers, including an appreciation of the mechanical properties of thick plate, and the necessity for balance in attaining optimum placement of reinforcements, the abilities of the shop for which the design is detailed, the problems of inspection to assure the quality necessary for hazardous service, the handling and shipping problems, as well as numerous other details.

The resulting design must be a compromise of many features. It must never compromise the integrity, nor utility of the vessel, but, where necessary, compromise design details to obtain the most economic reactor vessel. This compromise does not minimize the cost of the reactor vessel alone, but must consider the cost of the reactor vessel internals, control rod drive mechanisms, system piping arrangements, and many other associated equipment costs, to minimize the system costs. It would be extremely tedious and quite boring to detail each of the decisions that the designer must make in order to accomplish a design meeting the Code requirements and the practical considerations of fabricating a vessel of this size. A few of these decisions, however, will be described on the following pages.

The thickness of the shell courses must be kept to the minimum Code requirement to permit the steel mills to obtain the maximum cross rolling of the ingots and to develop the best mechanical properties obtainable in the size plate that will be necessary to fabricate the reactor vessel economically. Herein, it

has been presupposed that the shell of the reactor vessel would be fabricated from plate. This is still an open area in the detailed design of the reactor vessel since ring forgings or forged plate may be desired as an alternate to the rolled plate shell. This presumption is basic to this report, all operations subsequently described would vary if plate were not used.

Having kept the plate shell to a minimum thickness, it is now necessary to place all the reinforcement for the vessel nozzle openings into the thickness of the nozzles. Consideration of the level of radiation that would occur directly opposite the nuclear core makes it desirable to locate the shell discontinuities as far from the core as possible. Consideration of the interaction between structural discontinuities in the cylindrical shell makes it desirable to separate the vessel flange and nozzles as much as possible. Consideration of the cost of the height of the control rod drive mechanism within the reactor vessel makes it desirable to place all nozzles on the same elevation. The necessity for having 14 nozzles of the size indicated results in the inability to locate all nozzles on the same elevation. A compromise has, therefore, been effected by placing two courses of nozzles in the shell of the reactor vessel, minimizing the distances between the nozzle levels and between the upper nozzles and the vessel flange.

Although this compromise increases the cost of the control rods and vessel internals and may change the cost of the external piping loops, it is not without some benefit. The welding of nozzles into a vessel shell course is a series operation. Each of the nozzles is to be welded from the inside of the reactor vessel in the down-hand position. If all 14 nozzles were located in one shell course, the time required for welding the nozzles into the course would be twice that required for this welding with the proposed design and an appreciable extension to the over-all schedule for fabrication would result.

It is unfortunate that the design requirements for the vessel and head flanges result in such a large size that no producer of ring forgings in the United States can supply these flanges as one-piece ring forgings. There is, however, no reason why two nor more forgings can not be welded together, fully inspected, and satisfactorily meet the requirements for this service. There are indications that foreign sources could supply one-piece ring forgings for these large, heavy flanges. A thorough investigation of these sources must be undertaken since the savings in cost and time can not be neglected.

As will be mentioned in the section under Materials, one area of concern related to the design of the reactor vessel is that of the mechanical properties of unusually heavy plates and forgings. Since Combustion Engineering has already purchased and formed plate of 10-1/2" thickness for a reactor vessel of 142" internal diameter, extensive information has been gained regarding the handling of these sections through heat treatment, quenching, and tempering operations and the attainable physical properties of these heavy sections. These plates, ordered to 10-1/2" thickness, weighed 62,200 pounds. The required plates for the 202" diameter reactor vessel would weigh half again this much, or approximately 96,000 pounds each. There is therefore the potential area for development of design background to determine the properties of this increased thickness of plate and how these properties can be maintained through the fabrication sequence. Although a high level of confidence exists in the ability of the material supplier to produce plate which can be made to meet the mechanical properties desired for application to this reactor pressure vessel, it is very desirable to secure such a heavy plate and process it through the forming, quenching, tempering, and stress relieving operations to determine what properties have been obtained with this material as will be discussed in detail later.

The next significant decision to be made under Code design consideration is the placement of reinforcements for vessel head openings in the thickness of the head itself. The geometry of the head penetrations relative to the diameter of the vessel permits the use of a hemispherical head. A hemispherical head is desirable for these large sizes since thinner sections are required for hemispherical heads than other shapes. A compromise may sometimes be required due to the cost of fabricating the extreme hillside penetrations which sometimes result from the decision to use a hemispherical head. However, in this case, the hillside nozzle penetrations are not severe. Placing all reinforcements in the head material permits the nozzles to be located on the required close pattern, and permits the use of a non-full-penetration weld for attachment of the housings. This type of weld will permit the accurate machining of the head penetrations in the vessel closure to be performed concurrently while the fabrication of the nozzle housings proceeds on a parallel production line basis.

The support structure for a vessel of such tremendous weight is complicated by the desire to minimize structural discontinuities which might lead to stress concentrations; to provide room for nuclear instrumentation; and, to avoid areas of high nuclear radiation and the effects of this radiation on the properties of the material. It was, therefore, decided to combine the analyses of the nozzle loads and the supports by including the additional set of load conditions for investigation of the nozzle shell area. This combination of loads is obtained by supporting the reactor vessel with U-bolts passing underneath the upper levels of nozzles. The use of U-bolt supports relieves the building constructor from the problems of precision leveling of the vessel support structure. The proper distribution of the weight of the vessel to each support is assured by the ability to measure the tension in each leg of the U-bolts. The same size stud tensioner may be used to tension the supports as is used in preloading the closure studs. The vessel is prevented from lateral movement by the "hinge" action of the seven U-bolts and the guidance of the square cross sections by building steel.

This discussion has covered the more significant areas which might be affected by requirements of the ASME Code and are applicable whether the present Code or the proposed new Code for nuclear vessels is applied. Based on the current draft dated October 1962, adoption of the proposed RULES FOR CONSTRUCTION OF NUCLEAR VESSELS, Section III of the ASME Code may permit a reduction in the basic wall thickness of the vessel. Otherwise, the adoption of this proposed Section of the Code would not be expected to significantly affect the design and fabrication of the reactor vessel as proposed for this application by Combustion Engineering, Inc. The requirements of the proposed rules relating to nondestructive examination of materials and the details covering the design of welded construction are, in general, consistent with the procedures currently employed by Combustion Engineering, Inc., for nuclear components and other applications requiring a high degree of structural reliability. Further, the caliber of engineering analysis, or design calculations, essential for compliance with the requirements for the acceptability of a design, as proposed in Section III of the Code, is no more demanding than those which would normally be performed by Combustion Engineering, Inc., for a vessel for this application.

Present indications are that the application of the proposed Section III will permit the wall thickness to be reduced in the cylindrical section from 13-7/8" minimum requirement for SA-302-B material to approximately 11-7/8" wall thickness. This is a very significant reduction in the thickness requirement and will have extensive ramifications in the design and delivery of the entire vessel.

Thus far, nothing has been said regarding the fabrication of the vessel completely within an existing manufacturing shop or the possibility of requiring partial field fabrication of subassemblies manufactured in an existing plant. Due to the many intrinsic values associated with fabrication of a critical vessel by a well-trained, stable work force, every effort must be made to complete the vessel in the manufacturing plant where familiarity with this class of products has been obtained by all workmen. This consideration should be given an even greater value than that of favorable economics associated with shop fabrication. Although there are not in existence all necessary facilities to manufacture this vessel expeditiously in the shop, most of the equipment is available; whereas, field erection and fabrication would necessitate building a partial manufacturing facility at the site of the nuclear reactor power station. The description of the additional facilities that would be required within Combustion Engineering shops will be included along with the description of the steps of manufacture involved for this vessel. The equipment necessary for partial field fabrication will be discussed in a later separate section.

Can a 202" inside diameter reactor vessel be designed, fabricated, and tested to meet the requirements of the ASME Boiler and Pressure Vessel Codes, Section VIII, Unfired Pressure Vessels, and Westinghouse - APD Equipment Specification 565118 as amended by Addenda I and II? The answer to the design question is an unqualified "Yes". To fabricate and test in accordance with these requirements, additional equipment must be available as will be detailed later.

MATERIALS

The design discussed herein is based upon the use of SA-302-B material for the shell and head segments. The flanges and nozzles are to be made from SA-336 (ASME Code Case 1236). Although the suppliers are unable to furnish these materials in the dimensions desired by the manufacturer, the vessel can be designed and manufactured from the sizes the mills can furnish. Combustion Engineering already has experience in forming, heat treating, quenching, and tempering SA-302-B plate ordered to 10-1/2" thickness. These plates were 116 x 180", and weighed over 62,000 pounds.

Plate manufacturers have produced only a limited number of plates in thicknesses greater than 11", and in the widths and lengths required by this design. A reduction of approximately four to one is obtainable in rolling ingots into 11" thick plate. To obtain thicker plates, smaller reductions in thickness during rolling must be accepted. The mills are not certain of the resultant soundness or physical properties in the thicker plates.

The mechanical properties of SA-302-B plate are fixed as a result of the quenching and tempering operations. Plate ordered to 13-7/8" minimum thickness will probably be delivered in a thickness of 14-1/4". In either thickness, "hot forming" is necessary as explained later. The mills, therefore, will not be asked to deliver the plate in the quenched and tempered condition. Experience has shown that a thickness of 14" will result in a slow cooling rate, on dip quenching, at the one-quarter thickness level, as well as at the center of the plate. This slow cooling rate, applied to presently used materials such as ASME SA-302-B plate material, will result in borderline charpy V-notch impact results, and in borderline tensile strength at the subsurface location. The figure shown on Page 11 shows the range of charpy values taken at 10⁰F for ASME

SA-302-B and SA-212-B materials in "normalized and tempered" as well as the "quenched and tempered" conditions. From this experience, it is concluded that both impact and tensile results with present standard materials may not meet the requirements of Table N-332 of the October, 1962 draft of the proposed Section III of the ASME Code. Even a reduction of thickness to 12", as possibly permissible under Section III, will not remove this question. It is therefore imperative that a full size plate be ordered to minimum thickness of 13-7/8" for testing to remove these doubts. If purchased from Lukens Steel Company, the plate will probably be 14-1/4" thick. This plate would be hot formed between contoured dies, closing the dies against the material to obtain a reproducible curvature. The 120° segment of formed plate would then be reheated, quenched and tempered. A portion of the plate would then be tested to determine the physical properties of this unusually thick plate. If satisfactory properties are not obtained, the plate would be heat treated once again to improve the properties, if possible. The contour of the piece would be measured to determine what changes occur during heat treatment. After testing the plate in this condition, it would be divided into three 120° parts to permit a cylindrical ring to be fabricated. The proposed welding techniques would be utilized and the resultant ring checked for out-of-roundness. The welds would then be tested for tensile and impact properties. With this experience, the vessel fabrication can be undertaken with confidence.

Twenty-one inch (21") thick forgings of Case 1236 forging material will probably meet yield strength minimums at the center of their thickness, but the tensile strength results will be borderline. The impact values at the center and possibly the 1/4 thickness level will be borderline. (See Figures 8 and 9 of Welding Research Council Bulletin No. 82 attached).

To arrive at a practical solution to the problems of handling, welding, and radiographic inspection of thick sections, a new material should be developed. This material should exhibit a slightly more hardenable analysis and an increase in the tensile properties. The possibility of developing such a new material has already met with favor in discussions with steel mills. The material might consist of a modification to SA-302-B material in which the carbon content is lowered, an addition of chromium on the order of 1% is made, and Boron is added on the order of .002 to .005%. Such an approach would require the actual making of a heat and working it into either a forging or plate as desired, followed by non-destructive testing, heat treatment simulating fabrication, testing of properties at various thickness levels, welding and testing of weld metal and heat affected zone properties, and submittal of these results to the ASME Code for a case interpretation. If the test of a full size plate of standard SA-302-B material suggested above shows that mechanical properties were not met, this second development program should be funded.

The thinner plate, which would result from use of a higher strength material, would allow a faster cooling rate at the center of the thickness and at all subsurface locations during dip quenching. This faster cooling rate would result in improved impact and tensile properties. Estimation of the possible cooling rate can be obtained from the data of the 1962 Adams Lecture, "Utilization of Quenching and Tempering for Improvement in Properties of Low Alloy Steels in Heavy Thicknesses for Welded Construction" by R. E. Lorentz, Jr. (copy attached). This data gives cooling rates possible in thicknesses up to 8-1/2". Further work is contemplated to determine the cooling rate of heavier thicknesses. Although extrapolation of the data in the paper to a thickness as heavy as 14" is of doubtful validity, the data indicates that the cooling rate at

the center of the thickness of a dip quenched material on the order of 14" thick will average 10°F. per minute or less from the quenching temperature to 825°F.

At this slow cooling rate, a material which exhibits a temperature time transformation diagram with transformations at longer times and lower temperatures is required. It is believed this can be accomplished in a new material with an analysis such as mentioned above.

Combustion Engineering suggests that experimental alloys of this type be produced and used to simulate the cooling rates of various thicknesses and determine the physical properties which result. Because a program of production and examination of full thickness and large size specimens is inordinately expensive, the work reported in the 1962 Adams Lecture was performed. The techniques developed therein will allow the use of smaller sample sizes for preliminary screening data, thereby minimizing the larger scale test work required.

The approach, described above, is timely and will reduce overall testing expenses as well as avoid expensive production equipment additions required for manufacture of the full-sized raw materials and handling the semi-finished and finished product. In addition, it is an approach compatible with further possible increase in operating pressure or temperature. The only question is what degree of increase in strength should be aimed for initially? A small increase in aimed for strength may be more readily obtainable in a practical time limit than a large increase in strength. The benefits from this research will extend to all equipment required in nuclear systems.

GENERAL FABRICATION PROCEDURES

When fabricating a reactor vessel of this great weight, it is especially important to plan all operations so that the delivery schedule can be met. The production planning begins after completion of the engineering required to meet the Code sizing and analyses of critical areas for the specified operating conditions, including thermal considerations. As soon as the designer has satisfied himself that all problems can be met within critical material ordering sizes, these critical materials will be purchased. Now, production planning and equipment burdening can be formalized.

There is a maximum weight of plate which the mills will now process. The maximum yield from an ingot is 50 tons, so, regardless of how you wish to dimension the plate, the gross weight can not exceed 100,000 pounds per piece. The mills will perform extensive testing and exercise careful control in fabricating these very large plates. They are quite interested in having plate used instead of flanges, and in assuring that quality is obtained in the plate material delivered. Not only will the mills test the plates, but Combustion Engineering, upon receipt, will immediately repeat a number of these tests and do further testing as the fabrication proceeds. Once the receipt inspection is complete, the manufacturer is ready to begin working the material.

In describing the completely shop fabricated vessel, the general procedure will be to discuss the fabrication problems in the sequence that operations are performed in the shop. Thus the description follows the component requiring the greatest elapsed time in the shop with back tracking to pick up the description of the subassemblies that are being made a part of the "critical assembly". From time to time certain equipment will be identified as not presently available,

and a rough word picture will be presented of the requirements of this new equipment. The transporting of the critical assembly as it moves through the shop will be discussed as a vital part of this description with mention of special jigs and fixtures which will be necessary for the complete fabrication. The various inspection procedures that must be used and tests that will be performed will be mentioned as they occur.

In this study, the assumption is made that the reactor vessel will be built with multiple piece forged flanges and with rolled plate shell courses. The engineering is complete. The order has been placed for 1,030,000 pounds of SA-302-B plate, 14-1/4" thick, in 12 pieces of such size as to make up four individual cylinders, each having three segments or three long seams. Over 282,000 pounds of nozzles and 238,000 pounds of forged material for flanges have been ordered. Later, as required by the schedule, the material for the upper head, weighing over 110,000 pounds, and the lower head, weighing over 120,000 pounds, will be purchased as "domes and segments". The material for the stainless steel thermal shields, weighing over 128,000 pounds; the studs, nuts, and washers, weighing over 75,000 pounds; and the weld wire for over 50,000 pounds of weld deposit stainless steel cladding, will be purchased.

The sequence in which the reactor parts are assembled must be carefully selected to minimize the number of special jigs and fixtures which might be required during the assembly operations and at the same time to minimize the problems of handling this large reactor vessel at its various assembly stages. After carefully weighing a number of possible assembly sequences, the sequence described herein has been selected for this feasibility study report.

The assembly sequence will join the vessel flange forging to the upper three shell courses. The fourteen coolant nozzles will then be inserted, and this assembly final machined to tolerances. The fourth, or lowest, shell course would be joined to the completed bottom head. The final fabrication steps would join these two assemblies by a weld which is only locally stress relieved. Inspection and shipment would complete the shop work. The reactor vessel head fabrication will be described, but is a distinctly separate assembly. The three piece thermal shield fabrication is not described since it is not unusual.

To assist the reader in visualizing the operations, sketches have been prepared to illustrate the shape of the part and the operations occurring. Please fold out the first of these sketches on Page II - 27,-28 and follow the description.

The description of fabrication operations will begin with the vessel flange. The reactor vessel flange has been designed with the required reinforcement located on the inside of the vessel. This decision requires that the thermal shield be segmented, but greatly reduces the flange procurement problem. The outside diameter of the flanges matches the outside diameter of the reactor vessel proper. The inside diameter of the flange permits clearance of the reactor vessel core barrel and internals, of course, but does not permit the thermal shield to be placed inside the reactor vessel as a continuous cylinder. This will require that the thermal shield be assembled inside the reactor vessel when erected at the site. As presently conceived, the vessel flange would be a ring of 230-3/16" (19' 3") outside diameter, 190-9/16" (almost 16') inside diameter x 31-1/4" in height. The vessel flange would be made from a ring having a rectangular cross section approximately 21" in width and 33" in height. None of the United States steel producers can supply this piece as a continuous ring forging. We must, therefore, purchase two forged segments, each weighing about 57,000 pounds, and join these segments.

The flange forgings will be purchased formed, quenched and tempered, to the requirements of ASME SA-336 (Code Case 1236). The welding of nickel-chrome-molybdenum forgings requires that the material be preheated before the welding is started and that the preheat temperature would be maintained until the parts are stress relieved. Therefore, the entire ring forging will be brought up to 300 to 400°F and maintained in this temperature range until fully stress relieved. In order to minimize distortion of the circular section of the ring during the welding of 19" of steel, the joint geometry will be designed for weld metal to be deposited from both the inside and outside of the ring. Fixtures must be made to stabilize this narrow ring, while preheated, in the vertical position while being repositioned between welding operations. The machine used for welding the joint from the outside in the downhand position will require modification to permit it to reach the necessary height above the floor. After the two segments are joined by welding and are fully stress relieved, each joint must be radiographically examined.

A discussion of the problem of radiographic examination of very thick sections seems to be in order. Combustion Engineering has a 15,000,000 electron volt Betatron for radiographic examination of heavy plate. Unfortunately, even such a powerful piece of equipment as this can not be used successfully to radiographically examine thickness in excess of 15". It will, therefore, be necessary to obtain more powerful radiographic inspection equipment in order to verify the integrity of the 21 inch weld seams placed in the vessel and head flange forgings. It is probable that new equipment for radiographic examination would have been required even for the 14 inch wall thicknesses in order to meet a reasonable delivery schedule. The 15,000,000 electron volt Betatron requires, for 14 inch thicknesses, an exposure time of 3 to 4 hours for each 8 to 10 inches of weld seam covered by a single film.

The reactor vessel proper would require at least 2,500 hours of exposure time for radiographs, plus the time of handling the vessel to and from the radiographic room. It may be possible to develop and prove other inspection techniques satisfactory for proof of quality of weld seams. However, alternate means are not recommended by Combustion Engineering at this time. The radiographic inspection problem is not a deterrent to meeting the code requirements. It does require an extended schedule and the procurement of more powerful equipment to radiographically examine the vessel and head flange forgings. A linear accelerator will satisfactorily meet the requirements for radiographic examination of these extremely heavy thicknesses and reduce the exposure time per film to approximately 20 minutes for each of the 14" thickness shots. The approximate cost of \$750,000 for a complete facility would probably be justified by the reduction in schedule and the cost associated with the use of the Betatron facility with the extremely thick sections. Furthermore, the entrance to the existing Betatron room is of limited width (19' 6") and the handling of the reactor vessel in the sequence now proposed would require rebuilding the 5' thick concrete shielding wall to gain access to this room after the nozzles are attached to the shell.

Returning to the fabrication sequence, the radiographic examination of the vessel flange ring has been completed. The piece will be rough machined to the contour required for subsequent cladding operations and the weld groove preparation made for joining the flange to the shell plate cylinder. This rough machining operation will be performed on the 20' mill where the weight of 104,000 pounds is within permissible table loadings. The vessel flange forging is now ready for assembly to the upper nozzle shell course.

We now back track to investigate what has been happening to the plates from which the reactor vessel shell sections will be manufactured. It is possible that the plates for the reactor vessel shell courses can be obtained from the material supplier in a shorter time than the flange forgings can be obtained. Consequently, the operations on the shell may be started sooner than the operations on the flange forgings, and the shell may be idle while waiting for the completion of the flange forging. The plate will be purchased from the mills in the asrolled condition, without heat treatment, to a slightly modified composition, falling in the chemistry requirement of ASTM-A-302, Grade B. Upon receipt from the mills, the plates will undergo inspection by ultrasonic methods to indicate the weldability of the material as received, and to locate any objectionable defects. The first operations required in moving toward the subassembly of the shell courses consists of forming the 120 degree sectors under the 6,000 ton press. Special dies for the press are required in order that this plate can be "hot formed" to obtain the final required curvature and dimensions without subsequent "cold sizing". Normally on thinner plates, material is "hot formed", then heat treated, quenched and tempered, and "cold sized" to the required finished dimensions. Experience indicates that extremely thick plates can not be successfully cold sized after heat treatment, quenching and tempering operations are completed. The cold sizing operation tends to yield or flow the material in extremely thick plate away from the local area being "sized", without generally causing the plate to yield in bending to assume the design contour. This operation can result in deep "shim" marks on the inside surface of the plate and will be avoided on this thick plate. After inspecting the hot formed plate to assure the proper diameter, the plate will be put in the heat treating furnace, taken to 1,500 - 1,600°F and held at this temperature for four hours. The plate is now removed from the furnace and quickly immersed in a brine solution. After the entire plate temperature is below 400°F, the plate will be tempered by reheating in a furnace to $1025 \pm 25^\circ\text{F}$, holding at this temperature for at least one hour per inch of thickness, then cooling in the furnace to 600°F. Since the large beam press is not capable of forming this or any plate thickness to the ends of the curved section, excess length will be ordered for the unformed ends. During the forming operation, the edges of the plate under the beam press will be rolled outward and downward while being formed, thereby, leaving an improper contour along these edges. Excess width of plate will be ordered to account for this loss of dimensional accuracy. The plate after all heat treating, quenching, and tempering operations, will be checked for dimensional correctness. In the preparation of the weld grooves for long seam welding, the unformed material (ends of 120° arc) will be machined off. Metallurgical samples will be removed from these prolongations of the shell plate and the mechanical properties determined for the material after the heat treatment, tempering, quenching and a simulation of the stress relief operations has been performed. Nine of the twelve plates must pass across the 6,000 ton press in sequence to meet the critical assembly requirements. Each of the plates for the upper shell course will weigh about 76,500 pounds when formed. Each of the plates for the second shell course will weigh about 77,000 pounds when formed. The plates for the lower two shell courses will each weigh about 93,000 pounds when formed. The preparation for long seam welding will remove from 8,000 to 10,000 pounds of material as prolongations from the unformed ends of each of the plates.

The assembly operations are identical for each shell course. The operations on the first or uppermost shell course will be used as descriptive of all shell assembly operations.

The upper shell course is the narrowest shell course in the reactor vessel. From the three formed plates of this width, the orientation to form the truest cylinder will be selected and fit-up for long seam welding. The cylinder will be fit around an internal jig and tack welded to hold the cylindrical position. The weld groove preparation permits tack welding along the seam on the inside of the vessel. The cylinder will be positioned in front of the long seam welding machine which, must be modified to reach the height necessary to perform this welding operation. First, some six to ten weld beads will be made in each of the three long seams of each cylinder by submerged, tandem arc, process from the outside, the beads extending from the "starting blocks" to the "run-out blocks". The internal jigs will now be removed and the tack welds and root of the submerged arc welds just made removed by machining from the inside of the cylinder. The root of the submerged arc welds are inspected by magnetic particle procedures and the welding from the inside completed using the submerged arc process. The welding of the long seams from the outside is then completed using the submerged, tandem arc process. With the making of the three long seams, the subassembly will weigh 198,000 pounds. This assembly will present no problem of floor loading or handling at this stage of fabrication.

From the time the three plates were tack welded to the best dimensions for the circular cylinder until post welding stress relieving can be performed, the assembly must be kept above 300°F to prevent brittle cracking of the material. As soon as these three welds are completed, this subassembly must go into a furnace for stress relieving before cooled for further inspection. Stress relieving of this subassembly and each of the other three shell courses can be performed in the existing heat treatment furnace. These, and subsequent weld seams, will be radiographically inspected for welding defects after stress relieving. Repairs will be made as necessary and the weld seams again stress relieved and re-examined for quality. The problems associated with radiographic examination have been discussed with the vessel flange. In the event new equipment is not utilized, the examination of long seams on each shell course will require about 100 hours of exposure time. When the upper three shell courses have been assembled into cylinders, each will have the girth seam weld joint geometry machined.

A most interesting and unusual assembly sequence is proposed for this very large reactor vessel. Normally each shell course prepared would be individually weld deposit clad with stainless steel material and stress relieved at this time. Then each shell course would be joined to an adjacent shell course requiring another cycle through the stress relieving furnace. Looking at the history of a normally fabricated reactor vessel, we find that some parts of the completed vessel would pass through the stress relieving furnace between 15 and 20 times. These plates have been quenched and tempered to obtain beneficial mechanical and impact properties for application to reactor vessel fabrication. Although experience with normal wall thicknesses for reactor vessels has not shown deleterious effects on the properties of the material due to stress relieving cycles, it has been decided that these many trips through the stress relieving furnace should be avoided where practical. Therefore, the following unique assembly sequence has been selected to obtain the best properties in the material of the finished vessel. The parts thus far completed consist of the reactor vessel flange and three separate shell courses. These four pieces would be assembled into the truest circular cylinder obtainable by orienting the various shell courses. The fourth shell course would be proceeding toward completion but would not be required for some time in the fabrication sequence. The long seams made in each of the three shell courses would not be permitted to fall opposite each other in this assembly. With this fit-up operation completed, the cylinders

would be tack welded to each other on the inside surface. This cylinder would now be positioned on large powerful turning rolls and three separate welding machines positioned to simultaneously perform the girth welding operations joining the four pieces. In order to reach the height above the floor required to do this girth welding, modifications to the existing welding machines would have to be performed. This assembly would weigh some 761,000 pounds and because of this great weight, special turning rolls would be required to accurately and uniformly rotate the cylinder under the welding heads. From the time the four parts are tack welded to assemble the upper vessel cylinder until the assembly is stress relieved, it must be maintained at a temperature greater than 300°F. The welding operation would proceed by laying 6 to 10 passes of submerged arc welding into the grooves from the outside of the reactor. At this time, the tack welding between the parts, as well as the root of the submerged arc welding bead in each girth seam would be removed from the inside of the reactor vessel cylinder. An inspection of the root of the submerged arc welding passes would be performed by magnetic particle means. The submerged arc welding heads would now be positioned inside the cylinder at the lowermost point and the girth seam inside passes would be completed, either simultaneously or in series. Following the completion of the welding from the inside of the cylinder, the welding heads would be relocated on the outside of the reactor vessel and the seams completed.

The 758,500 pound weight of this assembly exceeds the permissible weight for the car bottom of the large stress relieving furnace in our heavy vessel fabrication bay. The car would be reinforced to carry this weight were it not that the next operations would increase the diameter of the assembly to the point where the vessel would not go into the existing furnace. Therefore, a special fixture will be required for handling the assembly and a special furnace will be needed for the stress relieving of the assemblies from this point forward.

The special stress relieving furnace required for this reactor vessel is to be made up of demountable portions. The vessel would be positioned on cast support blocks a sufficient height above the floor of the bay to permit insertion around these cast support blocks of prefabricated firebrick insulation panels. The support blocks will have air cooling passages to keep the heat from being conducted through the blocks into the concrete floor. The walls of the furnace containing the burners will be in prefabricated panels which will be assembled to form a serviceable stress relieving furnace. The top panels would be placed over this assembly by use of the overhead crane. A sheet metal insulated stack would conduct the hot gases from the furnace out through the side of the building and above the roof line. It is estimated that the cost of this temporary furnace will be approximately \$250,000.

After stress relieving, the welds would be radiographically examined. This assembly, constituting the upper portion of the reactor vessel proper, would require almost 900 exposure hours of the 15,000,000 electron Betatron to produce the films necessary to verify the integrity of the girth welds. Using a linear accelerator would reduce this exposure time by a factor of 10 to approximately 90 elapsed hours. In either case, the time for positioning the vessel and placing the penetrometer and film in position would be the same. All defects located by this radiographic examination would require repair work and subsequent stress relieving and re-radiographic examination before subsequent operations were performed.

This subassembly weight can not be handled by the existing overhead crane in Bay 29. It is, therefore, necessary to use jacks and skids to position the assembly on the case support blocks on the temporary furnace and to move it to each subsequent position in the manufacturing cycle.

The stainless steel cladding will be applied by automatic submerged arc, three-wire method. This method permits deposition of a layer of stainless steel cladding of about 1/4" thickness in a single pass. A cylinder of this length could be weld cladded with existing equipment by reaching from opposite ends toward the center of the vessel. The reactor vessel shell would again have to be preheated prior to the application of the stainless steel. Care would be exercised in cladding the vessel flange forging, especially the contoured portions. Some hand welding will be required. This sequence of application of cladding over the entire subassembly once again reduces the number of times that the material would have to be processed through the stress relieving furnace, since in normal application the cylinders are cladded and after girth seams are made the joints must be overladded with stainless steel material and restress relieved once again. After the cladding has been applied, the reactor vessel would be stress relieved. If required, the clad surface would be examined by ultrasonic techniques to assure adequate bonding of the weld deposited material to the base metal. In any event, the cladding would be examined by dye penetrant techniques to assure soundness of the clad surface exposed to reactor coolant. It is desirable to modify our weld deposit cladding machinery to eliminate the use of the cantilever arm extending such a great distance from the end of the cylinder. Better control can be obtained of the weld deposit if the innerface height can be controlled closely in the welding operation. Realizing that this weld deposit cladding is being placed over the inside of cylinders which are in the as-formed and as-welded condition will require some special modifications to the weld deposited cladding machinery. One of these modifications will be the adaptation of the cladding head to ride on a beam extending the entire length of the cylinder and supported on each end by A-frames from the floor of the bay. The second adaptation of our weld cladding equipment will be an anticipator which would ride on the base material in front of the submerged arc deposition to anticipate the fluctuation of the distance between the base material and the welding head. The integration of the anticipator into the welding head may be accomplished prior to this contract, since Combustion Engineering has contracts requiring weld deposit cladding on as-formed cylindrical surfaces.

This assembly is now ready for the insertion of the coolant inlet and outlet nozzles. One inlet and one outlet nozzle will be inserted into the shell simultaneously. The cylinder will be braced to maintain roundness during the operations described hereafter. The two openings for the inlet and outlet nozzles directly adjacent will be laid out, either drilled or machined using a horizontal boring mill (or flame cut if this procedure is qualified) for the insertion and welding of the first large nozzles. At this point a restraint is reached in the assembly. The nozzles must have been fabricated and ready for assembly by welding into this major subassembly. Please fold out the second sketch on Page II - 31,-32.

Back tracking, a review will be made of what has been taking place in the preparation of each nozzle. The steps in the preparation of the nozzles will be repeated 14 times. Seven of these nozzles are used as coolant inlet passages and seven as coolant outlet passages. The outlet nozzles are located in the first or uppermost shell course, the inlet nozzles are located in the second shell course. As of this time, we can not state with assurance that a contour forging

can be obtained economically to satisfy the shape requirements for the tapered inside of these nozzles. It must be recognized that, in the design of this vessel, no reinforcing has been placed in the vessel shell; therefore, the nozzle must include all reinforcement. The reinforcement is designed as close to the vessel shell as is permitted by the Code in order to minimize the diameter of the furnace necessary for subsequent stress relieving operations. Upon receipt of each 22,000 pound forged nozzle from the supplier, careful receipt inspection and nondestructive testing will be performed. Areas where future welds are to be made must be completely ultrasonic inspected, including recording. Each nozzle will then be machined to the contour required for inside cladding and the final external contour. As the inside of the nozzle must conform to the 202" diameter, the nozzles will be set into fixtures and the vessel end machined to this dimension, using the 7 inch horizontal boring mill. The tapered surface of the nozzle throat will be blended into the 202" diameter surface by a combination of machining and hand grinding. The piece will be preheated and the stainless steel weld deposited cladding completed, continuing the preheat until the stainless steel safe end and carbon steel hydrostatic test caps are attached. Cladding the nozzle throats can be performed by machine welding; other areas must be performed with manual cladding overlay procedures. The safe end will be attached using an inconel weld. The nozzle is now fully stress relieved, the weld joints attaching the safe end and hydrocap radiographically examined, and the weld preparation on the outside surface prepared for welding into the shell courses. The nozzle is in its finished condition requiring only safe end weld preparation after the hydrocaps are removed.

We now go back to the main vessel subassembly and begin the repetitive process of putting 14 nozzles into the shell. The first two nozzles are carefully located and after the shell and nozzles have been preheated, tackwelded into position. The tackwelding would be done on the outside of the reactor vessel. The weld groove preparation for the attachment of the nozzles to the shell would be similar to the previous welds described in that a number of weld beads would be deposited using the submerged arc welding technique with the two special machines operating simultaneously in the adjacent nozzles. The operations would then be stopped and the outside weld groove preparation made by chipping away the tackweld and the root of the submerged arc passes already applied. The preheated assembly would be rotated until the two nozzles are at the top of the cylinder where the magnetic particle inspection of the root of the submerged arc welds could be made and the outside portion of the weld and fillet completed using manual welding procedures. The weld would then be completed from the inside of the reactor vessel using the two machines, and blending by manual welding. This assembly must be stress relieved prior to the radiographic examination of the welds attaching the nozzles to the vessel shell. The special furnace previously described would be utilized for the stress relieving operation and when the vessel is cooled, the nozzle welds would be radiographically examined. All repairs will be stress relieved and radiographically examined until acceptable. Only after the attachment welds are fully inspected would the bracing, inserted in the cylinder to maintain the roundness, be relocated for the operation of putting in the second pair of nozzles. As each pair of holes are cut into the shell, over 20,000 pounds of base metal is removed. However, since each pair of nozzles being welded into the shell course will weigh over 36,000 pounds, the weight of the shell course will gradually increase from 758,000 pounds, not including the stainless steel cladding, to over 863,000 pounds.

It is fortunate that one end of the assembly can be supported by the overhead crane while the turning rolls are replaced by the high strength cast supports utilized in the furnace. By using the technique of holding first one end

and then the other, the special skidding frame can be positioned under the vessel when it must be moved from one point to another. A number of steel plates would be purchased to act as a bearing surface on the concrete floor of the bay, spreading the load uniformly over a larger area. The turning rolls will be supported on a frame which can be moved over these base plates on rollers to permit the transporting of the vessel from the welding position to the furnace to the X-ray room, etc.

When all fourteen nozzles have been inserted into the reactor vessel, the final welding operations, prior to finish machine operations, will be undertaken. These welding operations consist of back cladding over the nozzle to nozzle shell course welds; the insertion of the internal support ring, which acts as a divider between the inlet and outlet reactor fluid plenums; and the external seal ring on the vessel flange, which is used during refueling operations. All weld repairs to the cladding would also be performed at this time. The vessel assembly would now pass through the furnace for the last planned stress relief. None of these welds will require radiographic examination. However, the clad surfaces will require a dye penetrant inspection for surface continuity. The critical assembly is now ready for final machining operations.

Final machining of the upper assembly of the reactor vessel is done on the reactor vessel flange surfaces, the surfaces and keyways of the internal support ring, the external seal ring, and stud holes. All machining would be done with a seven inch horizontal boring mill. The internal support ring must be machined in close tolerance relationship with the flange bearing surface, and vessel keyways. The reactor core support structure will be clamped between the head flange and the internal support ring, with keyways aligning the head, vessel, and internal structure.

The machining of these parts requires a special outboard bearing and boring bar arm to machine the 202" diameter so deep within the vessel. The other machining operations are not unusual for the horizontal mill. The machined surfaces of cladding will be dye penetrant inspected again. If repair to the clad surface is necessary, manual welding procedures will be used. These procedures have been qualified to permit minor repair to austenitic stainless steel without subsequent stress relieving. To assure that the heat affected zone from repair welding does not reach base metal, the surface to be machined will be "surveyed" as well as clad with extra thickness of stainless steel. It should be emphasized that "asdeposited" clad surfaces are in quite adequate condition for ultrasonic inspection and dye penetrant inspection. There is a uniform ripple in the surface, caused by successive weld beads, which does not interfere with the inspections or form pockets which might collect particles.

The stud holes would be accurately bored, their alignment checked in the straight bored condition, then threading operations would take place. It is necessary to check the alignment of these holes in the straight bored condition since there is no adequate way of measuring the parallelism of the holes after they are tapped. The tapping operation on the stud holes would complete machining of this major subassembly which is the critical delivery item on our fabrication schedule.

Another checkpoint in our critical path assembly of the reactor pressure vessel is now reached. It is time to back track and find out what has been happening to the bottom head of the reactor vessel. Please fold out the sketch on Page II - 31,-32. The bottom head has started out as a formed plate dome, sometimes referred to as a cap or dollar section. All segments of the bottom head

are ordered to a thickness of 6-9/16", but will probably be delivered as 7-5/16" thicknesses to assure the minimum requirement. The diameter of this cap measured across the concave surface is 60". The bottom head has two courses of segments. The first course has eight segments, carrying the hemispherical shape from the 60" diameter to 151" diameter measured across the concave surfaces. The second course of 10 segments completes the hemispherical shape enlarging the diameter from 151" to 207-7/16". All of these parts are formed on dies having a radius of 103". It will be noted that the resulting diameter of 206" is greater than the inside diameter of the reactor vessel; however, a complete hemisphere is not fabricated. The "tangent line" for the theoretical hemisphere is some 19" above the bottom head to bottom shell course weld junction. In preparing the bottom shell course weld groove for assembly to the bottom head, a taper of approximately 19" will be made on the bottom of the cylinder. This taper will blend the shell plate into the hemispherical head. By having the diameter of the hemispherical head greater than the inside diameter of the reactor vessel shell, the center lines of each of the components will nearly match and there is less stress discontinuity in this junction. Upon receipt of the head material from our supplier in the formed, quenched and tempered condition, Combustion Engineering will trim, fit, bevel and assemble the two courses prior to making the joining welds as a final operation in preparing the bottom hemispherical head. Each of the segments must have the welds (corresponding to the longitudinal direction on the sphere of the earth) prepared with run-out strips for the submerged arc welding. The weld grooves are prepared for all of the seams by use of special jigs on the 20 foot mill.

Each course of segments for the head will be joined as above and machined to contour on the large vertical boring mill. The inside surface would be weld cladded, stress relieved and inspected. The dome and first course are then joined, stress relieved, radiographically inspected, repaired, etc. The bottom head assembly is completed by joining the second course to this assembly and passed through the same inspection and heat treatment operations. This bottom head would now weigh 122,000 pounds and is ready to be joined to the lower shell course. Please fold out the last sketch, Page II - 33,-34.

The fourth shell course is identical with the courses which have been previously discussed with the critical assembly. It will weigh, upon completion, over 230,000 pounds. It will have gone through the forming, heat treating, quenching, tempering, long seam welding and internal cladding operations. It will have had attached to it the thermal shield support lugs and will then meet a point of subassembly with the bottom head.

The combined weight of the lower shell course and bottom head would be 355,000 pounds. After the girth weld has been stress relieved and X-rayed, all seams in the assembly would be back-cladded and stress relieved, completing the welding requirements prior to the final assembly of the reactor vessel shell.

When two final subassemblies are put together, the reactor vessel shell will weigh in excess of 1,200,000 pounds. The two subassemblies will be joined on a shipping fixture with special rolls for supporting and turning 600 tons for the final girth seam welding operation. The shell would be locally preheated in the vicinity of this final girth seam and the weld made in the usual manner, including the back chipping and back welding of the base material and the back cladding of the weld seam from the inside before the final local stress relief is performed. Obviously, these operations require a great deal of planning and involve discomfort for the welders inside the shell course, which is preheated

above 300°F. When the stress relief is complete, the piece will be brought down to normal temperatures and the radiographic inspection of the final weld performed at this location. This will require bringing the 15,000,000 volt Betatron head or linear accelerator, out of its shielded room, by the use of extension cables, to the shipping area at the end of Bay 29. The Betatron has such intense penetration ability that only little "back-scattering" results. The use of one inch steel shielding plates for back-scattering and careful monitoring will permit radiographs to be made in a roped-off area. Repairs of this weld would be made if required, the seam again stress relieved by the local stress relief technique and re-inspected by radiography.

Careful measurement of the as-built dimensions would now be performed on the shell to determine whether interference is avoided with the core internals and the thermal shield. These pieces would not be fit-up inside the reactor vessel prior to its being erected in place at its final destination. The unit would, however, be hydrostatically tested before it left the plant. In order to do so the closure head must have been completed by this time.

Once again the clock is turned back to see what has been happening with the closure head for the reactor vessel. The closure head will consist of a dome segment, and a single course of ten segments. The diameter of the dome, or cap, will be 80 inches. It will be formed on a radius of 97 inches, which is smaller than the radius of the lower head. The smaller radius reduces the thickness required when the control rod housings are inserted, but this head must be ordered to 8-5/8 inches thickness, with delivery of 9-3/8 inches thickness probable. This reduced radius will require machining of the male and female dies used in forming the lower head dome and segments. The reduction in radius, and the reduced diameter of the closure flange compared with the reactor vessel permits the completion of the closure head with a single course of segments. The operations in preparing the spherical portion of the closure head are the same as those explained for the lower head. It is probable that the dome, the course of segments, and the closure flange will be clad with stainless steel as individual items. This will permit the use of lower capacity weld positioners, without necessitating excessive stress relieving operations. The dome and segments for the head course will weigh about 185,000 pounds as received from the mill. The preparation of pieces for welding will remove in excess of 70,000 pounds which becomes scrap. The assembly will be fit-up, welded, probably machined on the inside surface before cladding, and prepared for welding to the closure flange.

Parallel with this fabrication, the head flange has been being prepared. Unlike the vessel flange, this forged ring has come in three segments, due to the larger size. It is a forging some 230" O.D. by 38" in height and about 22" in thickness. The piece has been contour machined to within a half inch of its final dimension at the mill. Upon receipt of the vessel head flange forgings, the segments would be welded together, completely stress relieved, radiographically inspected, repaired, etc. The surfaces of the flange exposed to the reactor coolant (including preparation for the subsequent use of a welded seal) would be weld deposit clad with stainless steel. The forged flange would now be welded to the partial hemispherical top head and this final strength weld would receive the required inspections and heat treatments. The assembly, now weighing over 244,000 pounds, would be placed in front of the 7" horizontal boring mill. The holes for the studs would be drilled through the flange to an undersized dimension. The keyways on the flange and penetration pattern through the head would be rough machined to locate the centers for the subsequent final machining

and insertion of the control rod mechanism housings. The inside termini of the penetrations for control rods would be machined to receive "buttering" of the manganese molybdenum material with nickle-chrome-iron. The "Inconel" layer will permit subsequent welding of the housings to the head without the necessity for a following stress relief operation. The assembly would now weigh 188,000 lbs. With these machining operations complete, the head is preheated and the "buttering" of the penetration termini and the welding of handling lugs for the vessel head would take place. The back cladding of all weld seams and cladding of keyways with stainless steel would take place at this time. All welding requiring stress relieving now completed, the subassembly would pass through the furnace for the last time and receive its final radiographic examinations and complete ultrasonic and dye penetrant inspections. The head again would be set up in front of the 7" horizontal boring mill and the gasket face and the grooves for the O-ring seals would be machined in the face of the flange. The weld preparation for attachment of a welded seal, if and when needed, would be machined. The keys, for alignment of the head to core internals, would be machined to tolerance. Final drilling of the stud holes and final machining for all the head penetrations would be performed. The subassembly is now positioned for the floor work of inserting the fifty-seven control rod mechanism housings into the holes in the closure head. These housings will be shrink fitted into their proper holes and strength welded to the "Inconel" over-lay located at the termini on the inside surface of the head. This strength weld would then be ground to blend the housing and head contours. Each of these welds would be non-destructively tested by liquid penetrant test as the weld is made. This would complete the fabrication of the closure head and it would be taken from the floor to the hydrostatic testing area.

The hydrostatic test of the vessel and head require unusual operations and careful handling. The studs are placed in the vessel flange with the two special alignment studs. The closure head is supported on a set of turning rolls. The rolls permit rotation of the head for alignment to the studs. The head and rolls are jacked to the proper height for the fit-up operation. Care must be taken to prevent injury to the stud threads. After the head is in place, the alignment studs are replaced with regular studs, using counter weights and overhead crane, with special rotation device. The washers and nuts are assembled and tightened using the stud tensioners in the horizontal position. Special handling techniques are required for positioning and using the tensioners.

The control rod mechanism housings require hydrostatic seal plugs. Hydro caps were assembled with the nozzles during the fabrication stages. Each of the nozzle hydro caps must have vent nozzles to permit venting of compressible fluids, which will collect during the filling for hydro testing. It must be noted here that the assembled head weighs over 200,000 pounds, and the studs, nuts, and washers add over 64,000 pounds, bring the total weight to be supported during hydrostatic testing to over 2,150,000 pounds. The design of the shipping structure to support this weight and distribute the load so that it will not crush through the reinforced concrete floor, will result in a very expensive and rugged shipping frame. A first concept of this fixture is sketched on Page II - 38.

After the vessel has been hydrostatically tested, the head will be removed by reversing the sequence of operations described above. A portable horizontal mill will be brought to the vessel location and the hydrostatic caps will be removed. The nozzle safe ends will have weld preparations prepared and shipping covers placed over the openings. The interior of the vessel will be cleaned and the head will have its hydro plugs removed and the interior of the head cleaned.

Shipping covers will be placed across the flange opening of the head and of the vessel. The head will be placed on a separate shipping frame. How the vessel will be transported from the fabrication bay to the barge for shipment will be discussed under the section titled "Transportation".

The component which has not been described -- the thermal shield -- does not present a fabrication or handling problem. Therefore, its fabrication sequence will not be described.

It must be cautioned that the above comments are those of our present thinking of the sequence of fabrication for the reactor vessel. There are many variations in the design which may be permitted due to different manners of solving the particular design and fabrication problems. It should be noted that there has been no machining for alignment of the coolant nozzles after welding to their vessel shell courses. We do not anticipate a great deal of misalignment for these nozzles, however, that which does occur can not be corrected. The maintenance of roundness of these shell courses is dependent upon the quality of the bracing inserted as each of the nozzle openings are cut into the vessel shell and the nozzles subsequently welded therein. Meeting code requirements of a maximum of one percent out-of-roundness is expected to be accomplished in the shell courses containing nozzles. The shell courses opposite the reactor core should meet one-half of one percent out-of-roundness except in the immediate vicinity of the three long seams.

The major equipment additions or modifications to present facilities are:

- 1) Furnace for stress relieving of vessel with nozzles and hydro caps reaching a 29 foot circle.
- 2) Relocating the shielding wall of the Betatron room to permit the vessel, with nozzles and hydro cap attached, to enter, moving the Betatron head to the vessel or most likely, the addition of a linear accelerator facility.
- 3) Modifications to welding equipment to permit placement at the required height above the work floor.
- 4) Contour dies for forming the shell plates.
- 5) Special fixtures for rotating up to 600 tons for welding, consisting of idler and power rolls with load spreading frames.
- 6) Special fixture for moving up to 600 tons from area to area within the shop. This fixture may also be suitable for support of the complete assembly weight, over 2,150,000 pounds during the hydrostatic testing, provided the rolls can be withdrawn. In the later event, its use as a shipping structure would be desirable.
- 7) Equipment to perform local stress relief of closing weld.
- 8) Cables for rigging with special spreader bars as necessary.
- 9) Jacks - hydraulic.
- 10) Floor bearing plates to distribute concentrated loads.

PARTIAL FABRICATION AT SITE

The problems associated with the field fabrication of the partially completed subassemblies prepared in the manufacturing plant can not be approached with the confidence that the problems associated with complete shop fabrication have been discussed. Where partial ring courses have been assembled in the field, the material usually has been of such thickness and composition to permit welding without subsequent stress relieving. Where stress relieving has been required, the weld has been far enough away from discontinuities to permit proper local stress relief.

Why is field assembly desirable? If the size is so great that the completed assembly can not be transported to the site, field assembly must be performed. If the weight is so great that the assembly can not be lifted into position, it must be built in place. If the site is so remote that transportation costs are excessive, it may be economical to move the shop equipment to the site.

Thus, without knowing the site facilities, assumptions must be made of the maximum size and weight which will be fabricated in the shop. Shipment by railroad appears to be a logical restriction on the size of subassembly, since barge shipment would delay the schedule excessively. This restriction prevents the shop fabrication of any of the complete shell courses. The bottom head dome and first ring of segments can be assembled in the shop. The second ring of segments can be partially assembled into two 180 degree subassemblies and shipped by rail.

The vessel shell pieces would be formed and weld preparations machined for field welding. The nozzles would be clad with stainless steel and machined for field assembly into the nozzle shell courses. The weld groove preparations would be made on the flange forgings at the mill and the parts shipped direct to the field site. The U-bolts for vessel support, the studs, nuts, and washers would be completed in the shop and delivered to the field. Thus, it is seen that very little work would be performed on the reactor vessel if rail shipment of most of the part is necessary.

The field shop -- and shop it must be -- must provide facilities for supporting the weights of the subassemblies previously described. There will have to be several large welding machines suitable for both long seam welding and girth welding. The cladding could be done with the positioners and girth welding machines. There would have to be a large furnace erected at the site to stress relieve the subassemblies. Radiographic inspection would require a large investment in portable equipment -- or rather in making the equipment required portable. All the machining operations could be performed with a large horizontal boring mill. Extensive fixturing would be necessary, and the schedule of fabrication would suffer from a severe burdening problem. The ultrasonic, magnetic particle, and dye penetrant inspection equipment would not be problems.

The envisioned sequence would follow the fabrication sequences described for the shop fabricated vessel, with the priority for scheduling being given to the lowermost parts of the vessel to permit placement in the container as completed. The two subassemblies of the second ring of lower head segments would be joined and attached to the dome and first ring. This part of the vessel weighs 121,500 pounds. The bottom of the containment structure would have sufficient sand placed to support the entire reactor vessel until the supports can be placed. The lower shell course would be completed in the field shop, stress relieved,

AS RECEIVED FROM MILL

CLADDING & MACHINING

17,500 #

FINISHED NOZZLE WT = 18,200 #

WEIGHT OF THIS ASSEMBLY
VARIES FROM 758,000 LBS
(NO NOZZLES), TO 863,000
LBS (14 NOZZLES)
EACH NOZZLE ADDS 7,500 LBS
NET TO VESSEL

FINISHED HEAD
WT. 121,500#
SCRAP WEIGHT
70,000#

ASSEMBLED 2nd COURSE

FIRST COURSE &
DOME ASSEMBLY

DOLLAR PL

radiographically inspected, repaired, again stress relieved, re-radiographically inspected and placed atop the lower head. Each of the other shell courses would follow the operations described previously. The courses would be joined by manual welding or by a special machine welding device. Welding in the horizontal position requires much more time than welding in the down-hand position. Less metal can be "puddled". The problems of preheating these pieces and maintaining the temperature until the pieces are fully stress relieved may require the use of material other than SA-302-B. The radiographic examination of the completed weld will require unusual surveillance or exclusive of other crafts from the main reactor building. There is serious doubt that adequate stress relieving could be obtained on this design using the local stress relieving techniques.

The problems of obtaining tolerances on the core support ledge and keyways, and the vessel flange have no apparent solution. A special machine or special mounting for the boring mill must be provided so that these surfaces can be machined in place.

Due to Combustion Engineering's capability to produce very large vessels, little experience has been obtained in solving problems of field fabrication. It is also quite probable that there does not exist any experience in field fabrication of such heavy sections, of such large size, and of such great weight. The units built in Great Britain certainly do not offer the experience to bring confidence that this vessel can be so fabricated. The conclusion, therefore, is reached that this reactor vessel should be completely shop fabricated.

INSPECTION

Up to this time we have spoken about the problems concerning the design, the material, and the fabrication of the reactor vessel. Since this equipment will contain a nuclear core for the production of power, it must be considered as being designed for hazardous service. To safeguard the public from the effects of any failure of this equipment, the integrity of the vessel must be assured through adequate inspections.

What inspections would be applied to this vessel? Let us list these in the order in which they will occur as the component goes through the fabrication sequence. First of all, the calculations of the strength of the design would be performed and verified by the Insurance Inspectors. With the design having met the Code requirements, the selection of the material and the properties to be obtained from the material would be scrutinized. During the production of these materials by our suppliers, the quality of the billet, the type and amount of cross rolling of the plates, the chemical analyses of the melt before the billet is formed and specimens taken from the billet croppings would be carefully examined. When the final plate is finished, the edges would be cropped and processed through their anticipated heat treatments before checking the mechanical properties of this material. These inspections would consist of: a check analysis of the chemistry; verification of the mechanical properties of yield and ultimate strengths; determination of impact properties; of weldability; and, perhaps, the determination of the nil-ductility temperature. The mill, having satisfied themselves that the plate meets the requirements of the specifications, would ship the material to the vessel fabricator.

Upon receipt at the shop, the plate would undergo ultrasonic inspection and magnetic particle examination to determine the presence of rejectable defects. The edges of the plates and other locations where weld preparations would subsequently be machined will be inspected carefully to determine that we have the desired plate quality in the vicinity of the welds. After the forming, heat treating, tempering and quenching operations are complete; the ends of the plate would be removed and given a simulated stress relief. Specimens would be removed to determine for information the impact properties of the material and the mechanical properties throughout the cross-section. The next operation, machining the weld preps on all edges of the plate, would be followed by dimensional inspection and magnetic particle testing of the weld groove preparations. When the welds are complete, they will be radiographically inspected. If defects are found, triangulation or ultrasonic inspection may be used to assist in accurately locating the position of the defect in the weld. When the cladding is complete, it will be ultrasonically inspected to assure good bond and dye penetrant inspected to assure surface quality. Each of the nozzle-to-shell course welds will be radiographically examined and clad surfaces inspected by the ultrasonic and dye penetrant procedures. In fact, every weld in the pressure containing boundary will be radiographically examined and all surfaces exposed to the primary fluid will be clad with stainless steel, ultrasonically and dye penetrant inspected to assure the proper integrity. The exception to radiographically examining all pressure boundary welds is the attachment of control rod housings to the head, where dye penetrant inspection of the "Inconel" weld to the "Inconel" overlay will be made. Here the joint geometry prevents interpretation of any radiograph which might be made of this attachment. The dimensional inspections of the diameters and out-of-roundness of the shell, the determination that the cylinder is not distorted over its length, the verification of the tolerances on the core barrel support ledge, vessel flange, closure head flange, the positions and alignments of the control rod mechanism housing would be measured and recorded. The quality of the threads in the vessel flange, on the studs and nuts would be carefully verified as well as the measurement of the axes of the tapped holes in the vessel flange to assure that the head may be removed from the vessel without removing all studs from the vessel. The reactor vessel studs, nuts and washers pass through several complete inspections. Eddy current and ultrasonic testing will be performed on the stud material. Magnetic particle testing of the finished parts will be performed to assure that no stress raisers exist. The control rod housings would be carefully inspected for dimensional accuracy and positioning during the fabrication operations. The Inspection Department would verify that the proper stress relieving temperatures and times were maintained, that all manufacturing procedures were being carried out in accordance with the qualified procedures. The entire reactor vessel head and assembly would be hydrostatically tested to one and one-half times the design pressure to prove the integrity of the pressure containing parts.

After the hydrostatic test, the interior of the reactor vessel will be cleaned and the as-built dimensions recorded to assure that the reactor vessel internals can be assembled into the reactor vessel when it is erected at the site.

TRANSPORTATION

The problem of transporting the material can be broken down into several phases. These phases are: the inbound transportation; handling of the material and subassemblies within the shop area; movement of the completed assembly to the dock loading facility; and loading it upon the barge for shipment by inland

and overseas waterways. The unloading of the reactor vessel at its destination will present quite similar problems to be encountered during loading at the manufacturer's plant, and in many ways will exceed the other handling problems due to the necessity for picking the reactor vessel completely off the shipping structure and lowering it into its position in the containment building.

Inbound transportation of raw materials will not present problems greater than those being handled by shipments of various materials today. This inbound transportation would be an appreciable problem if United States forging suppliers could provide these large diameter forged rings in a single piece. Since the closure and head flanges must be shipped to us in either 180 degree segments or 120 degree segments for welding in our shop, the forgings can be transported by rail. There will be 12 heavy plates, each weighing approximately 50 tons which must be shipped to our plant site. These plates are, however, unusually thick and therefore the width and length of the plate are not excessive for rail transportation. The nozzles do not exceed the capacity of rail shipment, of course. It can, therefore, be said that there exists no unusual problems in handling the raw materials from material suppliers into the shop.

The transportation of the materials and subassemblies inside Bay 29 have been mentioned during the discussion of the fabrication sequence. Bay 29 of Combustion Engineering is equipped with one (1) 20 ton crane and one (1) 75 ton crane. The overhead cranes handle the subassemblies during the fabrication sequence until assembled into the upper cylinder. At this time the weight would go to over 375 tons. Combustion Engineering would not make a clean lift of this weight with their 250 ton crane, but could handle this assembly on a fixture, see sketch on next page, in a manner similar to that which will be utilized to move the vessel from the fabrication bay to the dock facility. This weight, however, must be removed from the turning rolls during the stress relieving of the welds. This problem will be resolved when the design of the new furnace and the fixture are finalized. Now, it is conceived that the weight would be taken off the fixture by lifting one end, the fixture removed, and the weight lowered onto the furnace support. After the stress relieving operation is complete, the vessel will be placed back on the shipping frame by the reverse of the above sequence and the vessel then moved to the following operation. The assembly of the bottom head and bottom shell course to the critical assembly will take place on the shipping structure utilizing rolls to turn the vessel for the girth welding operation. A local stress relief will be performed on this girth weld utilizing a portable furnace around this one seam. A length at least twice the shell thickness on each side of the weld metal will be brought up to the stress relieving temperature. The insulation will be carefully designed to give a uniform reduction in the shell temperatures from that point up to the nozzles of the reactor vessel to prevent distortion of the entire shell. Much care must be exercised in insulating and cooling the reactor vessel while heating in order to maintain gradient control. With this assembly we would have over 600 tons of reactor vessel and approximately 100 tons of shipping structure ready for transportation and loading on the barge on the Tennessee River.

The movement of this assembly and fixture, weighing approximately 700 tons, from Bay 29 to the Tennessee River can be accomplished in a number of ways. As is now conceived, a well-designed road bed would be laid from the fabrication bay to the point on the Tennessee River where the barge would be docked. Although quite desirable, it is not believed to be practical to construct a wheeled vehicle for transporting 700 to 800 tons. The present concept

WELDING & X-RAY

is to use the tried method of rollers under a well-designed shipping frame, bearing on steel plates, see sketch on next page. The bearing plates will be "leap-frogged" around the vessel during movement. The movement of the vessel and fixture along the bearing plates will be controlled by winching. The winches may be located on prime movers or secured to "deadmen" along the road bed. The reactor vessel for the Selni Power Station was loaded on a railroad deck type barge. This barge was designed for the inland waterways movement of railroad cars and had a reinforced deck suitable for concentrated loads. It was not suitable, however, for ocean voyages. If delivery is required on either ocean coast, a barge suitable for towing in open water would be brought to Chattanooga. Either barge would be docked at the end of the road bed and sunk onto a prepared sand bottom. The space between the road bed and the barge would be bridged over for loading. With the barge flooded to settle securely, the vessel would be winched aboard and secured, then the barge pumped out to refloat. The movement of the barge would not differ from that of any other shipment by inland waterways.

For partial fabrication of the vessel at the plant site, the handling problems of the site become quite severe. It has been assumed that only parts suitable for rail transportation -- except for the closure -- would be prepared in the shop. At the plant site provision would be required, therefore, for moving and erecting loads exceeding 300,000 pounds. The fabrication area at the site must be provided with proper floors to support these loads. Probably the easiest way of moving the subassemblies from the field shop to the erection site will be by laying a railroad spur through the field shop. The single greatest transportation problem for the partial field fabricated vessel remains the positioning of the subassemblies in place for welding.

Concluding the statements about transportation of the vessel, the problems are found to be of very severe magnitude and to require special shipping fixtures and road beds for movement. These are, however, no unsurmountable obstacles in the way of transporting even a vessel of this weight from the Chattanooga fabrication plant to the barge for shipment through the inland waterways and/or over the open seas to the designated reactor plant site. The costs for this transportation and the time involved in accomplishing the move are greater than those normally considered for delivery of a reactor vessel. Still, the design, manufacture, and delivery of this vessel are feasible.

SUMMARY

The initial evaluation of the problems associated with producing a pressurized water reactor vessel to the requirements mentioned earlier has indicated that such a project is feasible. Complete manufacture of the vessel in the shop will require several heavy duty fixtures, modifications to welding machines, a new stress relieving furnace, widening the access to the radiographic examination room, and construction of a road bed to the Tennessee River (under 1,500 feet). A study of barges available to transport this vessel is recommended. Partial fabrication in the shop with completion at the site is not considered feasible or desirable.

Although the standard Code materials appear to meet the requirements, it is suggested that a full sized plate be ordered from a plate manufacturer and processed through the fabrication steps as assurance of meeting the quality necessary for this application. Also, in the event this test shows marginal or

ROLLERS

BEARING PLATES

ROLLERS & BEARING PLATES WILL
BE "LEAP-FROGGED" TO FRONT

BAY 29 TO BARGE & BARGE
TO SITE SHIPPING & UPENDING RIG

unsatisfactory results, it is suggested that a program be funded to develop and secure Code acceptance of an alternate high strength material which will permit thinner walls and a significant reduction in weight of the vessel. Adoption of the proposed Section III of the ASME Boiler and Pressure Vessel Code will improve the feasibility of this project.

3. MULTI-LAYER CONSTRUCTION

Two years ago, in anticipation of the future need for large, heavy reactor vessels, Westinghouse APD investigated the feasibility of a new type of vessel construction for nuclear applications which offers manufacturing flexibility as to overall size and shell thickness as compared to solid wall construction. This technique, multi-layer construction, was a specialty of the A. O. Smith Corporation of Milwaukee, Wisconsin, and has been successfully applied to the design and construction of over 10,000 commercial vessels since 1933.

The A. O. Smith Corporation recently withdrew from the process equipment business, but has since concluded an agreement with Chicago Bridge & Iron Co. in which the exclusive right to manufacture and sell multi-layer vessels was turned over to them. All necessary equipment and technology will be made available to Chicago Bridge & Iron Co. and a careful transition program is now in effect.

The investigation resulted in the choice of this type of construction for the reactor vessel used in the experimental Saxton Reactor Plant. The reactor vessel is 58 in. inside diameter, 20 ft. 2 in. overall length and design pressure of 2500 psia.

Multi-layer pressure vessels differ from conventional pressure vessels only in the method of fabrication of the main cylindrical shell course. The bottom head, removable closure head and main nozzles are identical to those used in conventional reactor vessels. The main shell of a multi-layer vessel is comprised, of many layers of relatively thin plate, each separately formed into a barrel, wrapped and welded one to another until the required total thickness is attained.

The choice of a multi-layer vessel offers several distinct advantages over solid wall construction as follows:

- 1) The relatively thin plate used in multi-layer construction exhibits better metallurgical properties than thick plates for the same application.
- 2) Precompression built into the inner layers of a multi-layer shell by the combination of wrapping load and weld shrinkage techniques results in a favorable stress distribution through the wall under internal pressure.
- 3) The inner clad barrel serves as the fluid containing portion of the vessel and, therefore, is the only layer of the multi-layer shell which must be leaktight.
- 4) Multi-layer construction in reactor vessels offers definite manufacturing flexibility as to overall size and shell thickness as compared to standard solid wall construction. Shell thickness, limited by roll size and roll capacity in solid wall construction is essentially unlimited in multi-layer construction since the shell is built up of many layer wraps of relatively thin material. The "thin plate" metallurgy of the layer wrap material precludes the necessity for stress relieving the completed vessel. Thus, it is feasible to ship multi-layer reactor vessels to the site in sections and perform a field weld of the sections without the complications involved in solid wall construction.

- 5) The welds connecting the multi-layer shell to the bottom head and flange forgings are relatively free from weld shrinkage stresses and do not require stress relief.

Before the decision was made to adopt multi-layer construction for the Saxton reactor vessel, several important problem areas which were unique to multi-layer construction for nuclear application were thoroughly investigated to assure safe and reliable operation. The neutron and gamma bombardment causes a temperature gradient in the shell resulting in a higher temperature at the outer layer wraps. This gradient then causes a reduction in interface pressure between layers which reduces the overall thermal conductivity of the shell and results in increased thermal stresses. An analytical investigation of the combined thermal and pressure stresses in the Saxton vessel indicated safe operation well within allowable ASME code stresses for the material used. In order to confirm the validity of the analytical techniques used to calculate the gamma heating effect, the Saxton vessel was instrumented with thermocouples located at various locations around the outside of the multi-layer shell in the core area. Operating results obtained show that the analytical method developed for predicting the stress effect due to gamma heating has been proven conservatively safe.

A second unique problem in multi-layer construction which was thoroughly studied prior to acceptance of this technique for the Saxton application related to ASME code compliance. The techniques employed in multi-layer construction impose two specific deviations on the rules and requirements of either Section I or Section VIII of the ASME code. These are the inability to interpret radiographs of the girth seam welds, and elimination of final stress relief of the finished vessel. Since the definitive radiographic examination of girth seam welds for Section I or Section VIII code approval is not possible because of the influence of the seams between layers in a multi-layer shell, A. O. Smith Corp. developed an ultrasonic inspection technique which can be an acceptable alternate to radiography.

The final stress relief of all pressure welds required by Section I and Section VIII of the ASME code is purposely eliminated in multi-layer construction. This stress relief would tend to remove the desirable precompressive stresses built into the shell layers, with the resulting loss of interface pressure between layers which reduces the overall thermal conductivity of the shell. Because of the inherent ductility and close welding control employed in multi-layer construction, the elimination of stress relief does not limit the reliability or compromise the safety of this type of construction in pressure vessels. In the welding of multi-layer shells to solid wall forgings and plates, any residual stresses in the weld metal and heat affected zone are greatly diminished through lateral movement of the individual layer plates during welding. In addition, mechanical peening has proved to be effective in controlling distortion in these welds. Also, the use of low hydrogen welding electrodes enhances the ductility of the as-deposited weld metal. Further, the weld heads in a multi-layer section joining a solid wall section are multi-pass, and each successive weld layer progressively grain refines and heat treats the preceding layers and heat affected zones. The safety and reliability of non-stress relieved vessels has also been demonstrated by A. O. Smith in both extensive shop tests and field operating experience over the past 30 years.

In conclusion, the design of a 202 in. I.D. multi-layer reactor vessel appears to be feasible. The design techniques for extrapolation of the 58 in. I.D. Saxton Plant vessel are available. However, the ability to fabricate, inspect, and test a multi-layer vessel of the required size is in sufficient doubt that a feasibility study must first be undertaken.

SECTION III - MINIMUM SITE REQUIREMENTS

I. GENERAL

The reference hypothetical Middletown site represents a reasonably ideal site on which to construct a 1000 MWe all nuclear plant. Other potential sites for constructing a plant of this size, however, do not possess such favorable characteristics which prompted a limited consideration of minimum site requirements of a general nature. Another area of significant interest appeared to be the means by which the huge reactor vessel could be handled at the site, assuming both ideal and adverse terrain conditions between the dock and the plant proper.

In the absence of partial or complete field fabrication of the reactor vessel, the plant site must be accessible by water to permit delivery of the vessel by a barge. The physical size and weight of this vessel are such that it would be impractical to consider movement by roads or railroads for any appreciable distance.

The reference plant occupies an area of approximately 1200 acres which can be considered to be a practical minimum for a single unit station. The necessity of exclusion area is dismissed as a consideration because it is believed that sufficient safety features (such as double containment) can be designed into the plant to remove this requirement.

The condenser cooling water requirements based on a constant output of 1058 MWe at 1.5 In. Hg. A. condenser pressure are shown on Figure III-1.

2. REACTOR VESSEL HANDLING ON SITE

2.1 MIDDLETOWN SITE

This section describes the method employed for unloading the reactor vessel, handling it on the site, and erecting it on its support within the plant container. Since it presents the more difficult problem, handling the long reactor vessel designed for full length control rod followers is described. This vessel weighs approximately 750 tons without cover or internals.

Because of its great size and weight, the vessel cannot be handled on a railroad car or wheeled truck. Although transporting this vessel across the site is a tremendous undertaking, the problem it presents to skilled riggers is one of selecting and using the necessary equipment. It will involve a longer time period and greater number of man-hours. A possible method of transporting this vessel is described below. When skilled riggers study the problem on an actual job site, however, they will undoubtedly devise improvements.

The reactor vessel is mounted on a barge at its point of origin and is delivered to the job site by marine haul. The North River can be navigated by tugs and barges at the job site.

The reactor vessel arrives at the job site mounted on the deck of the barge on the shipping cradle on which it originated. This cradle consists of a horizontal platform of 24 in. and 18 in. steel I-beams braced together. Three curved steel cradles support the vessel which is strapped to the skid by rods and turn-buckles. Supplementary steel members provide the necessary bracing against the vessel's shifting or overturning.

To receive the loaded barge, a slip is excavated and dredged at the shore line as shown on Figure IV-1, Vol. I. A steel sheet piling barrier approximately 100 ft. long is driven, extending from the 970 ft. contour on the bank into the river. South of this barrier, a slip 70 ft. wide is excavated, with its bottom sloped from Gr. 964 at the 970 ft. contour to approximately Gr. 954 in the river bed.

Immediately before the arrival of the barge, a level gravel bed is spread over the slip bottom suitable for grounding the barge. Delivery of the barge is accomplished in late winter while the river stage is fairly high, but before the spring freshets. However, should these take place while the barge is in the slip, no difficulty would be experienced.

The barge is maneuvered by its tugs into the slip and held in position with spring lines attached to bollards or pile clusters. After delivery, the barge is sunk by filling its compartments with water until its bottom rests evenly on the gravel bed. After the barge is grounded, tugs depart until the barge is empty and available for removal from the site.

A working area is constructed of gravel placed north of the steel piling barrier. This filled area extends the full length of the barrier, and its crown is Gr. 970. In the meantime, a suitable wide, even, gravel surfaced haulage-way has been provided on the site between the working area and the plant container. Plant roads are utilized where available and where there will be no intolerable interference with normal traffic. This haulage-way traverses the normal grades found on the site which have been smoothed and graded, so that no slope exceeds

DESIGN COOLING WATER TEMPERATURE VS. TOTAL CONDENSER SURFACE
AREA AND COOLING WATER REQUIRED

five percent. A runway, consisting of greased 10 in. steel I-beams suitably braced and clamped together so that the top surfaces form a platform, is laid down for a distance equivalent to a minimum haul. Eventually, sections of this runway are picked up from behind the vessel and relaid ahead of it, thus reducing runway cost to a minimum.

When ready for off-loading, the reactor vessel and its skid are jacked and cribbed up from the deck of the barge. A section of the steel runway is then laid between the deck and the gravel fill working area. Using cribbing and rollers, the reactor vessel on its skid is moved sidewise from the barge along the skid runway to cribbing erected on the gravel fill.

Hauling is accomplished by four or more heavy-duty steel-tread tractors, each capable of exerting a drawbar pull of at least 80,000 lbs. Rollers consist of heavy wall steel pipes, 8 in. in diameter, filled with concrete. For this vessel, it is estimated that rolling resistance might amount to 150,000 lbs. with the pull for the five percent grade adding another 75,000 lbs. Tractive effort is obtained from the tractors by means of multiple steel cables attached to pulling eyes on the skid. By hauling directly with tractors, the use of winches and deadmen is avoided.

The reactor vessel is unloaded with its spherical bottom end heading east. The vessel is hauled across the site in an easterly direction approximately 800 ft., passing north of the waste holdup tank area. From here, skidding and rolling continues in a southerly direction for about 300 ft. until the vessel is lined up in front of the equipment hatch entrance of the plant container with the reactor head flange facing this entrance.

The reactor vessel is then skidded and rolled through the equipment hatch entrance until the head flange has passed into the reactor cavity through a temporary opening in the interior concrete cavity wall which purposely has been left for this operation. This wall opening will be closed after the vessel is in place.

The polar crane in the plant container is employed for final placement of the reactor in a vertical position on its support. The polar crane is normally intended to handle steam generators, reactor coolant pumps, reactor coolant piping, and other equipment inside the plant container, both during initial erection and subsequent maintenance. The entire crane is not sized for handling the reactor vessel. Studies have indicated, however, that it is more economical to size a portion of the crane for handling the reactor vessel than to use temporary rigging to move the vessel into its final vertical position. The polar crane, which has a nominal rating of 200 tons, is positioned lengthwise over the horizontal reactor vessel. As shown in Fig. III-2, the crane bridge is immobilized and blocked with temporary supports from the cavity walls in order to shorten the effective span. The two 275-ton trolleys have sufficient combined capacity, with allowable overload for a single lift, to raise the reactor vessel.

The reactor head flange has been provided with a temporary lifting plate bolted to the flange. An equalizing bar and link assembly suspended from the two crane hooks is attached to a central eye in the plate. The reactor head end is lifted just free from the skid and the cradles detached. The reactor spherical bottom now rests on a greased steel plate on the top of the skid beams. The head end of the reactor is again lifted gradually and the spherical end slides forward until the entire vessel has been raised to its vertical position as the spherical end leaves the steel skid.

While suspended in the vertical position, the vessel is now shifted and rotated so that its supporting lugs are positioned over the corresponding brackets in the walls of the reactor cavity. The vessel is then lowered onto the supports and the lifting rig is removed.

There are seven supports on the vessel and seven brackets in the concrete wall cavity with suitable steel plate facing. A pair of jack screws is provided on each of the vessel lugs. These screws are adjusted until the vessel flange is level, and until there is approximately a one half inch gap between the underface of the vessel lug and the steel-faced bracket. Each of the seven gaps is then accurately measured and a suitable steel spacer or shim is machined to suit and inserted. Finally, the vessel supports are keyed in place. A portable boring machine, using first a pilot drill and then several larger drills and finally a reamer, cuts a 4 in. hole in the support, partly in the vessel lug, and partly in the steel facing of the supporting bracket. A hardened steel pin is then inserted in each of the seven holes and held in place with a retainer plate. The reactor vessel is now effectively keyed so that it is free to expand radially on the pins, but is anchored against movement in any direction under the external forces and moments which will be imposed upon it during operation.

2.2 ALTERNATE SITES

The foregoing description covers the handling of the reactor vessel on the prescribed site where firm ground exists with grades not in excess of five percent.

Two alternate site conditions have been considered:

- 1) Handling the reactor vessel up a grade not exceeding ten percent.
- 2) Handling the reactor vessel across a piece of swampy ground.

In these alternate site conditions, the unloading of the reactor vessel from the shipping barge onto the ground and the handling into the plant container are the same as for the basic handling plan.

In handling the reactor vessel on its steel skid across a site involving grades up to ten percent, the methods employed are identical with those in the basic plan. A steel runway is laid down and continuously picked up from behind the vessel and relaid ahead of it. The vessel on its steel skid is drawn by tractive effort on steel rollers along the steel beam runway. The skid is continually blocked from the rear to prevent return movement. This effort requires heavier equipment, greater drawbar pull, more time, and an increase in labor cost.

The haul across the site in the basic plan was approximately 1,200 lin. ft. Deducting the common cost of unloading the vessel from the barge to the ground and moving it inside the plant container, the average cost of moving the vessel across the site with grades not over five percent is estimated at \$30 per lin. ft. The same basic rate would apply to reasonably longer haulage for distances not in excess of 2,000 ft. to 3,000 ft. The problem of cross-country haulage for a number of miles has not been studied, and the above basic rate would not apply. When hauling this same vessel for several thousand feet with grades up to ten percent, it is estimated that the basic rate would increase by about \$4 per lin. ft.

An alternate problem of transporting the vessel across a swampy area has been investigated. In order to derive a cost figure, it has been necessary to assume a specific swamp. This swamp is located between the shore and the plant container and cannot be skirted. The swamp is 300 ft. wide at the point where the crossing must take place. The ground is firm on both sides of the swamp crossing. There is no natural drainage from the swamp, which is fed by a small brook and probably also from underground springs. Water stands in the swamp about 3 ft. below firm ground and some patches of sedgy ground rise above the water. Some soundings have been taken in the swamp, and data obtained from the local electric power company, which has one transmission tower in the swamp, indicate the following soil conditions:

Ground	Gr. 0 ft.
Water	To minus 12 ft.
Mud	To minus 15 ft.
Fine sand, silt, and water	To minus 30 ft.
Gravel hardpan	Minus 30 ft. and below
Stopped drilling at	Minus 40 ft.

A temporary trestle is now presumed constructed across the swamp. This trestle consists of a braced bents formed from wood piles driven into the hardpan 30 ft. below ground grade. The bents are surmounted by timber caps which extend beyond the piling to support a wooden walkway on each side. Bents are 7 ft. apart longitudinally, and the trestle caps support steel wide flange I-beams on which the pipe rollers are placed. For approach and exit spans, the steel beams act as rocker beams. The procedure for moving the load is the same as for flat ground operation. A crawler crane moves the cribbing and rollers ahead of the load as it follows the load across the trestlework. Tractive effort is obtained by pulling from the far side of the swamp with extra long cables.

The additional cost of crossing the swamp is estimated at \$250 per linear foot.

SECTION IV - RECOMMENDED AREAS FOR CONTINUED STUDY

1. INTRODUCTION

In developing the reference plant, numerous variations in plant design and alternate approaches were uncovered that appeared sufficiently attractive toward reducing capital costs and improving plant performance to warrant detailed examination. Establishing technical feasibility and the time limitation for completing this study prevented any investigation into design variations except for the preparation of Alternate Core Design #1 and limited examination of the rod cluster control concept (Alternate Core Design #2). Consequently, in keeping with the overall objective of this study, the reference plant design has not been optimized and continued study is recommended in the following major areas.

2. NUCLEAR EVALUATION

Foremost among the recommended areas for continued investigation is the nuclear evaluation of the reactor core. The intensive analysis performed thus far has established the feasibility of operating such a large reactor. There remains, however, the task of translating feasibility into the most optimum core design. Analysis of code conversion should continue to establish the adequacy of presently available computer codes necessary for the core design. Power distribution studies must be extended to determine the effect of control rod motion on power distribution under all operating conditions. Analysis of instabilities to investigate xenon oscillations in the core requires additional effort to determine their amplitude and the effect of damping upon them.

3. REACTOR CORE

The reference thermal, hydraulic, and mechanical design should be optimized. The use of smaller fuel assemblies, suggested by results of the nuclear evaluation analysis and additional study of fuel management schemes, is very necessary.

A core design based on the rod cluster control concept should be prepared in sufficient detail to permit an economic and technical comparison with the reference core.

4. REACTOR PLANT

Instabilities in the core were detected at the outset of the nuclear evaluation and continued to exist throughout most of the nuclear evaluation study. Consequently, development of a reactor control system to provide the required reactivity response while maintaining a reasonable power distribution could not be undertaken and will require additional design effort. A plant stability and transient analysis should also be carried out simultaneously with the design of the reactivity control system.

The inside diameter of the reactor vessel had to be fixed at the outset of the study in order to have the feasibility study of this component commence as soon as possible. A reduction in vessel I.D. from 202 inches to 190 inches is

now indicated. The amount of cost savings incurred by reductions in vessel size, plant container, internals, shielding, and other affected areas, should be evaluated.

A shop fabricated reactor vessel of the size considered must be transported by water and may severely limit potential sites for construction of a 1000 MWe nuclear plant. It is believed that on-site fabrication is possible and a study of methods and costs is recommended.

Nonuniformly spaced reactor vessel nozzles would eliminate the need for expensive bends or fittings in the reactor coolant piping near the vessel nozzles. With seven reactor coolant loops, the savings could be appreciable.

The chemistry of the water in the spent fuel pit, combined with the existing radiation field, dictated the use of stainless steel for storage racks and pit linings. However, the cost of stainless steel is high enough to warrant study of alternate materials such as carbon steel with ceramic or glass inserts.

5. PLANT ARRANGEMENT

It was not possible to carry through this study with two completely different arrangements of plant facilities, and the one chosen is considered to be very good. Other arrangements are of course possible, but one in particular would be worth an economic study. In this proposed alternate, the turbine building, plant container, and auxiliary buildings would be in one line along the river front, with the switchyard on the offshore side.

6. TURBINE GENERATOR PLANT

An economic study of the following items, either singly or as a group, would generate information of value:

- a) Unit transformers. Arrangement and cost studies of electrical equipment, using the system of unit transformers at each large motor driver, with the motors at low voltage.
- b) High voltage switchyards. Consideration of high voltage switchyard and step-up transformer design and arrangement, not now a part of this study.
- c) Static inverter systems. Investigation of static direct current-alternating current inverter systems in which the inverter is normally alternating current driven, but will transfer in an emergency to direct current drive within a few cycles.
- d) Concentric phase buses. Investigation of concentric phase bus connections at generators and at potential and current transformers.

7. PLANT CONTAINER

The plant container selected for this study is a single containment concept with a design leak rate not to exceed 0.1% of the contained air per day. This is entirely satisfactory for the low population density Middletown site. However, for a site having a high population density, consideration should be given to the use of a plant container utilizing leaktight double containment to determine the additional cost.

SECTION V - RESEARCH AND DEVELOPMENT PROGRAM

The design, construction, and successful operation of a 1000 MWe pressurized water reactor plant is contingent upon successful completion of the Research and Development Program presented in this section. This Research and Development Program denotes the required extension of that technology to be incorporated within the Southern California Edison and Round 3A plants.

Preparation of the reference design and nuclear evaluation revealed both the existence of new problems unique to very large cores and an increase in severity of existing problems. Principal among these problems are the uncertainties and difficulties in calculating core power distribution, possible core instabilities, and resolution of a satisfactory fuel management scheme. While the core physics and reactor core will require the major development effort, other technical problems exist in chemical and reactor plant development.

As reflected in Volume I, Section V, the advantages to be realized from the second alternate core concept utilizing rod cluster control are considered to be of such importance that the necessary development effort for this concept is included herein. It is envisioned that additional study will be performed, prior to the date that core development must be initiated, to quantitatively establish the technical and economic advantages of this concept. If it is then decided to incorporate the concept into the ultimate core design, the development tasks associated with the conventional cruciform type control rod can be deleted once the technical feasibility and superiority of rod cluster control is established beyond question.

The 1000 MWe Research and Development Program is divided into five major groups, and descriptions of tasks in each group are furnished. Figure V-1 depicts the schedule for performance of these tasks. It should be noted that the program is consistent with the overall plant construction schedule and assumes that authorization to proceed with this plant, is granted May 1, 1965.

It should also be pointed out that the materials development program for the reactor vessel must begin well in advance of overall program approval in order to meet the required component delivery schedule. Consequently, work should be authorized on this specific task about July 1, 1963, if a 1970 plant completion date is to be maintained.

Cost estimates for the Research and Development Program are shown in Table V-1. The estimated total cost of the Research and Development Program for the 1000 MWe plant is \$10,471,300. This total cost figure includes development of both conventional (cruciform) control rod, and "rod cluster control" designs. It is intended, as noted above, that both programs be carried in parallel until such time that it is evident, beyond question, that one, or the other of the concepts should be adopted for the 1000 MWe reactor. It is obviously not possible to state now at what stage of development this decision will be made; however the cost of the development associated with each concept is identified below, and may be used to estimate probable program costs.

The development associated with conventional control rods will cost approximately \$1,300,000. The "rod cluster control" development program will cost approximately \$1,650,000.

Table V-1

TOTAL R & D PROGRAM COST FOR 1000 MWE PWR

<u>Task</u>	<u>Engineers & Scientists</u> Man-Months	<u>Technicians & Draftsmen</u> Man-Months	<u>Direct Labor Overhead & Fee</u> \$	<u>Interworks Requisitions</u> \$	<u>Materials & Equipment</u> \$	<u>Total</u> \$
1. Program Administration	55.0	7.0	178,100	-	-	178,100
2. Core Physics Evaluation	572.0	347.0	2,678,200	1,365,000	-	4,043,200
3. Reactor Core Development	618.0	693.0	3,371,200	463,500	942,000	4,776,700
4. Chemical Development	84.0	94.0	483,200	14,000	246,900	744,100
5. Reactor Plant Development	98.0	42.0	439,200	10,000	280,000	729,200
Total Program Cost						<u>\$10,471,300</u>

1000 MWE PRESSURIZED WATER REACTOR
RESEARCH AND DEVELOPMENT PROGRAM

FIG. V - I

ED. SK. 285125-C

With the exception of the reactor vessel materials development effort, the total program will require 54 months for completion, commencing July 1, 1965. A curve of approximate dollar expenditure by fiscal years for the proposed program is shown in Figure V-2. The cumulative dollar expenditure curve is shown in Figure V-3.

1. PROGRAM ADMINISTRATION

TASK GROUP 1.0

Objective

The objective of this task is to provide the necessary administrative and budgetary control and coordination of the overall research and development program for the 1000 MWe nuclear power plant.

Work to be Performed

Research and development tasks under the program will be scheduled and their progress will be followed. Periodic reports will be prepared and issued. The level of effort for each task will be monitored to determine the correlation with budgetary estimates and expected progress. Appropriate control measures will be taken. The success in achieving task objectives will be determined, and possible alternatives will be evaluated, as required, to insure completion of programs necessary for the commercial operation of a 1000 MWe nuclear power plant by December 31, 1970.

ESTIMATED DOLLAR EXPENDITURES BY HALF-YEARS FOR 1000 MW(e) RESEARCH & DEVELOPMENT

FIG. V - 2

CUMULATIVE EXPENDITURES FOR 1000 MW(e) RESEARCH & DEVELOPMENT

FIG. V - 3

E. D. SK. 301525-B

2. CORE PHYSICS EVALUATION

TASK GROUP 2.0

The physics of a 1000 MWe PWR core presents a number of unique problems due to size. Analytical simplification techniques used for smaller cores, such as the separation of flux into components, is not acceptable. In addition, there is analytical evidence of core instabilities, which may or may not prove to be of physical significance. Power distribution, with its effect on fuel management, is more difficult to calculate for a large core.

All of the above problems form the basis for the core physics research and development program detailed in the following task descriptions, numbers 2.1 through 2.6.

In addition, there is included development of in-core instrumentation and evaluation of an automatic control system, critical experiments and the compilation of applicable data.

TASK 2.1 - SEPARABILITY AND FLUX SYNTHESIS METHODS DEVELOPMENT

Objectives

In a small uniform cylindrical reactor core, the fundamental flux mode can be readily separated into radial and axial components, which can be studied independently. However, as cores become non-uniform (due to such effects as fuel loading patterns, burnup, control rod insertion, etc.), the flux is not necessarily separable into components, and incorrect conclusions may be drawn if separability is assumed. In the non-uniform core, the errors involved in assuming separability could be expected to increase as the core becomes larger since more critical masses of core material are present. The presence of many critical masses accentuates the "local criticality" problems. Consequently, in a very large reactor, a small part of the core might have the capability of becoming critical without assistance from the remainder of the core. One can even conceive of having several areas in the core, each essentially independently critical. Under these conditions, the assumption of separability would be invalid and it would lead to incorrect conclusions.

The conditions which exist in the 1000 MWe PWR are such that the validity of separability is in doubt. The overall core dimensions are of the order of 50 neutron migration lengths. The high burnup results in large reactivity differences (25%) between fresh and partially burned fuel elements. Some control rods will be partially inserted in the core at all times; this will tend to make the core more heterogeneous and less separable.

The advantage of having a reactor in which separability is valid is that the analysis is greatly simplified. Radial and axial effects can be studied independently and the results superimposed. Techniques of superimposing the results are referred to as flux synthesis. By means of flux synthesis, a three-dimensional reactor can be analyzed by using only one-dimensional techniques or a combination of one and two dimensional techniques. The alternative is to use a three-dimensional code. The three-dimensional codes should be avoided where possible, not only because of the large expense involved, but also because the limitations of present day computers prohibits a detailed description of a core in three dimensions.

The objective of this program is to determine the degree of validity of flux separability in the 1000 MWe PWR and to develop appropriate methods of flux synthesis so that the core can be analyzed adequately with one and two dimensional codes.

Work to be Performed

Core configurations typical of those expected in the 1000 MWe PWR will be examined to determine the accuracy of separating the flux into radial and axial components. Results from a two-dimensional R-Z PDQ will be compared with results from one-dimensional radial and axial problems. Also, three-dimensional UFO results will be compared with the synthesis of results from an X-Y PDQ and an axial calculation. The effect of burnup on separability will be examined by comparing a two-dimensional R-Z TURBO result with two one-dimensional CANDLE results synthesized into a two-dimensional result. Cases which represent a severe test of the theory (i.e., partial control rod insertion, etc.) will be included so that an evaluation of the range of applicability can be made.

Flux synthesis techniques proposed in the past will be evaluated to determine whether they are satisfactory for the 1000 MWe PWR. The previously developed methods are generally applicable only if the assumption of separability is near to being valid. For the 1000 MWe PWR flux synthesis methods might have to be developed which provide acceptable results even if the flux is not accurately separable. This will require a "multi-channel" approach; i.e., the radial or X-Y power distributions at a given axial plane cannot be reduced to a single set of constants. Instead, the radial or X-Y power distributions are used to determine several sets of constants, each of which is applicable to a prescribed region (or channel). As the number of channels increases, the model approaches a conventional three-dimensional calculation. If many channels are required for an accurate representation, the total computer time may approach that of a three-dimensional calculation. However, a more detailed representation of the core is permitted by the flux synthesis approach than by a three-dimensional calculation. Digital computer programs will be developed as required to implement the methods of flux synthesis finally selected.

TASK 2.2 - ANALYSIS OF CORE INSTABILITIES INCLUDING XENON OSCILLATIONS

Objectives

During the study of the physics of the 1000 MWe PWR, several problems in the area of instabilities were found which require extensive further work. The problems can be grouped separately into computational and physical problems, with a zone of uncertainty in between to include those problems in which it is not clear whether the instability is real or simply an analytic difficulty.

In a reactor core in which leakage is very small, the power distribution is very sensitive to local reactivity perturbations. This is particularly true in a large core with a relatively flat distribution. This condition exists in the 1000 MWe PWR after the burnup effects flatten the power distribution. The studies have indicated that power distribution calculations for the 1000 MWe PWR strongly tax the capability of present calculational methods. For example, the common few group diffusion theory calculations require several times as many iterations as normally encountered to satisfy a given convergence criteria; in addition, the power distribution is sensitive to the convergence criteria unless

a much tighter convergence is used than normally required. These effects can result in roughly a 10-fold increase in the number of iterations per problem. The maximum number of iterations permitted in a problem had to be increased in several standard codes in order that the 1000 MWe PWR study could be completed. In some codes, it was found that the power distribution was sensitive to the number of mesh points up to the limit of points now allowed in the code. In other cases, the power distribution was found to be sensitive to the integration routine used; e.g., Simpson's Rule or Trapezoidal Rule. All of these problems require much more study to determine whether an improved calculational technique can be devised to improve the speed and accuracy of the calculations. One subject of this task is to define the problem areas, recommend possible solutions to those problems and to carry out numerical analysis and digital computer programming required to implement the recommendation.

In addition to any instabilities which may have been observed due to computational difficulties (convergence difficulties, etc. as just discussed), there appear to be physical instabilities which must be studied in detail. These physical instabilities could be categorized as power redistribution sensitivities and oscillations.

By power redistribution sensitivity is meant a small local reactivity change which can result in a major change in the power shape. This was demonstrated in the study by investigation of an axial case (13 ft. core height) which had a group of control rods inserted in the upper 1/4 core. After a series of burnup steps which gradually flattened the power until the axial maximum-to-average ratio was approximately 1.1, some of the control rods were withdrawn from the core by reducing an equivalent poison cross section in the upper 1/4 core. When sufficient poison was removed to increase k_{∞} by approximately 1% in 1/4 of the core, the axial maximum-to-average power ratio increased to approximately 2.5. These values do not include the non-uniform effects of Doppler and xenon; fortunately, from an operational standpoint, Doppler and xenon have a very large flattening effect in this core. However, these results illustrate the extreme sensitivity of the 1000 MWe PWR core. In a very large core, the power peak can exist in several different regions, with very similar eigenvalues associated with each position. If the core is operating with the power peak in one region and a small negative reactivity is added to that region while a small positive reactivity is added to a second region, the power peak can shift abruptly to the second region. The second position can have a considerably higher hot channel factor associated with it, with the possibility of local core burnout. A shift in the power peak might occur during operation of the 1000 MWe PWR, for instance, as control rods are withdrawn and chemical poison is added to the moderator. A second object of this task is to examine the operating conditions for which the power redistribution sensitivity characteristic of the 1000 MWe PWR could be important and determine the restrictions in operation which are required to safely operate the 1000 MWe PWR.

Another problem related to the core sensitivity is the problem of spatial power oscillations. The most common cause of such oscillations is the time and position dependence of the xenon poisoning effect. Spatial oscillations can occur in large low leakage cores, and have even been observed in the Shippingport reactor. The oscillations can occur when the reactivity in xenon is comparable to the difference in eigenvalues between the fundamental flux mode and the higher harmonics. The eigenvalue difference is reduced as the cores become larger. It is further reduced as the power distribution flattens. The conditions in the 1000 MWe PWR are expected to result in spatial power oscillations. An analysis of xenon oscillations by the method of Randall

and St. John (Nucleonics 16, No. 3, 82 (1958)) has predicted damped oscillations for the nominal temperature coefficient; for the minimum expected coefficient the analysis predicts the 1000 MWe PWR is on the threshold of sustained xenon oscillations. Since the analysis has limitations and simplifications (neglect of non-linearities, etc.), it should only be used as a guide to determine whether a reactor is near or far from the conditions resulting in oscillations. If the analysis predicts that the core conditions are near those giving oscillations, a much more detailed analysis should be undertaken. A third object of this task is to perform a detailed analysis of power oscillations and determine whether they can occur, what the consequences will be, and how they can be controlled.

Work to be Performed

An evaluation of the standard design codes will be made to determine what modifications are required to design the 1000 MWe PWR. The types of modifications which are anticipated include increasing the number of regions, points, compositions, and iterations allowed in a problem. Convergence studies will be made to determine the adequacy of the convergence techniques presently used. If some problems fail to converge or if excessive computer time is required, modifications in the convergence procedure will be considered. Core sensitivity problems will be studied using one and two dimensional codes. Reactivity effects which could result in major changes in the power shape will be considered. These will include such effects as control rod motion, water density differences (bulk boiling is allowed in the hot channel), and non-uniform burnup. The non-uniform Doppler effect will be considered since it has an important effect in reducing the sensitivity of the core. Restrictions on core operation due to the sensitivity will be specified, if any restrictions are required. The possible restrictions might include a limited range of control rod motion, a limit on steam voidage in the hot channel, or limits on the rate of changing power levels.

Spatial power oscillations will be studied by applying several criteria which have been developed in the past to predict instabilities. The eigenvalues corresponding to the higher harmonics will be calculated by modified diffusion theory codes. The difference between the fundamental and higher harmonic eigenvalues can be used to predict stability. If either sustained or damped oscillations are predicted for the 1000 MWe PWR, as anticipated, a detailed study of the effect of the oscillations on the core performance will be made. For typical core configurations which would be expected to exhibit the least stability, an attempt will be made to analytically incite oscillations. This will be done by simulating a reactivity perturbation in a diffusion theory code and, then, examining the resultant transient caused by the redistribution of xenon. In order to study the problem, the analytical model must contain the features which cause and limit the oscillation. Consequently, as a minimum requirement, the code must calculate Doppler and xenon effects at each point. Axial oscillations can be studied with a one-dimensional analysis; however, azimuthal oscillations, which are of more serious concern, require a two-dimensional study. The dependence of the oscillatory behavior on core size will be examined. The effect of the Doppler and moderator temperature coefficients will be studied. If power oscillations prove to be a serious problem, design modifications to improve the coefficients will be considered. Means of eliminating or reducing the oscillations through modified control procedures will be studied. This would include operating control rods individually rather than in symmetric groups. This procedure might permit an operator to reduce local power peaks caused by an oscillation; however, it also might simply result in the peak being shifted to a new location. If no completely satisfactory means is developed to control the

oscillations, restrictions on the operation will be specified. The restrictions would undoubtedly include an increase in the design hot channel factor.

TASK 2.3 - ANALYSIS OF POWER DISTRIBUTIONS AND FUEL MANAGEMENT

Objectives

The studies on the 1000 MWe PWR have indicated that power distributions are extremely difficult and expensive to calculate. The high degree of sensitivity to small reactivity effects may require higher hot channel factors for increasing core sizes.

To illustrate the types of problems encountered in calculating power distributions in the 1000 MWe PWR, consider the following case. A core configuration typical of that expected in the 1000 MWe PWR was studied using the two-dimensional PDQ code in X-Y geometry. Using a convergence criterion which had been suitable for previous light water reactor designs ($\epsilon = .05$), the maximum to average radial (actually X-Y) power ratio (F_R) was calculated to be 1.48. To test convergence, ϵ was changed to .04 and the problem was rerun. The eigenvalue changed by only one part in a million; however, F_R increased to 1.54. The convergence was then reduced to $\epsilon = .03$ and the problem rerun. This time the eigenvalue changed by three parts in a million; F_R increased to 1.60. The PDQ study was not continued beyond this point because of the large amount of computer time involved. However, a one-dimensional representation of the same problem was run until well converged and the indicated value of F_R was 1.80. It should be pointed out that none of these values of F_R includes local peaking effects due to water slots, control rod followers, etc.; also, the flattening effects of Doppler and xenon are not included. These results are indicative of the difficulties found in calculating the power distribution in very large cores. This also indicates the large uncertainties which exist in the power distributions at present.

The objective of this task is to thoroughly examine the problem of power distribution in the 1000 MWe PWR and to recommend revisions in the core which will meet the specified hot channel factors in the reference design. This will include fuel management studies since it has been found that methods of managing the fuel which are suitable for smaller reactors are not acceptable in the 1000 MWe PWR due to adverse power distributions.

Work to be Performed

Power distributions in the 1000 MWe PWR will be calculated with the available codes. Several one-dimensional diffusion theory codes are available. In addition, several versions of the two-dimensional PDQ are available. If three-dimensional calculations are needed, the UFO code is operational, although the core detail must be limited in a three-dimensional representation. If preliminary studies indicate transport effects are important in some cases, those cases will be studied using transport codes such as DSN. Point-wise burnup effects can be studied in one and two dimensions with CANDLER and TURBO. Digital computer program development will be carried out as necessary to make such calculations possible.

Sufficient power distributions will be calculated to evaluate the hot channel factors for the 1000 MWe PWR. The calculations will include the effects of fuel

management; a cycling method which provides an acceptable power distribution will be developed. If a cycling scheme is chosen such that the core performance depends on fuel element size, a study will be made to determine the optimum size from an overall economics standpoint. The reactor control will be specified including the size, number, and composition of rods, and the required boron concentrations in the moderator.

TASK 2.4 - DEVELOPMENT OF IN-CORE INSTRUMENTATION AND EVALUATION OF AUTOMATIC REACTOR CONTROL SYSTEM

Objective

Rather extensive in-core instrumentation is needed in the 1000 MWe PWR in order that the reactor can be operated safely without having an extremely conservative design. Some of the instrumentation may be direct reading so that an operator will be able to act on the basis of the instrumentation results.

The characteristics of the 1000 MWe PWR core are expected to be considerably different from those typical of smaller reactors; for example, power oscillations must be anticipated. The instrumentation requirements are different and development is required to determine the required number, type, and location of the in-core instruments. In addition, the development program will determine how the information from the instrumentation will be used; i.e., what action the operator should take depending on the results of the instrumentation.

Since a large number of instruments are anticipated, and a large amount of data will be available, an automatic control system may be desirable. The results from the instrumentation would be fed into a computer, which would analyze the data, decide on a course of action (such as move a control rod), and then automatically take the remedial action. The automatic control system could improve the performance of the reactor since it might reduce operation at part power which could be required at times while an operator evaluates data and decides on a course of action.

Work to be Performed

The work performed as part of the other tasks, including power distribution analysis and power oscillation studies, will be valuable aids in the completion of this task. The number, type and position of the instruments will be specified. Methods for determining the hot channel factors from the results of the instrumentation will be developed. Criteria will be established such that an operator will know what course of action to take after observing the data from the instrumentation. For example, if a sizeable flux tilt is indicated by the instrumentation, the operator may be required to reduce power and then rearrange control rods in a predetermined manner.

Studies will be made to evaluate the advantages and disadvantages of using an automatic control system to control the reactor. The studies will determine whether the control and operation can be specified clearly enough so that a computer could decide on a course of action based on the instrumentation, rather than rely on some degree of judgment of the operator. It is possible that, with extensive in-core instrumentation, a computer could operate the reactor more efficiently than a human. However, an automatic control system may be of most benefit after the reactor has been operated manually for one or more core loadings and the operation has become rather routine; after a period of operation,

the physical processes within the reactor will be better understood and a computer could be programmed to anticipate practically all conceivable situations.

TASK 2.5 - NUCLEAR DATA AND REACTOR THEORY

Objectives

Because of the sensitivity of the power shapes in large reactors to local reactivity, accurate calculations of effects related to reactivity are extremely important. Since all such calculations are based on nuclear data characteristic of the reactor constituents, a necessary activity in the research and development program is the study and evaluation of basic nuclear data and theoretical models. The objective of this task is to provide such data, based on the best available experimental measurements and the most acceptable nuclear theory, in a form suitable for use in reactor calculations.

Work to be Performed

Calculations of the performance of nuclear reactor cores are based on equations relating basic reaction rates in materials of which the reactor is constructed. To accurately represent the interaction probabilities of neutrons with these materials as a function of neutron energy, a great deal of very detailed information must be compiled. This information is based on experimental data obtained in nuclear physics research, mostly at universities and national laboratories. In addition, extensive research work is in progress on the development of theories describing nuclear reactions which can be utilized in the correlation of experimental data and in the prediction of interaction probabilities when experimental data are not available.

The activity on this task involves the continuous follow and evaluation of the current experimental and theoretical work and the interpretation of this work in terms of its applicability to reactor analysis. It also includes the detailed computational effort required in order to assemble new data and to prepare it in the proper form for use in the libraries of cross sections utilized in nuclear reactor analysis.

TASK 2.6 - CRITICAL EXPERIMENTS

Objectives

Initial studies in connection with the 1000 MWe reactor have indicated that in such large, low buckling systems the power distribution is quite sensitive to local or non-uniformly distributed variations in reactivity. For this reason, accurate experimental knowledge concerning the magnitude of reactivity effects is of great importance for the design of such cores. Critical experiments proposed for this reactor include measurements in light water moderated UO_2 lattices at 5% enrichment and measurements in light water moderated lattices fueled with mixtures of PuO_2 and UO_2 . The experiments at 5% enrichment are proposed in order to extend the available experimental data to bracket the possible feed enrichments required for such a high burnup core. The plutonium measurements are proposed to provide experimental information required to permit the accurate evaluation of the effect of fuel burnup on the reactivity characteristics of the core. The objective of this task is to support the experimental work and the associated analytical effort required for the planning and analysis of the experiments.

In addition to experiments planned to investigate characteristics of the reactor associated with the fuel materials, experiments are planned as part of the investigation of the rod cluster control concept. These experiments have as their objective, the determination of the worth of control elements composed of clusters of cylindrical neutron absorbing rods in specific configurations and the development and evaluation of analytical methods for the prediction of the worth of such control elements and their effect on power distribution.

Work to be Performed

A. Experimental Program

The experimental program will consist of measurements of critical size and bucklings at two water to uranium ratios of cores assembled from fuel rods to be provided by the AEC. In addition, foil activation experiments will be carried out to determine the characteristics of the neutron spectrum and to obtain information concerning the ratio of specific reaction rates which are characteristic to the lattice. Because of the availability of experimental data for uranium at lower enrichments obtained in the program of multiregion critical experiments, the necessary experiments with U-235 enriched fuel will be limited to a single enrichment of approximately 5% to extend the range of available data.

With regard to plutonium fuel, the objective is to obtain experimental data concerning the reactivity of plutonium fueled reactors which are adequate for the evaluation of analytical methods. Because of the lack of data for oxide fuel rods in light water, experimental data for at least two enrichments and two isotopic compositions of plutonium will be required. The principal experiments to be performed will be measurements of critical size, buckling, and power distributions at two water to uranium ratios.

To investigate the rod cluster control concept, experiments will be carried out involving clusters of cylindrical control elements in various configurations. Experiments will be carried out in realistic design configurations and also in simplified configurations necessary for the development and evaluation of analytical methods. Various materials such as Ag-In-Cd, B₄C and EuO₂ will be investigated with stainless steel or the cladding material. To assist in the evaluation of analytical methods, experiments will also be performed with cadmium absorbers and aluminum clad.

B. Planning and Analysis

In order to carry out a meaningful program of critical experiments, a parallel analytical effort is required. This program will involve the analysis of theoretical methods and the design of experiments which will provide a suitable test of theory. As experimental data are obtained, a parallel computational effort will be carried on and a correlation established between theoretical and experimental results. The experimental program will be modified as necessary to assure fullest benefits from the program. Information so obtained will be utilized to adjust the analytical methods so that they properly account for the effects of plutonium over the range of enrichments and isotopic composition studied, and procedures for incorporating the analytical methods thus developed into reactor burnup and fuel cycling calculations will be established.

In connection with the experiments on rod cluster control, the analytical program will be oriented toward the development of methods for the representation of rod cluster control elements in reactor criticality and power distribution

experiments. This will involve the development of theoretical methods adequate for the accurate prediction of the worth of cluster control elements and the effects of such elements on power distribution. It is anticipated that this development will proceed in two steps. First, highly detailed methods will be perfected which will accurately predict the critical experiments and which can be confidently extended to new configurations. Then, procedures for the specification of equivalent diffusion theory methods will be developed, so that the cluster control elements can be satisfactorily incorporated into the diffusion depletion analysis of power reactor cores.

3. REACTOR CORE DEVELOPMENT

TASK GROUP 3.0

Development of the core for the 1000 MWe plant requires confirmation of the acceptability of its metallurgical, mechanical and hydraulic design. The tasks of Reactor Core Development are intended to provide this assurance by means of tests of full and reduced size models of fuel assemblies, fuel rods, control rods and followers and reactor internals. Experience has shown that flow, structural and irradiation testing of core components frequently uncover problems and bring about solutions, which would otherwise be possible only after plant construction, and then at great expense.

In the 1000 MWe core design, several characteristics of the design emphasize the need for the tasks outlined herein. These are:

- 1) The 11 ft. active fuel length.
- 2) Use of elastically collapsed stainless steel fuel cladding.
- 3) Short control rod follower design.
- 4) Short reactor inlet plenum.
- 5) Use of extensive in-core instrumentation.
- 6) The large size of the reactor lower core plate.

TASK 3.1 - FUEL ASSEMBLY DESIGN DEVELOPMENT

Objectives

The objectives of this program are to develop and test prototype fuel assemblies for operation in a 1000 MWe pressurized water reactor.

Work to be Performed

The fuel assemblies will consist of UO_2 pellets clad in stainless steel or zirconium tubes with cruciform shaped control rods between fuel assemblies. The clad is supported and accurately positioned by stainless steel structure which permits axial movement of the tubes due to thermal expansion.

In order to achieve the required thermal, hydraulic and nuclear design characteristics, certain modifications over existing designs, as listed below, must be made to the fuel assembly mechanical design. These modifications affect the mechanical characteristics of vibration, thermal bow, fretting, and thermal ratcheting. To accurately determine these effects, it is necessary to conduct tests on prototype fuel assemblies.

Three major modifications are involved:

- a) The reduction of the mass of poison within the active region of the core by use of elastically collapsed stainless steel fuel clad or zirconium fuel cladding, and the use of thinner structural members in the fuel assemblies.

- b) The use of a skirt at the fuel assembly ports to limit bypass flow and control rod guiding on these skirts instead of on the core plates.
- c) The reduction of flux peaking by the use of several different clad thicknesses and the reduction of clearance between fuel assemblies.

While the development of collapsed clad is being performed under a separate R & D program, a minimal program is necessary for autoclave testing.

This program involves the mechanical and hydraulic testing of 11 ft long fuel assemblies under full scale flow, pressure, and temperature. This will yield data on fuel clad fretting and vibration, integrity of structure and joints, coolant pressure drop and velocity distribution. This test will run for approximately 5000 hours in conjunction with the test of a control rod assembly, and it will be interrupted periodically to inspect the assemblies.

After completion of these tests, the fuel assemblies will receive a final inspection and be then subjected to static mechanical tests to determine their stiffness. Following this, thermal bowing tests will be conducted. With the alteration planned in the fuel assembly structure and the fact that there may be several different clad thicknesses in each assembly, correlation with tests in previous development programs may not be feasible.

Vibration tests will be run on stainless steel or zirconium fuel assemblies. The grid spacings will be varied in order to obtain the optimum spacing compatible with several clad thicknesses while maintaining natural frequencies out of range of the impressed frequencies.

TASK 3.2 - IRRADIATION PROOF TESTING OF FUEL ASSEMBLIES

Objective

The objective of this program is to determine the irradiation behavior of proposed fuel assemblies in a bulk-boiling environment. Defected and nondefected rods of elastically-collapsed stainless steel and Zircaloy clads will be tested.

Work to be Performed

The program will place specific emphasis on operating conditions applicable to the 1000 MWe plant, i.e., bulk boiling. Two problems of a defected element in a bulk-boiling environment are: (a) water logging of Zircaloy cladding and (b) oxidation of UO_2 fuel.

Ideally, it would be desirable to defect elements in the loop after they have been irradiated to high burnups. This approach will be taken if feasible.

In addition to the loop irradiations, other irradiations of fuel rods are desirable in a power reactor. Variables to be investigated include effect of heat flux, cold diametral gap between the clad and the pellets, wall thickness, cold work, and fast neutron exposure.

TASK 3.3 - EFFECT OF HYDRIDING ON MECHANICAL PROPERTIES OF ZIRCONIUM-BASE ALLOYS

Objective

The primary objective of this program is to determine the effect of the maximum degree of hydriding anticipated in the 1000 MWe reactor on the mechanical properties of the zirconium-base alloys.

Work to be Performed

Extensive experience with zirconium-base alloys has clearly demonstrated that the degree of hydriding increases rapidly with temperature. Under bulk boiling conditions and long term operation as anticipated in this reactor, it is expected that the hydrogen pickup will be appreciably higher than in nonboiling conditions. It is important to determine the maximum degree of hydriding which occurs in environments anticipated for the 1000 MWe reactor and the effect of hydrogen pickup on the mechanical properties of zirconium-base alloys.

The effect of hydrogen pickup on tensile, yield, ductility, impact and strain fatigue properties will be determined. The investigation will be conducted on three zirconium-base alloys: Zircaloy-2, Zircaloy-4, and zirconium-2.5% niobium. The tests will be conducted at a minimum of four hydrogen levels. The strain fatigue properties will be determined under uniaxial and biaxial loading.

TASK 3.4 - SHORT TERM IRRADIATION TESTS ON OXIDE FUEL WITH VARIOUS O/U RATIOS

Objective

One of the primary reasons for limiting the power level at which UO_2 fuel may operate in a water reactor is the concern that UO_2 will oxidize if a cladding defect occurs, thereby lowering the thermal conductivity of the fuel and increasing the probability of center melting. The primary objective of this program is to determine the relative thermal performance of hyperstoichiometric UO_2 as a function of O/U ratio and to determine if center melting could occur in the core.

Work to be Performed

Although UO_2 has been demonstrated to be an excellent fuel in many respects for water reactors, one of the major drawbacks is the low thermal conductivity of the material. The thermal conductivity of UO_2 decreases rapidly with increasing oxygen content in the composition range $\text{UO}_{2.00}$ to $\text{UO}_{2.25}$. At room temperature, the thermal conductivity of $\text{UO}_{2.25}$ is only 25% of that of stoichiometric UO_2 . Thus, if UO_2 would oxidize to $\text{UO}_{2.25}$ or higher, center melting would occur if the integrated thermal conductivity (from 300°C - 2750°C) were decreased by a factor of four.

This task is concerned with irradiating UO_2 rods of varying O/U ratio and determining their relative thermal performance. UO_2 pellets will be sintered in atmospheres of varying partial pressures of oxygen to obtain pellets having different O/U ratios. The rods having O/U ratios of approximately 2.00, 2.10, 2.25, 2.33, and 2.67 will be irradiated for short periods (under 100 hours), and then examined in a hot cell. Relative values of thermal conductivity will be

determined from metallographic structures. Relative values of fission gas release will also be obtained in the hot cells.

TASK 3.5 - CONTROL ROD DESIGN DEVELOPMENT

Objectives

The objectives of this program are to design and test a prototype control rod assembly for operation in a 1000 MWe pressurized water reactor.

Work to be Performed

The control rod assembly consists of two sections, the upper or absorber section and the lower follower section. It is planned to use a rodged type flexible absorber of stainless steel clad silver-indium-cadmium pellets or other neutron absorbing material.

Due to the large span of this assembly and the requirements to reduce flux peaking and bypass flow, mechanical alignment problems are more severe than in smaller reactors. Consequently, it is important that this unit be evaluated in a Hydraulic Test Channel. Wear, flutter, static and dynamic loading must be investigated at several core elevations: 1) at the fuel assembly guiding surfaces, 2) between these surfaces, and 3) at the guiding surfaces built into the guide tubes above the core.

Several mechanical problems are recognized due to the short follower. These are: 1) dynamic loading of the follower as it re-enters the lower guide surfaces on the fuel assembly. The follower must be able to enter these guides without causing damage to either the fuel assembly or itself. It must also sustain a column loading without buckling or adversely affecting the control rod joint. 2) With the absorber section out of the core, the follower will be in contact with a single set of guides at the top. It will be necessary to evaluate the vibrational resistance of the follower under these conditions. 3) The effect of corrosion and fretting must be studied.

Layouts and detail drawings will be made and one control rod and one follower assembly will be fabricated. The control rod assembly will then be tested in the WAPD Reactor Evaluation Channel in conjunction with the Fuel Assembly Development tests described in another subtask of this program. The control rod assembly will be stepped and scrambled numerous times to determine the effect of acceleration forces on all parts of the assembly.

The tests will last about 5000 hours and will be interrupted once for inspection of all components. The absorber-to-follower joint will be checked for fretting action between the stainless steel absorber and zirconium follower.

A final inspection will be conducted at the conclusion of the test and inspection data will be compared with the original to determine the amount of wear and distortion.

TASK 3.6 - CONTROL ROD MATERIALS DEVELOPMENT

Objectives

The primary objective of this task is to develop an improved control rod material possessing high strength and excellent corrosion resistance.

Work to be Performed

From strength and corrosion viewpoints, the silver-base control rods presently employed in closed-cycle water reactors are far from ideal. The high corrosion rate of the base-alloy requires that the material be clad or plated; however, there is a probability of coating failure thereby exposing the silver-base control material to the environment. The release of silver is an especially important potential hazard in the case of the very large 1000 MWe plant during refueling.

This program is specifically aimed at developing an optimum control rod which has the best overall features of high physics worth and good metallurgical qualities such as corrosion resistance and strength. Several parallel avenues will be explored initially:

- a) Improved silver-base alloys having very good corrosion resistance.
- b) Improved cladding or plating materials and techniques for silver-base alloys.
- c) Evaluation of other control rods especially the rare-earths dysprosium and europium.

TASK 3.7 - REACTOR INTERNALS DESIGN DEVELOPMENT

Objectives

The objectives of this program are to develop and test the mechanical design of a reactor internals structure for operation in a 1000 MWe pressurized water reactor.

Work to be Performed

In order to meet the requirements of this program, an internals structure must be developed that is practical to build and can be fabricated at a reasonable cost. This design is 50% larger in diameter than the internals structure of present large pressurized water reactors which now are considered to be of maximum size for a single casting.

Other designs used large weldments that proved very costly, appreciably increased production time, and also increased the possibility of scrapping in the late stages of production. It is evident that neither of these methods by themselves will be satisfactory.

A three-part program is necessary:

- a) Analytical work to develop the basic design, factoring in such requirements as size, load, and the addition of a skirt attached below the core. The analytical work will culminate in a design for fabrication of models.
- b) Four models will be required: (1) a model of the lower structure consisting of the lower core plate, shroud tubes, lower support plate, skirt, core barrel flange and loading apparatus to simulate the core weight, (2) a model of the guide tube structure used to absorb the

scram loads consisting of the upper core plate, guide tubes, guide tube support plate and the same loading apparatus as in (1), (3) a model of the core barrel-to-upper barrel joint to study joint and bolt stresses and deflections under load with an impressed thermal gradient, (4) a model of a section of the internals to be used in a test loop to obtain hydraulic data and compatibility with the fuel assembly and control rod designs.

- c) Mechanical model testing with measurement of stress and deflection and analysis of the data. If modifications are required, the model will be altered and retested.

TASK 3.8 - HYDRAULIC TEST ON MODEL REACTOR VESSEL

Objective

The short lower plenum (approx. 4 ft) of the 1000 MWe core design may cause maldistribution of core inlet flow. The objective of this task is to measure the flow distribution from a scale model and to verify and improve the inlet flow distribution by testing various designs of reactor internals.

Work to be Performed

A small scale model ($\approx 1/8$ scale) of the reactor vessel will be tested in a Westinghouse low pressure water loop. Dummy fuel assemblies will be developed to simulate the hydraulic behavior of the core. Flow distribution at the core inlet and exit will be measured. This information will be used to optimize the design of the lower core skirt, flow baffle and/or perforated plate at core inlet. The experimental results will be analyzed. The inlet flow engineering hot channel subfactor will be determined and incorporated in the reactor thermal design by using THINC Code. THINC is a Westinghouse thermal and hydraulic IBM-7094 Code for calculating the coolant conditions in the core as well as those in hot channels.

TASK 3.9 - HYDRAULIC TEST ON FUEL ASSEMBLY

Objectives

The objectives of this task are to verify the engineering hot channel factor by measuring the flow distribution inside a fuel assembly, and to verify the calculated bypass flow.

Work to be Performed

A full size fuel assembly with a short (3.7 ft) control rod follower will be tested in a low pressure water loop. Electric heaters will be used to simulate the fuel rod. Instrumentation will be installed to measure the following quantities:

1. Flow distribution inside the fuel assembly.
2. Temperature distribution in the fuel assembly in order to calculate and evaluate the flow mixing among the rod lattice.
3. Bypass flow in the control rod slot.

The preceding measurements can be used to confirm the hot channel factors, verify the calculated bypass flow rate, and to verify the THINC Code calculations. THINC is a Westinghouse thermal and hydraulic IBM-7094 Code for calculating the detailed coolant conditions in the hot channel of a rod assembly.

An analysis will follow to re-evaluate the thermal and hydraulic design of the 1000 MWe core. The results of this analysis will give higher confidence and/or a larger safety margin to the thermal design of this core.

TASK 3.10 - MECHANICAL DESIGN & TESTING OF IN-CORE INSTRUMENTATION STRUCTURE

Objectives

The objectives of this program are to develop and test the mechanical structure required to locate and operate the necessary in-core instrumentation.

Work to be Performed

Present designs utilize a nearly equal number of neutron flux detectors and thermocouples. However, the 1000 MWe PWR plant will use a greater percentage of thermocouples over neutron flux detectors. Operational data obtained in the Saxton Reactor indicates that it may be desirable to "gang" thermocouples at a fuel assembly. This would require a greater number of thermocouples than previously used. Furthermore, the increase in size of the internals structure will cause modifications to the present in-core instrumentation structural arrangements.

With the large number of fuel assemblies, there will be more instrument probes than used in present reactors. Tolerances will have a greater effect in creating alignment problems between the instrument guide tubes and their respective fuel assembly cells. The chances of snagging an entering guide tube in the fuel assembly structure may be increased. It therefore becomes necessary to develop different instrument guide tubes. Several methods will be studied, such as:

1. A guide tube located through the center of the drive shaft.
2. Individually removable neutron flux detectors. This simplifies the structure by eliminating the telescoping requirement.
3. The location of a fixed instrument guide tube in the fuel assembly. This would eliminate the chance of snagging a moving guide tube on the fuel assembly structure.

As the analysis proceeds, other methods, which may prove more promising will be evaluated.

TASK 3.11 - FUEL ASSEMBLY DESIGN DEVELOPMENT - ROD CLUSTER CONTROL

Objectives

The objectives of this program are to develop and test prototype fuel assemblies for operation in a 1000 MWe pressurized water reactor with rod cluster control.

Work to be Performed

In order to achieve the required thermal, hydraulic, and nuclear design characteristics of this plant, certain basic modifications are to be incorporated in the fuel assembly mechanical design. These are:

- A. Integral control elements in the fuel assembly to replace the control rods between fuel assemblies, as used in present pressurized water reactors.
- B. The reduction of the mass of neutron poison within the active region of the core by use of thin, elastically collapsed stainless steel fuel clad or zirconium fuel clad, and the reduction in the mass of structural members.
- C. The use of different clad thicknesses in various locations in the fuel assemblies.

The above are expected to reduce flux peaking, bypass flow, and the amount of poison in the core. Other design modifications, such as reduction of clearance between fuel assemblies and elimination of fuel assembly "cans" may also be incorporated.

These modifications require prototype testing of the fuel assembly so that vibration, thermal bow, fretting, thermal ratcheting, alignment, scram loading and the overall structural integrity of the fuel assembly may be evaluated. A two-part program is planned.

- A. The conceptual design of stainless steel or zirconium clad fuel assemblies will be developed and detail assembly drawings prepared.
- B. The second part of this program involves testing, evaluation, modification and additional testing, if necessary. The mechanical and hydraulic performance of complete fuel assemblies under full-scale flow, pressure and temperature will be established, yielding data on fuel clad fretting and vibration, integrity of structure and joints, coolant pressure drop and velocity distribution.

This test will run for approximately 5000 hours and will be interrupted periodically to inspect the assemblies. It will be run in conjunction with Control Rod Development noted under a separate task. After completion of tests, the fuel assemblies will receive a final inspection and then be subjected to static mechanical tests to determine their stiffness. Following this, thermal bowing tests will be conducted.

Vibration tests will be run on the stainless steel or zirconium fuel assembly. The grid spacings will be varied in order to obtain the optimum spacing compatible with several clad thicknesses while maintaining natural frequencies out of range of the impressed frequencies.

The development of elastically collapsed clad has been initiated under a separate research and development program. It is assumed that this separate program will be completed in time to make information available to guide the design of the 1000 MWe prototype assemblies. Therefore, separate development for collapsed clad testing is not included here except for minimal autoclave testing.

TASK 3.12 - CONTROL ROD DESIGN DEVELOPMENT - ROD CLUSTER CONTROL

Objectives

The objectives of this program are to design and test a prototype control rod assembly which will successfully fulfill the design requirements for operation in a 1000 MWe pressurized water reactor utilizing rod cluster control.

Integral control rods are installed within fuel assemblies rather than between fuel assemblies as with cruciform rods. A number of these elements is contained in one fuel assembly. The rods are cylindrical and replace fuel rods in the fuel assembly grid structure. A thin perforated shroud (.010 in. or less) is placed through grids of the fuel assembly, acting as a guide for the control element, and also incorporating a dashpot.

Work to be Performed

In developing the design of the rod cluster control elements, the following must be investigated:

1. Fretting of the control elements in their shrouds and through the guides.
2. Vibratory forces on the control elements and the natural frequency of these elements.
3. Scram load effect on the control elements thimbles, guide tubes and drive shaft.
4. A dashpot for the system either integral with element shroud or on the drive shaft.
5. Alignment of the elements for core loading and during reactor operation.
6. Heat generation and heat transfer in the element shrouds.
7. Flow effect on the partially withdrawn elements in the guide tubes.
8. Time to scram the control assembly and terminal velocity.

Extensive mechanical, thermal and hydraulic analytical work will be required. Following this, preliminary layouts will be made and a full scale model of one control element assembly will be built and tested to size components. The vibratory stability of the fuel elements under full flow conditions will be evaluated.

Detail drawings will then be completed and the necessary equipment will be fabricated for test in a hydraulic test loop at full pressure and temperature. The test will include a number of steps, and scrams in excess of those expected over its normal lifetime. One control element assembly will be required. The tests will be in conjunction with Fuel Assembly Design Development and will last about 5000 hours. Approximately midway in the test, all components will receive a thorough inspection. Alterations will be incorporated, if required.

TASK 3.13 - ROD DRIVE MECHANISM DEVELOPMENT - ROD CLUSTER CONTROL

Objectives

The objectives of this program are to design and test a low cost, low lift, high speed rod drive mechanism for operation in a 1000 MWe pressurized water reactor utilizing rod cluster control. A further objective is to improve the dynamic characteristics of the mechanism and drive shaft latching system over present mechanisms.

Work to be Performed

Integral control rods are much lighter in weight than conventional (cruciform) control rods, permitting the use of a smaller mechanism and thus the possibility of a significant cost saving.

In addition, it is considered that simplification and improvement can be achieved in several areas. Specific considerations are as follows:

A. Drive Mechanism Pressure Housing

Modification to the existing pressure housing appears at this time to offer the most significant gain. It is felt that the present bore can be reduced by minimizing the size of the control rod "stepping" components and magnetic poles housed within the bore. Among the advantages of this are:

- (1) Smaller pressure forces in the housing permitting a reduction in the pressure housing wall thickness.
- (2) Machining costs would be reduced.
- (3) A decrease in the overall size of the drive mechanism unit.

B. General Simplification and Improvement

General design simplifications and improvements are planned in the magnetic poles, rod stepping components and latches. These would include,

- (1) Improvement in the latching and unlatching sequence of the "fingers" to the control rods.
- (2) A new spring system to curtail fluttering of the spring mass system and reduce the impact when a rod step is taken in either direction. This change may result in recovery of magnetic pole face area, lost in reducing the housing bore diameter.

A prototype unit incorporating these improvements will be built and tested in a hydraulic test loop in conjunction with the development of the fuel assembly and control rods.

TASK 3.14 - REACTOR INTERNALS DESIGN DEVELOPMENT - ROD CLUSTER CONTROL

Objectives

The objectives of this program are to develop and test the mechanical design of a reactor internals structure for operation in a 1000 MWe pressurized water reactor with rod cluster control.

Work to be Performed:

In order to meet the requirements of this program, an internals structure must be developed that is practical to build and can be fabricated at a reasonable cost. This design is 50% larger in diameter than the internals structure of present large pressurized water reactors which now are considered to be of maximum size for a single casting. Other designs used large weldments that proved very costly, appreciably increased production time, and also increased the possibility of scrapping in the late stages of the production. It is evident that neither of these methods by themselves will be satisfactory.

A three-part program is required:

- a) Analytical Work: to develop the basic design, factoring in such requirements as size, load, and load distribution. The analytical work will culminate in a design for fabrication of models.
- b) Model Building: four models will be required. (1) A model of the lower structure consisting of the lower core plate, columns, lower support plate, skirt, core barrel flange and loading apparatus to simulate the core weight. (2) A model of the guide tube structure used to absorb the scram loads, consisting of the guide tubes, struts, guide tube support plate and the same loading apparatus as in (1). (3) A model of the core barrel to upper barrel joint study joint and bolt stresses and deflections under load with an impressed thermal gradient. (4) A model of section of the internals to be used in a test loop to obtain hydraulic data, and compatibility with the fuel assembly and control rod designs.
- c) Mechanical Model Testing: with measurement of stress and deflection, and analysis of the data. If modifications in the model are required, it will be altered and retested.

TASK 3.15 - MECHANICAL DESIGN AND TESTING OF IN-CORE INSTRUMENTATION - ROD CLUSTER CONTROL

Objectives

The objectives of this program are to develop and test the mechanical structure required to locate and operate the extensive in-core instrumentation in the 1000 MWe core utilizing rod cluster control.

Work to be Performed

Provisions are made in the internals for inserting equipment to read the neutron flux over the entire length of the core and coolant temperature below the upper core plate at the fuel assembly exit nozzle.

Fuel assemblies, with and without control elements will be instrumented.

It is necessary to develop instrument guide tube details considerably different from those previously used. Several designs will be studied, such as:

1. A guide tube located through the center of the mechanism drive shaft.
2. An instrument guide tube located between control rod guide tubes.
3. A guide tube permanently located in the fuel assembly to mate with a guide tube located in the control rod guide tubes, and the neutron flux detectors individually removable.

The location of a fixed instrument guide tube in the fuel assembly can also be applied in methods 1 and 2. In this application, it would eliminate the problem of lining up the guide tube with the proper fuel assembly cell during core loading operations. Since this is largely a tolerance stack problem, it will be somewhat accentuated by the size of this plant.

This program will include analytical studies, design, and mechanical evaluation in a hydraulic test loop. The analysis will include vibration and thermal effects, handling and tolerance studies. Work in the hydraulic test loop will be concurrent with the Internals, Fuel Assembly, Control Rod, and Rod Drive Mechanism Programs all noted under separate task descriptions. This will include determination of the amount of flow disturbance and fretting inside the guide tubes.

Prior to test in the hydraulic loop, models of several instrumentation guide tube schemes will be tested under simulated operation and core loading conditions.

4. CHEMICAL DEVELOPMENT

TASK GROUP 4.0

The Chemical Development Task (tasks 4.1 through 4.3) details the program which is necessary for the 1000 MWe reactor plant. The principal concern of this program is the effect of the higher boric acid concentrations and the coolant quality (2% @ hot channel exit, steady state) on corrosion, hideout, chemical stability and interaction with "crud." Both in-pile and out-of-pile tests are proposed.

TASK 4.1 - CORROSION STUDIES

Objective

The objective is to make certain that no unforeseen corrosion effects result from the use of coolant with the high boric acid concentration required of the 1000 MWe plant.

Work to be Performed

This reactor plant differs from other chemical shim plants being designed primarily in size, and in the absolute concentration of boric acid required for shim control. To date, studies of boric acid solutions have been limited largely to concentrations of 1500 parts boron per million parts water (ppm) or less, whereas the first fuel cycle for the 1000 MWe plant requires from 2780 to 2190 ppm boron, depending on core condition. In general, boric acid solutions are only slightly more corrosive than water.

The studies proposed are:

1. A short corrosion loop test of one month's duration simulating a start-up cycle, at 3800 to 2200 ppm boron.
2. A long-term corrosion loop test (10 months loop operation) at 2200 ppm boron.

Both tests will be "high pH" tests; i.e., with potassium or lithium hydroxide added. Corrosion specimens of a variety of materials will be inserted in the loop and will be periodically examined.

TASK 4.2 - PHYSICOCHEMICAL AND HIDEOUT STUDIES

Objective

The objective of this task is to extend knowledge of the properties and interactions of boric acid chemical shim solutions and corrosion products, thus increasing the level of confidence in the safety of chemical shim plants.

Work to be Performed

A considerable effort has been expended as part of the Large Reactor Research and Development program on the study of the properties of and interactions between boric acid solutions and corrosion product oxides (crud). This work has led to the identification of two basic types of hideout - one, an

equilibrium or thermodynamic effect of the physicochemical interaction of crud and boric acid and the other a kinetic, mass transfer process requiring nucleate or bulk boiling at a heating surface. A continuation of this effort is required to extend the studies to higher concentrations.

The physicochemical studies will involve:

- a) The preparation of borates of the corrosion product metals and the determination of their stability in high temperature, high concentration boric acid solutions, and
- b) The preparation of synthetic cruds and the study of their behavior with respect to interaction with boric acid solutions, and the measurement of such other properties of the cruds as is necessary to increase the understanding of these interactions.

The refined procedure for measuring in situ hideout at a heat transfer surface (simulating the fuel rods) involves a test loop with a test section in a reactor critical facility. The test section will be designed to maximize the heater surface area to liquid volume ratio thus maximizing the hideout effect on critical facility reactivity.

TASK 4.3 - IN-PILE BOILING SHIM STUDIES

Objective

The objective of this task is to proof test the boiling shim concept under reactor radiations with the high boric acid concentration (and high pH) specified for the 1000 MWe plant.

Work to be Performed

The three characteristics of primary importance which must be investigated before changing from pure water to a chemical shim solution for the primary reactor coolant are (1) corrosivity of coolant, (2) thermal and radiation stability (including hideout) and (3) corrosion product dynamics in the coolant (release, transport and deposition on plant surfaces and their behavior with respect to purification by filtration and ion exchange). The acceptability of partially neutralized boric acid solutions in concentration up to 1500 ppm with respect to all of these characteristics has been essentially proven in the Large Reactor Research and Development Program; however, in all three cases, slightly adverse effects are noted. How dependent these adverse effects are on gross boron concentration is not known, hence a final in-pile chemistry proof test at the higher boron concentration is mandatory. This is especially true since bulk boiling will be permitted (2% quality in the hot channel), and boiling is known to affect radiation stability and corrosion product deposition adversely.

The in-pile chemistry test will be of about four or five months duration and will be designed to permit the determination and evaluation of the three decisive characteristics. The test section will be a fueled assembly operating under, as nearly as possible, the thermal and hydraulic conditions predicted for the thousand megawatt reactor hot channel. Chemical analysis of the coolant and post irradiation examination of the fuel assembly, out-of-pile corrosion specimens, and ion-exchange bed will then yield the desired information.

5. REACTOR PLANT DEVELOPMENT TASKS

TASK GROUP 5.0

This task grouping details the proposed research and development programs concerned with reactor plant components and systems.

The extremely large size of the reactor vessel requires the use of thicker plate than ever used before in this service. It is necessary to determine if suitable material properties can be obtained in this thickness, or whether a higher strength alloy is necessary to reduce thickness.

The control and protection of this large reactor core requires the integration of a vast amount of core and plant instrumentation data with the control system. A program is proposed to develop a reactor control and protection system which will utilize in-core instrumentation as a direct source of control information.

TASK 5.1 - REACTOR VESSEL MATERIAL DEVELOPMENT

Objectives

The reactor vessel will be significantly larger than those now planned or under construction. A development effort to determine the actual material properties attainable is necessary.

Because of uncertainties in the material properties of very thick plates, it is also vitally important that development work on a new and better alloy be accelerated. It has been well recognized that a steel with higher hardenability would be very desirable, and, to this end, a cooperative program among U. S. Steel, Combustion Engineering and WAPD has been under way at a fairly low level of effort. Successful development of this new steel would result in a reduction in thickness of the reactor vessel, with the associated advantages of better material properties, lower cost, and greater ease of fabrication.

A two-part program is, therefore, recommended to assure that a reactor vessel with the necessary material properties can be ordered in 1965 if operation by 1970 is desired.

Sub Task A - Investigation of Properties of A302B

Objective

To determine the strength and brittle fracture properties in A302B plate in thickness suitable for the 1000 MW plant.

Work to be Performed

All work is to be performed in cooperation with a reactor vessel manufacturer.

1. Procure one or more representative plates for fabrication, heat treatment, inspection and material property tests.

2. Perform standard or simulated standard fabrication processing, including forming, heat treating, welding, and inspection.
3. Perform tensile, Charpy impact, drop weight and other tests deemed necessary to determine the strength and brittle fracture characteristics.
4. Perform process development in areas shown to be needed by the above.

Sub Task B - Development of Ni-Cr-Mo Steel

Objectives

In cooperation with a steel maker and reactor vessel manufacturer, procure representative plates of the Ni-Cr-Mo Steel now under development and perform the necessary fabrication development testing to prove out the material for reactor vessel use.

Work to be Performed

1. Procure full size plates of 4, 8 and 12 in. thickness.
2. Perform standard or simulated standard fabrication processes of forming, heat treating, welding, stress relieving and inspection.
3. Perform physical and mechanical tests to determine the strength and brittle fracture characteristics obtainable with the best fabrication techniques.
4. Perform process development shown to be necessary by the above.

TASK 5.2 - REACTOR CONTROL AND PROTECTION SYSTEM

Objective

The objective of this task is the development of a reactor control and protection system that will result in stable and safe operating conditions.

Work to be Performed

The 1000 MWe chemical shim control and protection has become more complex in comparison to previous PWR and will consist of several automatic sub-systems. This task will integrate pertinent sub-systems and reduce the dependence upon administrative or manual control during load changes.

The equipment requirements for the protection system will be optimized to assure a high degree of operational reliability. Methods for making a building-block type system flexible for accepting inputs from in-core instrumentation and all channels involved, and performing logic functions required will be studied. A reactor protection system will be designed.

Increased concentration of boron in a chemical shim reactor due to higher burnup introduces the need for developing a central system for a moderator coefficient that will probably be positive initially and become progressively negative. Initially, therefore, the plant is inherently unstable without an effective control

system. The applicability of the latch type mechanism for the range of expected moderator coefficients will be evaluated. Computer studies will be made for the examination of possible control system configurations.

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1000 MW NUCLEAR POWER PLANT

UNITED STATES ATOMIC ENERGY COMMISSION

WESTINGHOUSE ELECTRIC CORPORATION • STONE AND WEBSTER ENGINEERING CORPORATION

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