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CONTENTS

APPENDIX 3

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QUESTION 3A.2 Provide a plot of fuel temperature versus the volumetric fraction of the total fuel at that temperature in the core at end-of-life conditions. Describe the method of calculation, state all assumptions, and provide typical radial pin profiles and the gross peaking factors used.

ANSWER Refer to 3.2.3.2.4g A plot of fuel temperature versus the volume fraction is shown in Figure 3A.2-1 at 100 percent power. A typical fuel cycle power distribution for equilibrium cycle, end-of-life conditions was used. The bundle average powers shown in Figure 3A.2-1 were used to obtain the fuel rod heat rates. A symmetrical cosine axial power distribution with a 1.5 max/avg value as shown in PSAR Figure 3.2-11 was used to predict the axial distribution. It was assumed that 97.3 percent of the power is generated in the fuel. The fuel rods were divided into 14 axial and 10 radial segments to obtain the temperature distribution for this analysis. The heat rate for every fuel rod in the core was increased by a local peaking factor of 1.05 to account for uncertainties in the calculation of local peaks. This has the bulk effect of raising reactor power to 105 percent.

The fuel temperature calculation model is outlined in PSAR Section 3.2.3.2.4g. The fuel conductivity curve identified as GEAP-4624 in Figure 3.2-49 was used to provide conservative values for fuel conductivity in the hottest regions of the core at the end of life. The maximum powers occurred in fuel assemblies with one and two cycles of operation as shown in Figure 3A.2-2, and the assemblies with the highest burnup did not exceed 1.043 times the average power for the case analyzed. The calculation shown in Figure 3A-1 was made by grouping all segments of fuel by temperature and assigning a conservative value for the fuel-to-clad heat transfer coefficient for typical end-of-life conditions. This is illustrated by the temperature profiles shown in Figure 3A.2-3. Typical fuel-to-clad heat transfer coefficients used were 280 and 480 Btu/hr-ft^2-F for 6 and 10 kw/ft heat rates respectively. The corresponding beginning-of-life coefficients are about 630 and 940 Btu/hr-ft^2-F at 6 and 10 kw/ft heat rates.

The temperature profiles are based on a uniform heat generation rate in the fuel. This is a conservative assumption since a larger fraction of power is generated at the outer periphery of the fuel than in the center region.

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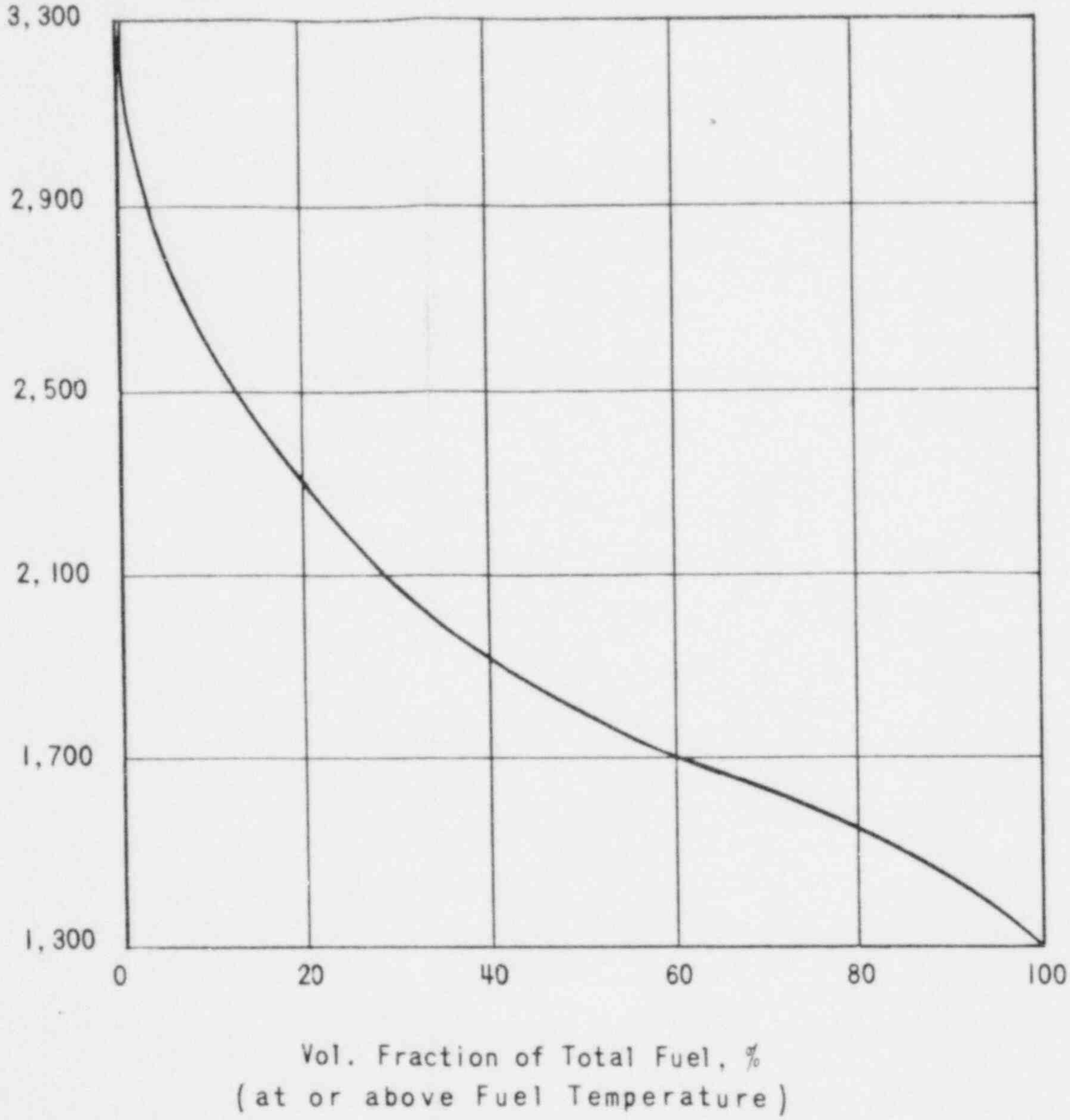

FIGURE 3A.2-1
FUEL TEMPERATURE VERSUS TOTAL FUEL
VOLUME FRACTION FOR EQUILIBRIUM
CYCLE AT END OF LIFE

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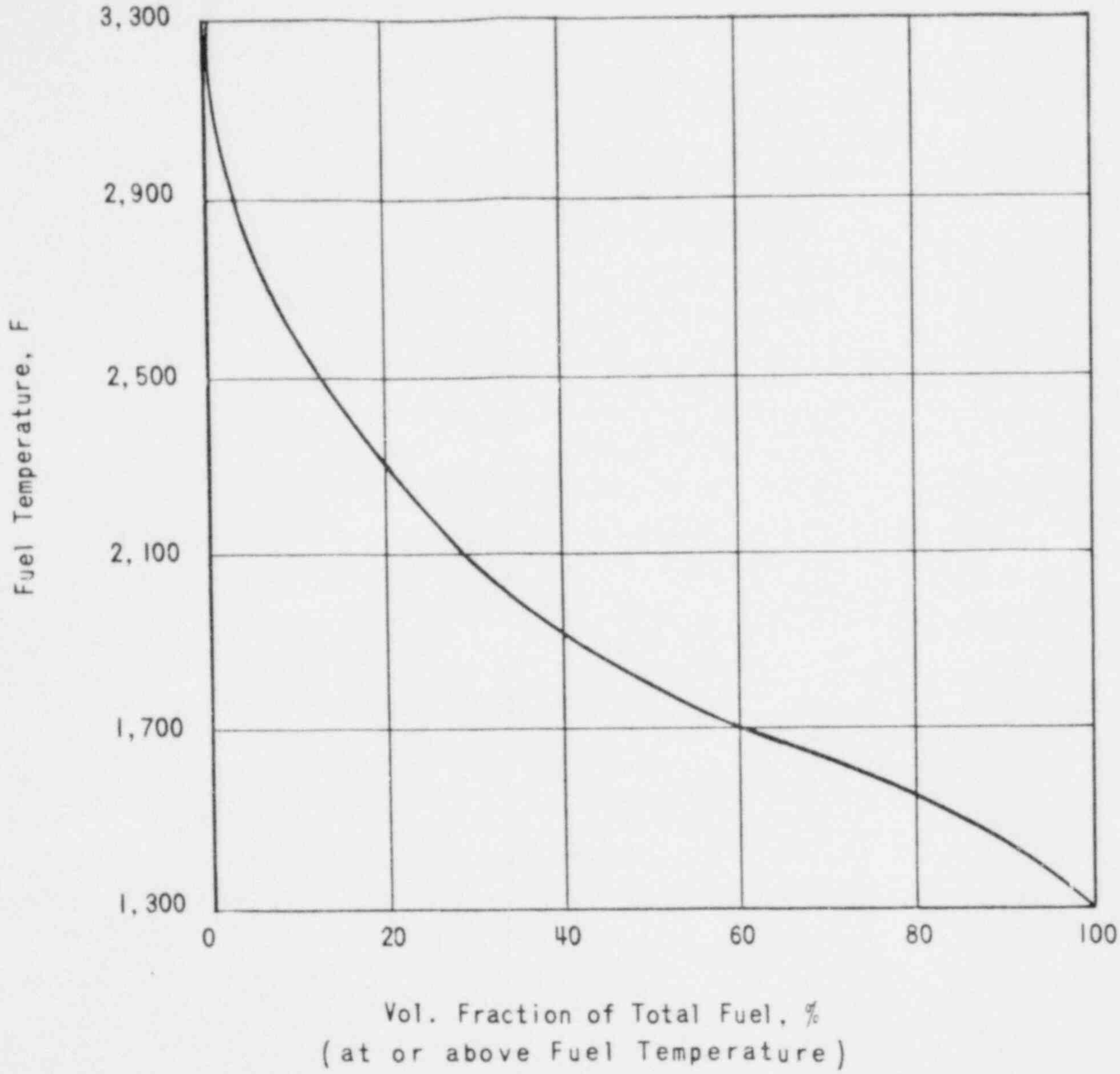

FIGURE 3A.2-1
FUEL TEMPERATURE VERSUS TOTAL FUEL
VOLUME FRACTION FOR EQUILIBRIUM
CYCLE AT END OF LIFE

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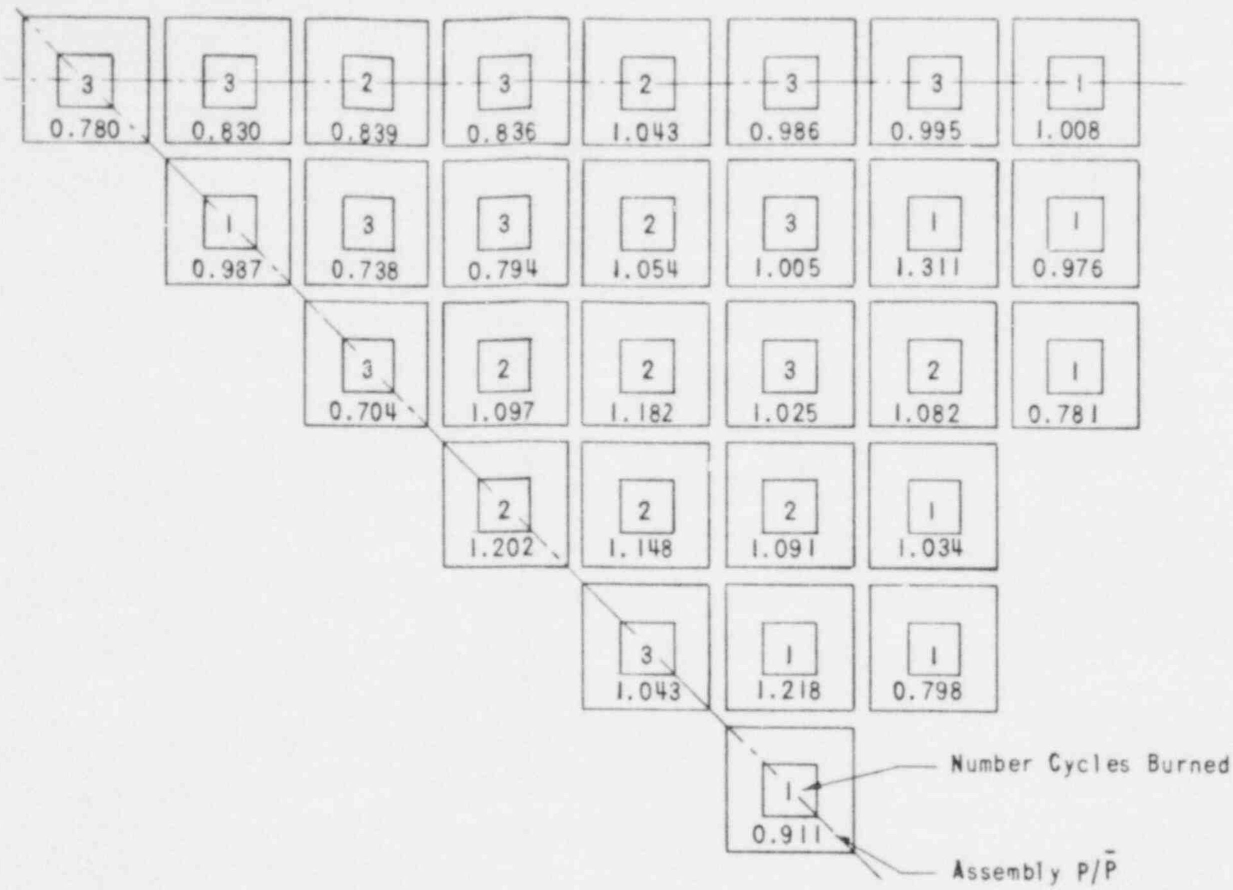

FIGURE 3A.2-2
TYPICAL REACTOR FUEL ASSEMBLY POWER DISTRIBUTION AT END OF LIFE EQUILIBRIUM CYCLE CONDITIONS FOR 1/8 CORE

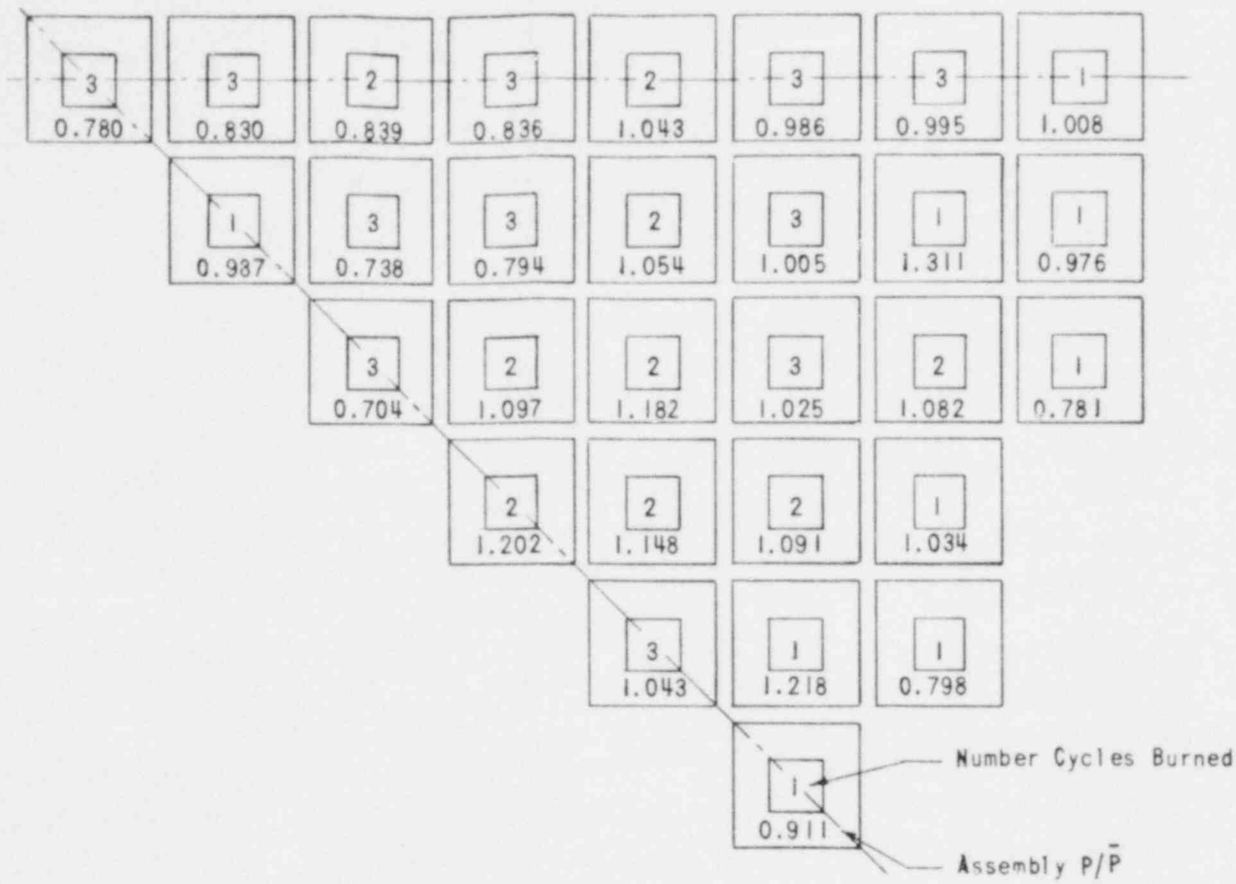

FIGURE 3A.2-2
TYPICAL REACTOR FUEL ASSEMBLY POWER
DISTRIBUTION AT END OF LIFE EQUILIBRIUM
CYCLE CONDITIONS FOR 1/8 CORE

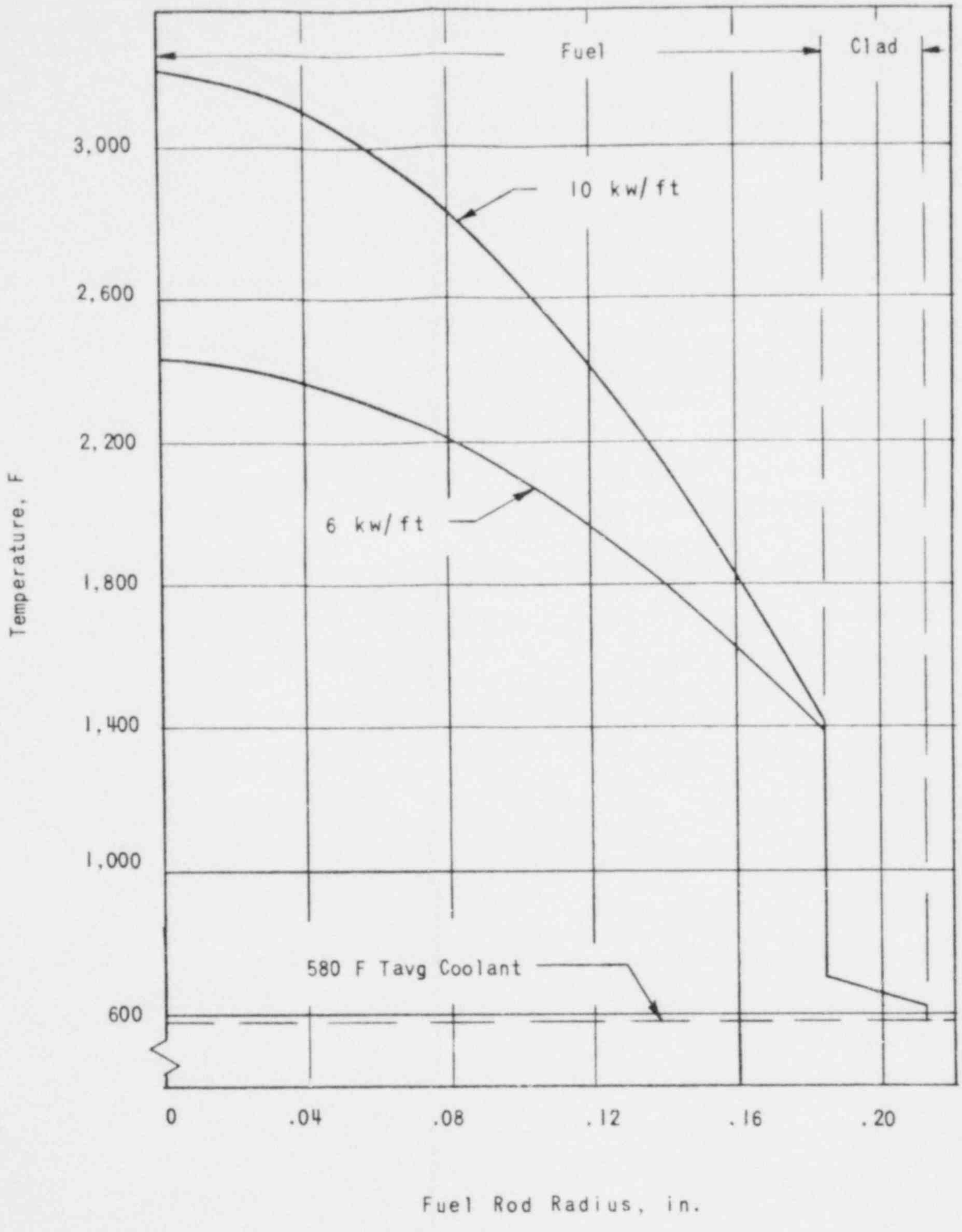

FIGURE 3A.2-3
FUEL ROD TEMPERATURE PROFILES
AT 6 AND 10 KW/FT

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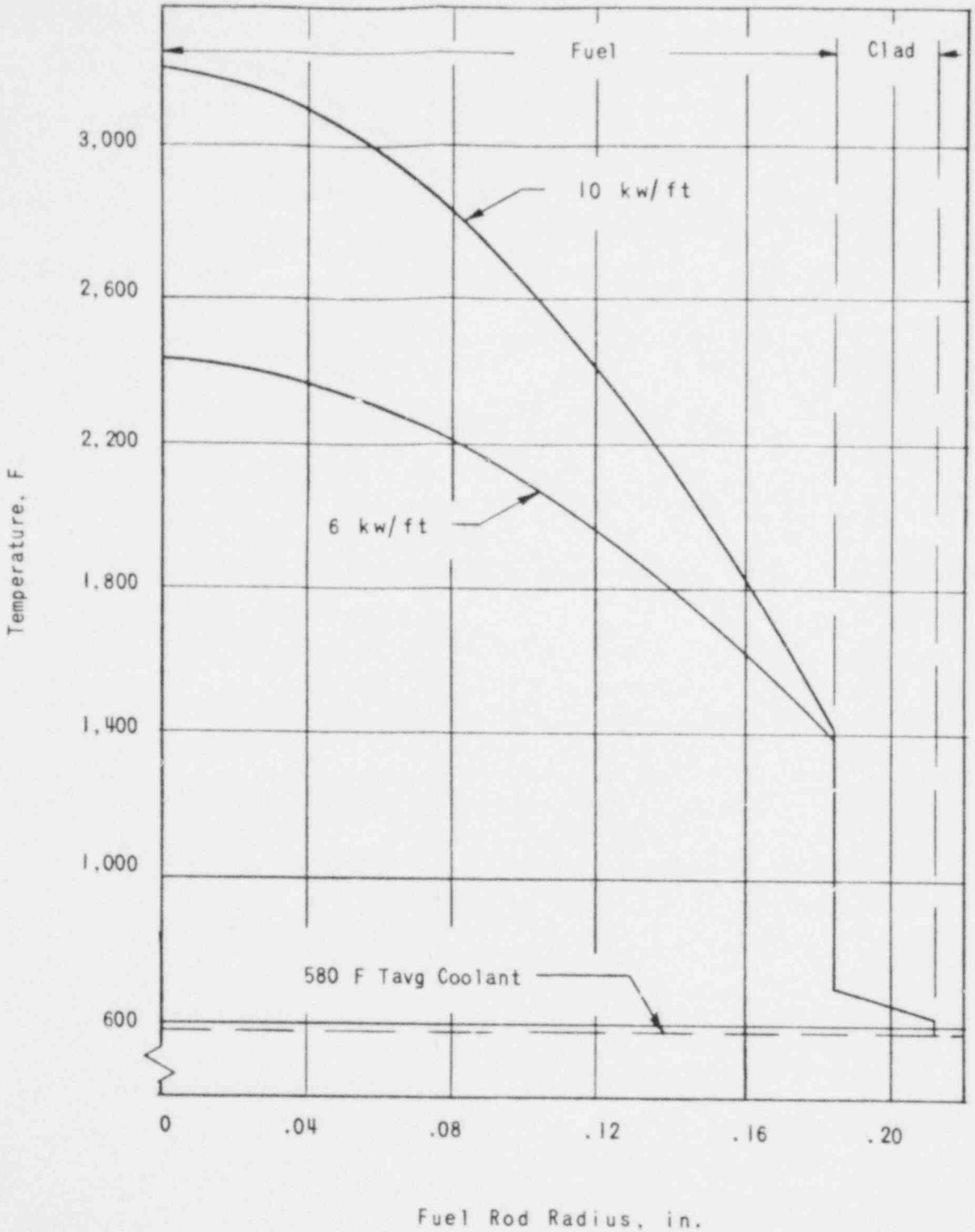

FIGURE 3A.2-3
FUEL ROD TEMPERATURE PROFILES
AT 6 AND 10 KW/FT

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QUESTION 3A.3

Discuss the effect of dropping control rods without snubber action. What procedures ensure detection of gas accumulation in the snubber? Could timed control rod drop tests be performed which would detect the absence of snubbing action and are these planned (for example, following a shutdown)?

ANSWER

Refer to 3.2.4.3.2

The complete absence of any snubbing action of the control rod drive mechanism on a trip action, i.e., free fall drop, is considered to be incredible. For such an action to occur under reactor operating conditions, it would be necessary for the rack housing to be void of any water from the vent cap to the bottom of the snubber cylinder. This void could be established only by a bubble of air and/or gases of 590 in.^{3}.

Operating procedures require that all drives be vented during fill-up of the reactor vessel with reactor coolant. If a drive were not vented as a result of an administrative error, the entrapped air would be compressed to a volume of 14-in.^{3} at operating pressure. This volume is much less than the 590-in.^{3} volume required to fill the snubber with gas.

Gas generation by radiolytic decomposition during operation is prevented by the hydrogen concentration in the water. However, even if a drive were filled with water containing no hydrogen, the gas bubble generated for a year of operation would be smaller than the 14-in.^{3} bubble which might occur from entrapped air. Thus, gas generation during operation could not lead to a situation in which the water in the snubber was replaced by gas.

This analysis demonstrates that neither entrapped air nor gas generation could lead to loss of snubber action. Accordingly, neither procedures to detect gas accumulation in the snubber, nor testing to detect gas accumulation in the snubber, nor testing to detect the absence of snubbing action are considered necessary following the reactor shutdown, and none are planned.

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QUESTION 3A.4

Describe the preliminary design of the means to prevent a vapor lock in the hot leg after an accident which we understand to be check valves located in the core barrel.

3A.4.1 Discuss alternate solutions including rearrangement of the primary system.

3A.4.2 Describe the assumptions used in calculating the required capacity, number and size of the valves and the redundancy in number and capacity to be provided.

3A.4.3 Indicate the location of the valves in the core barrel and the means provided to remove, test, and inspect them.

3A.4.4 Discuss the hinge design with respect to failure of components and availability for inspection.

3A.4.5 Indicate the consequences of loss of a valve from the standpoint of detecting the occurrence, damage to the core from flow bypass and physical damage potential of the loose valve.

ANSWER

Refer to 3.2.4

A vapor lock problem could arise if water is trapped in the steam generator blocking the flow of steam from the top of the reactor vessel to a cold leg break. Under this condition the steam pressure at the top of the reactor would rise and force the steam bubbles through the water leg in the bottom of the steam generator. This same differential pressure that develops a water leg in the steam generator will develop a water leg in the reactor vessel which could lead to uncovering of the core.

The most direct solution to this problem is to equalize the pressure across the core support shield, thus eliminating the depression of the water level in the core. This can be accomplished by vent (check) valves in the core support shield which provide direct communication between the upper reactor plenum and the top of the annulus. These vent valves open on a very low pressure differential to allow steam generated in the core to flow directly to the leak from the reactor vessel. Although the flow path in the steam generator is blocked, this is of no consequence since there is an adequate flow path to remove the steam being generated in the core.

The preliminary design of this valve is shown in Figure 3A.4-1. The valve disc hangs closed in its natural position. A flat, stainless steel seat inclined 5 degrees from vertical insures

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against flow from the annulus to the upper plenum chamber assembly. In the event of an accident, the reverse pressure drop will open the valve. At all times during normal reactor operation the pressure in the annulus on the outside of the core support shield is greater than the pressure in the upper plenum chamber on the inside of the core support shield. Accordingly, the vent valve will be held closed during normal operation. With four reactor coolant pumps operating, the pressure differential is 42 psi resulting in a several thousand pound closing force on the vent valve.

Under accident conditions the valve will begin to open when a pressure differential in a direction opposite to the normal pressure differential of about 0.3 psi develops. At this point the opening force on the valve counteracts the natural closing force of the valve. With a pressure differential of no greater than 1.5 psi, the valve would be fully open. With this pressure differential the water level in the core would be at about the top of the core. In order for the core to be half-uncovered, assuming solid water in the bottom half of the core, a pressure differential of 3.7 psi would have to be developed. This would provide an opening force of about 3 times that required to open the valve completely. This is a conservative limit since it assumes equal density in the core and the annulus surrounding the core. The hot, steam-water mixture in the core will have a density much less than that of the cold water in the annulus, and somewhat greater pressure differentials could be tolerated before the core is more than half-uncovered.

In summary, the reactor design includes vent valves in the core support shield to prevent a pressure unbalance which might interfere with core cooling following a loss-of-coolant accident. In its natural state and under all normal operating conditions, the vent valve will be closed. In the event of a loss-of-coolant accident in the cold leg of the reactor loop, the valve will open to permit steam generated in the core to flow directly to the leak and will prevent the core from becoming more than 1/2-uncovered after emergency core coolant has been supplied to the reactor vessel.

3A.4.1 Alternate Solutions

In question 15.1 of PSAR Supplement 4, and in Item 2 of Supplement 5 of Dockets 50-269, 270, 287 (Duke Power Co.), alternate solutions involving rupture discs, steam relief, or steam generator drains were originally discussed as potential solutions to this problem. Rearrangement of the reactor coolant system is another potential solution. All of these alternates were considered in the evaluation of the best solution which led to the selection of vent valves in the internals as the solution to be utilized. The selection of vent valves as the solution eliminates the possibility of a pressure differential across the core support shield which could prevent core cooling by the emergency coolant. The vent valves, which are closed during all normal operating conditions, will be opened by the pressure differential existing under accident conditions before that differential could interfere with emergency core cooling and without the requirement for an internal actuating signal or energy source. Thus, they provide a solution for the problem.

Continued design and evaluation has indicated that a reliable valve suitable for this service can be manufactured and installed in the internals. Accordingly, more detailed design of alternate solutions has not been carried out.

3A.4.2 Assumptions

An analog computer simulation has been developed to evaluate the performance of the vent valves in the upper reactor plenum chamber. The results are being analyzed to demonstrate that adequate steam relief exists so that cooling of the core will be accomplished.

The basic model is a simulation of the reactor coolant system which includes the effect of the emergency cooling by the core flooding system, the effect of steam generation in the once-through steam generators, the effect of steam generation in the core, and the effect of operation of the vent valves. The model is composed of four basic regions that simulate the water volume in the annulus between the reactor vessel and the core, the water volume in the core, the steam volume above core water level, and the steam volume in the region between the vent valves and the break location. Fluid flow between each of these regions, flow from the emergency injection system, steam flow though the break, and possible water spillage from the break are all considered. The core volumetric heat generation and heat transfer to a changing water level in a five-section core is considered.

Preliminary results of the computer program, have determined that eight 14-inch diameter valves will be required. Conservative assumptions on core decay heat, flow losses, heat transfer coefficients, and the available capacity from the emergency injection systems have been used.

The performance of the valves must meet the criterion for core cooling that has been defined in 14.2.2.3.2 of the PSAR (page 14.2-8) and is quoted below:

> "The performance criterion for emergency core cooling equipment is to limit the temperature transient below the clad melting point so that fuel geometry is maintained to provide core cooling capability. This equipment has been conservatively sized to limit the clad temperature transient to 2,300 F or less as temperatures in excess of this value promote a faster zirconium-water reaction rate, and the termination of the transient near the melting point would be difficult to demonstrate."

3A.4.3 Location

The preliminary arrangement consists of 14-in. diameter vent valve assemblies installed in the cylindrical wall of the internals core support shield (refer to Figure 3.2-59 of the PSAR). The valve centers are coplanar and are 42 inches above the plane of the reactor vessel coolant nozzle centers. In cross section, the valves are spaced around the circumference of the core support shield wall.

Each valve assembly consists of a hinged disc, valve body with sealing surfaces, split-retaining ring, and fasteners. Each valve assembly is installed into a machined mounting ring, integrally welded in the core support shield wall. The mounting ring contains the necessary features to retain and seal the perimeter of the valve assembly. Also, the mounting ring includes an alignment device to maintain the correct orientation of the valve assembly for hinged-disc operation. Each valve assembly will be remotely handled as a unit for removal or installation. Valve component parts, including the disc, will be of captured-design to minimize the possibility of part loss to the coolant system, and all fasteners will include a positive, locking device. The hinged-disc will include an integral arm hook, eye, or other device for remote inspection of disc function.

During refueling outages after the reactor vessel head and the internals upper plenum assembly have been removed, the check valves will be accessible for visual and mechanical inspection. A remote inspection tool will be provided to engage with the previously mentioned valve disc hook or eye. With the aid of this tool, the valve disc can be manually exercised to evaluate the disc freedom. The hinge design will incorporate special features, as described in the reply to Question 5.1.4 below to minimize the possibility of valve disc motion impairment during its service life.

Remote installation and removal of the vent valve assemblies will be performed with the aid of another tool which will include unlocking and operating features for the mounting ring. This handling tool design will be functionally developed and tested on a full-size mockup of the vent valve installation configuration prior to valve manufacture.

With the aid of the above described inspection tool, a visual inspection of the valve body and disc sealing faces can be performed for evaluation of observed surface irregularities.

3A.4.4 Design

The valve disc, hinge shaft, shaft journals (bushings), disc journal receptacles, and valve body journal receptacles will be designed to withstand without failure the internal and external differential pressure loadings resulting from a loss-of-coolant accident. These valve materials will be nondestructively tested and accepted in accordance with the ASME Code III requirements for Class "A" pressure vessels.

The hinge materials will be selected on the basis of their corrosion resistance, surface hardness, antigalling characteristics, and compatibility with mating materials in the reactor coolant environment.

The hinge design will consist of a shaft, two valve body journal receptacles, two valve disc journal receptacles, and four flanged shaft journals (bushings). Loose clearances will be used between the shaft and journal inside diameters, and between the journal outside diameters and their receptacles.

This feature provides eight loose rotational clearances to minimize any possibility of impairment of disc-free motion in service. In the event that one rotational clearance should bind in service, seven loose rotational clearances would remain to allow unhampered disc free motion. In the worst case, at least four clearances must bind or seize solid to adversely affect valve disc free motion.

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In addition, the valve disc will contain a self-alignment feature so that the external differential pressure will adjust the disc seal face to the valve body seal face. This feature minimizes the possibility of increased leakage and pressure-induced deflection loadings on the hinge parts in service.

The external side of the disc will be contoured to absorb the impact load of the disc on the reactor vessel inside wall without transmitting excessive impact loads to the hinge parts as a result of a loss-of-coolant accident.

In conclusion, the failure of hinge parts or impairment of valve disc free motion is considered very unlikely.

The determination of hinge-free motion by inspection is described in reply to Question above. A remote inspection of hinge parts is not planned until such time as a valve assembly is removed because its disc-free motion has been impaired. In the unlikely event that a hinge part should fail during normal operation, the most significant indication of such a failure would be a change in the disc-free motion as a result of altered rotational clearances.

3A.4.5 Loss of Vent Valve

An arrangement consisting of valves with a 14-in. diameter throat was investigated. In the event the disc from one of these valves is completely removed, a small reduction in effective core flow for heat removal will be experienced. Approximately 5.7 percent of the incoming flow will bypass the core through the valve opening. However, the reduction of resistance results in an increase in total system flow of about 1.1 percent. The net reduction of flow for core heat removal is 4.6 percent.

The minimum DNB ratios for the reduced effective core flow compare with the full flow ratios as follows:

Percent Rated Power	DNBR (Full Flow)	DNBR (Reduced Flow)
100	1.76	1.68
107.5	1.53	1.44
112	1.40	1.30
114	1.34	1.24

DNB ratios were determined for the worst corner, wall, or unit cell for the postulated worst case with the most recent revision of the W-3 correlation as discussed in PSAR paragraph 3.2.3.2.45. The minimum DNB ratio at the

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scram set-point of 107.5 percent power is well above the minimum recommended value of 1.30, and a substantial DNB margin is maintained even if the maximum overpower of 114 percent is accidentally reached concurrent with the accidental loss of the valve disc. The minimum DNB ratio of 1.30 is maintained up to 112 percent power.

The valve disc functions in the inlet coolant annulus formed by the reactor vessel wall and the internals assembly. All valve component parts and related internals mounting parts will be designed to withstand the internal and external differential pressure loadings resulting from a loss-of-coolant accident. Valve materials will be nondestructively inspected and accepted in accordance with the ASME Code III requirements for Class "A" pressure vessels. Each valve assembly will be statically tested to a higher pressure than external design pressure for a loss-of-coolant accident to assure its structural adequacy prior to service. In addition, all valve component parts, including the disc, will be of captured-design to minimize the possibility of part mis-location or loss to the reactor coolant system. In conclusion, the loss of the valve disc or other valve component parts to the coolant system is considered very remote.

In the unlikely event that a valve disc should be lost to the coolant system by disc failure and capture-bond failure, the disc would fall downward through the inlet annulus to the vicinity of the reactor vessel bottom head. At the entrance to the bottom head, the loose disc may impart an indirect impact to the stop blocks welded to the vessel inside wall. Other than local surface damage, no failure would occur to the stop block which is designed to withstand a proportion of the total impact of the internals assembly as described in 3.2.4.1 of the PSAR. A more conservative assessment of damage potential assumes a valve disc free-fall and indirect impact on the spherical inside surface of the vessel bottom head. The spherical surface would deflect the disc direction of travel, and in the worst case, direct the disc to the base of an core instrument penetration. The reactor vessel interior clad surface and incore penetration weld pads would receive local surface damage, but the reactor vessel pressure integrity would remain intact.

The valve is designed, inspected, and tested so that the probability of loss of a valve disc is very small. Even if it does occur, the loose valve disc would not cause serious physical damage, and the DNB ratio in the core would only be reduced slightly. Thus, the loss of a valve disc, without detecting the occurrence, will not cause damage to the core.

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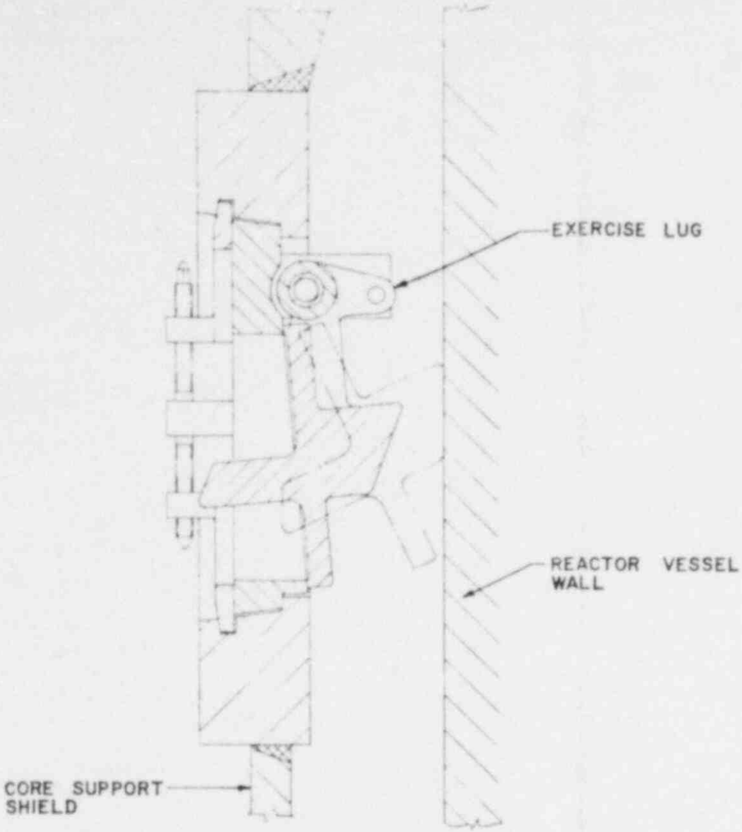

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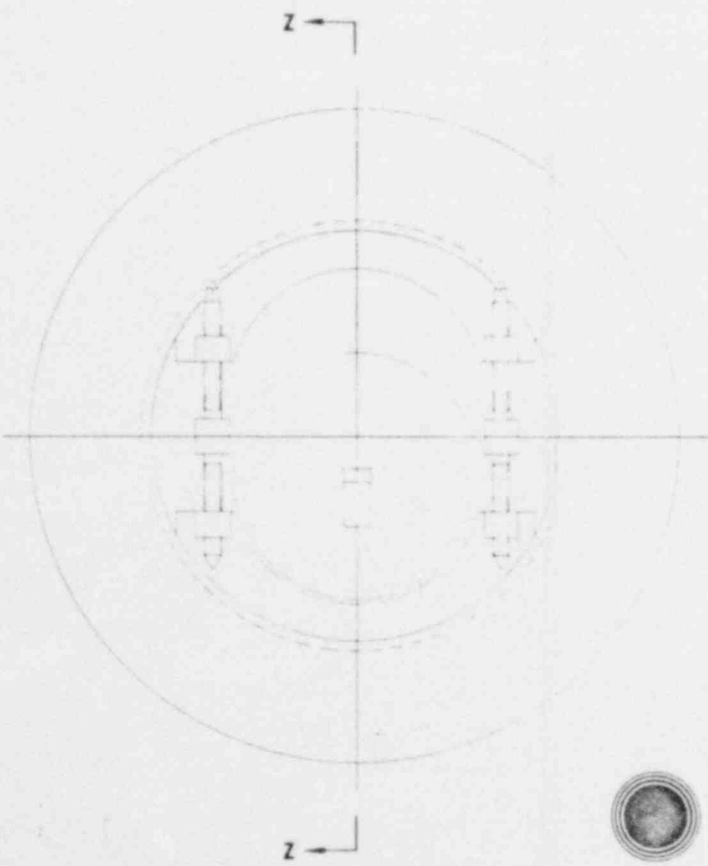

FIGURE 3A.4-1
INTERNALS VENT VALVE

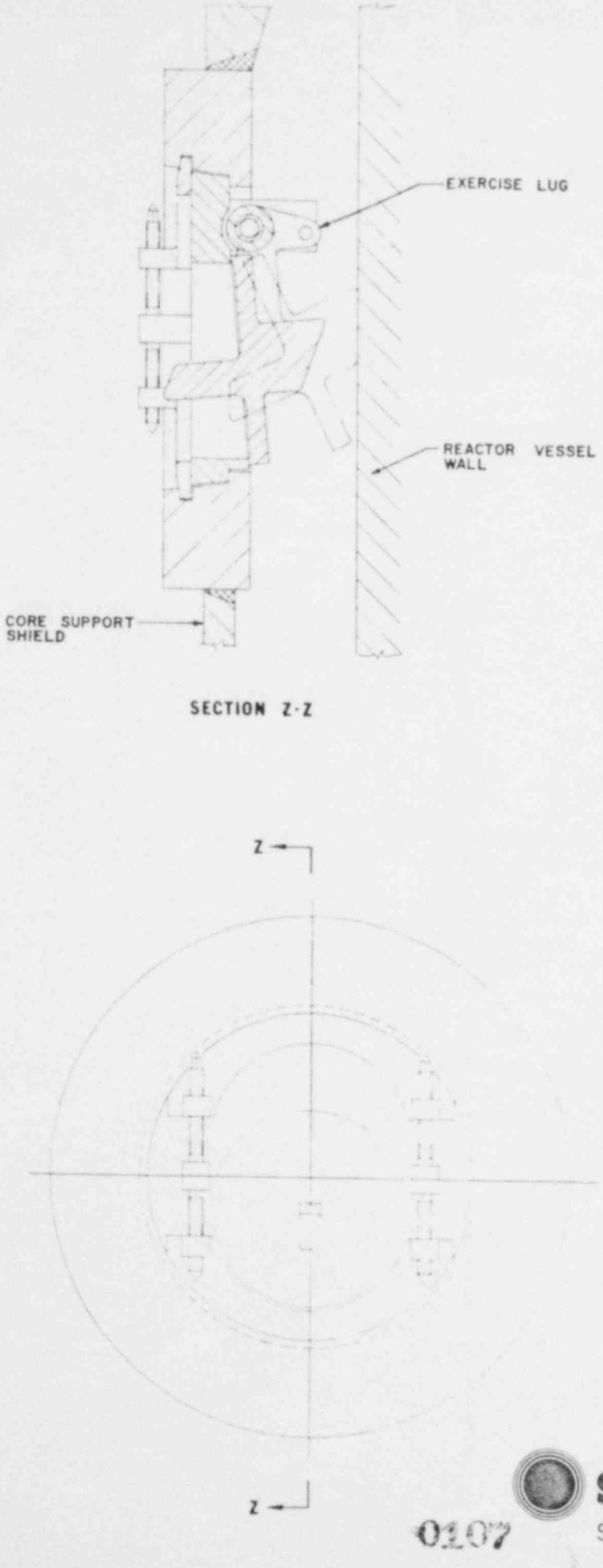

FIGURE 3A.4-1
INTERNALS VENT VALVE

QUESTION 3A.5

Please revise the PSAR to incorporate the results of your more detailed reactivity calculations, which you summarized in our meeting of August 8, 1967.*

3A.5.1 For each of the reactivity worths of Table 3.2-4 of the PSAR, give the expected range of variation due to uncertainties in measurements or calculations. Discuss the basis for the ranges given.

3A.5.2 What is the effect of initial reactor operation with the greatest expected value of positive moderator coefficient on the reactivity control distribution listed in Table 3.2-4.

3A.5.3 What is the basis for the specification of the excess control rod worth of 1.6% $\Delta k/k$ over the holddown requirements of 5.4% $\Delta k/k$.

ANSWER
Refer to 3.2.2.1.2

The following reactivity control distribution revisions reflect the change of the 16 fixed shim rods to movable control rod assemblies, and the results of more detailed 2-D calculations.

3A.5.1 Each of the four basic groups of Table 3.2-4 are discussed below.

a. Controlled by Soluble Boron

The items in this group are controlled by soluble boron, the total holddown varying with operating conditions and core life. The basic safety parameters, i.e., control rod worths and the moderator temperature coefficient, as listed in the PSAR have been generated at boron levels in excess of that expected. These evaluations were made at hot, rated power conditions as listed in Table 3.2-6 of the PSAR, i.e., 1,860 ppm boron. This level, when compared with Figure 3.2-1, is approximately 240 ppm higher than expected for maximum boron at the start of the first cycle. The resulting rod worths are lower, and the moderator temperature coefficient is more positive than would be expected. Analysis of various experimental data regarding reactivity levels indicates a possible uncertainty of approximately 1% $\Delta k/k$. The excess reactivity of approximately 2.4% $\Delta k/k$ as represented by the 240 ppm boron above illustrates basic design conservatism.

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* Revisions are included in SMUD PSAR as submitted November 15, 1967; see last paragraph of following answer.

b. Controlled by Inserted Control Rod Assemblies

The reactivity value specified for transient xenon control was set by core maneuvering requirements. The particular groups of rods selected for this purpose will be chosen such that the resultant power peaking, for insertion or withdrawal, will not exceed design values, nor will the value of any one of the rods exceed the value used in the ejected rod safety evaluation. Uncertainties in peak xenon as associated with the activation of this bank should affect only the relative maneuverability of the core. Analysis of experimental rod worths indicates that the uncertainty associated with the calculation of the transient bank would be relatively small, and under the selection criteria stated above would affect only the relative core maneuverability similar to the peak xenon.

c. Controlled by Movable Control Rod Assemblies

(1) The basic uncertainty in the Doppler deficit and the associated Doppler temperature coefficient is the fuel temperature. Variations of as much as ± 300 F have been investigated, although uncertainties of ± 200 F are considered reasonable. This maximum variation of ± 300 F results in a $\pm 0.3\%$ $\Delta k/k$ swing in the Doppler deficit, and a variation of less than $\pm 1.0 \times 10^{-6}$ $(\Delta k/k)/F$ in the Doppler coefficient.

(2) Variations in the equilibrium xenon and the equilibrium xenon control bank worth are compensated for by the soluble boron. Uncertainties in the equilibrium xenon are considered as part of the initial reactivity uncertainty previously discussed. The same criteria set forth for the transient xenon bank (item b) will be applied in the selection of the equilibrium bank.

(3) The moderator temperature deficit, which results from the negative moderator temperature coefficient during power changes from zero to 15 percent rated power near the end of core life, varies primarily with end-of-life fuel condition. Calculations for various core cycles indicate a maximum variation of ± 20 percent. As an additional conservatism in this control balance, an uncertainty of $\pm 0.2\%$ $\Delta k/k$, i.e., $\pm 33\text{-}1/3$ percent, was considered.

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(4) The regulating and dilution control rod bank operates between the 75 and 95 percent withdrawn positions. Any uncertainty in the value of this bank as operated within these limits will be taken up in the soluble boron dilution frequency.

(5) The shutdown margin of 1% $\Delta k/k$ is a minimum requirement which is amply covered by the minimum available worth as illustrated in the following paragraph d-(4).

(6) The total movable control worth required is then stated conservatively as $4.0 \pm 0.5\%$ $\Delta k/k$. The variations results from paragraphs c-(1) ($\pm 0.3\%$) plus c-(3) ($\pm 0.2\%$).

d. Available Control Rod Assembly Worths

(1) The total CRA worth has been calculated for various core conditions and reflects an allowance for the following worth-reducing effects:

(a) Boron level

(b) Spectrum changes

(c) Core cross section variation

(d) Control poison burnup

(e) Hot, rated power, operating uncertainty

Although some of these effects are a function of core lifetime, all are considered to be in effect from the beginning of Cycle 1. Therefore, the total worth of 10% $\Delta k/k$ represents a conservative minimum.

(2) The individual rod worths (stuck or ejected) are taken at time zero, and first cycle conditions, and reflect only the first of the five reducing effects listed above. The boron level does not have a strong effect, and the resulting rod worth represents a near maximum value.

(3) The available CRA worth of 7.0% $\Delta k/k$ is a minimum obtained by subtracting the maximum stuck rod worth from a minimum total pattern worth. Therefore, the movable CRA worth available of 5.6% $\Delta k/k$ also represents a conservative minimum.

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(4) Comparison of the maximum required movable rod worth (4.5% $\Delta k/k$) to the minimum movable rod worth available (5.6% $\Delta k/k$) shows an excess of 1.1% $\Delta k/k$.

3A.5.2 As stated in Paragraph a of the answer to Question 4.1.1, a maximum boron level was the basis for the various evaluations of Table 3.2-4, thereby reflecting a maximum moderator temperature coefficient effect.

3A.5.3 The excess control rod worth of 1.6% $\Delta k/k$ was specified as a basis for conservatism in the reactivity control balance of Table 3.2-4. This is illustrated in Paragraph d of the answer to Question 4.1.1 above, and results from the minimum movable CRA worth available (5.6%) less the total nominal movable control worth required (4.0%).

Pages 3.2-4, 3.2-5, 3.2-6, 3.2-7, 3.2-8 and 3.2-9 and Figures 3.2-1 and 3.2-6 of the PSAR include the corrected reactivity values given above.

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QUESTION 3A.6 (DRL 3.1)

Discuss your plans for providing a negative moderator coefficient of reactivity throughout core-life in the event detailed studies show this to be a design requirement.

ANSWER

In the event that it becomes necessary or desirable to provide a negative moderator coefficient of reactivity, burnable poison rods will be added to the core. The burnable poison will most likely be boron, but the choice of carrier and rod design is currently under study.

A series of critical experiments have been conducted (and reported in BAW-3492-1) to study the physics effects of cylindrically shaped, lumped poison distributed throughout a light-water-moderated nuclear reactor. The lumped poisons were borosilicate glass rods, silica glass rods, and aluminum-clad B_4C rods. Measurements included critical size and composition, poison rod reactivity worth, $\partial\rho/\partial h$ and excess reactivity, gross power distribution, and thermal flux distribution around the central poison rod. These experiments have been analyzed and a nuclear calculative model has been developed.

The choice of carrier and rod design will be completed about mid 1968. At this time design studies will be started to arrive at configurations and boron loadings for various core designs.

The analytical work will be completed well in advance of contractual commitments. If it should be necessary to incorporate burnable poison in the design, the addition will not effect the present schedule.

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QUESTION 3A.7 (DRL 3.2) Describe your derivation of the "power Doppler coefficient" given in Table 3.2-3 of the PSAR and compare the time constant of this coefficient with that of the system in the analytical model.

ANSWER The power coefficient reported in Table 3.2-3 in the PSAR is defined as

$$\alpha_T = \frac{\partial \rho}{\partial \overline{T}_{fuel}} \cdot \frac{\partial \overline{T}_{fuel}}{\partial \phi} = \frac{\partial \rho}{\partial \phi}$$

The first term is the prompt doppler coefficient and the second term is used to change the units of the power coefficient to $\frac{\Delta K}{K}$/unit flux.

The time constant of the power coefficient is prompt and the time constant of the system (xenon oscillation) is of the order of 25 to 30 hours.

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QUESTION 3A.8 (DRL 3.3) Submit the latest available results of those analyses on xenon oscillations described on pages 3.2-21 and 3.2-23 of the PSAR and specify the dates when the remaining analyses will be completed.

ANSWER The current modal and digital analysis employ a term in the power coefficient to account for the reactivity feedback effects from the moderator.

In this sense, the Doppler effect is still prompt and the feedback from the moderator is treated analytically as if it were prompt since the calculations are static. This system allows for the accounting of the minimum negative feedback mechanisms in a system with a positive moderator coefficient.

a. Modal Analysis

A modal analysis has recently been completed wherein the stability index (or margin) of the plant was determined as a function of:

(1) Moderator coefficient

(2) Power distribution

(3) Xenon cross section

(4) Iodine yield 3

(5) Xenon yield

(6) Fuel temperature

(7) Doppler coefficient

A topical report covering the details of this work will be released in mid 1968.

Essentially the modal analysis work reported in the PSAR has been revised and expanded to include the moderator effect on the power coefficient and to include the concept of stability index.

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The power coefficient is now defined to be:

$$\frac{\partial \rho}{\partial P_T} = \frac{\partial \rho}{\partial T_f} \cdot \frac{\partial T_f}{\partial P_T} + \frac{\partial \rho}{\partial T_m} \cdot \frac{\partial T_m}{\partial P_T}$$

T_f = Fuel temperature

T_m = Moderator temperature

ρ = Reactivity

P_T = Power

The stability margin for the revised modal analysis is defined as follows:

3

$$z = -\frac{1}{2}(\lambda_i + \lambda_x) - \frac{1}{2}\sigma_x \bar{\phi}_j \left[1 + \frac{a_x (X_j - \gamma_x)}{\mu_j^2 - a_T \bar{\phi}_j} \right]$$

λ_i = Decay constant of Iodine-135

λ_x = Decay constant of Xenon-135

σ_x = Microscopic cross section of Xenon-135

$\bar{\phi}_j$ = Product of unperturbed flux distribution and the square of the first mode buckling weighted over volume.

a_x = Reactivity per unit flux held by saturation xenon divided by core migration area.

X_j = Product of unperturbed xenon distribution and square of first mode buckling integrated over volume.

γ_x = Yield of xenon divided by sum of iodine and xenon yields.

a_T = Power coefficient in units of reactivity per unit flux divided by core migration area.

μ_j^2 = Constant proportional to difference in fundamental and first mode buckling.

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The stability margin (z) is related to the stability margin (b) reported in BAW-305, "Xenon Oscillations," (Reference 17 on page 3.4-2 of the PSAR) as follows:

$$z = -\frac{b}{2}$$

On page 3-5 of the above reference it is shown that

$$\omega = \frac{-b \pm (b^2 - 4c)^{1/2}}{2}$$

The stability margin (z) is then the real part of the solution to the ω equation shown above.

b. Digital Analysis

A one-dimensional (axial) digital analysis is currently underway that will provide the information necessary to calculate the stability index of the plant as a function of those seven parameters listed in part A above. The report of this work will be released during the last quarter of 1968. 3

The digital calculations are being done with a diffusion depletion program that interates on fuel and moderator temperature as a function of power, where the power is defined for a region. (The geometry used in the calculation describes the core height with fifteen equal volume fuel zones plus a top and bottom reflector.) The spectrum is recalculated for each region to reflect the modified temperatures and power.

In the study, the core is perturbed and the resulting oscillatory behavior is plotted for the point where the maximum power peak has occurred as a function of time and power. The stability index for this case is found then by using the calculated data in a least squares fit program to solve the following equation:

$$P - P_o = A_e^{\,z(t-t_o)} \sin \frac{2\pi (t-t_o)}{\omega}$$

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where

P_o = Power at oscillation mode

t_o = First time power at oscillations mode is equivalent to P_o.

P = Power at anytime z

ω = Oscillation period

z = Stability index

The program prints out the stability index z and the normalized index z^1 wherein the results have been modified to include the finite time step correction from the work of C. G. Poncelet and A. M. Christie.

In general then, the current modal and digital analyses employ a term in the power coefficient to account for the reactivity feedback effects from the moderator.

In this sense, the doppler effect is still prompt and the feedback from the moderator is treated analytically as if it were prompt since the calculations are static. This system allows for the accounting of the minimum negative feedback mechanisms in a system with a positive moderator coefficient.

QUESTION 3A.9 (DRL 3.4) Discuss the detection system for xenon oscillations and indicate the expected minimum sensitivity of this system during power operation.

ANSWER Nine of the in-core instrumentation locations will be designated as the xenon detection system. The read-out of these positions will be available to the operator through the online computer as well as through a secondary device such as a multipoint recorder.

3

The application of this system for detection of xenon oscillation and its minimum sensitivity is being examined through the analysis of experimental data. The analysis should be completed by the end of 1968. However, previous performance data are available. A series of Physics Verification Program Reports developed under AEC Contract No. AT(30-1)-3647 and B&W Contract No. 41-2007 have previously been submitted to the Commission for review. Much of the data compiled was taken by self-powered detectors and shows the performance capabilities of the detectors. Upon initial installation, the self-powered detector has the capability to

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measure the relative flux within 5 percent of the true flux when used in conjunction with an adjacent background detector. Normal readout of the incore monitors is through the computer whose inaccuracies are negligible and are included in the above tolerance. The sensitivity of the detector will decrease with exposure to neutron flux due to transmutation of the emitter in the detector. However, by use of integrated current inventories, it is felt that the additional inaccuracies shall be no more than 1 percent per year for the average flux conditions. 3

To detect xenon oscillations it is necessary to have a device with good reproducibility and which can detect relative flux changes of 2-3% full power. The incore monitoring system has this capability.

QUESTION 3A.10 (DRL 3.5)

Describe the two-dimensional analysis method for evaluation of xenon instabilities.

ANSWER

Two dimensional analysis will be performed using RZ and XY geometries in the HARMONY program. Feedback mechanisms will be available in both options.

The RZ analysis will be performed to give the relation between the 1-D axial and the 2-D in regard to the stability margin of the design plant.

The XY program is planned for the determination of core behavior as a function of the most sensitive core parameters as discussed in the answer to Question 3A.8.

Control mechanisms will be evaluated in both systems.

QUESTION 3A.11 (DRL 3.6)

Assuming that control rods are used to stabilize xenon oscillations, give the maximum values anticipated for the transient and steady-state errors in local power density at the hot spots.

ANSWER

Assuming that part length control rods will be used to stabilize axial xenon oscillations, the resulting power peaks from transients will be a function of the position of these rods in relation to other rods that probably will be fully inserted.

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The determination of the peaking will be performed in 3-dimensions as soon as the system is available. A first approximation of this assumed control system will be available from the RZ HARMONY study.

3 At present, nuclear design studies allow for an estimated 10% uncertainty due to transient xenon. As a consequence the maximum peaking factors used in the thermal analysis of the core contain an estimated 10% uncertainty factor to account for the change in peaking resulting from xenon oscillations. These corrections are contained in the maximum design values listed in the PSAR Table 3.2-1 and will be confirmed as stated above.

QUESTION 3A.12 (DRL 3.7)

Indicate the margin of xenon stability by giving the power level at which xenon oscillations are predicted to occur at various times during core life.

ANSWER

As stated in the PSAR (Section 3.2.2.2.3, beginning on page 3.2-20), the plant is not expected to be subject to diverging power oscillations as a result of the redistribution of xenon.

3 The R&D program underway includes the development of 1, 2, and 3 dimensional digital programs with Doppler and moderator feedback mechanisms to ascertain the stability margin of the plant in question. The stability margin analysis will include the effect to account for finite time step length in the calculation as well as the effects on stability due to the uncertainties in such reactor parameters as:

(1) Moderator coefficient

(2) Power distribution

(3) Xenon cross section

(4) Iodine yield

(5) Xenon yield

(6) Fuel temperature

(7) Doppler coefficient

The stability margin will be determined for the useful life of the plant.

Control mechanisms for spatial oscillations will be developed at the same time in the event that they will be required for the plant.

The program is planned for completion of the 1 and 2 dimensional analysis by the end of 1968 and the completion of the 3 dimensional analysis by late 1969. These completion dates allow sufficient time to respond to the results should that be necessary or desirable.

If, for instance, the study shows that the plant is marginal or unstable to axial disturbances then part length control rods will be provided to assure that axial stability can be assured through the judicious use of these rods. Should it be necessary or desirable to reduce the positive moderator coefficient this will be done through the addition of fixed shims or burnable poison.

See also answer to question 3A.8. 3

QUESTION 3A.13 (DRL 3.8) Discuss the fuel management plans and techniques that will limit maximum fuel burn-up to 55,000 MWD/MTU and describe the associated uncertainties.

ANSWER The average burnup at the end of life (930 days) in the hot fuel rod of the Rancho Seco core is calculated to be 38,150 MWD/MTU. This value has been determined as follows:

1.	Calculated hot bundle average burnup, MWD/MTU	33,000
2.	Hot fuel rod burnup factor	1.05
3.	Margin for calculated accuracy	1.10
4.	Hot rod maximum average burnup, MWD/MTU	38,150

Accounting for local burnup along the length of the fuel rod results in a calculated hot rod local maximum of 42,000 MWD/MTU. The uncertainties associated with the calculation of the fuel burnup are about 10% as indicated in item 3 above. This 10% uncertainty is included in the calculated hot rod local maximum burnup of 42,000 MWD/MTU. Fuel management plans include continuing surveillance of fuel element burnup.

3 The fuel management program planned for use in conjunction
4 with the incore detector system and online computer will provide a history of the burnup for each assembly, thus allowing the intelligence required to make comparison of the fuel management program with the comprehensive calculative program.

The uncertainties associated with the determination of neutron flux by use of the incore detectors are about 5% plus 1% per year as stated in the answer to question 3A.9. The total uncertainties associated with the determination of the fuel burnup through the use of the incore detectors and the
3 online computer are no more than 10%.

Regardless of which of the two methods is used to determine fuel burnup, the maximum hot rod local burnup including all uncertainties is not expected to exceed 42,000 MWD/MTU at the initial core thermal power level of 2,452 Mwt. The maximum design burnup of 55,000 MWD/MTU referred to in 3.1.2.1, 3.2.3.2.4, and 3.2.4.2.2 of the PSAR is only a limit specified for the thermal and mechanical design of the reactor core.

The comprehensive calculative program is a 2-dimensional, digital computer program taking into account specified maneuvering operations to be used over the life of the first cycle. Proper account will be taken for the known inadequacies of diffusion theory as well as those ascertained for the calculative model in regard to power peaking.

QUESTION 3A.14 (DRL 3.9)

Discuss your calculational model and indicate the error band on the fast neutron flux (E_n 1.0 Mev) at the pressure vessel inner surface which was calculated to be 3.4×10^{10} (n/cm^2-sec). Include in the discussion:

(a) How azimuthal variations are treated in the analysis and relate these to the azimuthal placement of the surveillance specimens.

(b) The uncertainties associated with the attenuation factor of $6.0 \times 10^{13}/3.4 \times 10^{19}$ or 1760 and relate their potential consequences to higher values of NDTT for the pressure vessel wall.

(c) The maximum fast neutron exposure (see pg. 4.1-8) is indicated to be 3.0×10^{19} (n/cm^2) or, at 807 load factor, 1.9×10^{10} (n/cm^2-sec). Explain the relationship between this design limit and the data given in Table 3.3-7 of the PSAR with respect to the factor of 2 conservatism indicated on page 3.2-14 of the PSAR.

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ANSWER

As described in Section 3.2.2.1.7 of the PSAR the calculation of fluxes at the reactor vessel were based on the TOPIC program. This code provides a few-group solution to the 1-dimensional transport equation in cylindrical geometry using the S_n technique. The calculations were performed using an equivalent core diameter of 128.9 inches. Increases in the flux locally on the vessel wall due to fuel element corners extending beyond the equivalent core diameter are estimated to be between 15 and 20 percent. The placement of surveillance specimens within the reactor vessel will be such that the integrated exposure to the specimens will be as great or greater than the maximum exposure to the vessel wall. Calculations will be performed to correlate the fast neutron exposure on the vessel wall to that on the surveillance specimen. In addition, the dosimeters such as those determined to be appropriate by referring to ASTM method E-261 will be placed in the specimen holder to provide 3
measurements of the fast neutron exposure to the surveillance specimens (see page 4.4-5). For example, a Cadimum encased capsule of Neptunium 237 may be used. 4

Figure 3A.14-1 shows the attenuation of the neutron flux above 0.82 Mev between the core and the reactor vessel wall as computed by the TOPIC program. The fluxes as shown are based on an estimated lifetime average power density at the core edge of 36.5 watts/cc. This includes a 1.3 estimated axial power peaking factor at the outer edge of the core, averaged over plant life. To account for differences between TOPIC calculations and thermal flux measurements in the LIDO pool (see page 3.2-13 of the PSAR), a scaling factor of 2 was applied to the calculated flux values at the vessel wall. This results in a flux level of 1.8×10^{10} n/cm^2-sec which is the predicted flux on the vessel wall including axial power peaking, averaged over the life of the plant.

Based on the value of 1.8×10^{10} the effective relaxation length between the core edge and the vessel wall is 7.9 cm. 3
A comparison has been made between this predicted relaxation length with that determined from various measurements. Comparisons were made with experiments performed on the SM-1 mockup[1], on the R2-0 reactor at Studsvik Research Centre[2], and on a shielding mockup of the reactor vessel and internals of the Rancho Seco design at the B&W Critical Experiment Laboratory[3]. To reflect the lower density water in the Rancho Seco design the relaxation lengths from the various experimental results were derived as follows:

$$= \frac{W/\rho + S}{l_n \ \phi_o/\phi}$$

where λ = relaxation length, cm.

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W = water distance between ϕ_o and ϕ, cm.

ρ = water density between core and vessel in Rancho Seco design, gm/cc

S = steel thickness between ϕ_o and ϕ, cm.

ϕ_o = neutron flux at core edge, n/cm^2-sec.

ϕ = neutron flux at vessel wall, n/cm^2-sec.

The effect of core geometry on the relaxation lengths was estimated by assuming the Rancho Seco design to be equivalent to an infinite slab and determining the fraction of an infinite slab represented by the various experiments. For the box-like SM-1 and R2-0 cores the conventional truncation techniques based on Sievert's integral were used, and for the cylindrical core in the B&W experiment the method of Taylor and Obenshain[4] was used.

On the SM-1 mockup, through two inches of iron and about 11 inches of water, the relaxation length, as determined from $^{32}S(n,p)^{32}P$ measurements, was 6.8 cm. In the R2-0 experiment, through a water thickness equivalent to the distance between the core and reactor vessel on the Rancho Seco design, measurements with sulphur foils yielded a relaxation length
3 of 8.1 cm, and data from the $^{115}In(n,n')^{115m}In$ reaction yielded a 7.7-cm relaxation length. In the B&W experiment on the Rancho Seco design, data from sulphur foils showed a relaxation length of 8.0 cm. The predicted flux attenuation is thus in good agreement with the experimental data.

To obtain a design value for the nvt on the reactor vessel the predicted flux at the vessel wall was modified to include the following conservatisms.

1. Radial power peaking factor. The maximum power density at the outer edge of the core, averaged over the life of the plant, is estimated to be 36.5 watts/cc. The power density at the core edge at the end of each core cycle, resulting from radial power shifting, is estimated to be 53 watts/cc. For design purposes the fluxes were increased by a ratio of 53/36.5 to reflect the transient rather than the average power density, resulting in a safety factor of 1.45.

2. Axial power peaking factor. The axial power peaking factor at the outer edge of the core, averaged over the life of the plant, is estimated to be 1.3. The maximum axial peaking at any time during life is 1.7. To arrive at a design value the fluxes were normalized to the maximum value of 1.7 instead of the expected average of 1.3. This provides a safety factor of 1.3.

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The total safety margin provided by the above factors is 1.9. When the predicted flux of 1.8×10^{10} n/cm^2 is multiplied by this safety factor, the resulting design value for neutron fluxes above 0.82 Mev at the vessel wall is 3.4×10^{10} n/cm^2-sec. Calculations using 68 neutron groups on the P1 option of the P3MG1 program showed that about 12 percent of the total flux at the vessel wall above 0.82 Mev was between 0.82 Mev and 1.0 Mev. This yields a flux above 1.0 Mev of 3.0×10^{10} n/cm^2-sec. Over a 40-year life with an 80-percent use factor, the nvt on the vessel wall for neutrons greater than 1.0 Mev is 3.0×10^{19} n/cm^2. 3

Based on the above analysis it is concluded that the predicted fluxes, when multiplied by the 1.9 safety factor, provide a conservative value for the vessel nvt. This conservatism is more than sufficient to accommodate the estimated flux variations in the azimuthal direction. It is not expected that the nvt will be any greater than the design value of 3.0×10^{19}. Consequently, no increase in the NDTT above the design level is anticipated.

REFERENCES

1. McLaughlin, et al., Effect of Radiation Damage on SM-1, SM-1A and PM-2A Reactor Vessels, APAE-107, October, 1961.

2. Aalto, et al., "Measured and Predicted Variations in Fast Neutron Spectrum in Massive Shields of Water and Concrete", Nuclear Structural Engineering 2, pp. 233-242, August, 1965.

3. Clark, R. H., and Baldwin, M. N., Physics Verification Program, Part II, BAW-3647-4, June, 1967.

4. Taylor, J. J., and Obenshain, F. E., Flux from Honogeneous Cylinders Containing Uniform Source Distributions, WAPD RM-213, December, 1953. 3

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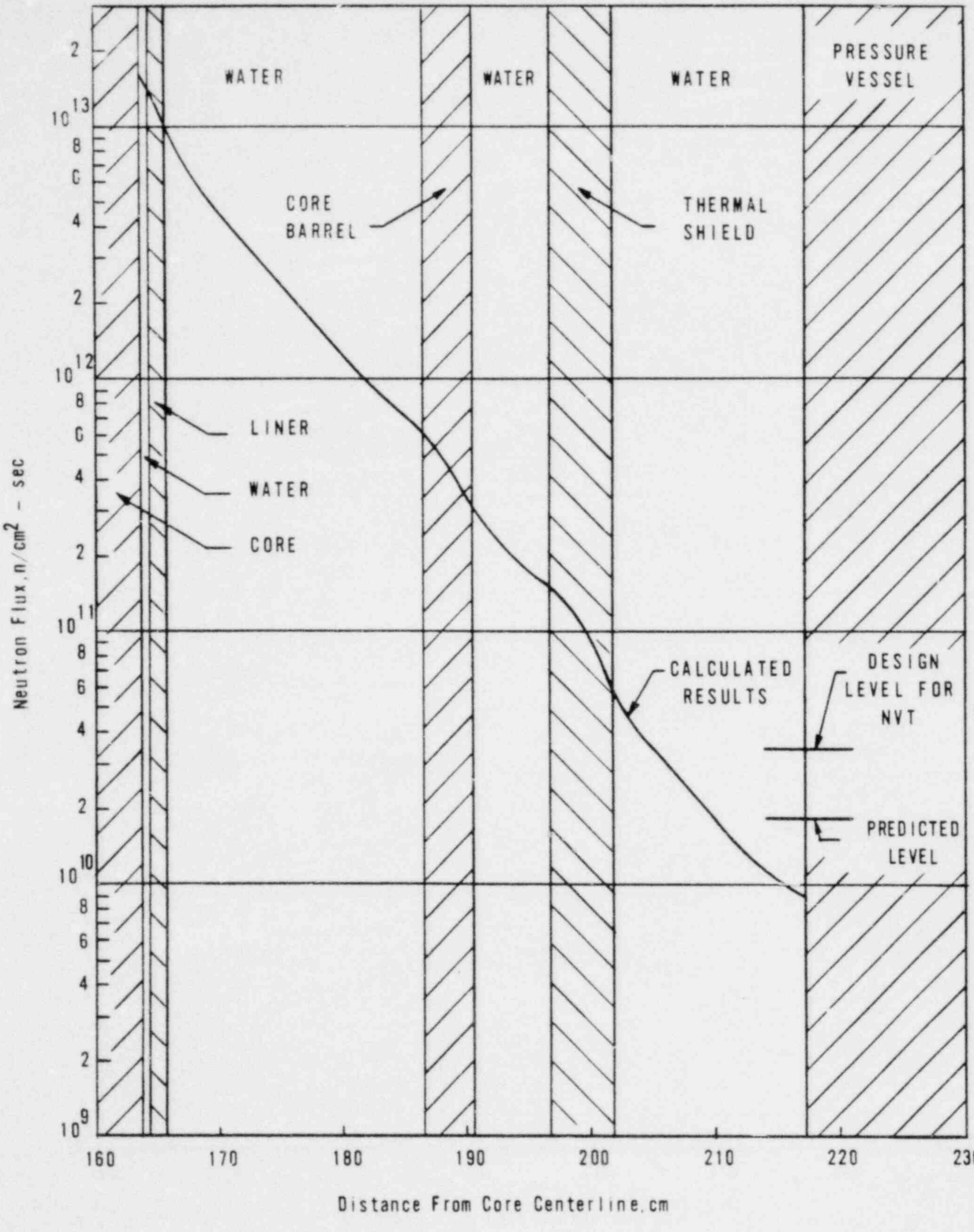

FIGURE 3A.14-1
ATTENUATION OF NEUTRON FLUX ABOVE 0.82 MEV
BETWEEN CORE EDGE AND VESSEL WALL

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QUESTION 3A.15 (DRL 3.10)

Discuss the probability for a single fuel pin to undergo DNB during the first three years of power operation at rated conditions. (Alternatively, specify the number of fuel pins that have greater than 50% probability for undergoing DNB during three years of power operation at rated conditions). Include in your discussion:

(a) The potential consequences of a single fuel pin undergoing DNB during full power operation.

(b) The time behavior of events that occur in those fuel pellets located in the vicinity of the DNB surfaces.

(c) Definition of the word jeopardy as used in the PSAR to describe the conclusions of your statistical anslyses.

ANSWER

The probability of any fuel pins undergoing a departure from nucleate boiling (DNB) during the life of the fuel is extremely small. The probability that the hottest channel in the core will experience a DNB is equal to or less than 0.2 percent at rated conditions with the W-3 correlation. This implies that if a hot channel worst condition occurs an exhaustive number of times that in 2 out of 1000 occasions such a channel would be in jeopardy of experiencing a DNB. This does not mean that in two cases DNB's will positively occur; consequently, the statement has been made that the rods are in jeopardy or exposed to damage from a possible DNB. The probability as used in the design analysis is a statement of a mathematical possibility that DNB's will occur. Each channel examined statistically has some mathematical probability of experiencing a DNB; however, the low probabilities of DNB in the reactor assures a very small real likelihood of DNB. From an engineering design viewpoint such low probabilities suggest that no failures are expected. The low probabilities of DNB in this reactor are of the same order of magnitude as those in the various PWR reactors now successfully operating with no evidence of DNB induced fuel failures.

DNB's are not very likely to occur until the probability of such an occurrence reaches a value of about 50 percent. A 50 percent probability of DNB is not reached in the hot channel until the DNB ratio reaches a value of 1.0. By comparison the DNB ratio at rated power is 1.76 based on the worst case. There is no hot channel in the core with a 50 percent probability of DNB at rated or overpower conditions.

The power peaking history for the various core regions indicates the maximum nuclear peaking will occur early in core life. At later times in life, the nuclear factors are lower. This supports the opinion that if a DNB could possibly occur, it would be early in core lifetime. At this time in life the internal pressure would be considerably less than system pressure, and a clad failure caused by a DNB would result in collapse of the clad against the fuel. In the very unlikely event a DNB occurs, it is not expected that a DNB condition would cause progressive DNB's in surrounding channels because:

(a) The most probable pin to fail under normal operation is the pin with the highest peaking factor which is usually surrounded by pins with lower nuclear peaking factors and lower coolant flow requirements (see Figure 3.2-56 preliminary safety analysis report).

(b) Any reduction in flow surrounding a pin undergoing a DNB will result in a local increase of flow to other pins in the vicinity.

(c) Any attempt at channel blockage (resulting from clad failure) would promote local turbulence which would enhance the heat transfer capability.

(d) The relatively cold control rod channels and their dispersed arrangement provide an effective heat sink.

(e) DNB's would be expected to occur when channel void fractions are above 40 percent. A corresponding reduction in moderating hydrogen atoms will produce a very large negative local power coefficient and a reduction in pin power. A concurrent rise in fuel temperature will also produce a negative power effect.

(f) PWR operating experience and DNB test data for multi-rod assemblies has not shown any tendency for such assemblies to undergo gross fuel rod failures or propagation of DNB conditions.

A DNB experienced in an annular heat transfer limit test conducted by the core designer where the inner tube was heated, resulted in a small local hole in the clad. The nature of the failure did not indicate a condition that would affect flow in surrounding channels.

The local fuel and clad time behavior following a DNB would depend on local transition and film boiling heat removal modes. The potential for significant heat transfer beyond DNB coupled with the favorable physical arrangement and inherent local power self-regulating characteristics will retard the propagation of DNB conditions.

To substantiate and amplify the statements made above the behavior of a single fuel rod or fuel assembly undergoing DNB during rated power operation will be examined by making:

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(a) A thermal-hydraulic analysis of conditions in a fuel rod cell without consideration of feedback effects on power generation.

(b) A thermal-nuclear analysis of conditions in a fuel assembly to account for gross feedback effects on power generation.

(c) A thermal-nuclear analysis of conditions in a fuel rod cell to account for feedback effects on power generation in the fuel rod.

In each analysis the following will be done:

1. DNB ratios and probabilities at rated power maximum design conditions will be calculated. 3

2. DBN will be arbitrarily imposed and fuel rod internal pressure, clad surface temperature, fuel temperature, and clad corrosion characteristics will be examined.

3. Conditions in adjacent fuel cells to determine if DNB or clad failure will propagate will be examined.

Analysis (a) will be completed in the 4th quarter of 1968, analysis (b) will be completed in the 2nd quarter of 1969, and analysis (c) will be completed in the 4th quarter of 1969.

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QUESTION 3A.16 (DRL 16.4) Discuss your analysis and design efforts for use of part length rods to control potential axial and diametral xenon oscillations. Include in this discussion a description of your latest design concept and its estimated performance characteristics.

ANSWER As stated in the answer to question 3A.12 an R&D effort is currently under way wherein a control system of part length control rods is being analyzed for the control of axial oscillations. This system will be verified by two-dimensional calculations and the interaction of these rods with other full length rods that possibly could be inserted will be ascertained in some three-dimensional analysis.

The tendency towards diametral oscillations is a function of the magnitude of the positive moderator coefficient as well as other parameters. If a design were analyzed as having little or no stability margin the first step would be to reduce the positive moderator coefficient to a point where stability could be assured in conjunction with a favorable power shape. Enough two dimensional calculations will be performed to verify or normalize the azimuthal stability margin of the design as predicted by the modal analysis. It is expected from survey work performed to date that azimuthal stability can be assured for some designs simply by the proper shaping of the power radially over the time that the moderator coefficient is of a specified positive magnitude and then the plant will be stable for the remainder of the cycle without any other devices being employed. For some other designs considered, the positive moderator coefficient will be reduced by the addition of burnable poison. Only a very nominal amount of work has been done to date to indicate the worth of full length rods used as a control mechanism for azimuthal oscillations. This device will be studied later. At this time, part length rods have not been considered as a control method for azimuthal oscillations.

As outlined in the answer to question 3A.12 the above analytical work has been scheduled and most of it is now under way.

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