# 4.3 NUCLEAR DESIGN

## 4.3.1 DESIGN BASES

→(DRN 02-1538)
The bases for the nuclear design of the fuel and reactivity control systems are discussed in the following subsections. The Cycle 1 information presented in these sections is representative of the current cycle as modified by Appendix 4.3A.
←(DRN 02-1538)

### 4.3.1.1 Excess Reactivity and Fuel Burnup

The excess reactivity provided for a cycle is based on the depletion characteristics of the fuel and burnable poison and on the desired burnup for the cycle. The desired burnup is based on the projected operating cycle for Waterford 3. The average burnup is chosen to ensure that the peak burnup is within the limits discussed in Subsection 4.2.3.2.12. This design basis, along with the design basis in Subsection 4.3.1.8, satisfies General Design Criterion 10.

### 4.3.1.2 Core Design Lifetime and Fuel Replacement Program

The core design lifetime and fuel replacement program for Cycle 3 and beyond is based on approximately eighteen month refueling with roughly 40% of the fuel assemblies replaced at each refueling outage.

### 4.3.1.3 Negative Reactivity-Feedback

In the power operating range, the net effect of the prompt inherent nuclear feedback characteristics (fuel temperature coefficient, moderator temperature coefficient, and moderator pressure coefficient) tends to compensate for a rapid increase in reactivity. The negative reactivity feedback provided by the design satisfies General Design Criterion 11.

### 4.3.1.4 Reactivity Coefficients

→(DRN 02-1538)
The values of each coefficient of reactivity are consistent with the design bases for net reactivity feedback (Subsection 4.3.1.3), and analyses that predict acceptable consequences of postulated accidents and anticipated operational occurrences, where such analyses include the response of the Reactor Protection System ((RPS).
←(DRN 02-1538)

### 4.3.1.5 Burnable Poison Requirements

The burnable poison reactivity worth provided in the design is sufficient to ensure that the moderator coefficients of reactivity are consistent with the design bases in Subsection 4.3.1.4.

### 4.3.1.6 Stability Criteria

The reactor and the instrumentation and control systems are designed to detect and suppress xenon-induced power distribution oscillations that could, if not suppressed, result in conditions that exceed the specified acceptable fuel design limits. The design of the reactor and associated systems precludes the possibility of power level oscillations. This basis satisfies General Design Criterion 12.

4.3.1.7 Maximum Controlled Reactivity Insertion Rate

The core, control element assemblies (CEAs), Reactor Regulating System, and boron charging portion of the Chemical and Volume Control System are designed so that the potential amount and rate of reactivity insertion due to normal operation and postulated reactivity accidents do not result in:

a) Violation of the specified acceptable fuel design limits

b) Damage to the reactor coolant pressure boundary

c) Disruption of the core or other reactor internals sufficient to impair the effectiveness of emergency core cooling.

This design basis, along with Subsection 4.3.1.11 satisfies General Design Criteria 25 and 28.

4.3.1.8 Power Distribution Control

→(DRN 02-1538)
The core power distribution is controlled such that, in conjunction with other core operating parameters, the power distribution does not result in violation of the limiting conditions for operation. Limiting conditions for operation and limiting safety system settings are based on the accident analyses described in Chapter 15, such that: specified acceptable fuel design limits are not exceeded as a result of anticipated operational occurrences, and the limits of acceptable consequences are not exceeded for other postulated accidents. This basis, along with Subsection 4.3.1.2, satisfies General Design Criterion 10.
←(DRN 02-1538)

4.3.1.9 Shutdown Margins and Stuck Rod Criteria

The amount of reactivity available from insertion of withdrawn CEAs under all power operating conditions, even when the highest worth CEA fails to insert, will provide for at least one percent shutdown margin after cooldown to hot zero power, and any additional shutdown reactivity requirements assumed in the safety analyses. This basis, along with Subsection 4.3.1.10, satisfies General Design Criteria 26 and 27.

4.3.1.10 Chemical Shim Control

The Chemical and Volume Control System (CVCS) (Subsection 9.3.4) is used to adjust the dissolved boron concentration in the moderator. After a reactor shutdown, this system is able to compensate for the reactivity changes associated with xenon decay and reactor coolant temperature decreases to ambient temperature, and it provides adequate shutdown margin during refueling. This system also has the capability of controlling, independently of the CEAs, long-term reactivity changes due to fuel burnup and reactivity changes during xenon transients resulting from changes in reactor load. In particular, any xenon burnout transient may be accommodated at any time in the fuel cycle. This design basis, along with Subsection 4.3.1.9 satisfies General Design Criteria 26 and 27.

4.3.1.11 Maximum CEA Speeds

Maximum CEA speeds are consistent with the maximum controlled reactivity insertion rate design basis discussed in Subsection 4.3.1.7. Maximum CEA speeds are also discussed in Section 4.2.

→(LBDCR 13-014, R309)

4.3.2 DESCRIPTION

←(LBDCR 13-014, R309)

4.3.2.1 Nuclear Design Description

→(DRN 00-644; 02-1477, R12)

This section summarizes the nuclear characteristics of the core and discusses the important design parameters that affect the performance of the core in steady-state and normal transient operation. Summaries of nuclear design parameters for Cycle 1 are presented in Table 4.3-1, and Figure 4.3-1. Design limit values for these and other parameters are discussed in the appropriate sections.

←(DRN 02-1477, R12)

The first cycle design features a 3 batch loading scheme in which the type B and C fuel assemblies contain rods of two different enrichments. In this approach, the three pins in each of the corners of every B assembly and every C assembly were replaced by pins containing a lower fuel enrichment. This unique system of enrichment zoning offers improved long-term control over the local assembly power distribution.

←(DRN 00-644)

Fuel enrichment and burnable poison distributions are shown in Figure 4.3-1. The other three quadrants of the core are symmetric to the displaced quadrant. Physical features of the lattice, fuel assemblies, and CEAs are described in Section 4.2.

→(DRN 00-644)

Assembly enrichments, core burnup, critical soluble boron concentrations and worths, plutonium buildup, and delayed neutron fractions and neutron lifetime are shown in Table 4.3-1. The soluble boron insertion rate shown in this table, as discussed in Subsection 9.3.4, are sufficient to compensate for the maximum reactivity addition due to xenon burnout and normal plant cooldown. This maximum reactivity addition rate for which the CVCS will be required to compensate is given in Table 4.3-1. The maximum value occurs for an end-of-cycle cooldown, where the moderator temperature coefficient is most negative.

←(DRN 00-644)

4.3.2.2 Power Distribution

4.3.2.2.1 General

→(DRN 04-1096, R14)

At all times during operation, it is intended that the power distribution and coolant conditions be controlled so that the peak fuel centerline temperature and the minimum departure from nucleate boiling ratio (DNBR) are maintained within operating limits supported by the safety analyses (Chapter 15) with due regard for the correlation between measured quantities, the power distribution, and uncertainties in the determination of power distribution.

←(DRN 04-1096, R14)

→(DRN 01-1103; 02-1477)
Methods of controlling the power distribution include the use of CEAs to alter the axial power distribution, decreasing CEA insertion by boration, thereby improving the radial power distribution, and correcting off-optimum conditions which cause margin degradations (e.g., CEA misoperation).
←(DRN 01-1103; 02-1477)
As an operator aide, the Core Operating Limit Supervisory System (COLSS) will indicate to the operator how far the core is from the operating limits and give an audible alarm should an operating limit be exceeded. Such a condition signifies a reduction in the capability of the plant to withstand an anticipated transient, but does not necessarily imply a violation of fuel design limits. If the margin to fuel design limits continues to decrease, the RPS assures that the specified acceptable fuel design conditions are not exceeded by initiating a trip.

The COLSS, described in Section 7.7 and Reference 1, continually generates an assessment of the margin to linear heat rate and DNBR operating limits. The data required for these assessments include measured in-core neutron flux data, CEA positions, and coolant inlet temperature, pressure, and flow. In the event of an alarm indicating that an operating limit has been exceeded, power must be reduced unless the alarm can be cleared by improving either the power distribution or another process parameter. The adequacy of the COLSS calculations is verified periodically.

In addition to the monitoring performed by COLSS, the RPS continually infers the core power distribution and thermal margin by processing reactor coolant data, signals from ex-core neutron flux detectors, each containing three axially stacked elements, and input from redundant reed switch assemblies to indicate CEA position. In the event the power distributions or other parameters are perturbed as the result of an anticipated operational occurrence that would violate fuel design limits, the high local power density or low DNBR trips in the RPS will initiate a reactor trip. The relationship between the design power distributions and the monitoring instrumentation is discussed in detail in Reference 1. The dependence of the excore detector readings on the power distributions is also detailed in Subsection 4.3.3.1.1.

4.3.2.2.2 Nuclear Design Limits on the Power Distribution

The design limits on the power distribution stated here were employed during the design process both as design input and as initial conditions for accident analyses described in Chapters 6 and 15. However, for the monitoring system, it is the final operating limit determination that is used to assure that the consequences of an anticipated operational occurrence or postulated accident will not be any more severe than the consequences shown in Chapter 15. The initial conditions used in this operating limit determination are actually stated in terms of PLHGR and required power margin for minimum DNBR.

The design limits on power distribution are as follows:

a) The limiting three-dimensional heat flux peaking factor, $F_q^n$ was established for full power conditions at 2.28 and 2.35, for, first and equilibrium cycles, respectively. The lower value for the first cycle reflects the presence of burnable poison shims in the fuel lattice and a corresponding reduction in the number of fuel rods. $F_q^n$ is defined in Subsection 4.4.2.2.2.1, listing C and is termed the nuclear heat flux factor or the total nuclear peaking factor.

An $F_q^n$ of 2.28 in combination with uncertainties and allowances on heat flux which give the initial peak linear heat rate assumed in the safety analyses constituted one limiting combination of parameters for full power operation in the first cycle. Other combinations of parameters which will result in acceptable consequences of the safety analysis do exist; e.g., a higher $F^n$ is acceptable at a reduced power level. Implementation in the technical specification is via an operating limit on the monitored peak linear heat generation rate.

→(EC-13881, R304)

b) The margin to the minimum DNBR (using the ABB-NV and WSSV-T DNBR correlations as discussed in Subsection 4.4.2.2 and 4.4.4.1), which is available to accommodate anticipated operational occurrences, will be as acceptable as the margin calculated with the following combination of:

←(EC-13881, R304)

1) The coolant conditions

2) The axial power distribution

3) The axially integrated radial peaking factor, $F_r^n$, is the rod radial nuclear factor or the rod radial peaking factor and is defined in Subsection 4.4.2.2.2.1, listing A.

An $F_r^n$ of 1.55, the set of axial shapes displaced in Figure 4.4-3, and the coolant conditions assumed in the safety analyses constitute one limiting combination of parameters for full power operation. Other combinations giving acceptable accident analysis consequences are equally acceptable. Implementation of these limits in the technical specification is via an operating limit on allowed minimum monitored DNBR underflow vs. measured incore axial shape index. This operating limit is based on consideration of many different allowed operating conditions (axial and radial power distributions as well as coolant conditions) at any axial shape index.

It will be shown in the following subsections that operation within these design limits is achievable.

4.3.2.2.3 Expected Power Distributions

→(DRN 01-1103; 02-1477)

Figures 4.3-2 through 4.3-13 and 4.3-14 through 4.3-18 show typical first cycle planar radial and unrodded core average axial power distributions, respectively. They illustrate conditions expected at full power for various times in the fuel cycle as specified on the figures. It is expected that the normal operation of the reactor will be with limited CEA insertion so that these power distributions represent the expected power distribution during most of the cycle. The three-dimensional peaking factor, $F_q$ expected during steady-state operation is then just the product of the unrodded planar radial peaking factor $(F_r)$ and the axial peaking factor. The maximum expected value of $F^n$ is 1.85 during the first cycle and, as can be seen from the above figures, occurs near the beginning-of-cycle for steady-state, base loaded operation with no CEA insertion. The uncertainty associated with these calculated power distributions is discussed in Subsection 4.3.3.1.2.2.6.

The capability of the core to follow load transients without exceeding power distribution limitations depends on the margin to operating limits compared to the margin required for base loaded, unrodded operation. In order to illustrate the maneuvering capability available in Waterford 3, the results of calculations of the power distributions and power peaking factors during distributions are calculated by QUIX (see Subsection 4.3.3.1.1), a one-dimensional spatial flux calculational model that considers the effects of the time and spatial variations of xenon and iodine concentration, CEA position, thermal and moderator density feedback mechanisms, as well as the effect of the burnup distribution near end-of-cycle. Since QUIX does not have fuel depletion capability, axial-dependent depletion effects are included in end-of-cycle calculation by using and end-of-cycle axial nuclide distribution computed from one-dimensional (axial) PDQ depletion calculations. Estimates of $F_q$ and $F^n$ are obtained by synthesis of the three-dimensional power QUIX calculations. The QUIX model accepts values of radial peaking factors for each type of CEA bank insertion (unrodded, bank six inserted, bank five inserted, etc.). These radial peaking factors are input for the appropriate core average burnup condition and are applied over that axial region of the core having the specified CEA bank configuration (e.g., unrodded, bank six inserted, etc.). These radial peaking factors are weighted by the axial power distribution to obtain an axially integrated radial peaking factor. The value of this integrated radial peaking factor of each planar region is not, in general, expected to occur at the same fuel pin location. The magnitude of the input radial peaking factors is determined primarily by the number and location of the inserted CEAs; it is evaluated at the full power conditions and taken to be independent of power level.

←(DRN 01-1103; 02-1477)

→(DRN 01-1103; 02-1477)
Figures 4.3-25 through 4.3-32 show the calculated axial power distributions and associated nuclear peaking factors during a typical day of a maneuvering transient to either 50 or 35 percent of the full power conditions. Also shown on these figures are the CEA locations during the transient.
←(DRN 01-1103; 02-1477)
The detailed axial power distribution within any assembly is a function of the location of that assembly within the core as well as the time in life, CEA insertion, etc. For DNB analysis, a conservatively flat local radial distribution is used to evaluate intra-assembly mixing and crossflow. The conservative distribution is shown on Figure 4.3-33; the power in the hottest rod in that assembly is assumed to be at the design limit peaking factor. Care is taken in the fuel management to ensure in effect that no flatter distribution occurs in assemblies that are limiting or near limiting with respect to DNB. In Subsection 4.3.3.1.2, the accuracy of calculations of the power distribution within a fuel assembly is discussed.

4.3.2.2.4 Allowances and Uncertainties on Power Distributions

In comparing the expected power distributions and implied peak linear heat generation rate (PLHGR) produced by analysis with the design limits stated in Subsection 4.3.2.2.2 consideration must be given to the uncertainty and allowances associated with on-line monitoring by COLSS.

Reference 1, a CE Topical Report on COLSS, contains the conclusion based on detailed numerical evaluations for cores similar to Waterford 3 that a penalty factor of 7.5 percent should be applied to COLSS determinations of $F^n$. The uncertainty analysis provided for Waterford 3 is described in Subsection 7.7.1.5. In addition, a power level uncertainty factor of 1.02, an engineering factor of 1.03, and an augmentation factor to account for power spiking associated with fuel densification are customarily included. The latter factor varies axially, but can be expected to have a value on the order of 1.03 at the elevation of the axial peak. A multiplicative combination of these factors leas to an overall penalty of 16.3 percent on monitored PLHGR. Similarly, it has been demonstrated in Reference 1 than an uncertainty of 4.6 percent is associated with the thermal margin calculation performed by COLSS.

4.3.2.2.5 Comparisons Between Limiting and Expected Power Distributions
→(DRN 02-1477)
As was discussed in Subsection 4.3.2.2.3, Expected Power Distributions, the maximum expected unrodded $F^n$ that occurs during the first cycle at full power is 1.85. Augmenting this value by the uncertainties and allowances discussed above provides an upper limit on $F_q^n$ of 2.16 which is well below the design target of 2.28.
←(DRN 02-1477)

→(DRN 02-1477)
←(DRN 02-1477)

Similarly, even allowing for the 4.6 percent uncertainty on the monitoring of thermal margin the maximum expected unrodded $F^n$ that occurs at full power is well below the design limit of 1.55 stated in Subsection 4.3.2.2. Again, as demonstrated by the calculations of the power distributions expected to occur during maneuvering transients, no appreciable loss in thermal margin is expected to occur during these transients.

4.3.2.3 Reactivity Coefficients

Reactivity coefficients relate changes in core reactivity to variations in fuel or moderator conditions. The data presented in this section and associated tables and figures illustrate the range of reactivity coefficient values calculated for a variety of operating accident conditions. Subsection 4.3.3 presents comparisons of calculated and measured moderator temperature coefficients and power coefficients for operating reactors. The good agreement shown in that subsection provides confidence that the data presented in this section adequately characterize the Waterford 3 reactor. Table 4.3-3 presents a comparison of the reactivity coefficients calculated for the Waterford 3 reactor with those used in the safety analyses described in Chapter 15. For each accident analysis, suitably conservative reactivity coefficient values are used. Since uncertainties in the coefficient values, as discussed in Subsection 4.3.3.1.2, and other conservatisms are taken into account in the safety analyses, values used in the safety analyses may fall outside the ranges in a conservative direction of the data presented in this section. A more extensive list of reactivity coefficients is given in Table 4.3-4.

The calculational methods used to compute reactivity coefficients are discussed in Subsection 4.3.3.1.1. All data discussed in subsequent subsections are calculated with two-dimensional, quarter-core nuclear models. Spatial distributions of materials and flux weighting are explicitly performed for the particular conditions at which the reactivity coefficients are calculated. The adequacy of this method is discussed in Subsection 4.3.3.1.2.

4.3.2.3.1 Fuel Temperature Coefficient

The fuel temperature coefficient (FTC) is the change in reactivity per unit change in fuel temperature. A change in fuel temperature affects not only the thermal expansion of the fuel pellet but, in addition, the reaction rates in both the thermal and epithermal neutron energy regimes. Epithermally, the principal contributor to the change in reaction rate with fuel temperature is the Doppler effect arising from the increase in absorption widths of the resonances with an increase in fuel temperature. The ensuing increase in absorption rate with fuel temperature causes a negative FTC. In the thermal energy regime, a change in reaction rate with fuel temperature arises from the effect of temperature dependent scattering properties of the fuel matrix on the thermal neutron spectrum. In typical

PWR fuels containing strong resonance absorbers such as U-238 and Pu-240, the magnitude of the component of the FTC arising from the Doppler effect is more than a factor of 10 larger than the magnitude of the thermal energy component.

Figure 4.3-34 shows the dependence of the calculated FTC on the fuel temperature, both at the beginning and the end of the first cycle.

4.3.2.3.2 Moderator Temperature Coefficient

The moderator temperature coefficient (MTC) relates changes in reactivity to uniform changes in moderator temperature, including the effects of moderator density changes with changes in moderator temperature. Typically, an increase in the moderator temperature causes a decrease in the core moderator density and, therefore, less thermalization, which reduces the core reactivity. However, when soluble boron is present in the moderator, a reduction in moderator density causes a reduction in the content of soluble boron in the core, thus producing a positive contribution to the MTC. In order to limit the dissolved boron concentration, burnable poison rods (shims) are provided in the form of cylindrical pellets of alumina with uniformly dispersed boron carbide particles. The number of shims is given in Table 4.3-1 and their distribution in one quadrant of the core is shown in Figure 4.3-1. The distribution is identical for the other three quadrants. The reactivity control provided by the shims is given in Table 4.3-1. This control makes possible a reduction in the dissolved boron concentration to the values given in Table 4.3-1.

→(DRN 00-644; 06-895, R15)
The calculated MTC for various core conditions at beginning and end of first cycle are given in Table 4.3-4. The MTC are more negative at end-of-cycle (EOC) because the soluble boron n the coolant is reduced. The build-up of equilibrium xenon produces a net negative change of $-0.45 \times 10^{-4} \Delta\rho/°F$ in the MTC; this change is due mainly to the concomitant reduction in critical soluble boron. The changing fuel isotopic concentrations and the changing neutron spectrum during the fuel cycle depletion also contribute a small negative component to the MTCl.
←(DRN 00-644; 06-895, R15)

The dependence of the MTC on moderator temperature at beginning-of-cycle (BOC) and EOC (at constant soluble boron) is shown in Figures 4.3-35 and 4.3-36, respectively. These figures also show the expected MTC at reduced power levels (corresponding to reduced moderator temperatures) based on power reductions accomplished with soluble boron only and with CEAs only. These two modes of power reduction result in the most positive and most negative MTC expected to occur at reduced power levels. These figures show the expected MTC for the full range of expected operating conditions and accident conditions addressed in Chapter 15.

#### 4.3.2.3.3 Moderator Density Coefficient

The moderator density coefficient is the change in reactivity per unit change in the average core moderator density at constant moderator temperature. A positive moderator density coefficient translates into a negative contribution to the total moderator temperature coefficient, which is defined in Subsection 4.3.2.3.2. The density coefficient is always positive in the operating range, although the magnitude decreases as the soluble boron level in the core is increased. The calculated density coefficient is shown in Table 4.3.-4, and curves of density coefficient as a function of density for several soluble boron concentrations are presented in Figure 4.3-37. These curves are based upon two-dimensional PDQ calculations and have been generated over a wide range of core conditions including the range of both normal operating conditions and accident conditions. The density coefficients explicitly used in the accident analyses are based upon core conditions with the most limiting temperature coefficients allowed by the technical specification. Table 4.3-3 shows comparison of the expected values of the moderator temperature coefficients with those actually used in the accident analyses.

#### 4.3.2.3.4 Moderator Nuclear Temperature Coefficient

The moderator nuclear temperature coefficient is the change in reactivity per unit change in core average moderator nuclear temperature, at constant moderator density. The source of this reactivity dependence is the spectral effects associated with the change in thermal scattering properties of water molecules as the internal energy, which is represented by the bulk water temperature, is changed. The magnitude of the moderator nuclear temperature coefficient is equal to the difference between the MTC, defined in Subsection 4.3.2.3.2, and the moderator density coefficient, defined in Subsection 4.3.2.3.3.

#### 4.3.2.3.5 Moderator Pressure Coefficient

The moderator pressure coefficient is the change in reactivity per unit change in reactor coolant system pressure. Since an increase in pressure increases the water density, the pressure coefficient is merely the density coefficient expressed in a different form. The calculated pressure coefficient at full power is shown in Table 4.3-4.

#### 4.3.2.3.6 Moderator Void Coefficient

The anticipated occurrence of small amounts of local subcooled boiling in the reactor during full power operation results in a predicted core average steam (void) volume fraction of substantially less than one percent. Changes in the moderator void fraction produce reactivity changes that are quantified by the void coefficient of reactivity. An increase in voids decreases core reactivity, but the presence of soluble boron tends to add a positive contribution to the coefficient.

The calculated values of moderator void coefficient are shown in Table 4.3-4. Curves showing void coefficient vs. void content can be inferred directly from the density coefficient curves provided in Figure 4.3-37.

4.3.2.3.7 Power Coefficient

The power coefficient is the change in reactivity per unit change in core power level. All previously described coefficients contribute to the power coefficient, but only the MTC and the FTC contributions are significant. The contributions of the pressure and void coefficients are negligible, because the magnitudes of these coefficients and the changes in pressure and void fraction per unit change in power level are small. The contribution of moderator density change is included in the MTC contribution.

In order to determine the change in reactivity with power, it is necessary to know the changes in the average moderator and effective fuel temperatures with power. The average moderator (coolant) temperature is controlled to be a linear function of power.

The core average linear heat rate is also linear with power. The average effective fuel temperature dependence on the core average linear heat rate is calculated from the following seem-empirical relation:

$$T_f = T_m + 128.36\,\alpha \underline{P}^2 \qquad (1)$$

$T_m$ is the average moderator temperature (°F), $\alpha$ is a weighting factor accounting for the number of spatial dimensions used in the calculations, $\underline{P}$ is the average thermal power generated per unit fuel rod length (kW/ft), and $T_f$ is the average effective fuel temperature (°F). The basis for this relation is discussed in Subsection 4.3.3.1.2.2.4.

The total power coefficient at a given core power can be determined by evaluating, for the conditions associated with the given power level, the following expression:

$$\frac{dp}{dP} = \frac{\partial p}{\partial T_f} \cdot \frac{\partial T_f}{\partial P_m} + \frac{\partial p}{\partial T_m} \cdot \frac{\partial T_m}{\partial P} \qquad (2)$$

The first term of equation (2) provides the fuel temperature contribution to the power coefficient, which is shown as a function of power in Figure 4.3-38.

The first factor of the first term is the fuel temperature coefficient of reactivity discussed in Subsection 4.3.2.3.1 and shown in Figure 4.3-34. The second factor of the first term is obtained by calculating the derivative of equation (1).

$$\frac{\partial T_f}{\partial P} = 128.36\alpha - 3.5664\alpha P \qquad (3)$$

The second term in equation (2) provides the moderator contribution to the power coefficient. The first factor, the MTC, is discussed in Subsection 4.3.2.3.2 and shown in Figures 4.3-35 and 4.3-36. The second factor is a constant since the moderator temperature is controlled to be a linear function of power.

→(DRN 00-644)
Since the factors $\partial p / \partial T_f$ and $\partial p / \partial T_m$ are functions of one or more independent variables; e.g., burnup, temperature, soluble boron content, xenon worth, and CEA insertion, the total power coefficient, $d\rho / dP$ also depends on these variables.
←(DRN 00-644)
The power coefficient tends to become more negative with burnup because the FTC and MTC become more negative (see Figures 4.3-34 through 4.3-36). The insertion of CEAs, while maintaining constant power, results in a more negative power coefficient, because the soluble boron level is reduced and because of the spectral effects of the CEAs.

The full power values of the overall power coefficient for the unrodded core at BOC and EOC are shown in Table 4.3-4.

4.3.2.4 Control Requirements

There are three basic types of control requirements that influence the design of this reactor;

a) Reactivity control so that the reactor can be operated in the unrodded, critical, full power mode for the design cycle length.

b) Power level and power distribution control so that the reactor power may be safely varied from full-rated power to cold shutdown, and so that the power distribution at any given power level is controlled within acceptable limits.

c) Shutdown reactivity control sufficient to mitigate the effects of postulated accidents.

→(LBDCR 13-014, R309)
Reactivity control is provided by several different means. The amount and enrichment of the fuel and burnable poison shims are design variables that determine the beginning-of-cycle and end-of-cycle reactivity for an unrodded, unborated condition. Soluble boron and CEA poisons are flexible means of controlling long-term and short-term reactivity changes, respectively.
←(LBDCR 13-014, R309)
The following paragraphs discuss the reactivity balances associated with each type of control requirement.

4.3.2.4.1 Reactivity Control at BOC and EOC

The reactivities of the unrodded core at BOC and EOC with no soluble boron are shown in Table 4.3.2. This table includes the reactivity worth of equilibrium xenon and samarium, and shows the reactivity available to compensate for burnup and fission product poisoning. Soluble boron concentrations required for criticality at various core conditions are shown in Table 4.3-1. Soluble boron is used to compensate for slow reactivity changes such as those due to burnup, changes in xenon content, etc. The reactivity controlled by burnable poison shims is also given in Table 4.3-1. At EOC, the reactivity worth of the residual poison is less than one percent, and the soluble boron concentration is near zero. The reactor is to be operated in essentially an unrodded condition at power. The CEA insertion at power is limited by the power dependent insertion limit (PDIL) for short term reactivity changes.

4.3.2.4.2 Power Level and Power Distribution Control

→(DRN 00-644; 01-1103, R12)

The regulating CEA groups may be used to compensate for changes in reactivity associated with routine power level changes. In addition, CEAs may be used to compensate for minor variations in moderator temperature and boron concentrations during operation at power. The reactivity worth of regulating CEA control groups is shown in Table 4;.3-5. Soluble boron is used to maintain shutdown reactivity at cold zero power conditions.

←(DRN 00-644; 01-1103, R12)

4.3.2.4.3 Shutdown Reactivity Control

The reactivity worth requirements of the full complement of CEAs is primarily determined by the power defect, and the shutdown margin and the stuck rod criteria discussed in Subsection 4.3.1.9. Table 4.3-6 shows the reactivity component allowances that define the total reactivity allowance. These data are based on the EOC conditions when the fuel and moderator temperature coefficients are the most negative and thus when the shutdown reactivity requirement is a maximum. Each allowance component is further discussed below. No CEA allowance is provided for xenon reactivity effects; e.g., undershoot, since these effects are controlled with soluble boron rather than with CEAs.

→(LBDCR 13-014, R309)

The worth of all CEAs except the most reactive, which is assumed stuck in the fully withdrawn position, provides more shutdown capability than required by the total reactivity allowance shown in Table 4.3-6. This excess shutdown margin is shown in Table 4.3-7 for BOC and EOC. The margin is more than sufficient to compensate for calculational uncertainties in the nominal design allowances and in the CEA reactivity worth. Thus, the shutdown reactivity control provided in this design is sufficient at all times in the cycle.

←(LBDCR 13-014, R309)

4.3.2.4.3.1 Fuel Temperature Variation

The increase in reactivity that occurs when the fuel temperature decreases from the full power value to the zero power value is due primarily to the Doppler effect in U-238. The CEA reactivity allowance for fuel temperature variation shown in Table 4.3-6 is a conservative allowance for the EOC conditions. Measurements of first cycle power coefficients at Fort Calhoun, Calvert Cliffs, and Millstone-2 lead to a power defected of 1.2 percent $\Delta\rho$ (Subsection 4.3.3.1.2.2.4). The slight increase in power defect with exposure due to the presence of plutonium isotopes is offset by the reduction in the fuel temperature resulting from fuel swelling and clad creep-down. Therefore, the power defect does not change significantly with increasing cycle number and the value of 1.4 percent $\Delta\rho$ in Table 4.3-6 is an adequately conservative variation allowance.

4.3.2.4.3.2 Moderator Temperature Variation

The moderator temperature variation allowance is large enough to compensate for any reactivity increase that may occur when the moderator temperature decreases from the full power value to the zero power (hot standby) value. This reactivity increase, which is primarily due to the negative MTC, is largest at the EOC when the soluble boron concentration is near zero and the moderator coefficient is strongly negative. At BOC when the MTC is less negative, the reactivity change is smaller.

The CEA reactivity allowance for moderator temperature variation given in Table 4.3-6 is actually the sum of three allowances. The first, and most important, is the allowance for the MTC effect. The second is an allowance for the reduction in CEA worth resulting from the shorter neutron diffusion length at the zero power moderator density relative to the full power moderator density. This allowance is necessary because the CEA worths shown in Table 4.3-5 were calculated at full power. The third allowance is intended to cover the reactivity effects associated with the greatest expected axial flux redistribution resulting from the difference in moderator temperature profile between full and zero power, and the asymmetric axial burnup distribution at EOC.

#### 4.3.2.4.3.3 Moderator Voids

Reducing the power level from full power to zero power causes an increase in reactivity resulting from the collapsing of steam bubbles caused by local boiling at full power. The amount of void in the core is small and is estimated to be substantially less than one percent at full power. As with the moderator temperature effect, the maximum increase in reactivity from full to zero power occurs at EOC when the least amount of dissolved boron is present. The reactivity effect is small, and the allowance for this effect is shown Table 4.3-6.

#### 4.3.2.4.3.4 Control Element Assembly Bite

→(DRN 01-1103; 02-1477)

The CEA bite is the amount of reactivity worth in CEAs that can be inserted in the core at full power to initiate ramp changes in reactivity associated with load changes, and to compensate for minor variations in moderator temperature, boron concentration, xenon concentration, and power level. The reactivity allowance for this effect is shown in Table 4.3-6.

←(DRN 01-1103; 02-1477)

→(DRN 01-1103)

←(DRN 01-1103)

4.3.2.4.3.6 Shutdown Margin and Accident Analysis Allowance

The allowance shown in Table 4.3-6 for shutdown margin is consistent with that assumed under various postulated accident conditions addressed in Chapter 15, which result in predicted acceptable consequences.

4.3.2.4.3.7 Available Reactivity Worth

Table 4.3-7 shows the reactivity worths of the full complement of CEAs, and the highest reactivity worth of a single CEA in the fully withdrawn position, at BOC and EOC. This table also compares the available net shutdown worth (including the effects of the stuck CEA) to the reactivity worth requirements from Table 4.3-6.

As discussed in Subsection 4.3.3, the uncertainty in total CEA reactivity worth is five percent and the uncertainty in the stuck CEA worth is less than 10 percent. Even allowing for the maximum calculated errors for total CEA worth in both of the adverse directions, sufficient shutdown margin is available.

4.3.2.5 Control Element Assembly Patterns and Reactivity Worths

→ (DRN 00-644)

The locations of all CEAs in one of four symmetrically rodded core quadrants are shown in Figure 4.3-39. The CEAs designated as regulating control rods are divided into six groups; the shutdown CEAs are divided into two groups. These groups are identified, for first cycle operation, in Figure 4.3-39. All CEAs in a group are withdrawn or inserted quasisimultaneously. Shutdown groups are inserted after the regulating groups are inserted and are withdrawn before the regulating groups are withdrawn. The reactivity worths of sequentially inserted CEA groups at beginning of life and end of first cycle are shown in Table 4.3-5, and the rod radial peaking factors ($F_r^n$) for these configurations are shown in Table 4.3-8.

It is expected that the core will be essentially unrodded during full power steady-state operation, except for limited insertion of the first regulating group in order to compensate for minor variations in moderator temperature and boron concentration as described in Subsection 4.3.2.5.4. For operation with substantial CEA insertion, the relationship between power level and the maximum permitted CEA insertion is typified in Figure 4.3-40. This figure also illustrates the regulating group insertion order (6-5-43-2) and the 40 percent fixed overlap between successive regulating groups. Compliance with the power dependent insertion limits throughout the cycle ensures that adequate shutdown margin is maintained and that the core conditions are no more severe than the initial conditions assumed in the accident analyses described in Chapter 15.

Reactivity insertion rates for the safety analysis of the Waterford 3 core are presented in Chapter 15. The full power CEA ejection accident (Subsection 15.4.3.2) considers the ejection of one CEA from a fully inserted lead bank. The ejected CEA worth is calculated

← (DRN 00-644)

→(DRN 01-1103; 02-1477)
by the difference between the preejection and the post ejection reactivity of the core computed static methods. The maximum ejected CEA worth used in the safety analysis is conservative since (1) the lead regulating bank is not expected to be fully inserted at full power, and (2) a 10 percent conservatism is applied to the ejected CEA worth. A similar analysis is performed for the CEA ejection analysis from zero power, except that the initial condition for this incident assumes that all regulating banks are fully inserted.
←(DRN 01-1103; 02-1477)
The CEA withdrawal incident from low power (Subsection 15.4.1.2) is analyzed with the maximum calculated differential reactivity insertion rate resulting from a sequential CEA bank withdrawal with 40 percent overlap, including a 10 percent reactivity uncertainty. As discussed in Subsection 15.4.1.3., the CEA withdrawal incident from full power is analyzed from the insertion of the lead bank which maximizes the reactivity insertion and the power shape change during the CEA withdrawal. Reactivity insertion rates are calculated by a static axial model of the Waterford 3 core. The calculated reactivity insertion rate resulting from the sequential CEA withdrawal is presented in Figures 4.3-41 and 4.3-42.
→(DRN 02-1477)
The CEA drop incident (Subsection 15.4.1.4) is analyzing by selecting the dropped CEA that maximizes the increase in the radial peaking factor. A conservatively small negative reactivity insertion is used in the accident analysis.
←(DRN 02-1477)
The typical reactivity insertion during a reactor SCRAM is presented in Section 15.0. This reactivity insertion is computed by static axial models at various scram CEA positions, and it is used for all accidents which are terminated by a scram, unless otherwise indicated. The reactivity insertion is conservative since (1) a flat end-of-cycle power shape is assumed for the calculations, (2) only the minimum shutdown worth of 8.85 percent $\Delta\rho$ is assumed to available, and (3) the influence of delayed neutrons on the transient power shape is neglected. The scram reactivity for other incidents such as the loss of flow are performed by a parametric analysis considering various initial power shapes and the scram reactivity associated with those power shapes.

### 4.3.2.6 Criticality of Reactor During Refueling

The soluble boron concentrations during refueling are shown in Table 4.3-1. These concentrations ensure that the $k_{eff}$ of the core during refueling does not exceed 0.95.

4.3.2.7 Stability

4.3.2.7.1 General

Pressurized water reactors (PWRs) with negative overall power coefficients are inherently stable with respect to power oscillations. Therefore, this discussion will be limited to xenon induced power distribution oscillations. Xenon induced oscillations occur as a result of rapid perturbations to the power distribution which cause the xenon and iodine distributions to be out of phase with the perturbed power distribution. This results in a shift in the iodine and xenon distribution that causes the power distribution to change in an opposite direction from the initial perturbation and thus an oscillating condition is established. The magnitude of the power distribution oscillation can either increase or decrease with time. Thus, the core can be considered to be either unstable or stable with respect to these oscillations. Discussed below are the methods of analyzing the stability of the core with respect to xenon oscillations. The tendency of certain types of oscillations to increase or to de crease is calculated, and the method of controlling unstable oscillations is presented.

4.3.2.7.2 Method of Analysis

Xenon oscillations may be analyzed by two methods. The first method consists of an explicit analysis of the spatial flux solution accounting for the space-time solution of the xenon concentrations. Such a method is useful for testing various control strategies and evaluating transitional effects (such as power maneuvers). The second method consists of modal perturbation theory analysis, which is useful for the evaluation of the sensitivity of the stability to changes in the reactor design characteristics, and for the determination of the degree of stability for a particular oscillatory mode.

The stability for a reactor can be characterized by a stability index or a damping factor which is defined as the natural exponent which describes the growing or decaying amplitude of the oscillation. A xenon oscillation may be described by the following equation:

→(DRN 06-914, R15)

$$\phi(\bar{r},t) = \phi_o(\bar{r}) + \Delta\phi_o(\bar{r})\, e^{bt}\, sin(wt + \delta)$$

←(DRN 06-914, R15)

where

$\phi(\bar{r},t)$ is the space-time solution of the neutron flux

$\phi_o(\bar{r})$ is the initial fundamental flux

$\Delta\phi_o(\bar{r})$ is the perturbed flux mode

b is the stability index

$\omega$ is the frequency of the oscillation

$\delta$ is a phase shift

Modal analysis consist of an explicit solution of the stability index b using known fundamental and perturbed flux distributions. A positive stability index b indicates an unstable core, and a negative value indicates stability for the oscillatory mode being investigated. The stability index is generally expressed in units of inverse hours, so that a value of -0.01/hr would mean that the amplitude of each subsequent oscillation cycle decreases by about 25 percent (for a period of about 30 hours for each cycle).

Xenon oscillation modes in PWRs can be classified into three general types: radial, azimuthal, an axial. To analyze the stability for each oscillation mode, only the first overtone needs to be considered since higher harmonic modes decay more rapidly than the first overtone Furthermore, since the first overtone of a radial oscillation decays more rapidly than the first overtone of an azimuthal oscillation, only the latter of these two modes will be considered in detail.

4.3.2.7.3 Expected Stability Indices

4.3.2.7.3.1 Radial Stability

A radial xenon oscillation consists of a power shift inward and outward from the center of the core to the periphery. This oscillatory mode is generally more stable than an azimuthal mode. This effect is illustrated in Figure 4.3-43, which shows that for a bare cylinder the radial mode is more stable than the azimuthal mode. Discussion of the stability for radial oscillator mode is therefore deferred to the azimuthal mode.

4.3.2.7.3.2 Azimuthal Stability

An azimuthal oscillation consists of an X-Y power shift from one side of the reactor to the other. Modal analysis for this type of oscillation is performed for a range of expected reactor operating conditions.

The expected variation of the stability index during the first cycle is shown in Figure 4.3-44. These results are obtained from analyses which consider the spatial flux shape changes during the cycle, the changes in the moderator and Doppler coefficient during the cycle, and the change in xenon and iodine fission yield due to plutonium buildup during the cycle. As is shown on the figure, the expected stability index is no greater than -0.035/hr at any time during the cycle for the expected mode of reactor operation. Comparison of predicted stability index with those actually measured on operating cores, as discussed in Subsection 4.3.3.2.3, provide a high confidence level in the prediction of azimuthal stability. Measurements of xenon spatial stability in large cores have been made[2] which provide confidence in the methods that are used to predict the azimuthal stability of this core.

4.3.2.7.3.3. Axial Stability

→(DRN 02-1477)

Axial xenon oscillations consist of a power shift toward the top and bottom of the reactor core. This type of oscillation may be unstable during the first cycle. Table 4.3-9 shows the calculated variation of the axial stability index during the first cycle. It is anticipated that control action with CEAs may be required to limit the magnitude of the oscillation. As discussed in Subsection 4.3.2.2, the axial power

←(DRN 02-1477)

→(DRN 01-1103; 02-1477)
distribution is monitored by COLSS and the RPS. Based on the COLSS measurement of the axial power distribution, the operator may move CEAs so as to control any axial oscillations.

4.3.2.7.4 Control of Axial Instabilities

The control of axial oscillations during a power maneuver is illustrated in Figures 4.3-25 through 4.3-32. CEAs are used throughout these maneuvers to limit the change in the power distribution. The difference between an uncontrolled and a controlled xenon oscillation is illustrated in Figure 4.3-45. It was assumed in the calculation of the controlled oscillation that the CEAs were moved in such a way as to preserve the initial shape in the core prior to the initiating perturbation. The calculations are performed at the end of the first cycle which corresponds to the expected least stable condition for axial xenon oscillations.
←(DRN 01-1103; 02-1477)

4.3.2.7.5 Summary of Special Features Required by Xenon Instability

The RPS described in Subsection 7.2.2 is designed to prevent exceeding acceptable fuel design limits and to limit the consequences of postulated accidents. In addition, a means is provided to assure that under all allowed operating modes, the state of the reactor is confined to conditions not more severe than the initial conditions assumed in the design and analysis of the RPS.

Since the reactor is predicted to be stable with respect to azimuthal xenon oscillations, no special protective system features are needed to accommodate azimuthal mode oscillations. Nevertheless, a maximum quadrant tilt is prescribed in the technical specifications along with prescribed operating restrictions in the event that the tilt is exceeded. The azimuthal power tilt is determined by COLSS and included in the COLSS determination of core margin. The azimuthal power tilt limit is accounted for in the RPS.

4.3.2.7.5.1 Features Provided for Azimuthal Xenon Effects

a) Administrative limits on azimuthal power tilt

b) Monitoring and indicating the azimuthal power tilt in COLSS as well as accounting for this tilt in the COLSS determination of core margin

c) Accounting for azimuthal power tilt limit in the RPS.

4.3.2.7.5.2 Features Provided for Axial Xenon Effects and Power Distribution Effect and Control

→(DRN 01-1103)

a) CEAs for control of the axial power distribution, if required

←(DRN 01-1103)

b) Monitoring and accounting for changes in the axial power distribution in COLSS

c) Monitoring and accounting for the axial power distribution in the RPS.

4.3.2.8 Vessel Irradiation

The design of the reactor internals and of the water annulus between the active core and vessel wall is such that for reactor operation at the full power rating and an 80 percent capacity factor, the vessel fluence greater than 1 MeV at the vessel wall will not exceed $3.68 \times 10^{19}$ n/cm$^2$ over the 40 year design life of the vessel. The calculated exposure includes a 10 percent uncertainty factor.

The maximum fast neutron fluxes greater than one MeV incident on the vessel ID and shroud ID are as shown in Table 4.3-10. The fluxes are based on a time averaged equilibrium cycle radial power distribution and an axial power distribution with a peak to average of 1.20. The calculation assumed a thermal power of 3560 MWt. The models used in these calculations are discussed in Subsection 4.3.3.3.

4.3.3 ANALYTICAL METHODS

4.3.3.1 Reactivity and Power Distribution

4.3.3.1.1 Method of Analysis

→(DRN 06-871, R15)
The nuclear design analysis for low enrichment PWR cores is based on a combination of multigroup neutron spectrum calculations, which provide cross-sections appropriately averaged over a few broad energy groups, and few-group one, two, and three dimensional diffusion theory calculations of integral and differential reactivity effects and power distributions. Multigroup calculations include spatial effects in those portions of the neutron energy spectrum where volume homogenization is inappropriate; e.g., the thermal neutron energy range. Most of the calculations are performed with the aid of computer programs embodying analytical procedures and fundamental nuclear data consistent with the current state of the art.
←(DRN 06-871, R15)

Comparisons between calculated and measured data that validate the design procedures are presented in Subsection 4.3.3.1.2. As improvements in analytical procedures are developed, and improved nuclear data become available, they will be added to the design procedure, but only after validation by comparison with related experimental data.

Few-group cross-sections for subregions of the core that are represented in spatial diffusion theory codes; e.g., fuel pin cells, moderator channels, structural member cells, etc., are calculated by the CEPAK lattice program. This program is the synthesis of a number of computer codes, many of which were developed elsewhere; e.g., FORM,(3) THERMOS(4) and CINDER(5). These programs are interlinked in a consistent way with inputs from differential cross-section data from an extensive library.

The entire neutron spectrum is represented by 83 neutron groups between 0 and 10 MeV. Neutron leakage in a single Fourier mode is represented by either P-1 or B-1 approximations to transport theory throughout this entire range. Resonance shielding is determined analytically; the Hellstrand correlation is employed for U-238, with appropriate adjustments guided by Monte Carlo calculations of resonance capture in U-238 so as to provide agreement

with selected measurements of the conversion ratio. Plutonium resonance integrals are determined from an intermediate resonance formulation using equivalence relationships for the lattice representation.[7]

Appropriate Dancoff correction factors are determined for uniform lattices by three-region model of the unit cell to provide a better description of the effect of scattering in the clad on resonance capture. For heterogeneous lattices, this calculation is extended to include heterogeneities by nearest neighbor approximations. Included also in the resonance shielding calculation is an appropriate account of resonance overlap effects between different uranium and plutonium isotopes. In the thermal energy range, the effects of cell environment on the spatially dependent neutron spectrum within the lattice cell can be included in those cases where such effects are important. An isotropic scattering and temperature-dependent effects associated with the hydrogenous moderator are approximated by the use of transport-corrected, temperature-dependent scattering kernels generated by the GAKER program.[8] A major subroutine of CEPAK provides depletion calculations in the reactor cell to describe the evolution of the reactor spectrum and the appropriate cross-section averages, the production of fission products, including xenon, and samarium , and the production of conversion products as plutonium isotopes. Various criticality search options are available to approximate the effects of reactivity control on the neutron spectrum and thus on the few-group cross-section averages over energy.
→ (DRN 00-644)
Boundary conditions and equivalent diffusion theory constants for individual elements of a CEA are calculated by the CERES program. For a one region CEA in cylindrical geometry boundary conditions are calculated in each multi-group by the method of successive generations, with capture probabilities based on the tabulations of Stuart and Woodruff[9]. Two region CEAs are transformed to fictitious homogeneous CEAs by matching extrapolation lengths on the outer surface as defined by Kear and Ruderman.[10] The homogeneous CEAs are then treated as above. Fictitious few-group diffusion parameters for use in multidimensional diffusion theory calculations are obtained using methods defined by Wachspress[11] and Henry.[12]

Effective diffusion theory constants for burnable poison rod shim cells are calculated by a sequence of programs consisting of HAMMER[13] DTFIV,[14] and MO-807[15]. HAMMER is employed as a few-group region wise cross-section generator for the shim cell; DTF-IV is employed in a one dimensional representation of the shim cell and environment to define relative reaction rates between shim and fuel cells; and MO-807 is employed to calculate the effective diffusion theory constants.
← (DRN 00-644)
Static and depletion dependent reactivities and nuclide concentration, flux, and power distributions in one, two, and three-dimensional representations of the core are determined by a diffusion-depletion program, PDQ-X. This program is an extension of the PDQ-7[16] and HARMONY[17] programs to include the following optional capabilities:

a) Moderator and fuel temperature feedback in the three-dimensional geometry option, fuel temperature feedback in two-dimensional geometry.

b) Poison content criticality searches as well as spatial feedback on the power distribution by fuel and moderator temperature in the one dimensional geometry option.

These calculations employ macroscopic or microscopic cross-section data for time independent or depletion effects respectively generated by the methods described in the preceding paragraph.
→ (DRN 00-644)
The spatial flux in the reactor core is customarily assumed to be separable into radial and axial components. Thus the detailed spatial variations of the flux and power are obtained from the calculation of the broad group fluxes in a two-dimensional calculation of a typical plane in the reactor. The calculation is corrected for axial leakage effects by the use of a group dependent buckling, which is obtained from a one dimensional core average axial calculation.
← (DRN 00-644)
For detailed calculations of pinwise power distributions, a mesh rectangle is assigned to each pin cell. The inter-assembly water channel, the core shroud, and the core barrel are each represented explicitly in standard fine-mesh design calculations. In addition, water holes, shim boron cells, and control rods are each represented. For first cycle calculations a quarter-core representation with reflective boundary conditions at the core centerlines is usually adequate. For cores with an asymmetric fuel loading or for the calculation of ejected rod worths, full-core representations are used.

The quarter-core fine mesh representation described above requires about 25,000 mesh points to describe the geometry of a 217 assembly reactor core. For some applications this detail is not required. For these purposes coarse-mesh calculations are constructed having only a few mesh regions per assembly (typically 16). Coarse-mesh calculations are capable of accurately predicting gross power distributions, core reactivity, and depletion characteristics but cannot accurately calculate pin power peaks. Cross-sections for coarse-mesh calculations are obtained by spatially collapsing, by batch type, the fine mesh cross-sections in such a way as to preserve the individual reaction rates.. To improve the gross power distribution in coarse mesh calculations, it has been found necessary to use a non-regular mesh structure within individual assemblies in order to reproduce the large thermal flux gradients at the assembly interfaces.

In a large PWR core, the calculated power distributions are fairly sensitive to the treatment of the reflector cross-section. Terney[18] has compared transport and diffusion calculations of the albedo and shown that the latter substantially underpredicts the reflector albedos in the fast (top) group and that the power distribution is shifted toward the core center when compared to multigroup transport theory results. When the fast diffusion coefficients in the reflector are altered to make the transport and diffusion theory albedos agree, the power distributions are also brought into agreement.

In addition, Terney[18] has obtained an additional correction factor for mesh size effects that is applicable to coarse mesh problems. The use of this formula gives the correct albedos at the reflector-core interface, as well as improved distributions for coarse mesh problems.

The HARMONY[17] scheme of cross-section organization is used to input cross-sections for the PDQ spatial calculations. The fuel cross-sections are calculated for each batch type at the batch average conditions. For the more important cross-section, the microscopic cross-sections is tabulated as a function of the fuel exposure (in MWd/MTU). For other

crossections, a value is input that has been averaged over the exposure history of the core. Separate cross-section sets are generated for the water holes, poison rods, water channels, reflectors, and control rods.

The depletion of the core is accomplished by utilizing a set of linearized depletion chains specifying the coupling (neutron capture or fission) between the isotopes in the chain. Except for I-135, Xe-135 and Sm-149, the fission products are added together into a single lumped fission product. The exposure intervals of the depletion calculations are usually chosen to be 1000 MWd/MTU during which is assumed that the flux and microscopic cross-sections are unchanged. At the end of each exposure interval, the fluxes and cross-sections are recalculated. Shorter exposure intervals are used for recalculation of cross-sections, which vary rapidly with depletion, such as shim boron. As in the spectrum calculation, the soluble boron concentration and axial leakages are modified at each exposure interval.

The local fuel temperature has a large effect upon the spatial power distribution and therefore has to be factored into the spatial calculations. This effect is included by the following form of power feedback calculation. The simplest method of modeling this feedback is by introducing in the batch-wise spectrum calculation the appropriate power level and effective fuel temperature. The broad-group cross-sections then reflect the average batch conditions.

A more detailed treatment of the Doppler feedback is obtained by introducing a set of power dependent microscopic cross-sections to account for the change in a cross-section due to the change in local power level. In this way a detailed Doppler feedback calculation can be performed for each fuel pin cell.
→(DRN 00-644)
The treatment of thermal hydraulic effect in the axial direction is more complex because the effect of moderator temperature on the local moderator density and neutron spectrum must be included for all isotopes. Thus, the fuel cross-sections must include both a fuel temperature and moderator density dependence, and both moderator and fuel temperature are allowed to vary in the axial direction.
←(DRN 00-644)
→(DRN 02-1477)
Axial depletion calculations are performed by the PDQ-X code, whose results are used to supply the necessary input to the QUIX code, which generates the data required by the monitoring and control systems. In addition to the eigenvalue problem, QUIX will perform four types of search calculations to attain a specified eigenvalue; viz., a poison search, buckling search, CEA region boundary search, and moderator density dependent poison search. The effects of moderator and fuel temperature feedback on the power distribution can be treated.
←(DRN 02-1477)
The QUIX code has the capability of simulating excore detector responses expected during operation. The calculated normalized core average power distribution is first corrected by the application of CEA shadowing factors to simulate the peripheral fuel assembly power distribution. Shape annealing factors (defined below) are then applied to the peripheral axial power distribution to simulate the integrated response of the subchannels of the three-element excore detectors.

CEA shadowing is the change in excore detector response resulting from changing the core configuration from an unrodded condition to a condition with CEAs inserted, while maintaining constant power operation. Although CEA shadowing is a function of azimuthal location, its effect is minimized by placing the excore detectors at azimuthal locations where minimum CEA shadowing occurs. CEA shadowing factors can be determined using detailed two-dimensional power distribution (XY-PDQ's) representing the cumulative presence of the various CEA banks and the shielding code SHADRAC.(19) SHADRAC calculates fast neutron and gamma ray spectra, heating and dose rates in a three-dimensional system utilizing a moments method solution of the transport equation. The core, vessel internals, vessel, and excore detector location are rated explicitly in the calculation.

Normalized CEA shadowing factors are relatively constant with burnup and power level changes made without moving CEAs. CEA shadowing factors at beginning and end of first cycle life are as shown in Table 4.3-11.

Figure 4.3-46 shows the typical behavior of the CEA shadowing factor during a CEA insertion and withdrawal sequence. QUIX simulated factors and experimentally measured CEA shadowing factors during this transient situation are shown to have quite good agreement over a significant range of CEA insertions.

Shadowing factors account for the radial effects and annealing accounts for the axial effects on the excore detector responses. Due to neutron scattering in the various regions separating the core and the excore detectors, each detector sub-channel responds to neutrons from the entire length of the core and not just from the section immediately opposite the sub-channel. This effect is independent of the axial power shape and the azimuthal CEA shadowing factors. Typical shape annealing functions, given as fractional response per percent of core height for a three sub-channel system, are shown in Figure 4.3-47.

Shape Annealing Functions (SAFs) are determined from a series of fixed source calculations in which the source is given by an isotropic fission spectrum distributed uniformly, radially and axially throughout successive horizontal slices of the core. The result of each fixed-source calculation is the response in each of the excore detectors. These results are then normalized so that the sum over all the detectors and fixed-source calculations representing the entire height of the core would equal 1.0. The effect of variations in the core height used for each fixed source calculation is removed and the units of the SAF converted to percent of axial height of the core by dividing the results for each case by the respective percent of core height used for the source of that calculation. The SAF is then plotted for each ex-core detector as the fraction of the total response percent of core height versus the percent of core height.

Cases Involving Geometric Models with Azimuthal Symmetry

For cases which may be described adequately by azimuthally symmetric models of the geometry (see Figure 4.3-48), the response at the detector given a fixed source in some increment of the core, may be determined by first solving the Boltzmann transport equation in Rz - cylindrical geometry (Reference 20).

Using the method of discrete ordinates the DOT II W or DOT III codes (References 21 and 43) can be used to solve an approximation to the Boltzman transport equation. DOT III is a more recent version of DOT II W. The major features of the method used in both codes are as follows:

a) Energy dependence is considered using the multigroup treatment.

b) The derivative terms and spatial dependence are approximated using a finite difference technique.

c) Dependence upon the direction variables is treated using the discrete ordinates method.

d) The scattering integral in Equation (A) is evaluated using a discrete ordinates quadrature in combination with a Legendre expansion of the scattering kernel to approximate anistropic scattering.

The use of DOT III provides the additional option of "bootstrapping" the calculation to overcome computer core storage limitations. In this technique the problem geometry is divided into sequential, overlapping regions and each is calculated separately, but in sequence with the appropriate angular fluxes used as a source in a subsequent calculation for an adjacent region.

Cases Involving Geometric Models with Partial Azimuthal Symmetry

In some problems it is not possible to adequately model the geometry unless a three-dimensional representation is used. In these cases the response of each detector is obtained by first solving the 3-D form of the transport equation.

The current state-of-the-art in solving the transport equation involves the use of the Monte Carlo method and for this work in particular the MORSE code. (Reference 44) The major features of the MORSE code are as follows:

a) Multigroup treatment of energy dependence,

b) Combinatorial geometry representation of the problem geometry,

c) Generalized Gaussian Quadrature for the treatment of the angular distribution of group-to-group transfers.

The usual problem in utilizing MORSE is that the amount of computer CPU time required to calculate the desired results to the necessary statistical precision is too large for practical consideration. This typically occurs in problems with overall dimensions of many mean-free-paths and moderate geometric complexity.

→ (DRN 00-644)
The existence of even a partial azimuthal symmetry in the problem geometry, such as is shown in Figure 4.3-48, reduces the difficulty of the calculation greatly. The geometry may be divided into two overlapping parts, one with complete azimuthal symmetry. The radiation field in this portion of the geometry may then be calculated using the DOT II W/DOT III codes and the other portion calculated using the MORSE code and a source generated along an interface between the two portions of the geometry.
← (DRN 00-644)
This source is generated for MORSE by the DOMINO code (Reference 45), a utility code which converts the boundary angular flux from a DOT II-W/DOT III "RZ" geometry, calculation into the appropriate format for use as a source in MORSE.

The Rz-DOTs and MORSE use $P^3$, scattering cross-sections based on the CASK (DLC-23E) cross-section set (Reference 46).

The shape annealing factors are purely geometric correction factors applied to the peripheral axial power distribution. As such, the effects of time in fuel cycle, transient xenon redistribution and CEA insertion, although affecting the peripheral bundle power shape, do not effect the geometric shape annealing correction factors. Figure 4.3-49 compares the peripheral axial shape index with the external shape index during a CEA and PLCEA motion test for the Palisades reactor. Shown are the result of QUIX simulations of the test as well as experimental data taken during the test.

From this curve, we can conclude that even though the axial power distribution in the core and on the core periphery were changing during this transient, the relationship between the excore response and the peripheral response was not changing. These results justify not only the separability of CEA shadowing and shape annealing as summed in QUIX but also demonstrate that shape annealing is purely a geometric effect, independent of the peripheral axial power distribution.

The excore detector temperature decalibration effect is the relative change in detector response as a function of reactor water inlet temperature. The temperature decalibration effect is calculated utilizing SHADRAC with explicit representation of core, vessel internals, vessel, and detector location for various reactor inlet temperatures. Typical detector temperature decalibration effect as a function of inlet temperature normalized to an inlet temperature of 525°F is as shown in Figure 4.3-50.

Final normalization of the CEA shadowing, shape annealing, and temperature decalibration constants will be accomplished during start-up testing.

4.3.3.1.2 Comparisons with Experiments

The nuclear analytical design methods in use for Waterford 3 have been checked against a variety of critical experiments and operating power reactors. In the first type of analysis, reactivity and reaction rates calculations are performed, which leads to information concerning the validity of the basic fuel cell calculation. The second type of analysis consists of a core follow program in which power distributions, reactivity coefficients, reactivity depletion rate, and CEA worths are analyzed to provide a global verification of the nuclear design package.

4.3.3.1.2.1 Critical Experiments

Table 4.3-12 summarizes the properties of the fuel rods employed in the lattices analyzed. The enrichments, pin dimensions, and water-to-fuel ratios are similar to those used in pressurized power reactors. Tables 4.3-13 and 4.3-14 summarize pertinent characteristics of the lattices and the eigenvalues calculated for the uranium and mixed oxide lattices, respectively.

The average eigenvalue for the uranium oxide lattices is $1.0020 \pm 0.0020$ and for the mixed oxide lattices it is $1.0043 \pm 0.0034$.

Reaction rates were measured in some of the lattices noted in the previous section and are shown, along with definitions of the measured quantities, in Table 4.3-15.

→(DRN 00-644; 06-895, R15)
Although some scatter can be seen in the differences between measured and calculated values, the average error in the prediction of reaction rates is of the order of one percent only, except for the epicadmium-to-subcadmium fission rate for U-235, which is consistently overestimated by seven to eight percent. This overprediction of the fast fission rate in U-235 is due to simplifying assumptions in the resonance shielding, which were shown not to affect the reactivity level or the isotopic depletion rates.
←(DRN 00-644; 06-895, R15)

4.3.3.1.2.2 Power Reactors

The accuracy of the calculational system in its entirety can only be assessed through the analysis of experimental data collected on operating power reactors. The data under investigation consists of critical conditions, reactivity coefficients, and rod worths measured during the startup period, and of critical conditions, power distributions, and reactivity coefficients measured throughout the various cycles.

4.3.3.1.2.2.1 Startup Data

Because of the clean core configuration prevailing during the initial zero power operation, the startup data is extremely valuable in assessing the validity of the physics design package.

The soluble boron concentration that has to be added to the moderator to bring the unrodded reactor critical is a measure of the excess reactivity present in the core to accommodate the negative reactivity insertion due to the power escalation and the fuel depletion. The boron concentrations measured in cold and hot conditions for various reactors are compared with the calculated predictions in Table 4.3-16, showing an average error of $-8 \pm 13$ ppm.
→(DRN 00-644)
The moderator temperature coefficient is the change in reactivity resulting from a unit change in moderator temperature. As the temperature increases and the moderator density decreases, two phenomena take place, affecting the reactivity in two opposite directions. The reduced water density affects adversely the slowing down of neutrons to thermal energies, reducing the fissions rates and thus the reactivity. Another effect of the reduced water density is a displacement of the soluble boron, which results in a reduction of the thermal absorption rate and an increase in reactivity. In a fresh core, when the
←(DRN 00-644)

soluble boron concentration is at its maximum, the second effect may overcome the first one and the moderator temperature coefficient may be positive at low power levels. As the core depletes and the boron concentration is reduced to maintain criticality, the first effect becomes predominant, and the moderator temperature coefficient becomes increasingly negative. The result of analyses of beginning of cycle temperature coefficients is given in Table 4.3-16, showing an excellent agreement between measured and calculated values. The average-error in the eight coefficients given in the table is only $-0.04 \pm 0.08 \times 10^{-4}$ $\Delta\rho/^{\circ}F$.

4.3.3.1.2.2.2 CEA Reactivity Worth

Comparisons were made between the predicted and measured CEA worths for individual banks inserted sequentially, for the Maine Yankee, Fort Calhoun, Calvert Cliffs, and Millstone II reactors. Table 4.3-17 summarizes this comparison between calculation and experiment. In the evaluation of the experimental data, revised values of delayed neutron fractions published in Reference 31 were used. The comparisons demonstrate that CEA reactivity worths can be calculated to within seven percent of the experimental value. A comparison between calculation and measurement of ejected, stuck and dropped CEA worths is given in Table 4.3-18 for Palisades, Fort Calhoun, and Maine Yankee. The differences between calculation and measurement are approximately the same as in the individual CEA bank calculations. For these comparisons the stuck, ejected, and dropped CEA worths are calculated to within eight percent of the experimental value.

4.3.3.1.2.2.3 Depletion Calculation

Over 50 spent fuel samples from Yankee Rowe Core I were subjected to isotopic and radio-chemical analyses which were performed in the Tracerlab Laboratory at Richmond, California and by the Vallecitos Atomic Laboratory of the General Electric Company(32). Depletion calculations were performed on the Yankee core for comparison with the above measurements. Figure 4.3-51 compares measured and calculated values of the Pu/U mass ratio versus exposure, and Figure 4.3-52 shows a comparison for the relative isotopic composition of plutonium as a function of fractional U-235 depletion.
→ (DRN 00-644)
Comparisons of the depletion behavior of the measured and calculated critical boron concentrations for the Palisades Maine Yankee, and Fort Calhoun reactors are shown in Figures 4.3-53, 4.3-54, and 4.3-55, respectively. These comparisons indicate good agreement for the Palisades reactor and a relatively consistent bias between calculation and measurement for the Maine Yankee and Fort Calhoun reactors of approximately 50 to 70 ppm for the range of conditions shown in the figures. Best estimate reactivity predictions for these latter two classes of reactors are currently obtained by increasing the calculated critical boron concentration by approximately 50 ppm. The a priori knowledge of the expected bias between measured and calculated soluble boron concentration is factored into the design calculations; and results in better estimates of these characteristics which are sensitive to the boron level such as moderator temperature coefficients, power distributions, and cycle length. The resulting difference between the best estimate predicted soluble boron concentration and the measured critical soluble boron concentrations after adjustment to standard conditions is within approximately 0.4 percent reactivity, of which a significant fraction is associated with the spread in the experimental data.
← (DRN 00-644)

4.3.3.1.2.2.4 Fuel Temperature and Power Coefficients

The power coefficient is expressed in terms of reactivity change per unit change in power. This coefficient consists mostly of two components: one results from the reactivity change associated with the core average coolant temperature, and the other, which will be discussed here, is due to the change in fuel temperature. At each power level, an equilibrium exists between the power produced in the fuel, the fuel temperature, the heat transfer between the fuel and the coolant, and the energy removed by the coolant. The fuel temperature can be calculated directly by a heat transfer calculation, or indirectly by the analysis of the reactivity effects associated with a fuel temperature change. Both approaches have been used and lead to very consistent results.

The reactivity effects attributable to the fuel temperature are due to the Doppler broadening of the cross section resonances, mostly those of uranium 238, as well as to the change in scattering properties of the oxygen present in the fuel. The power coefficient can be expressed as

$$\frac{\partial \rho}{\partial P} = \frac{\partial \rho}{\partial T} \cdot \frac{\partial T}{\partial P}$$

The determination of the first term, $\partial\rho/\partial T$, is performed by the lattice code CEPAK, in which the Hellstrand correlation is used to calculate the Doppler broadening of U-238[6] and an equivalence principle is used for the plutonium isotopes[7]. The second term, $\partial T/\partial P$, is calculated by the FATES code[33], Waterford 3 fuel pin as well as for a Millstone II fuel pin, which has different dimensions but the same fuel densification properties. FATES shows that the fuel temperature is insensitive to the pellet diameter, and therefore that the measured power coefficients $\partial\rho/\partial P$ for Millstone II can be used to establish the validity of the fuel temperature correlation.

The following correlation is semi-empirically derived from the FATES results and the analysis of the Millstone II power coefficients.

$$T_f(F) = T_m + 128.36\alpha\underline{P} - 1.7832\alpha\underline{P}^2$$

in which $\alpha$ is a weighting factor accounting for the number of dimensions in spatial calculations.

$\alpha = 1.0$ in three dimensional,
$\alpha = 1.1415$ in two dimensional,
$\alpha = 1.0198$ in one dimensional axial calculations,

$\underline{P}$ is the linear heat generation rate in the fuel rod in kW/ft; $T_m$ is the coolant temperature; and $T_f$ the pellet average fuel temperature used in the resonance broadening calculation.

The results of the Millstone II power coefficient analysis are displayed in Figure 4.3-56.

4.3.3.1.2.2.5 Moderator Temperature Coefficients

Moderator temperature coefficients were measured at or near full power for a number of reactors of the 133 or 217 assembly design, at various core exposure throughout the cycles. The analysis of this data is a complement to the analysis of beginning-of-life, zero power coefficients presented earlier. The accuracy of the full power temperature coefficient predictions has a direct bearing on the prediction of most fast transients. Table 4.3-19 gives the results of the analysis, and shows that with standard two dimensional calculations, the average error is $(0.008 \pm 0.10) \times 10^{-4}$ $\Delta\rho/°F$. Three dimensional calculations, performed with full feedback, lead to essentially the same results, demonstrating the validity of two dimensional calculations.

4.3.3.1.2.2.6 Core Power Distributions

→ (DRN 00-644)

The accuracy of the power distribution predictions cannot be disassociated from the accuracy of the instrumentation which is used to measure these power distributions. The instrumentation in C-E reactors consists of fixed self-powered rhodium detectors, whose signals are fed into the computer code system INCA(34) which unfolds a full core power distribution with the help of precalculated coupling coefficients. The uncertainties associated with each step of this process, and the differences observed between calculated and measured three dimensional power distributions, are documented in detail in References 35 and 36. The overall errors or uncertainties presented in these topical reports are summarized in Table 4.3-20, showing a 6.9 percent uncertainty in the three-dimensional peak $F_q^n$.

← (DRN 00-644)

4.3.3.2 Spatial Stability

4.3.3.2.1 Methods of Analysis

An analysis of xenon-induced spatial oscillations may be done by two methods: time-dependent spatial calculations and linear modal analysis. The first method is based on computer simulation of the space, energy, and the time dependence of neutron flux and power density distributions. The second method calculates the damping factor based on steadystate calculations of flux, importance (adjoint flux), xenon and iodine concentrations, and other relevant variables.

The time-dependent calculations are indispensable for studies of the effects of CEA, core margin, out-of-core and in-core detector responses, etc., and are performed in one, two, and three dimensions with few-group diffusion theory, using tested computer codes and realistic modeling of the reactor core.

→(DRN 00-644)

The linear modal analysis methods are used to calculate the effect on the damping factor of changes in fuel zoning, enrichment, CEA patterns, operating temperature, and power levels. These methods, using information at a single point in time, are particularly suited to survey type calculations. Methods are based on the work of Randall and St. John(37) as extended by Stacey(38). These methods are verified by comparison with time-dependent calculations.

← (DRN 00-644)

4.3.3.2.2 Radial Xenon Oscillations

→ (DRN 00-644)

To confirm that the radial oscillation mode is extremely stable, a spacetime calculation was run for a reflected, zoned core 11 ft. in diameter without including the damping effects of the negative power coefficient. The initial perturbation was a poison worth of 0.4 percent in reactivity placed in the central 20 percent of the core for one hour. Following removal of the perturbation, the resulting oscillation was followed in four hour time steps for a period of 80 hours. The resulting oscillation diminished very rapidly with a damping factor of about -0.06 per hour. When this damping factor is corrected for a finite-time step size by the formula in Reference 39, a more negative damping factor is obtained indicating an even more strongly convergent oscillation. On this basis, it is concluded that a radial oscillation instability will not occur.

← (DRN 00-644)

4.3.3.2.3 Azimuthal Xenon Oscillations

→ (DRN 00-644)

Two-dimensional modal analysis techniques were used to calculate the damping factor for azimuthal oscillations, and included both the fuel temperature and moderator temperature components of the total power coefficient. These calculational techniques were used to predict the results of azimuthal oscillation tests at Maine Yankee at 75 percent power. The predicted damping factor of -0.045 per hour for azimuthal oscillations was found to agree well with the measured value of $-0.047 \pm 0.005$ per hour.

← (DRN 00-644)

Measurements of the azimuthal damping factor on other (earlier) reactors near full power further substantiate the techniques used to predict the damping factor for Waterford 3.

4.3.3.2.4 Axial Xenon Oscillations

→ (DRN 00-644)

To check and confirm the predictions of the linear modal analysis approach, numerical space-time calculations were performed for both beginning and end-of-cycle. The fuel and poison burnup distributions were obtained by depletion with soluble boron control, so that the power distribution was strongly flattened. Spatial Doppler feedback was included in these calculations. In Figure 4.3-57, the time variation of the power distribution along the core axis is shown near end-of-cycle with reduced Doppler feedback. The initial perturbation used to excite the oscillations was a 50 percent insertion into the top of the core of a 1.5 percent reactivity CEA bank for one hour. The damping factor for this case was calculated to be about +0.02 per hour; however, when corrected for finite-time step intervals by the methods of Reference 39, the damping factor is increased to approximately +0.04 per hour. When this damping factor is plotted on Figure 4.3-58 at the appropriate eigenvalue separation for this mode at end of cycle, it is apparent that good agreement is obtained with the modified Randall-St. John prediction. This good agreement is a result of the generally antisymmetric distribution of the moderator coefficient about the core midplane, and its flux and adjoint weighted integrals of approximately zero.

← (DRN 00-644)

Axial xenon oscillation experiments performed at Fort Calhoun at a core exposure of 7000 MWd/MTU and at Stade at beginning of cycle and at 12000 MWd/MTU[40] were analyzed with a space-time one-dimensional axial model. The results are given in Table 4.3-21 and show no systematic error between the experimental and analytical results.

4.3.3.3 Reactor Vessel Fluence Calculation Model

The calculated vessel fluence is obtained by combining the results of ANISN[41] and SHADRAC[19] in the following manner:

$$\phi(E) = \phi(ANISN) \cdot \frac{\phi A(SHADRAC)}{\phi B(SHADRAC)} \quad (6)$$

where:

$\phi$(E) is the neutron energy flux at the inner surface of the vessel,

$\phi$(ANISN) is the neutron energy flux obtained from ANISN,

$\phi$A (SHADRAC) is the neutron energy flux as calculated by SHADRAC in which the exact source geometry and a three-dimensional time averaged power distribution are used.

$\phi$B (SHADRAC) is the neutron energy flux as calculated by SHADRAC using a cylindrical source geometry and the power distribution obtained from ANISN.

→ (DRN 00-644)
The neutron flux as calculated by the above method has uncertainty limits of +10 percent, -40 percent. The total uncertainty is composed of 0 percent, -30 percent in the calculational method and ± 10 percent uncertainty in the combined radial and axial power distribution. The calculational uncertainty factors are obtained by comparing the ANISN-SHADRAC results with measurements from various operational reactors[42].
← (DRN 00-644)

4.3.3.4 Local Axial Power Peaking Augmentation

A reduction in $UO_2$ volume associated with fuel densification results in a shortening of the active fuel pellet stack height. If it is assumed that the reduction in the active stack length is not reflected by an equivalent increase in the length of the gas plenum but, instead, results in the formation of axial gaps within the fuel column, local power peaking is experienced in the vicinity of the fuel gaps. This arises because the decreasing neutron absorption, due to fuel removal, more than compensates for the fission loss.

Since the magnitude of the local power peaking in a given rod is a function of both the size and number of gaps in surrounding fuel rods, and the distribution of gaps within a given volume of the core cannot be defined explicitly, a statistical approach to the determination of the local peaking factor, resulting from the presence of gaps, is employed. This additional peaking due to gaps is called the augmentation factor. The augmentation factor, at any given plane, is defined as the ratio of the maximum power in that plane with the statistically expected distribution of gaps to the maximum power without gaps. The peaking augmentation factors are based on a 95 percent confidence level. That is to say, for each axial region, the augmented power is chosen so that there is a 95 percent probability that no more than one rod exceeds the augmented power.

A detailed discussion of the theoretical model, a description of the gap distribution characteristics employed in the model, the data from which these characteristics were established, and a discussion of the application of the model to the calculation of axial peaking augmentation factors are presented in Reference 33. Reference 33 describes the model in detail and gives as an example a calculation using input characteristics for a typical reactor.

Input information, which is specific to the reactor under consideration and is required for the calculation of augmentation factors, includes the fuel densification characteristics, the radial pin power distribution, and the single gap peaking factors. The fuel densification characteristics used in the calculation of augmentation factors for Waterford 3 are presented in Table 4.3-22. The radial pin power census used in calculation of the augmentation factors is given in Table 4.3-23. The integration of this radial pin power census into the R(x) function described in Reference 33 is illustrated here as Figure 4l.3-56. Figure 4.3-59 shows the specific assignment of fuel rod locations to radial groups and gives the power peaking associated with a single gap at each of these locations used in the calculation of the limiting augmentation factors for Waterford 3.

The axial dependent peaking augmentation factors for noncollapsed gaps are presented in Figure 4.3-60 for Waterford 3. Augmentation factors were calculated using limiting radial pin power distributions and single gap peaking factors, and therefore can be used throughout the first cycle.

## 4.3.4 CHANGES

A significant amount of core operating data has been incorporated into the nuclear design methods used for the design of this reactor. Operating reactor power distributions, critical boron concentrations, reactivity coefficients, and control rod worths were measured on the Maine Yankee, Fort Calhoun, Calvert Cliffs, and Millstone II reactors. The design methodology used for Waterford 3 is essentially identical to the methodology used in the analysis of the experimental data reported in Subsection 4.3.3.1.2. The good agreement between measured values and those predicted by the analytic methods used at C-E, as described in Subsection 4.3.3, lends confidence to the methods used in the design of this reactor.

As more experimental data becomes available, the methods verification program is extended, and the results of these analyses are implemented in the design of future cores.

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→(DRN 06-1059, R15; EC-9533, R302)

←(DRN 06-1059, R15; EC-9533, R302)

TABLE 4.3-1 (Sheet 1 of 2) Revision 307 (07/13)

## NUCLEAR DESIGN CHARACTERISTICS

| Item | Value |
|---|---|
| General Characteristics | |
| Fuel management | 3-batch, mixed central zone |
| First cycle average burnup, MWd/MTU | 12,731 |
| First cycle lifetime, full power hours | 8,075 |
| U-235 enrichments, w/o | |
| 18,188 rods | 1.87 |
| 17,408 rods | 2.41 |
| 13,984 | 2.91 |
| Core average | 2.35 |
| Core average $H_2O/UO_2$ volume ratio, first cycle, hot | 2.08 |
| →(DRN 02-1477, R12; 06-895, R15; EC-2800, R307) Number of control element assemblies ←(DRN 02-1477, R12; 06-895, R15; EC-2800, R307) | 87 |
| →(DRN 01-1103, R12; 02-1477, R12) ←(DRN 01-1103, R12; 02-1477, R12) | |
| Burnable Poison Rods | |
| Number | 1,632 |
| →(DRN 06-895, R15) Material ←(DRN 06-895, R15) | $B_4C$-$Al_2O_3$ |
| Worth W%p, at BOC | |
| Hot, 583°F | 7.5 |
| Cold, 68°F | 6.0 |
| Dissolved Boron | |
| Dissolved boron content for criticality, ppm, (CEAs withdrawn, BOC) | |
| Hot, zero power, clean 545°F | 832 |
| Cold, 68°F | 899 |
| Hot, full power, clean, 583°F | 719 |
| Hot, full power, equilibrium Xe | 452 |

→(DRN 06-895, R15; EC-2800, R307)

←(DRN 06-895, R15; EC-2800, R307)

TABLE 4.3-1 (Sheet 2 of 2) Revision 6 (12/92)

NUCLEAR DESIGN CHARACTERISTICS

| Item | Value |
|---|---|
| Dissolved boron content (ppm) for: | |
| Refueling, first cycle/later cycles | 1,720/2,150 |
| 10% subcritical, cold, first cycle (all CEAs out) | 1,530 |
| 10% subcritical, hot, first cycle (all CEAs out) | 1,590 |
| Design maximum soluble boron addition rate required to compensate for reactivity addition due to system cooldown and xenon decay, ppm/hr | 370 |
| Maximum reactivity addition rate due to system cooldown and xenon decay, $\%\Delta\rho$ | 2.9 |
| Boron worth, ppm/$\%\Delta\rho$ | |
| Hot, 583°F | 79 |
| Cold, 68°F | 62 |
| Neutron Parameters | |
| Neutron lifetime (minimum), microseconds | 30 |
| Delayed neutron fraction | |
| Beginning-of-cycle | 0.0072 |
| End-of-cycle | 0.0053 |

# Table 4.3-2

Intentionally Deleted

TABLE 4.3-3 Revision 6 (12/92)

COMPARISON OF CORE REACTIVITY COEFFICIENTS WITH THOSE USED N VARIOUS SAFETY ANALYSES

| | Moderator Temperature Coefficient ($\Delta\rho/°F \times 10^4$) | Doppler[a] (Coefficient) | Density Coefficient ($\Delta\rho/gm/cm^3$) |
|---|---|---|---|
| Coefficients from Table 4.3-4 | | | |
| Full power | | | |
| BOC | -0.8 | Figure 4.3-34 | 0.48 |
| EOC 1 | -2.3 | Figure 4.3-34 | N/A[b] |
| Zero power, CEAs at PDIL | | | |
| BOC | -1.1 | Figure 4.3-34 | N/A |
| EOC 1 | -2.0 | Figure 4.3-34 | N/A |
| Coefficients used in Cycle 1 Accident Analyses | | | |
| CEA withdrawal | | | |
| Full/zero power | +0.5/+0.5 | 0.85 | N/A |
| CEA misoperation (full length) | | | |
| Dropped CEA | -3.3 | 1.15 | N/A |
| CEA misoperation (part length) | | | |
| Dropped CEA | +0.5/-3.3 | 0.85/1.15 | N/A |
| Loss of flow | +0.5 | 0.85 | N/A |
| CEA ejection | | $0.85 \times W_R$ | N/A |
| BOC, full/zero power | +0.5/+0.2 | 1.0/1.57[c] | N/A |
| EOC 1, full/zero power | -1.3/-1.0 | 1.0/2.15 | N/A |
| Loss-of-coolant accident | N/A | 1.0 | (d) |

(a) Nominal values of the Doppler coefficient ($\Delta\rho/°F$) as a function of the fuel temperature are shown on Figure 4.3-34. The numbers entered in the Doppler column of this table are the multipliers applied to the nominal value for analysis of designated accidents.

(b) Not applicable.

(c) These are the values of $W_R$, where $W_R$ is a reactivity dependent factor, the origin of which is explained in Subsection 15.4.3.2.

(d) A curve of reactivity vs. moderator density is used for the LOCA evaluation. The value of density coefficient used corresponds to a $+ 0.5 \times 10^{-4}$ MTC for the large break analysis, and to a $+ 0.15 \times 10^{-4}$ MTC for the small break analysis.

TABLE 4.3-4

REACTIVITY COEFFICIENTS

| | |
|---|---|
| **Moderator Temperature Coefficient, $\Delta\rho$/°F** | |
| Beginning-of-cycle (719 ppm, soluble boron) | |
| Cold, 68°F | $-0.1\times10^{-4}$ |
| Hot, zero power, 545°F, no CEAs | $-0.5\times10^{-4}$ |
| Hot full power, 583°F no CEAs | $-0.8\times10^{-4}$ |
| Hot full power, equilibrium Xe, no CEAs | $-1.2\times10^{-4}$ |
| Hot zero power, regulating CEAs inserted | $-1.1\times10^{-4}$ |
| End-of-Cycle (0 ppm, soluble boron) | |
| Cold, 68°F | 0.0 |
| Hot zero power, 545°F, no CEAs | $-1.5\times10^{-4}$ |
| Hot full power, equilibrium Xe, no CEAs | $-2.3\times10^{-4}$ |
| Hot zero power, rodded, regulating CEAs inserted | $-2.0\times10^{-4}$ |
| **Moderator Density Coefficient, $\Delta\rho$/gm/cm$^3$** | |
| Hot, operating, 583°F | |
| Beginning-of-cycle, 719 ppm soluble boron | +0.048 |
| Fuel temperature contribution to power coefficient, /(kW/ft) | |
| Hot zero power | $-4.0\times10^{-3}$ |
| Full power | $-1.5\times10^{-3}$ |
| Moderator void coefficient $\Delta\rho$/% void | |
| Hot operating, 583°F | |
| Beginning-of-cycle, 719 ppm soluble boron | $-0.36\times10^{-3}$ |
| Moderator pressure coefficient, $\Delta\rho$/psi | |
| Hot, operating, 583°F | |
| Beginning-of-cycle, 719 ppm soluble boron | $+0.7\times10^{-6}$ |
| Overall power coefficient, $\Delta\rho$/(kW/ft) | |
| Hot, operating, 583°F | |
| Beginning-of-cycle, 719 ppm soluble boron | $-1.9\times10^{-3}$ |
| End-of-cycle, 0 ppm soluble boron | $-2.9\times10^{-3}$ |

TABLE 4.3-5

WORTHS OF CEA GROUPS (%Δρ)

| | BOC | EOC |
|---|---|---|
| Shutdown CEAs | 6.5 | 6.6 |
| Regulating CEAs | | |
| Group 1 | 1.3 | 1.1 |
| Group 2 | 0.7 | 0.6 |
| Group 3 | 1.0 | 1.1 |
| Group 4 | 1.0 | 1.0 |
| Group 5 | 0.4 | 0.5 |
| Group 6 (lead bank) | 0.4 | 0.4 |
| | 11.3 | 11.3 |

TABLE 4.3-6

CEA REACTIVITY ALLOWANCES (%$\Delta\rho$)

(Hot Full Power to Hot Zero Power)

| | |
|---|---|
| Fuel temperature variation | 1.4 |
| Moderator temperature | 2.0 |
| Moderator voids | 0.1 |
| CEA bite | 0.2 |
| Part-length CEA effects | 0.0 |
| Shutdown margin and accident analysis allowance | 5.15 |
| Total reactivity allowance | 8.85 |

TABLE 4.3-7 Revision 6 (12/92)

CALCULATED CEA WORTHS AND ALLOWANCES (%$\Delta\rho$)

| Condition | BOC | EOC |
|---|---|---|
| All full-length CEAs inserted, hot, 583°F | 11.35 | 11.30 |
| Total reactivity allowance, full power (from Table 4.3-6) | 8.85 | 8.85 |
| Stuck rod worth | 1.5 | 1.3 |
| Excess over nominal design allowance | 1.00 | 1.15 |
| Excess over nominal, assuming most adverse stack-up of CEA worth uncertainties | 0.10 | 0.25 |

TABLE 4.3-8

COMPARISON OF RODDED AND UNRODDED PEAKING FACTORS FOR VARIOUS RODDED CONFIGURATIONS AT BOC AND EOC

| Configurations | Maximum Rod Radial Peaking Factor $F_r^n$ | |
|---|---|---|
| | BOC | EOC |
| Unrodded | 1.30 | 1.31 |
| Bank 6 | 1.33 | 1.38 |
| PLCEA | 1.33 | 1.39 |
| Bank 6 and PLCEA | 1.43 | 1.46 |
| Bank 5+6(a) | 1.48 | 1.54 |
| Bank 4+5+6(a) | 1.54 | 1.50 |
| Bank 3+4+5+6(a) | 2.09 | 1.73 |
| Bank 2+3+4+5+6(a) | 1.70 | 1.71 |
| Bank 1+2+3+4+5+6(a) | 1.63 | 1.71 |

(a) No PLCEAs.

TABLE 4.3-9

CALCULATED VARIATION OF THE AXIAL STABILITY INDEX

DURING THE FIRST CYCLE[a] ($hr^{-1}$)

| Power Level (% of Full Power) | BOC | MOC [b] | EOC |
|---|---|---|---|
| 100 | +0.043 | +0.036 | +0.090 |
| 75 | +0.016 | +0.012 | +0.063 |
| 50 | -0.018 | -0.020 | +0.025 |

(a) Equilibrium xenon conditions.

(b) Middle of cycle.

TABLE 4.3-10 Revision 6 (12/92)

MAXIMUM FAST FLUX GREATER THAN 1 MeV (n/cm$^2$-sec)

| Neutron Group | Lower Bound On Energy (MeV) | Flux, Shroud, ID and Core Periphery | Flux, Vessel, ID |
|---|---|---|---|
| 1 | 7.41 | 2.69 (+11)[a] | 1.29 (+9) |
| 2 | 4.97 | 1.39 (+12) | 4.09 (+9) |
| 3 | 3.33 | 3.09 (+12) | 4.51 (+9) |
| 4 | 2.23 | 6.49 (+12) | 8.78 (+9) |
| 5 | 1.50 | 6.74 (+12) | 9.09 (+9) |
| 6 | 1.22 | 3.86 (+12) | 4.86 (+9) |
| 7 | 1.00 | 3.28 (+12) | 4.17 (+9) |
| Total | _____ | 2.51 (+13) | 3.68 (+10) |

(a) () Denotes power of ten

TABLE 4.3-11

CONTROL ELEMENT ASSEMBLY SHADOWING FACTORS

| | BOC | EOC |
|---|---|---|
| Unrodded | 1.000 | 1.000 |
| Reg. Bank 3 | 1.133 | 1.116 |
| PLR 9 | 1.054 | 1.054 |
| Reg. Bank 3 + PLR 9 | 1.197 | 1.175 |

TABLE 4.3-12

FUEL ROD DESCRIPTION

| Laboratory | Clad OD (in.) | Clad Thickness (in.) | Clad Material | Fuel Pellet OD (in.) | Fuel Density ($g/cm^3$) | Fuel Enrichment Wt % U-235 | Wt % $PuO_2$ |
|---|---|---|---|---|---|---|---|
| B&W | 0.4755 | 0.016 | SS 304 | 0.440 | 9.46 | 4.020 | 0 |
| B&W | 0.4748 | 0.032 | AL 6061 | 0.4054 | 10.24 | 2.459 | 0 |
| Yankee | 0.3383 | 0.0161 | SS 304 | 0.3000 | 10.18 | 2.700 | 0 |
| Winfrith | 0.4301 | 0.01051 | SS 304 | 0.3984 | 10.44 | 3.003 | 0 |
| Bettis | 0.453 | 0.028 | A1 | 0.3830 | 10.53 | 1.311 | 0 |
| Hanford | 0.426 | 0.027 | Zr-2 | 0.372 | 9.646(a) | 0.22 | 1.50 |
| Battele N.W. Westinghouse | 0.568 | 0.030 | Zr-2 | 0.508 | 9.869(a) | 0.72 | 2.20 |

(a) Effective fuel density

TABLE 4.3-13

RESULTS OF ANALYSIS OF CRITICAL $UO_2$ SYSTEMS

| Lattice | | W/O U-235 | Pitch (in.) | $H_2O/UO_2$ | Boron (ppm) | $K_{eff}$ | Ref |
|---|---|---|---|---|---|---|---|
| B&W-1273 | 1 | 4.020 | 0.595 | 1.137 | 0 | 1.0012 | 22 |
| | 2 | 4.020 | 0.595 | 1.137 | 3390 | 1.0053 | 22 |
| | 3 | 4.020 | 0.571 | 0.956 | 0 | 0.9984 | 22 |
| | 4 | 2.459 | 0.595 | 1.371 | 0 | 1.0042 | 22 |
| | 5 | 2.459 | 0.595 | 1.371 | 1075 | 1.0055 | 22 |
| B&W-3647 | 6 | 2.459 | 0.644 | 1.846 | 0 | 1.0027 | 23 |
| | 7 | 2.459 | 0.644 | 1.846 | 846 | 1.0044 | 23 |
| | 8 | 2.459 | 0.644 | 1.846 | 1536 | 1.0033 | 23 |
| Yankee | 9 | 2.700 | 0.405 | 1.048 | 0 | 1.0009 | 24 |
| | 10 | 2.700 | 0.435 | 1.405 | 0 | 1.0011 | 24 |
| | 11 | 2.700 | 0.470 | 1.853 | 0 | 1.0014 | 24 |
| | 12 | 2.700 | 0.493 | 2.166 | 0 | 1.0034 | 25 |
| Winfrith | 13(20°C) | 3.003 | 0.520 | 1.001 | 0 | 1.0021 | 26 |
| | 14(80°C) | 3.003 | 0.520 | 1.001 | 0 | 0.9994 | 26 |
| | 15 | 3.003 | 0.735 | 3.164 | 0 | | |
| | 16 | 3.003 | 0.492 | 0.779 | 0 | | 26 |
| Bettis | 17 | 1.311 | 0.6133[a] | 1.429 | 0 | 1.0005 | 27 |
| | 18 | 1.311 | 0.6504[a] | 1.781 | 0 | 1.0004 | 27 |
| | 19 | 1.311 | 0.7110[a] | 2.401 | 0 | 1.0011 | 27 |
| Average | | | | | | 1.0020± | 0.0020 |

(a) Triangular pitch

TABLE 4.3-14

RESULTS OF ANALYSIS OF PuO2-UO2 FUELED LATTICES

| Lattice | W/O U-235 | W/O $PuO_2$ | Pitch (in.) | $H_2O$/Fuel | Boron (ppm) | $K_{eff}$ | Ref |
|---|---|---|---|---|---|---|---|
| Hanford | 0.22 | 1.50 | 0.55[b] | 1.099 | 0 | 0.9998 | 28 |
| | | | 0.60[b] | 1.557 | 0 | 1.0026 | 28 |
| | | | 0.71[b] | 2.705 | 0 | 1.0081 | 28 |
| | | | 0.80[b] | 3.783 | 0 | 1.0071 | 28 |
| BNWL | 0.72 | 2.2[a] | 0.85[b] | 1.837 | 0 | 1.0068 | 29 |
| | | | 0.93[b] | 2.445 | 0 | 1.0093 | 29 |
| WCAP | 0.72 | 2.2[a] | 0.69 | 1.099 | 0 | 1.0019 | 30 |
| | | | 0.75 | 1.525 | 0 | 1.0067 | 30 |
| | | | 0.67 | 1.099 | 201 | 1.0013 | 30 |
| | | | 0.69 | 1.099 | 526 | 1.0011 | 30 |
| | | | | Average | | 1.0043 ± 0.0034 | |

(a) 7.654 W/O Pu-240 in Pu
(b) Triangular pitch

TABLE 4.3-15

REACTION RATES [a]

| Lattice Number | Measured | Calculated | Measured | Calculated | Measured | Calculated |
|---|---|---|---|---|---|---|
| Babcock and Wilcox | | ρ28 | | $\delta^{25}$ | | |
| 1 | 4.12±0.31 | 4.38 | 0.254±0.006 | 0.292 | | |
| 3 | 5.08±0.10 | 5.19 | 0.307±0.002 | 0.351 | | |
| 4 | 2.28±0.03 | 2.40 | 0.151±0.001 | 0.163 | | |
| | | ρ28 | | $\delta^{28}$ | MCR | |
| 6 | 1.85±0.02 | 1.81 | 0.063±0.006 | 0.056 | 0.484±0.011 | 0.480 |
| Winfrith | | RCR | | $\delta^{28}$ | Pu239/U235 | Fission |
| 13 | 4.158±0.03 | 4.203 | 0.0845±0.0009 | 0.0884 | 1.589±0.009 | 1.568 |
| 14 | 4.293±0.047 | 4.311 | 0.0881±0.0027 | 0.0903 | 1.637±0.009 | 1.608 |
| 16 | 4.789±0.053 | 4.874 | 0.1050±0.0018 | 0.1056 | 1.611±0.009 | 1.633 |
| Bettis | | ρ28 | | $\delta^{28}$ | $\delta^{25}$ | |
| 17 | 1.43±0.01 | 1.40 | 0.078±0.004 | 0.078 | 0.089±0.002 | 0.091 |
| 18 | 1.15±0.01 | 1.16 | 0.070±0.004 | 0.067 | 0.072±0.001 | 0.074 |
| 19 | 0.934±0.01 | 0.907 | 0.057±0.003 | 0.055 | 0.055±0.001 | 0.057 |

(a) Definitions:

ρ28 = epicadmium captures in U-238/subcadmium captures in U-238

$\delta^{25}$ = epicadmium fissions in U-235/subcadmium fissions in U-235

$\delta^{28}$ = Total fissions in U-238/total fissions in U-235

MCR = captures in U-238/fissions in U-235

RCR = MCR in lattice/MCR in thermal column

TABLE 4.3-16

BEGINNING OF CYCLE, ZERO POWER UNRODDED CHARACTERISTICS

| Reactor | Temperature F | Critical Boron (ppm) | | Temperature Coefficient x $10^{-4}$ $\Delta\rho$/F | |
|---|---|---|---|---|---|
| | | Measured | Cal culated | Measured | Calculated |
| Maine-Yankee I | 260 | 955 | 952 | 0.18 | 0.08 |
| | 525 | 988 | 989 | 0.13 | 0.23 |
| Maine-Yankee II | 525 | 809 | 799 | -0.24 | -0.29 |
| Fort Calhoun | 260 | 900 | 889 | 0.22 | 0.08 |
| | 525 | 933 | 911 | 0.26 | 0.21 |
| Calvert Cliffs | 260 | 1048 | 1062 | 0.20 | 0.14 |
| | 525 | 1106 | 1078 | 0.22 | 0.26 |
| Millstone II | 532 | 960 | 951 | 0.08 | 0.02 |
| Average Error (calc.-meas.) | | -8 $\pm$ 13 | | -0.04 $\pm$ 0.08 | |

TABLE 4.3-17 Revision 6 (12/92)

CONTROL ROD WORTH, BEGINNING OF CYCLE, ($\Delta\rho$) HOT - ZERO POWER

| | Main-Yankee | | Fort Calhoun | | Calvert Cliffs | | Millstone II | |
|---|---|---|---|---|---|---|---|---|
| Rod Banks | Measured | Calculated | Measured | Calculated | Measured | Calculated | Measured | Calculated |
| 7 | - | - | - | - | - | - | 0.76 | 0.74 |
| 6 | - | - | - | - | - | - | 0.50 | 0.47 |
| 5 | 0.57 | 0.58 | - | - | 0.55 | 0.54 | 0.33 | 0.29 |
| 4 | 0.37 | 0.38 | 0.60 | 0.54 | 0.35 | 0.35 | 1.37 | 1.36 |
| 3 | 0.92 | 0.93 | 0.58 | 0.57 | 0.93 | 0.90 | 0.62 | 0.62 |
| 2 | 0.86 | 0.95 | 2.10 | 2.01 | 0.78 | 0.74 | 1.40 | 1.40 |
| 1 | 0.80 | 0.80 | 0.99 | 0.91 | 0.95 | 1.04 | 0.69 | 0.70 |
| C | 1.21 | 1.23 | - | - | 1.30 | 1.40 | - | - |
| B | 0.76 | 0.75 | 2.13 | 2.09 | 0.99 | 0.87 | 0.39 | 0.46 |
| Total | 5.49 | 5.62 | 6.40 | 6.12 | 5.85 | 5.84 | 6.06 | 6.04 |
| Average relative error (%) (Calc-meas)/measured | | | 2.3 $\pm$ 3.8 | | -5.2 $\pm$ 3.7 | | -0.2 $\pm$ 7.4 | -0.3 $\pm$ 8.6 |
| | | | Average relative error, all cases (%): -0.6 $\pm$ 6.7 | | | | | |

TABLE 4.3-18 Revision 6 (12/92)

COMPARISONS OF CALCULATED AND MEASURED CEA WORTHS ($\Delta\rho$)

| Condition | CEA Worth | |
|---|---|---|
| | Calculated | Measured |
| Ejected CEA | | |
| Maine Yankee | | |
| 9 CEA pattern | 0.15 | 0.14 |
| Fort Calhoun | | |
| 17 CEA pattern | 0.29 | 0.28 |
| Palisades | | |
| 9 Rod pattern | 0.25 | 0.28 |
| 13 Rod pattern | 0.71 | 0.67 |
| Dropped CEA | | |
| Maine Yankee | | |
| Dual CEA | 0.15 | 0.14 |
| Fort Calhoun | | |
| Dual (peripheral) CEA | 0.14 | 0.15 |
| Dual (central) CEA | 0.18 | 0.19 |
| Stuck CEA (with all other CEAs inserted) | | |
| Maine Yankee | | |
| Dual CEA | 1.89 | 2.14 |
| Fort Calhoun | | |
| Dual CEA | 1.57 | 1.46 |

TABLE 4.3-19

AT-POWER ISOTHERMAL TEMPERATURE COEFFICIENTS

| Core | Exposure (MWD/T) | Boron (PPM) | Temperature Coefficient (x10 $\Delta\rho$/F) Measured | Calculated 2-D | Calculated 3-D |
|---|---|---|---|---|---|
| Fort Calhoun | 3000 | 597 | -0.13 | -0.29 | |
| | 8200 | 300 | -0.97 | -0.91 | |
| Maine Yankee 1 | 2000 | 750 | 0.03 | -0.01 | |
| | 4400 | 760 | 0.13 | 0.06 | |
| | 4400 | 712 | -0.06 | -0.04 | -0.04 |
| | 6500 | 640 | -0.19 | -0.19 | -0.21 |
| | 9000 | 500 | -0.47 | -0.46 | -0.48 |
| | 10360 | 420 | -0.61 | -0.67 | -0.58 |
| | 6700 | 537 | -0.50 | -0.45 | |
| Maine Yankee II | 400 | 525 | -0.93 | -0.69 | |
| | 4700 | 521 | -0.42 | -0.48 | |
| Average difference X (Calc-meas) | | | | 0.008 | 0.005 |
| Standard deviation of difference | | | | 0.100 | 0.024 |

TABLE 4.3-20 Revision 6 (12/92)

POWER DISTRIBUTIONS
SUMMARY OF CALCULATIONAL AND MEASUREMENT UNCERTAINTIES

| | |
|---|---|
| 1. Calculational Uncertainty on $F_q^n$ | 6.9% |
| 2. Measurement Uncertainty on $F_q^n$ | 5.8% |
| 3. Calculational Uncertainty on $F_r^n$ | 4.6% |
| 4. Measurement Uncertainty on $F_r^n$ | 4.6% |

TABLE 4.3-21 Revision 6 (12/92)

AXIAL XENON OSCILLATIONS

| Reactor | Exposure (MWd/MTU) | Period (hr) | | Dampling ($hr^{-1}$) | |
|---|---|---|---|---|---|
| | | Measured | Calculated | Measured | Calculated |
| Fort Calhoun | 7075 | 29 | 32 | -0.027 | -0.030 |
| Stade | BOC | 36 | 36 | -0.096 | -0.090 |
| Stade | 12200 | 27 | 30 | -0.021 | -0.019 |

TABLE 4.3-22 Revision 6 (12/92)

DENSIFICATION CHARACTERISITCS

| | |
|---|---|
| Core Height | 150 in. |
| Fractional Density Change | 0.01 |
| Clad Growth Allowance | 0.007 in./in. |

TABLE 4.3-23

RADIAL PIN POWER CENSUS

| Pin Power Interval[a] | Number of Pins with power in Interval |
|---|---|
| 0.00 - 1.00 | 15,700 |
| 1.00 - 1.05 | 6,324 |
| 1.05 - 1.10 | 10,516 |
| 1.10 - 1.20 | 11,344 |
| 1.20 - 1.29 | 5,696 |

(a) $F_q^n$ within the interval given.