4.4 THERMAL AND HYDRAULIC DESIGN

This section presents the steady-state thermal and hydraulic analysis of the reactor core, the analytical methods, and the experimental work done to support the analytical techniques during Cycle 1. Additional information for the current fuel cycle is discussed in Appendix 4.3A. Discussions of the analyses of anticipated operational occurrences and accidents are presented in Chapter 15. The prime objective of the thermal and hydraulic design of the reactor is to ensure that the core can meet steady-state and transient performance requirements without violating the design bases.

4.4.1 DESIGN BASES

Avoidance of thermally or hydraulically induced fuel damage during normal steady-state operation and during anticipated operational occurrences is the principal thermal hydraulic design basis. The design bases for accidents are specified in Chapter 15. In order to satisfy the design basis for steady-state operation and anticipated operational occurrences, the following design limits are established, but violation of these will not necessarily result in fuel damage. The reactor protective system (RPS) provides for automatic reactor trip or other corrective action before these design limits are violated.

4.4.1.1 Minimum Departure from Nucleate Boiling Ratio

→(DRN 03-2058, R14; EC-9533, R302; EC-30663, R307)

The minimum DNBR shall be such as to provide at least 95 percent probability with 95 percent confidence that departure from nucleate boiling (DNB) does not occur on a fuel rod having that minimum DNBR during steady-state operation and anticipated operational occurrences. A value of 1.19 using the CE-1 correlation, 1.12 using WSSV-T correlation, and 1.13 using ABB-NV correlation, coupled with the TORC code provides at least this probability and confidence. See Subsections 4.3A.4.1 and 4.3A.4.2 for current cycle critical heat flux correlations and DNBR limits.

←(DRN 03-2058, R14; EC-9533, R302; EC-30663, R307)

4.4.1.2 Hydraulic Stability

Operating conditions shall not lead to flow instability during steady-state operation and during anticipated operational occurrences.

4.4.1.3 Fuel Design Bases

→(DRN 04-1096, R14)

a) The peak temperature of the fuel shall be less than the melting point (5080 F unirradiated and reduced by 58 F per 10,000 MWd/MTU and adjusted for burnable poison per Reference 22) during steady-state operation and anticipated operation and anticipated operational occurrences.

←(DRN 04-1096, R14)

b) The fuel design bases for fuel clad integrity and fuel assembly integrity are given in Subsection 4.2.1. Thermal and hydraulic parameters that influence the fuel integrity include maximum linear heat rate, core coolant velocity, coolant temperature, clad temperature, fuel-to-clad gap conductance, fuel burnup and UO_2 temperature. Other than the design limits already specified, no limits need be applied to these parameters directly. No violation of the design limits specified here and no violation of the design bases specified in Subsection 4.2.1, are sufficient to ensure fuel clad integrity, fuel assembly integrity, and the avoidance of thermally or hydraulically induced fuel damage for steady-state operation and anticipated operational occurrences.

4.4.1.4 Coolant Flow, Velocity, and Void Fraction

→(DRN 00-644)

The primary coolant flow with all four pumps in operation shall be greater than the design minimum. A percentage of the flow entering the reactor vessel is not effective for cooling the core. This percentage is called the core bypass flow. The calculated core bypass flow shall be less than the design maximum. The design minimum value for the calculated core flow is obtained by subtracting the design maximum value for the calculated core bypass flow from the design minimum primary coolant flow. For thermal margin analyses, the design minimum value for the calculated core flow is used. These design flows are listed in Table 4.4-1.

←(DRN 00-644)

Design of the reactor internals ensures that the coolant flow is distributed to the core such that the core is adequately cooled during steady-state operation and anticipated operational occurrences. Therefore, no specific orificing configuration is used.

Although the coolant velocity, its distribution, and the coolant voids affect the thermal margin, design limits need not be applied to these parameters because they are not in themselves limiting. These parameters are included in the thermal margin analyses and thus affect the thermal margin to the design limits.

4.4.2 DESCRIPTION OF THERMAL AND HYDRAULIC DESIGN OF THE REACTOR CORE

4.4.2.1 Summary Comparison

The thermal and hydraulic parameters for the reactor are listed in Table 4.4-1. A comparison of these parameters with the Boston Edison Pilgrim Station Unit 2 reactor (Amendment 20, 1975, Docket No. 50-471) is given in Table 4.4-1.

→(EC-9533, R302; EC-13881, R304)

The principal differences between the two reactors are the total core heat output and the reactor inlet coolant temperature. With respect to the analysis of DNB, the Waterford 3 reactor was analyzed (through Cycle 16) using the CE-1 Correlation; [1][2] whereas, the Pilgrim reactor (Docket, No. 50-471) was analyzed using the original W3 Correlation.[3] The Waterford 3 Cycle 16 core was also analyzed using the WSSV-T correlation[23] and the ABB-NV correlation[24] due to the introduction of NGF assemblies in region quantities in that cycle. Beginning with Cycle 17, the core has consisted of only NGF assemblies; consequently, the Waterford 3 core is being analyzed using only the WSSV-T correlation[23] and the ABB-NV correlation[24].

←(EC-9533, R302; EC-13881, R304)

4.4.2.2 Critical Heat Flux Ratios

4.4.2.2.1 Departure from Nucleate Boiling Ratio

The margin of the DNB in the core is expressed in terms of the DNBR. The DNBR is defined as the ratio of the heat flux required to produce departure from nucleate boiling at the calculated local coolant conditions to the actual local heat flux.

→(EC-9533, R302; EC-13881, R304; EC-30663, R307)

Starting with Cycle 17, the DNB correlations used for design of the core are the ABB-NV correlation[24] and the WSSV-T correlation[23] for NGF assemblies. Based on statistical evaluation of the ABB-NV and WSSV-T correlations and relevant data, it is concluded that the appropriate minimum DNBR values are 1.13 (ABB-NV) and 1.12 (WSSV-T).

NRC evaluation of the uniform axial power distribution data resulted in their concluding that the CE-1 critical heat flux correlation[1][2], when coupled with the TORC code, provides an acceptable correlation of uniform axial CHF data and that the minimum acceptable DNBR is 1.19.[4]

←(EC-9533, R302; EC-13881, R304; EC-30663, R307)

→(DRN 00-644; EC-9533, R302; EC-13881, R304)
Therefore, the minimum DNBR used for design is 1.19. Table 4.4-1 gives the value of minimum DNBR for the coolant conditions and engineering factors in the table, for the radial power distributions in Figures 4.4-1 and 4.4-2, and for the 1.26 peaked axial power distribution in Figure 4.4-3. Values of minimum DNBR or maximum fuel temperature at the design overpower cannot be provided with any meaning. The concept of a design overpower is not applicable for Waterford 3 since the Reactor Protective System prevents the design limits from being exceeded.
←(DRN 00-644; EC-9533, R302; EC-13881, R304)

A comparison of the minimum DNBRs computed using different correlations for the same power, flow, coolant temperature and pressure, and power distribution is presented in Table 4.4-2. The minimum DNBR values in both the limiting matrix subchannel and the limiting subchannel next to the guide tube are presented. The correlations compared are the CE-1 correlation, the original W3 correlation,[3] the revised W3 correlation[5] and the B&W-2 correlation.[5] The differences between the original and revised W3 correlations as used here are in the C-factor and the cold wall correction factor.
→(DRN 00-644)
Additional comparisons are contained in CENPD-162[1]. In general, the CE-1 correlation predicts lower values of CHF than the B&W-2 Correlation, with the differences increasing with increasing inlet subcooling. In comparison with the W3 Correlation, the CE-1 Correlation tends to predict lower values of Critical Heat Flux (CHF) with high inlet subcooling and higher values of CHF with low inlet subcooling.
←(DRN 00-644)
The TORC computer code[6] is used to compute the local coolant conditions in the core and thereby the minimum DNBR. A discussion of the CE-1 DNB correlation and the analytical methods is presented in Subsections 4.4.4.1 and 4.4.4.5.2, respectively.

4.4.2.2.2 Application of Power Distribution and Engineering Factors

Distribution of power in the core is expressed in terms of factors that define the local power per unit length produced by the fuel relative to the core average power per unit length produced by the fuel. The method to compute these factors, which describe the core power distribution, is discussed in Section 4.3. The energy produced in the fuel is deposited in the fuel pellets, fuel cladding, and the moderator and results in the generation of heat in those places. The fraction of energy deposited in the fuel pellet and cladding is called the fuel rod energy deposition fraction. Accordingly, the core average heat flux from the fuel rods is determined by multiplying the core power by the average fuel rod energy deposition fraction and then dividing by the total heat transfer area. The energy deposition fractions used for DNB analyses for the average and the hot fuel rods are given in Table 4.4-1.
→(DRN 00-644)
The effects on the local heat flux and subchannel enthalpy rise of within tolerance deviation from nominal dimensions and specifications are included in thermal margin analyses by certain factors called engineering factors. These factors are applied to increase the local heat flux at the location of minimum DNBR and to increase the enthalpy rise in the subchannel adjacent to the rod with the minimum DNBR. Diversion crossflow and turbulent interchange mixing are not input as factors on subchannel enthalpy rise but are explicitly treated in the TORC code analytical model.

→(EC-9533, R302; EC-13881, R304; EC-30663, R307)
Cycle 16 is a mixed core consisting of standard fuel assemblies and NGF assemblies. Since NGF assemblies are more resistant to flow because of mixing vane spacer grids as compared to standard fuel assemblies, the hydraulic characteristics of these two types of fuel assemblies are modeled explicitly in TORC thermal-hydraulic calculations of coolant pressure drop and cross-flow between assemblies. Uncertainties in the power distribution factors are discussed in Subsection 4.4.2.9.4. Starting with Cycle 17, the core consists of a full core of NGF assemblies as stated previously. The ABB-NV critical heat flux correlation is used in the non-mixing vane region and the WSSV-T correlation is used in the mixing vane region.
←(DRN 00-644; EC-9533, R302; EC-13881, R304; EC-30663, R307)

4.4.2.2.2.1 Power Distribution Factors

a) Rod Radial Power Factor

The rod radial power factor is the ratio of the average power per unit length produced by a particular fuel rod to the average power per unit length produced by the average powered fuel rod in the core. The maximum rod radial power factor is the ratio of the average power per unit length produced by the highest powered rod in the core to the average power per unit length produced by the average powered fuel rod in the core. Radial power distributions are dependent upon a variety of parameters (control rod insertion, power level, fuel exposure, etc.). The core wide and hot assembly radial power distributions used for this analysis are shown in Figures 4.4-1 and 4.4-2. The maximum rod radial power factor for those figures is selected as 1.55 for better comparisons with Pilgrim Station Unit 2. The actual maximum rod radial power factor in the core will normally be lower; but it is not limited to a maximum value of 1.55. The only limits are those specified in Subsection 4.4.1. The protective system in conjunction with the reactor operator utilizing the Core Operating Limit Supervisory System (COLSS) ensures that those design limits are not violated.

b) Axial Power Factor

The axial power factor is the ratio of the local power per unit length produced by a fuel rod to the average power per unit length produced by the same fuel rod. The maximum axial power factor is the ratio of the maximum local power per unit length produced by a rod to the average power per unit length produced by the same fuel rod. The axial power distribution directly affects DNBR.

Typically, the farther the peak heat flux is from the core inlet, the lower the value of the peak heat flux needed to reach the DNBR limit. On the other hand, fuel temperature is almost independent of the location of the peak heat flux and is principally dependent on the value of the peak heat flux or linear heat rate. The axial power distribution and the maximum rod dial power factor are continuously determined and processed through the COLSS and the RPS such that the design basis limits are not exceeded. Section 4.3 describes the power distributions and their control. Figure 4.4-3 shows several axial power distributions and their control. Figure 4.4-3 shows several axial power distributions used for this analysis. The minimum DNBR in Table 4.4-1 is determined using the 1.26 peaked axial power distribution whereas the maximum heat fluxes are determined using the 1.47 peaked axial power distribution.

c) Nuclear Power Factor

The nuclear power factor is the ratio of the maximum local power per unit length produced in the core to the average power per unit length produced by the average powered fuel rod in the core. It is identical to the product of the maximum axial and radial power factors. For better comparisons with Pilgrim Station Unit 2, a value of 2.28 is selected for computing maximum heat fluxes. The actual value of the nuclear power factor will normally be lower throughout the cycle; but it is not limited to a maximum value of 2.28. The design limits are those specified in Subsection 4.4.1. The protective and supervisory systems assure that those design limits are not violated.

d) Total Heat Flux Factor

The total heat flux factor is the ratio of the local fuel rod heat flux to the core average fuel rod heat flux. The effects of fuel densification are not included in this factor. To determine the maximum local heat flux including the effect of gaps occurring between the fuel rod pellets, the augmentation factor should be applied. From this definition the total heat flux factor is the product of the nuclear power factor, the engineering heat flux factor, and the ratio of the hot to the average rod energy deposition fractions. The total heat flux factor is given in Table 4.4-1.

e) Augmentation Factor

→(DRN 00-644)

The densification of the fuel may lead to axial gaps in the fuel pellet stacks and can cause increased localized power peaking. This effect is expressed in terms of the augmentation factor which is defined as the ratio of the local heat flux to the unperturbed heat flux. The axial length of the localized power perturbation is called the gap length. Maximum values of the augmentation factor and gap length are given in Table 4.4-1. The effect of this factor on DNBR is discussed in Subsection 4.4.2.2.3.

←(DRN 00-644)

4.4.2.2.2.2 Engineering Factors

a) Engineering Heat Flux Factor

The effect on local heat flux due to normal manufacturing deviations from nominal design dimensions and specifications is accounted for by the engineering heat flux factor. Design variables that contribute to this engineering factor are initial pellet density, pellet diameter, and clad outside diameter.

→(EC-13881, R304)

These variables are combined statistically to obtain the engineering heat flux factor. The design value used for the engineering heat flux factor is based on deviations obtained from fuel manufacturing inspection data for over 25 batches of fuel for previous reactor cores. Similar tolerances and quality control procedures are used for Waterford 3, and as built fuel manufacturing data have been used to confirm that the factor given in Table 4.4-1 is conservative. The engineering heat flux factor is applied to the rod with the minimum DNBR and increases the heat flux when calculating DNBR.

←(EC-13881, R304)

→(EC-13881, R304)

It does not affect the enthalpy rise in the subchannel; the effect on the enthalpy rise in the subchannel due to normal manufacturing deviations from normal design dimensions and specifications is accounted for by the engineering enthalpy rise factor.

←(EC-13881, R304)

b) Engineering Factor on Linear Heat Rate

The effect of local linear heat rate due to deviations from nominal design dimensions and specifications is accounted for by the engineering factor on linear heat rate. Except for the clad outside diameter, the design variable that contribute to this factor are the same as those for the engineering heat flux factor. A value of 1.03 is applicable for the engineering factor on linear heat rate for Waterford 3.

c) Engineering Enthalpy Rise Factor

→(DRN 00-644)

The engineering enthalpy rise factor accounts for the effects of normal manufacturing deviations in fuel fabrication from nominal dimensions or specifications on the enthalpy rise in the subchannel adjacent to the rod with the minimum DNBR. Tolerance deviations (averaged over the length of the fuel rods that adjoin the subchannel) for fuel pellet density, enrichment, and diameter contribute to this factor. As-built fuel manufacturing data have been used to confirm that the factor given Table 4.4-1 is conservative.

The engineering enthalpy rise factor is applied by multiplying by the factor, the rod radial power factor of each of the fuel rods adjacent to the subchannel adjoining the rod with the minimum DNBR (see Figure 4.4-2). This increases the enthalpy rise in the subchannels which adjoin the same fuel rods.

←(DRN 00-644)

d) Pitch and Bow Factor

The pitch and bow factor is an allowance for the effect on enthalpy rise of the possible decreased flow rate in the subchannel resulting from a smaller than nominal subchannel flow area.

The pitch and bow factor given in Table 4.4-1 is applied by multiplying by the factor, the incremental enthalpy rise in the subchannel adjacent to the rod with the minimum DNBR (see Figure 4.4-2). This increases the enthalpy rise in that subchannel in the same manner as does the engineering enthalpy rise factor, but does not directly affect the heat input into the surrounding subchannels. The combined effects of divergent crossflow and turbulent interchange resulting from the higher heat input and enthalpy rise are computed by the TORC code. Additional discussions of fuel and poison rod bowing are presented in CENPD-225.[20]

4.4.2.2.3 Fuel Densification Effect on DNBR

The perturbation in local heat flux due to fuel densification is given in Table 4.4-1.

As shown in CENPD-207 (See Subsection 4.4.4.1), even much larger local heat flux variations have no significant adverse effect on DNB in Waterford 3 fuel assembly. Therefore, no specific allowance is made or required for the effect on DNBR of local heat flux variations due to densification of the fuel.

4.4.2.3 Linear Heat Generation Rate

→(DRN 00-644)

The core average and maximum fuel rod linear heat generation rates are given in Table 4.4-1. The maximum fuel rod linear heat generation rate is determined by multiplying core average fuel rod linear heat generation rate by the product of the nuclear power factor, the engineering factor on linear heat rate, and the ratio of the hot to the average fuel rod energy deposition factors. The effects of fuel densification are not included in the maximum fuel rod linear heat generation rate presented in Table 4.4-1; although, to determine the maximum local linear heat generation rate including the effect of gaps occurring between the fuel pellets, the augmentation factor should be applied.

←(DRN 00-644)

4.4.2.4 Void Fraction Distribution

→(DRN 00-644; EC-13881, R304)

The core average void fraction and the maximum void fraction are calculated using the Maurer method.[7] The void fractions discussed below are value for the reactor operating conditions and engineering factors given in Table 4.4-1, for the radial power distributions in Figures 4.4-1 and 4.4-2, and for the 1.26 peaked axial power distribution in Figure 4.4-3. For these conditions, only subcooled boiling occurs in the core.

←(DRN 00-644; EC-13881, R304)

The core average void fraction is less than 0.1 percent. The local maximum void fraction is 1.3 percent and occurs at the exit of the subchannel adjacent to the rod with the minimum DNBR. The average exit void fractions and qualities in different regions of the core are shown in Figure 4.4-4 for the core radial power distribution shown in Figure 4.4-1. The axial distribution of void fraction and quality in the subchannel adjacent to the rod with the minimum DNBR is shown in Figure 4.4-5. The average void fraction in that subchannel is 0.2 percent.

4.4.2.5 Core Coolant Flow Distribution

The core inlet flow distribution is required as input to the TORC thermal margin core (refer to Subsection 4.4.4.5.2). The inlet flow distribution 4-loop operation was determined from a reactor flow model test. Descriptions of the model test and the resulting core inlet flow distribution are given in Subsection 4.4.4.2.1.

Intentional selective orificing is not used in the core design.

4.4.2.6 Core Pressure Drops and Hydraulic Loads

4.4.2.6.1 Reactor Vessel Flow Distribution

The design minimum coolant flow entering the four reactor vessel inlet nozzles is given in Table 4.4-1. The main coolant flow path in the reactor vessel and the core support barrel, through the flow skirt and lower support cylinder, up through the core support region and the reactor core, through the fuel alignment plate, and out through the two reactor vessel outlet nozzles. A portion of this flow leaves the main flow path as shown schematically in

Figure 4.4-6. Part of the bypass flow is used to cool the reactor internals in areas not in the main coolant flow path and to cool the CEAs. Table 4.4-3 lists the bypass flow paths and the percent of the total vessel flow that enters and leaves these paths.

The thermal margin calculations conservatively use the design maximum bypass flow of 2.6 percent of the total vessel flow as compared to the calculated bypass flow of 2.1 percent shown in Table 4.4-3.

4.4.2.6.2 Reactor Vessel and Core Pressure Drops

The irrecoverable pressure losses from the inlet to the outlet nozzles are calculated using standard loss coefficient methods which are verified by flow model tests (refer to Subsection 4.4.4.2.1).

Pressure losses at 100 percent power, the design minimum primary coolant flow, and an operating pressure of 2250 psia are listed in Table 4.4-4 together with the coolant temperature used to calculate each pressure loss. The calculated pressure losses include both geometric and Reynolds number dependent effects. The calculated nozzle-to-nozzle pressure loss, using the same methods as above, and the as-measured pressure loss on operating plants are in good agreement, (refer to Subsection 4.4.4.2.1).

4.4.2.6.3 Hydraulic Loads on Internal Components

The significant hydraulic loads which act on the reactor internals during steady state operation are listed in Table 4.4-5. These loads are derived from analyses which make use of reactor flow model and components test results (refer to Subsection 4.4.4.2.1 and 4.4.4.2.2, respectively). All hydraulic loads in Table 4.4-5 are based on 120 percent of the design minimum primary coolant flow and a coolant temperature of 500 F.

→(DRN 03-2058, R14; EC-13881, R304)
When other coolant conditions and core power levels result in more limiting loading for individual components, the loads in Table 4.4-5 are adjusted in the detailed design analysis. For the power uprate to 3716 MWt, adjustments of this nature have been made to the hydraulic loads for use as input to the component stress analyses. The detailed design considers the steady state drag and impingement loads and the fluctuating loads induced by pressure pulsations, turbulence, and vortex shedding.
←(DRN 03-2058, R14; EC-13881, R304)

Hydraulic loads for postulated accident conditions are discussed in Subsection 3.9.2.5.

4.4.2.7 <u>Correlations and Physical Data</u>

4.4.2.7.1 Heat Transfer Coefficients

The correlations used to determine cladding temperatures for non-boiling forced convection and nucleate boiling are discussed here. The surface temperature of the cladding is dependent on the axial and radial power distributions, the temperature of the coolant, and the surface heat transfer coefficient.

The surface heat transfer coefficient for non-boiling forced convection is obtained from the Dittus-Boelter correlation[8] where fluid properties are evaluated at the bulk condition.

$$h_{db} = \frac{0.023k}{De} (N_R)^{0.8} \; (N_{Pr})^{0.4}$$

where:

h_{db} = Heat transfer coefficient, Btu/hr-ft^2-F

k = Thermal conductivity, Btu/hr-ft-F

De = Equivalent diameter = 4A/P_w, ft

N_R = Reynolds number, based on the equivalent diameter and coolant properties evaluated at the local bulk coolant temperature.

N_{Pr} = Prandt1 number, based on coolant properties evaluated at the local bulk coolant temperature.

A = Cross-sectional area of flow subchannel, ft^2.

P_w = Wetted perimeter flow subchannel, ft.

No specific allowance is made or considered necessary for the uncertainties associated with the Dittus-Boelter Correlation because the Dittus-Boelter Correlation is not used directly in computing thermal margin, but rather plays a part in determining pressure drop and cladding temperature. The validity of the overall scheme for predicting pressure drop is shown by the excellent agreement between predicted and experimental values obtained during the DNB test program and described in Subsection 4.4.4.1. The uncertainty associated with the cladding temperatures calculated for single phase heat transfer is not a major concern because the limiting fuel and cladding temperatures occur where the cladding-to-coolant heat transfer is by nucleate boiling.

The temperature drop across the surface film is calculated from:

$$\Delta T_{film} = q''/h_{db}$$

where:

q" = fuel rod surface heat flux, Btu/hr-ft^2

The maximum fuel rod heat flux is the product of the core average fuel rod heat flux and the total heat flux factor (refer to Table 4.4-1 and Subsection 4.4.2.2.2). At the location of maximum heat flux, nucleate boiling may occur on the clad surface. In the nucleate boiling regime, the surface temperature of the cladding is determined from the Jens and Lottes correlation: [9]

$$T_{wall} = T_{sat} + 60\ (q" \times 10^{-6})^{0.25}\ [\exp(-P/900)]$$

where:

P = Pressure, psia

q" = Defined above

T_{sat} = Saturation temperature, F

Nucleate boiling is assumed to exist if T_{wall} is less than the sum of $T_{coolant}$ plus ΔT_{film}.

The cladding surface temperature is calculated by summing the temperature of the coolant at the particular location and the temperature drop across the surface film, or if nucleate boiling is occurring, it is calculated directly from the Jens and Lottes correlation.

4.4.2.7.2 Core Irrecoverable Pressure Drop Loss Coefficients

Irrecoverable pressure losses through the core result from friction and geometric changes. The pressure losses through the lower and upper end fittings are calculated using the standard loss coefficient method and are verified by test (refer to Subsection 4.4.4.2.2). The correlations used to determine frictional and geometric losses in the core are presented in Subsection 4.4.4.2.3.

4.4.2.7.3 Void Fraction Correlations

There are three separate void regions to be considered in flow boiling. Region 1 is highly subcooled in which a single layer of bubbles develops on the heated surface and remains attached to the surface. Region 2 is a transition region from highly subcooled to bulk boiling where the steam bubbles detach from the heated surface. Region 3 is the bulk boiling regime.

The void fraction in Regions 1 and 2 is predicted using the Maurer method.(7) The calculation of the void fraction in the bulk boiling regime is discussed in Subsection 4.4.4.2.3.

4.4.2.8 <u>Thermal Effects of Operational Transients</u>

→(EC-13881, R304)

Design basis limits on DNBR and fuel temperature are established to assure that thermally induced fuel damage will not occur during steady-state operation and during anticipated operational occurrences. The COLSS provides information to the operator so he can assure that proper steady-state conditions exist. The RPS ensures that the design limits are not violated. The COLSS provides the reactor operator with a comparison of the actual core operating power to the licensed power and to the limiting powers based on DNBR and local power density. If the operating power reaches one of the limiting powers, an alarm is sounded. These limits are calculated by COLSS to provide sufficient margin not to exceed the design basis limits in the event the most limiting anticipated operational occurrence occurs simultaneously with the operating power being at the limiting power in steady state.

←(EC-13881, R304)

→(EC-13881, R304)
The COLSS thermal margin algorithm is an analytical approximation to the standard thermal margin design methods described in Subsection 4.4.4.5.2.
←(EC-13881, R304)

Approximations take the form of tabular data replacing complex algebraic functions as are used in the design code (for instance, fluid property routines). As such, there exist small random and systematic differences in the computed results from the two methods when comparison is made for identical initial conditions. Any non-conservatisms in these differences are accommodated by the following procedure:

A large number of cases are evaluated by both the design code and the thermal margin algorithm. The cases simulate a wide range of initial conditions expected in plant operation as allowed by the COLSS. A penalty factor is applied to the COLSS algorithm computed core power limit. This penalty accounts for the difference in the computed core power limit between the design code results and that of the algorithm results as determined from the cases discussed above. In this manner, the COLSS thermal margin algorithm is biased to give acceptable values of overpower compared to results calculated by the design analytical method.

Measurement uncertainties and calculational uncertainties are applied in a conservative manner in the COLSS calculation of the core operating power and the COLSS calculation of the core power limits. These uncertainties are discussed further in Subsection 7.7.1.3.4.

For automatic protection of the core, the RPS is designed to effect a rapid shutdown in the event that the thermal-hydraulic design limits are approached.

The core minimum DNBR and maximum local power density are determined by a core protection calculator (CPC), which uses core parameters either measured or calculated as input.

For the protective system, a DNB algorithm provides a rapid online calculation of DNBR. This algorithm, like the standard core analytical technique, uses the following core parameters either measured or calculated as input: core inlet temperature, pressure, flow, power, and power distribution. The CPC assessment of minimum DNBR is biased, in a manner similar to that of the overpower calculation performed in the COLSS, to give acceptable DNBRs compared to results calculated by the design analytical method.

Additional information concerning the supervisory and protective systems is contained in Sections 7.7 and 7.2, respectively, and additional discussion on the effects of thermal transients on waterlogged fuel elements is contained in Subsection 4.2.3. Analysis of anticipated operational occurrences to demonstrate that fuel design bases are met is presented in Chapter 15.

4.4.2.9 Uncertainties in Estimates

4.4.2.9.1 Pressure Drop Uncertainties

The reactor vessel pressure losses in Table 4.4-4 are the best estimate values calculated for the design minimum flow with standard loss coefficient methods. The uncertainties in the correlations for the loss coefficients and the dimensional uncertainties on the reactor vessel and internals are accounted for when determining maximum and minimum vessel hydraulic resistance. The uncertainties are estimated to be equivalent to approximately ± 10 percent of the best estimate vessel pressure loss.

4.4.2.9.2 Hydraulic Loads Uncertainties

→(DRN 00-644)

The hydraulic loads for the design of the internals, Table 4.4-5, are based on 120 percent of the design minimum flowrate (see Subsections 4.4.1.4 and 4.4.4.5.1).

←(DRN 00-644)

4.4.2.9.3 Fuel and Clad Temperature Uncertainty

Uncertainty in the ability to predict the maximum fuel temperature is a function of gap conductance, thermal conductivities, peak linear heat rate, and heat generation distribution. Uncertainties in gap conductance and thermal conductivity are taken into account in the analytical model. Uncertainties in the peak linear heat rate are accounted for by including the uncertainty in estimating the total nuclear peak and by including the uncertainties in fuel pellet density, enrichment, and pellet diameter expresses by the engineering factor on linear heat rate (Subsection 4.4.2.2.2).

Uncertainty in predicting the cladding temperature at the location of maximum heat flux is the uncertainty in the film temperature drop, which is minimal at this location where nucleate boiling occurs.

4.4.2.9.4 DNBR Calculation Uncertainties

a) The uncertainty in the calculation of minimum DNBR is divided into:

1) The uncertainty in the input to the core analytical model, the TORC code. This includes the core geometry, power distribution, inlet flow and temperature distribution, exit pressure distribution, single phase friction factor constants, spacer grid loss coefficients, divergent crossflow resistance and momentum parameters, turbulent interchange constants, and hot fuel rod energy deposition fraction.

2) The uncertainty in the analytical model to compute the actual distribution of flow and the local subchannel coolant conditions.

→(EC-9533, R302; EC-13881, R304)

3) The uncertainty in the CE-1 correlation for standard fuel assemblies and the WSSV-T and ABB-NV correlations for NGF assemblies to predict DNB.

←(EC-9533, R302; EC-13881, R304)

b) The following paragraphs discuss the above uncertainties and the allowances for them, if needed, in the thermal margin analysis of the core:

1) Uncertainty in the input to the core analytical model:

(a) Uncertainty in core geometry, as manifested by manufacturing variations within tolerances, is considered by the inclusion of engineering factors in the DNBR analyses; see Subsection 4.4.2.2.2 for a discussion of the method used to compute conservative values.

→ (DRN 00-644)

(b) Uncertainties on the power distribution factors are applied in the COLSS and RPS (see Subsection 7.7.1.3.4).

← (DRN 00-644)

(c) The non-uniformity of the core inlet flow distribution is obtained from flow model testing discussed in Subsection 4.4.4.2, and is included in the design method for TORC analyses - see Subsection 4.4.4.5.2.

(d) Non-uniformities in the core exit pressure distribution are included in the design method for TORC analyses - see Subsection 4.4.4.5.2.

(e) The Blasius single-phase friction factor equation for smooth rods is given and shown to be valid in Subsection 4.4.4.2.3. The spacer grid loss coefficient for the standard grid is obtained from pressure drop data discussed in Subsection 4.4.4.2.3.

(f) The value of minimum DNBR is relatively insensitive to crossflow resistance and momentum parameters.[6]

(g) Subsection 4.4.4.1 describes the testing to determine the inverse Peclet number which is indicative of the turbulent flow interchange between subchannels. The inverse Peclet number is input to the TORC code and is used to determine the effect of turbulent interchange on the enthalpy rise in adjacent subchannels. From the testing, a value of 0.0035 is justified.

(h) The same fuel rod energy deposition fraction is used for the hot rod as for the average rod. The hotter the rod, the lower is the actual value of energy deposition fraction with respect to that for the average rod. A lower energy deposition fraction reduces the hot rod heat flux and thereby increases its DNBR. The use of the average rod energy deposition fraction for the hot rod is therefore conservative. See Section 4.3 for a discussion of the calculation of the energy deposition fractions.

2) Uncertainty in the analytical model:

The ability of the TORC code to predict accurately subchannel local conditions in rod bundles is described in CENPD-161.[6] The ability of the code to predict accurately the core wide coolant conditions is described in CENPD-206.[10]

3) Uncertainty in the DNB correlation:

→(EC-9533, R302; EC-13881, R304; EC-30663, R307)

The uncertainty in the DNB correlation is determined by a statistical analysis of DNB test data. A value of 1.19 for the CE-1 correlation, 1.12 for the WSSV-T correlation, and 1.13 for the ABB-NV correlation has been shown to provide a 95 percent probability with 95 confidence that DNB will not occur on a fuel rod having that minimum DNB.[1][2][23][24]

←(EC-9533, R302; EC-13881, R304; EC-30663, R307)

4.4.2.10 Flux Tilt Considerations

An allowance for degradation in the power distribution in the x-y plane (commonly referred to as flux tilt) is provided in the protection limit set points even though little, if any, tilt in the x-y plane is expected.

The tilt, along with other pertinent core parameters, are monitored during operation by the COLSS (described in Section 7.7). If the core margins are not maintained, the COLSS actuates an alarm, requiring the operator to take corrective action. The CPCs actuate a trip if limiting safety system settings are reached.

The thermal margin calculations used in designing the reactor core are performed using the TORC code. The TORC code, which is described in Subsection 4.4.4.5.2, is based on an open core analytical method for performing such calculations and treats the entire core on a three-dimensional basis. Thus, any asymmetry or tilt in the power distribution is analyzed by providing the corresponding power distribution in the TORC input.

4.4.3 DESCRIPTION OF THE THERMAL AND HYDRAULIC DESIGN OF THE REACTOR COOLANT SYSTEM (RCS)

A summary description of the RCS is given in Section 5.1.

4.4.3.1 <u>Plant Configuration Data</u>

4.4.3.1.1 Configuration of the RCS

An isometric view of the RCS is given in Figure 4.4-7.

Table 4.4-6 lists the valves and pipefittings which form part of the RCS.

Table 4.4-7 lists the design minimum flow through each flowpath in the RCS.

Table 4.4-8 provides the volume, minimum flow area, flowpath length, height and liquid level of each volume, and bottom elevation for each component within the RCS.

The line lengths and sizes of the safety injection lines are given in Table 4.4-9 and Figure 4.4-8 (for Figure 4.4-8, Sheet 3, refer to Drawing G167, Sheet 3).

Table 5.1-1 provides a steady-state pressure, temperature, and flow distribution throughout the RCS.

4.4.3.2 Operating Restrictions on Pumps

The minimum RCS pressure at any given temperature is limited by the required net positive suction head (NPSH) for the reactor coolant pumps during portions of plant heatup and cooldown. To ensure that the pump NPSH requirements are met under all possible operating conditions, an operating curve is used which gives permissible RCS pressure as a function of temperature.

→ (DRN 00-644)

The reactor coolant pump NPSH restriction on this curve is determined by using the NPSH requirement for one pump operation (maximum flow, hence, maximum required NPSH) and correcting it for pressure and temperature instrument errors and pressure measurement location. The NPSH required versus pump flow is supplied by the pump vendor. Plant operation below this curve is prohibited. At low reactor coolant temperatures and pressures, other considerations require that the minimum pressure versus temperature curve be above the NPSH curve.

← (DRN 00-644)

4.4.3.3 Power Flow Operating Map (BWR)

This subsection is not applicable.

4.4.3.4 Temperature - Power Operating Map (PWR)

Reactor operation at power with one, two, or three pumps operating, or while in natural circulation is not allowed. However, decay heat may be transferred to the steam generator in any of the above cases. A temperature-power operating map (temperature control program) is provided in Subsection 5.4.10.

The adequacy of natural circulation for decay heat removal after reactor shutdown has been verified analytically and by tests on the Palisades reactor (Docket No. 50-255). The core ΔT in the analysis has been shown to be lower than the normal full power ΔT; thus the thermal and mechanical loads on the core structure are less severe than normal design conditions.

To assess the margin available in a post-coastdown situation, a study was made assuming termination of pump coastdown 100 seconds after reactor trip, with immediate flow decay to the stable natural circulation condition. It should be recognized that pump rotation will continue for substantially longer than 100 seconds. With the maximum decay heat load 100 seconds after trip, the system will sustain stable natural circulation flow adequate to give a thermal power-to-flow ratio of less than 0.9. This power-to-flow ratio was verified by tests completed on the Palisades reactor (Docket No. 50-255), the Omaha reactor (Docket No. 50-285), the Maine Yankee reactor (Docket No. 50-265) and the Calvert Cliffs I reactor (Docket No. 50-317).

Heat removed from the core during natural circulation may be rejected either by dumping to the main condenser or to the atmosphere; the rate of heat removal may be controlled to maintain core ΔT within allowable limits.

The flowrate through the reactor vessel is calculated by use of a computer code called COAST.(11) COAST predicts flow in the RCS with any combination of active and inactive pumps in a two-loop, four-pump plant. Momentum balances are performed on all the flow paths. Frictional losses, shock losses, the operating pump(s) head-flow characteristic curve(s), and an experimentally derived reverse flow, locked rotor, loss coefficient for the nonoperating pump(s) are utilized in determining the unique flow distribution through the system.

4.4.3.5 Load Following Characteristic

The design features of the RCS influence its load following and transient response. The RCS is capable of following the normal condition transients identified in Subsection 3.9.1.1. These requirements are considered when sizing the pressurizer spray and heater capacities and control setpoints. The charging/letdown system control setpoint are selected through detailed computer simulation studies. The Reactor Regulating System (RRS) reactivity insertion rate is also based on these requirements. In addition, the feedwater regulating system control setpoints are selected through computer analysis of these transients. Finally, these transients are included in the equipment specification for each RCS component to ensure the structural integrity of the system.

Load changes are initiated by adjustment of the Turbine Control System load reference setpoint which positions the turbine admission valve. The RRS senses a change in the turbine first stage pressure and positions CEAs to attain the appropriate coolant average temperature. The feedwater regulating system employs a three-element controller which senses changes in steam flow, feed flow, and water level and acts to maintain steam generator level at the desired point.

The pressurizer pressure and level control systems respond to deviations from preselected setpoints caused by the expansion or contraction of the reactor coolant and actuate the spray or heaters and the charging or letdown systems as necessary to maintain pressure and coolant volume.

4.4.3.6 Thermal and Hydraulic Characteristics Table

Principal thermal hydraulic characteristics of the RCS components are listed in Table 4.4-10.

4.4.4 EVALUATION

4.4.4.1 Critical Heat Flux

The margin to critical heat flux (CHF) or DNB is expressed in terms of the DNBR. The DNBR is defined as the ratio of the heat flux required to produce DNB at the calculated local coolant conditions to the actual heat flux.

→(EC-9533, R302; EC-13881, R304; EC-30663, R307)
The CE-1 Correlation[(1)(2)] for standard fuel assemblies and the WSSV-T and ABB-NV correlations[(23)(24)] for NGF assemblies were used with the TORC computer code[(6)] to determine DNBR values for normal operation and anticipated operational occurrences. Topical Reports CENPD-162[(1)] and CENPD-207[(2)] provide detailed information on the CE-1 correlation and source data, and also provide comparisons with other data and correlations. Topical reports WCAP-16523-P-A[(23)] and CENPD-387-P-A[(24)] provides detailed information on the WSSV-T CHF correlation and the ABB-NV CHF correlation.

The CE-1 correlation was developed in conjunction with the TORC code specifically for DNB margin predictions for fuel assemblies with standard spacer grids similar to those previously deployed in Waterford 3.
←(EC-9533, R302; EC-13881, R304; EC-30663, R307)

→(EC-9533, R302; EC-13881, R304; EC-30663, R307)
In brief, the CE-1 correlation is based on data from tests conducted for C-E at the Chemical Engineering Research Laboratories of Columbia University. Those tests used electrically-heated five x five array rod bundles corresponding dimensionally to a portion of a 16 x 16 or 14 x 14 assembly geometries each included tests to determine the effects on DNB of the CEA guide tube, bundle heated length, axial grid spacing, and lateral and axial power distributions.
←(EC-9533, R302; EC-13881, R304; EC-30663, R307)

The uniform axial power CE-1 Correlation[1] was developed from DNB data for six tests sections with the following characteristics:

Fuel Assembly Geometry	No. Heated Rods	Lateral Power Distr	Heated Length (ft.)	Axial Grid Spacing (in.)
16 x 16	25	Uniform	7	16.0
16 x 16	21	Nonuniform	7	18.3
16 x 16	21	Nonuniform	12.5	17.4
14 x 14	25	Uniform	7	14.3
14 x 14	21	Nonuniform	7	14.3
14 x 14	21	Nonuniform	12.5	14.3

Local coolant conditions at the DNB location were determined by using the TORC code in a manner consistent with the use of the code for reactor thermal margin calculations. The uniform axial power CE-1 correlation was developed from 731 DNB data for the following parameter ranges:

Pressure	1785 to 2415 psia
Inlet temperature	382 to 644F
Heat flux	0.213×10^6 to 0.952×10^6 Btu/hr-ft^2
Local coolant quality	-0.16 to 0.20
Local mass velocity	0.87×10^6 to 3.21×10^6 lb/hr-ft^2

The uniform axial power CE-1 correlation predicted the 731 source data with a mean and standard deviation of the ratio of measured and predicted DNB heat fluxes of 1.000 and 0.068, respectively. The validity of the CE-1 correlation for predicting DNB for 16 x 16 fuel assemblies was further verified by the analysis data obtained by repeating one of the tests for the 16 x 16 assembly geometry at the Winfrith Laboratory of the UKAEA.

For nonuniform axial power distributions the uniform axial power CE-1 correlation is modified by the F-factor[5]. The conservatism of that method of predicting DNB for 16 x 16 fuel assemblies with nonuniform axial flux shapes is demonstrated in CENPD-207[2]. CENPD-207[2] presents measured and predicted DNB heat fluxes for a series of tests using nonuniform axial power rod bundles representative of 16 x 16 or 14 x 14 fuel assemblies utilizing standard spacer grids. Those test sections had the following characteristics.

Fuel Assembly Geometry	No. Heater Rods	Lateral Power Distr	Axial Power Distr	Heated Length (ft.)	Axial Grid Spacing (in.)
16 x 16	21	Nonuniform	1.46 symmetric	12.5	14.2
16 x 16	21	Nonuniform	1.47 top peak	12.5	14.2
14 x 14	21	Uniform	1.68 top peak	12.5	17.4
14 x 14	21	Nonuniform	1.68 bottom peak	12.5	17.4

The DNB data from those tests were evaluated using the CE-1 correlation modified by the F-factor and the TORC code used in a manner consistent with the use of the code for reactor calculations. That evaluation included DNB data within the following parameter ranges:

Pressure	1745 to 2425 psia
Inlet temperatures	333 to 631F
Local coolant quality	-0.27 to 0.20
Local mass velocity	0.81 to 10^6 to 3.07 x 10^6 lb/hr-ft^2

It was found that the mean and standard deviation of the ratio of measured and predicted DNB heat fluxes were 1.229 and 0.125, respectively, for the 369 DNB data within the parameter ranges mentioned above.

Testing was also conducted with rod bundles representative of the 16 x 16 fuel assembly to determine the effect on DNB of local perturbations in heat flux. Results are presented in CENPD-207(2) for two nonuniform axial power rod bundles which were similar except that one test bundle had a heat flux spike (23 percent higher heat flux for a four in. length) at the location where DNB anticipated. The results show that there is no significant adverse effect on DNB due to that flux spike. Therefore, it is concluded that no allowance is required for the effect on DNB of local heat flux perturbations less severe than that tested.

One important factor in the prediction of DNB and local coolant conditions is the treatment of coolant mixing or turbulent interchange. The effect of turbulent interchange on enthalpy rise in the subchannels of 16 x 16 fuel assemblies with standard spacer grids is calculated in the TORC code by:

$$P_e^{\Lambda} = \frac{\omega'}{\bar{G} \quad \bar{D}_e} = 0.0035$$

where:

P_e^{Λ} = inverse Peclet number

ω' = turbulent interchange between adjacent subchannels, lb/hr-ft

$\bar{D}_e$ = average equivalent diameter of the adjacent subchannels, ft

$\bar{G}$ = average mass velocity of the adjacent subchannel, lb/hr-ft^2

The value of 0.0035 for the inverse Peclet number for use with the 16 x 16 fuel assembly with standard spacer grids was originally chosen based on cold water dye mixing tests conducted for the 14 x 14 assembly and for a "prototype" of the Palisades reactor fuel assembly. The validity of the inverse Peclet number of 0.0035 for the 16 x 16 assembly with standards grids was verified with data obtained in the tests conducted at Columbia University(1).

The design basis requires that the minimum DNBR for normal operation and anticipated operational occurrences be chosen to provide a 95 percent probability at the 95 percent confidence level that DNB will not occur on a fuel rod having that minimum DNBR. Statistical evaluation of the CE-1 correlation and relevant data shows that appropriate minimum DNBR is 1.13.(1)(2) Based on review of CENPD-152(1), the NRC requires use of a minimum DNBR of 1.19. Therefore, the minimum DNBR used for design is 1.19.

4.4.4.2 Reactor Hydraulics

4.4.4.2.1 Reactor Flow Model Tests

Design values for the reactor hydraulic parameters are obtained or verified by means of flow model tests. These flow model tests involve the use of scale reactor models and are part of the C-E reactor development program. The test programs provide information on flow distribution in various regions of the reactor, pressure loss coefficients, hydraulic loads on vessel internal components, and turbulence-induced pressure and velocity fluctuations.

C-E's PWR designs fall into seven basic geometric configurations as shown below:

Configuration	Reactor(s)	Distinguishing Hydraulic Features
1	Palisades	Four inlets, two outlets, cruciform control rods, 204 fuel assemblies.
2	Fort Calhoun	Four inlets, two outlets, CEAS, 133 fuel assemblies
3	Maine Yankee	Three inlets, three outlets, CEAS, 217 fuel assemblies, 137-in. long core.
4	Calvert Cliffs 1 & 2 St. Lucie 1 & 2 Millstone (Unit 2)	Four inlets, two outlets, CEAS, 217 fuel assemblies, 137-in. long core.
5	Arkansas Nuclear One Unit 2 Blue Hills Station	Four inlets, two outlets, CEAS. 177 fuel assemblies, 150-in. long core.
6	San Onofre (Units 2 & 3) Forked River Waterford, Pilgrim	Four inlets, two outlets, CEAS, 217 fuel assemblies, 150-in. long core.

7	System 80	Four inlets, two outlets, 241 fuel assemblies, modified upper and lower plena design, 150-in. long core.

Flow model tests have been conducted on configurations one through four and six. The Palisades and Fort Calhoun flow tests were run under contract with Battelle Memorial Institute using air as the test medium. The Maine Yankee and the configuration four reactor flow model tests were performed in a 15,000 gal/min cold water facility in the C-E Nuclear Laboratories. The flow models for configuration one through four were 1/5 scale models that simulated the entire reactor, from inlet to outlet. These models had closed cores. Allow model test was also performed with a 115 scale water flow model of the configuration six geometry: This model has an open core.

The design hydraulic parameter for Waterford 3 were obtained from results of the configuration six model test and by extrapolating from the flow model tests on configurations one through four. Where interpolation was required, geometric differences between configuration six and the earlier reactor configurations were accounted for by analytical means and by utilizing the experience gained from the earlier tests, during which numerous investigations were made of the effect of various internal components on flow distribution and pressure drop.

The principal design hydraulic parameters include:

- The core inlet flow distribution
- Reactor pressure losses
- Hydraulic loads on reactor internal components

The approaches for deriving the design hydraulic parameters are described as follows:

a) Core Inlet Flow Distribution

→ (DRN 00-644)

The core inlet flow distribution is required as input to the TORC thermal margin computer code (refer to Subsection 4.4.4.5.2). A core inlet flow distribution was determined for four-loop operation from the 115 scale water flow model of the configuration six reactor. the resulting core inlet flow distribution shows quadrantal symmetry. Most centrally located fuel assemblies, located at least one row in from the core peripheral boundary, have higher than average flow rates. The peripheral fuel assemblies have lower than average flow rates. The flow distribution is described in CENPD-206 P([10]).

← (DRN 00-644)

b) Reactor Pressure Losses

Reactor vessel pressure losses are determined with a standard calculational model. This model was developed partly on the basis of pressure loss results from flow model tests on the earlier reactor configurations one through four.

The calculational model divides the flow path through the reactor into segments; the principal flow path segments are :

1) The inlet region

2) The downcomer region

3) The lower plenum region

4) The core support structure region

5) The core region

6) The upper plenum region.

→(DRN 00-644)

A combination of analytical or empirical relationships are used for each flow path segment in the standard pressure loss calculational method. When empirical relationships are used for a new reactor, the coefficient(s) from the originating model tests are modified by analytical means to account for geometry variations between the original reactor geometry and that for the new reactor geometry.

→(DRN 06-871, R15)

Agreement between predictions by the standard calculational method and experimental pressure losses is found to be good. For example, from flow model tests on configuration four, the agreement between predicted and measured values for the segmental losses was within 15 percent while the nozzle-to-nozzle pressure losses were found to be systematically high relative to the measured values. Comparisons have also been made between nozzle-to-nozzle pressure drops measured in two C-E reactors (configurations 1 and 3) and values predicted by the standard calculational method. These later comparisons show agreement within seven percent, again with the predicted values being higher than the measured values.

←(DRN 00-644; 06-871, R15)

The vessel pressure losses were estimated with the standard calculational method, taking into account the observed systematic differences between predicted and measured pressure losses.

The core pressure drop is increased by six psi in the calculation of design hydraulic loads to account for the possibility of core crudding. The six psi value was chosen on the basis that it provides a sufficient margin to accommodate core crudding effects. Experience with operating plants indicates that any increases in core pressure drop due to crudding effects are much smaller compared to the six psi design value.

c) Hydraulic Loads on Reactor Internal Components

Hydraulic loads were estimated on the basis of both experimental data from flow model tests on reactor configurations 1 through 4 and by analytical means. When experimental data are used, they are first reduced to dimensionless form in terms of a pressure difference coefficient.

$$E = \frac{P_{local} - P_{ref}}{\rho V_{ref}^{2} / 2g}$$

a force coefficient,

$$C_F = \frac{F}{\rho V_{ref}^{2} A_{ref} / 2g}$$

or a velocity ratio,

$$\frac{V_{local}}{V_{ref}}$$

The quantities with subscript "ref" represent appropriate reference values: for example, the average velocity or pressure at the particular flow path station of interest. These dimensionless quantities are then converted to absolute quantities by multiplying by the appropriate reference quantity (i.e., by $\rho V^2_{ref} / 2g$ or V_{ref}) for the reactor of interest.

Adjustment to the resulting absolute quantities are made by analytical means if there are substantial differences in geometry between the reactor configuration for which the test data were derived and the reactor configuration of interest.

Further discussion of the philosophy of flow model testing appears in CENPD-12.(12)

4.4.4.2.2 Components Testing

Components test programs have been conducted in support of all C-E reactors. The tests subject a full-scale reactor core module comprising one to four fuel assemblies, control rod assembly and extension shaft, control element drive mechanism, and reactor vessel internals to reactor conditions of water chemistry, flow velocity, temperature, and pressure under the most adverse operating conditions allowed by the design. Two objectives of the programs are to confirm the basis hydraulic characteristics of the components and to verify that fretting and wear will not be excessive during the components' lifetime. When the reactor design is revised, a new program embodying the important aspects of the latest design is conducted.

Thus, components tests have been run on the Palisades design, with cruciform control rods, on the Fort Calhoun design with CEAs and rack-and-pinion control element drive mechanisms (CEDM), and on the Maine Yankee design with a dual CEA and a magnetic jack CEDM. A components test program on a typical 16 x 16 fuel assembly, a CEA, and magnetic jack CEDM has been performed. The results apply to Waterford 3.

During the course of the tests, information is obtained on fuel rod fretting, on CEA/CEDM trip behavior, and on fuel assembly uplift and pressure drop. The first two subjects are discussed in Section 4.2. The third is discussed below.

As part of the assessment of fuel assembly margin to uplift in the reactor, measurements are made of the coolant velocity required to lift the fuel assembly for an isothermal temperature range of 150 to 600 F at a system pressure of 350 to 2100 psia. To obtain the desired information, a fuel assembly was mounted either on load beams or liftoff probes. These devices are used to indicate the liftoff of the fuel assembly. Data reduction involves the calculation of an uplift coefficient, describing the hydraulic uplift force acting on the assembly; the coefficient is defined as follows:

→ (DRN 00-644)

$$K_{up} = Wo/\gamma V^2 A/2g_c$$

← (DRN 00-644)

where:

Wo = Wet weight of assembly with no flow, lb

V = Flow velocity in assembly at the point of liftoff, ft/sec

A = Envelope area of assembly, ft^2

→ (DRN 00-644)

γ = Water density, lb/ft^3

← (DRN 00-644)

A plot of the K_{up} data shows that they can be fitted by the relation:

$$K_{up} = \alpha N_R^{-B}$$

where α and β are peculiar to the particular components test being run and where the standard error of estimate is typically about 4 percent, including replication and instrument error.

→ (DRN 00-644)

The uplift coefficient and its associated uncertainty are employed in the analysis of the uplift forces on the fuel assemblies in the reactor. The force is determined at the least favorable location hydraulically for startup and steady-state operating conditions. Additional input to the calculation includes analytical corrections to the coefficient for the absence of the CEA, for crud formation, and for small geometrical differences among the fuel assemblies for the different reactor designs all nominally describable by the same components tests.

← (DRN 00-644)

Pressure drop measurements are also made during the components test program to verify the accuracy of the calculated loss coefficients for various fuel assembly components. Direct reduction of the pressure drop data yields the loss coefficients for the lower and upper end fitting region, while the rod friction loss from the measured pressure drop across the fuel rod region.

Loss coefficients for the upper and lower end fittings and spacer grids on the Waterford 3 fuel have been obtained from flow testing of the 16 x 16 fuel. These data have been provided to the NRC in Reference 21.

4.4.4.2.3 Core Pressure Drop Correlations

The total pressure drop along the active fuel region of the core is computed as the sum of the individual losses resulting from friction, acceleration of the fluid and change in elevation of the fluid and spacer grids. The individual losses are computed using the momentum equation and the consistent set of empirical correlations presented in the TORC code(6).

In the following paragraphs, the correlations used are summarized and the validity of the scheme is demonstrated with a comparison of measured and predicted pressure drops for single-phase and two-phase flow in rod bundles with CEA-type geometry.

For isothermal, single-phase flow, the pressure drop due to friction for flow along the bare rods is based on the equivalent diameter of the bare rod assembly and the Blasius friction factor:

$$f = 0.184 \; N_R^{-0.2}$$

The pressure drop associated with the spacer grids is computed using a grid loss coefficient (K_{SG}) given by a correlation which has the following form:

$K_{SG} = D_1 + D_2 \, (N_R)^{D_3} \pm$ Standard Error of Estimate

→(DRN 00-644)

The constants, D_n, are determined from pressure drop data obtained for a wide range of Reynolds Number for isothermal flow through CEA-type rod bundles fitted with standard spacer grids. The data comes from the DNB program (Subsection 4.4.4.1) and from the components test program (Subsection 4.4.4.2.2). The standard error of estimate associated with the loss coefficient relation includes replication and instrument error.

←(DRN 00-644)

To compute pressure drop either for heating without boiling or for sub cooled boiling, the friction factor given above for isothemal flow is modified through the use of the multipliers given in Pyle.[13] It is important to recognize that the multipliers were developed in such a way as to incorporate the effects of subcooled voids on the acceleration and elevation components of the pressure drop as well as the effect on the friction losses. Consequently, it is not necessary to compute specifically either a void fraction for subcooled boiling or the individual effects of subcooled boiling on the friction, acceleration, or elevation components of the total pressure drop.

→(DRN 00-644; EC-13881, R304)

The effect of bulk boiling on the friction pressure drop is computed using a curve fit to the Martinelli-Nelson data[14] above 2000 psia or the Martinelli-Nelson correlation[14] with the modification given in Pyle[13] below 2000 psia. The acceleration component of the pressure drop for bulk boiling conditions is computed in the usual manner for the case of two-phase flow where there may be a nonunity slip ratio.[15] The elevation and spacer grid pressure drops for bulk boiling are computed as for single phase flow except that the bulk coolant density $(\bar{\rho})$ is used, where:

←(DRN 00-644; EC-13881, R304)

$$\bar{\rho} = \alpha \rho_V + (1 - \alpha)\rho_1$$

and

α = bulk boiling void fraction

ρ_V = density of saturated vapor, lb/ft^3

ρ_1 = density of saturated liquid, lb/ft^3

The bulk boiling void fraction used in computing the elevation, acceleration, and spacer grid losses is calculated by assuming a slip ratio of unity if the pressure is greater than 1850 psia or by using the Martinelli-Nelson void fraction correlation(14) with the modifications presented in Pyle(13) if the pressure is below 1850 psia.

To verify that the scheme described above accurately predicts pressure drop for single-phase and two-phase flow through the 16 x 16 assembly geometry comparisons have been made of measured pressure drop and the pressure drop predicted by TORC,(6) for the rod bundles used in the DNB test program at Columbia University (refer to Subsection 4.4.4.1). Figure 4.4-9 shows some typical results for a 21-rod bundle of the 16 x 16 fuel assembly geometry (five x five array with four rods replaced by a control rod guide tube). The excellent agreement demonstrates the validity of the methods described above.

4.4.4.3 Influence of Power Distributions

The reactor operator, utilizing the COLSS, will restrict operation of the plant such that power distributions which are permitted to occur will have adequate margin to satisfy the design bases during anticipated operational occurrences. A discussion of the methods of controlling the power distributions is given in Subsection 4.3.2.4.2. A discussion of the expected power distributions is given in Subsection 4.3.2.2.3, and typical planar rod radial power factors and axial shapes are given in Figures 4.3-2 through 4.3-18. The full-power maximum rod radial power factor is taken as 1.55 and is used in the calculations of the core thermal margins which are given here in Section 4.4. Comparison with expected power distributions, discussed in Section 4.3, shows that this integrated rod radial power factor is at least 10 percent higher than all the calculated values and, therefore, is a meaningful value for thermal margin analyses.

→(DRN 02-1477)

If CEAs are inserted in the core, the same planar radial power distribution does not exist at each axial elevation of the core, nor does the same axial power distribution exist at each radial location in the core. From the analysis of many three-dimensional power distributions, the important parameters which establish the thermal margin in the core the maximum rod power and its axial power distribution.(10) Examination of many axial power distributions shows the 1.26 peaked axial power distribution in Figure 4.4-3 to be among those giving the lowest DNBRs. The combination of that axial shape and the maximum rod radial power factor of 1.55 is therefore a meaningful combination for DNB analyses. The maximum linear heat rate at a given power is determined directly from the core average fuel rod linear heat rate and the nuclear power factor. The value of 2.28 for the nuclear

←(DRN 02-1477)

power factor is selected and corresponds to the 1.55 rod radial power factor combined with the 1.47 peaked axial shape shown in Figure 4.4-3. As stated before, the supervisory and protective systems measure the maximum rod radial power factor and the axial power distribution in the core and ensure that the design limits specified in Subsection 4.4.1 are not violated.

4.4.4.4 Core Thermal Response

Steady-state core parameters are summarized in Table 4.4-1 for normal four pump operation. Figure 4.4-10 shows the sensitivity of the minimum DNBR to small changes in pressure, inlet temperature, and flow from the conditions specified in Table 4.4-1. The same 1.26 peaked axial power distribution and 1.55 maximum rod radial power factor are used.

The response of the core to anticipated operational occurrences is discussed in Chapter 15. The response of the core at the design over power cannot be presented with any meaning. The concept of a design overpower is not applicable for Waterford 3 since the RPS prevents the design basis limits from being exceeded.

The supervisory and protective systems will ensure that the design bases in Subsection 4.4.1 are not violated for any steady state operating condition of inlet temperature, pressure, flow, power, and core power distribution and for the anticipated operational occurrences discussed in Chapter 15.

4.4.4.5 Analytical Methods

4.4.4.5.1 Reactor Coolant System Flow Determination

The design minimum flow to be provided by the reactor coolant pumps is established by the required mass flow to result in no violation of the design limits in Subsection 4.4.1 during steady state operation and anticipated operational occurrences. This design minimum flow is specified in Table 4.4-1.

The reactor coolant pumps are designed to produce a flow greater than or equal to the design minimum flow for the maximum expected system flow resistance. The maximum system flow resistance is determined by adding an allowance for uncertainty to the best estimate system flow resistance. From this maximum system flow resistance, the required minimum reactor coolant pump head is determined.

Upon completion of the manufacturing and testing of the pumps, the characteristic pump head or performance curve is established. The expected maximum, best estimate, and minimum reactor coolant system flow rates are determined as follows:

a) Best Estimate Expected Flow

   The best estimate expected RCS flow is determined by equating the head loss around the reactor coolant flow path to the head rise supplied by the reactor coolant pumps (Subsection 5.4.1 has a description of the pumps).

b) Maximum Expected Flow

The maximum expected flow is determined in a manner analogous to the best estimate expected flow. A maximum pump performance curve for each pump is calculated from the uncertainty in flow measurement. This uncertainty is based on performance and acceptance testing done at the pump vendor's facility. The minimum pressure loss for the steam generator and piping is determined by subtracting 10 percent on best estimate friction losses and 20 percent on best estimate geometry losses. The minimum pressure loss for the reactor vessel is evaluated by considering the uncertainties in the correlations for the loss coefficients and the normal manufacturing deviations from nominal dimensions. The maximum expected flow results from the combination of the maximum pump curve and the minimum system resistance.

c) Minimum Expected Flow

The minimum expected flow is determined in a manner analogous to the maximum expected flow. The minimum expected flow results from the combination of the minimum pump curve and the maximum system resistance. The minimum expected flow will be equal to or greater than the design minimum flow.

→ (DRN 00-644)

Upon installation of the pumps in the Reactor Coolant System, the operating flow is determined from measurements of pressure differential across a pair of taps in each pump casing inlet and outlet. The individual loop flows are deduced from plots of pump flow vs pump Δp developed from calibration measurements made at the vendor's test facility. The total system flow is obtained by summing the loop flows. The uncertainties included in the calculation of the operating flow are the uncertainty associated with measurement of flow and pump differential pressure at the test facility, and the uncertainty in the measurement of pump differential pressure at the plant site. These uncertainties are statistically combined to give the overall uncertainty in primary coolant flow as determined from onsite tests. The best estimate flow reduced for uncertainties shall be greater than the design minimum flow.

← (DRN 00-644)

Any significant formation of crud buildup is detected by continuous monitoring of the Reactor Coolant System flow. A significant buildup of crud is not anticipated, however, because the water chemistry is designed to minimize crud buildup.

4.4.4.5.2 Thermal Margin Analysis

Thermal margin analyses of the reactor core are performed using the TORC code which is an open core analytical method based on the COBRA-IIIC code[16]. A complete description of the TORC code and its detailed application to core thermal margin analyses is contained in CENPD-161[6]. A brief description of the code and its use is given here.

The COBRA-IIIC code solves the conservation equations for mass, axial and lateral momentum, and energy for a collection of parallel flow channels that are hydraulically open to each other. Since the size of a channel in design varies from the size of a fuel assembly or more to the size of a subchannel within a fuel assembly, certain modifications were necessary to enable a realistic analysis of thermal-hydraulic conditions in both geometries. The principal revisions to arrive at the TORC code, which leave the basis structure of COBRA-IIIC unaltered, are in the following areas:

a) Modification of the lateral momentum equation for core wide calculations where the smallest channel size is typically that of a fuel assembly.

b) Addition of the capability for handling non-zero lateral boundary conditions on the periphery of a collection of parallel flow channels. This capability is particularly important when analyzing the group of subchannels within the hot fuel assembly.

c) Insertion of standard C-E empirical correlations and the ASME fluid property relationships.

Details of the lateral momentum equations and the standard empirical relationships are given in CENPD-161[6]. The application of the TORC code involves two or at most three stages where each stage is a separate TORC code computer run. The three stage approach is discussed below.

The first stage consists of calculating coolant conditions throughout the core on a coarse mesh basis. The core is modeled such that the smallest unit represented by a flow channel is a single fuel assembly. The three-dimensional power distribution in the core is superimposed on the core coolant inlet flow and temperature distributions. The core inlet flow distribution is obtained from flow model tests discussed in subsection 4.4.4.2, and the inlet temperature for normal four-loop operation is assumed uniform. The core exit static pressure distribution is obtained from flow model tests. The axial distributions of flow and enthalpy in each fuel assembly are then calculated on the basis that the fuel assemblies are hydraulically open to each other. Also determined during this stage are the transport quantities of mass, momentum, and energy which cross the lateral boundaries of each flow channel.

In the second stage, the hot assembly is analyzed with a coarse mesh in which the hot assembly and its adjoining fuel assemblies are modeled. The hot assembly is typically divided into four to five partial assembly regions. One of these regions is centered on the subchannels adjacent to the rod having the minimum DNBR. It need not be the highest powered rod in the fuel assembly. The three-dimensional power distribution is superimposed on the core coolant inlet flow and temperature distributions. The lateral transport of mass, momentum, and energy from the stage one calculations is imposed on the peripheral boundary enclosing the hot assembly and the neighboring assemblies. The axial distributions of flow and enthalpy in each channel are calculated as well as the transport quantities of mass, momentum, and energy which cross the lateral boundary of each flow channel.

→(EC-13881, R304)

The third stage involves a fine mesh modeling of the partial-assembly region which centers on the subchannels adjacent to the rod having the minimum DNBR. All of the flow channels used in this stage are hydraulically open to their neighbors. The output from the stage two calculations, in terms of the lateral transport of mass, momentum, and energy is imparted on the lateral boundaries of the stage three partial assembly region. Engineering factors are applied to the minimum DNBR rod and the hottest adjacent subchannel to account for uncertainties on the enthalpy rise and heat flow due to manufacturing tolerances. The local coolant conditions are calculated for each flow channel. These coolant conditions are then input to the DNB correlation and the minimum value of DNBR in the core is determined.

←(EC-13881, R304)

→(EC-13881, R304)
A more detailed description of this procedure with example is contained in CENPD-161[6]. This procedure is used to analyze any specific three-dimensional power distribution superimposed on an explicit core inlet flow distribution.
←(EC-13881, R304)

→(DRN 00-644)
The method used for design calculations is discussed in detail in CENPD-206[10]. In summary, the method is to use one limiting core radial power distribution for all analyses, to rise or lower the hot assembly power to provide the proper maximum rod radial power factor, and to use the core average mass velocity in all fuel assemblies except the hot assembly. The percent reduction for the hot assembly mass velocity is determined by comparison of results with the above detailed procedure. This methodology is used in the thermal margin analyses of the W3 reactor.
←(DRN 00-644)

4.4.4.5.3 Hydraulic Instability Analysis

Flow instabilities leading to flow excursions or flow oscillations have been observed in some boiling flow systems containing one or more closed, heated channels. Flow instabilities are a concern primarily because they may lead to a reduction in the DNB heat flux relative to that observed during a steady flow condition. Flow instabilities of several types have been observed or postulated for closed channel systems. Although the state of the art does not permit detailed theoretical analyses for each qW of flow instability, the available information on boiling systems indicates that flow instabilities will not adversely affect thermal margin of W3 during normal operation or anticipated operational occurrences.

→(DRN 00-644)
Flow instabilities which have been observed have occurred almost exclusively in closed channel systems operating at low pressures relative to PWR operating pressures. As shown by the tests discussed in Subsection 4.2.3, the resistance to coolant crossflow among subchannels of the 16 x 16 fuel assembly is extremely small. It would be expected that the low resistance to crossflow between adjacent subchannels would have a stabilizing effect, and that expectation is confirmed by the results of Veziroglu and Lee[17] who found that flow stability in parallel heated channels was enhanced by having cross connections between the channels. Increasing pressure has been found to have a stabilizing influence in many case where flow instabilities have been observed,[18] and the high operating pressure characteristic of PWRs tends to minimize the potential for flow instability. Kao, Morgan, and Parker,[19] who conducted flow stability experiments at pressures up to 2200 psia with closed parallel heated channels, found that no flow oscillations could be induced at pressure above 1200 psia for low power and power levels encountered in power reactors. Additional evidence that flow instabilities do not adversely affect thermal margin is provided by the data from the rod bundle DNB tests (see Subsection 4.4.4.1). Many rod bundles have been tested over wide ranges of operating conditions with no evidence of premature DNB or of inconsistent data which might be indicative of flow instabilities in the rod bundle.,
←(DRN 00-644)

→(DRN 00-644)
In summary, it is concluded that flow instabilities will not adversely affect thermal margin of Waterford 3 during normal operation and anticipated operational occurrences.
←(DRN 00-644; LBDCR 13-014, R309)

4.4.5 TESTING AND VERIFICATION

→(DRN 00-644; LBDCR 13-014, R309)
Data descriptive of thermal and hydraulic conditions within the reactor vessel will be obtained as part of the startup program described in Section 14.2. These will include hot and cold leg temperature, loop flowrates, and core power distributions. The data will be evaluated and compared with design calculations and parameters to assure that the reactor thermal and hydraulic behavior is as predicted.
←(DRN 00-644)

4.4.6 INSTRUMENTATION REQUIREMENTS

→(LBDCR 13-014, R309)
The in-core instrumentation system will be used to confirm core power distribution, perform periodic calibrations of the excore flux measurement system, and provide inputs to the COLSS. Further descriptions are contained in Section 7.7.
→(LBDCR 13-014, R309)

4.4.6.1 Valve and Loose Parts Monitoring System (V&LPMS)

The valve and loose parts monitoring equipment is provided to monitor the Reactor Coolant System (RCS) for loose parts in the reactor internals. In addition, the equipment is also used to detect primary safety valve position (see Section 1.9.23).

→(EC-26965, R305)
The V&LPMS meets the requirement of Regulatory Guide 1.133 as modified by NRC Technical Specification Amendment 104, and the Neutron Noise Monitoring system meets the requirements of ASME/ANSI OM-1987 Part OM-5.
←(EC-26965, R305)

→(DRN 00-644)
Loose parts become detectable when they are driven or waited against the inner walls of a pressure vessel or piping. A steady flow of coolant within the reactor vessels will wedge the loose part in a fixed position until flow is changed. When a loose part is driven from its position and hits the inner walls, the impact produces the sound waves radiating in the metal walls.

The Waterford 3 V&LPMS has been designed and manufactured by the Framatome and is included as part of the plant instrumentation for continuous monitoring of anomalous conditions due to a presence of loose parts in the RCS.

This system consists of sixteen high-temperature sensor assemblies; eight in Train A and eight in Train B, independent preamplifiers with shielded enclosures, all the hardware associated with mounting and wiring of the system and one signal processing and monitoring cabinet. Components installed inside containment are designed to comply with OBE requirements. The cabinet contains eight loose parts detector channels and four core internals channels (with signal conditioning and A/D conversion), system computer, mass storage, system, interface module, graphics printer, modem, back up cartridge tape drive, and software. Each detector module will monitor either channel A or B sensors via a toggle switch in the cabinet. Contact outputs which open on alarm for loose parts detected and pressurizer relief valve open are provided as interface to the main plant annunciator. Neutron noise monitoring consisting of the four core internal channels and neutron noise monitoring software is provided. A manual reset is provided to clear the alarm condition.
←(DRN 00-644)

The system includes an acoustic valve monitoring system consisting of four (4) acoustic Valve Monitoring System (VMS) detector modules, and acoustic monitoring system software. The system has the ability to provide monitoring and alarm functions without the system computer or hard disk in operation, and the ability to allow manual switching of the audio monitor through the channels with the computer or hard disk off line. Four (4) 0-10 VDC output channels are provided for the valve monitoring modules with two channels to the QSPDS and the remaining two channels to the PMC.

→(DRN 00-644)
The sensors are strategically located with two sensors at each collection region as shown in Figure 4.4-11. They are designed to detect the impact sound waves and transduce the detected unusual vibration signals to the main control room. The sensors and immediate sensor cable are tested and qualified for 10^{10} rad., 0-100 percent humidity, 100g vibration, and 650°F.
←(DRN 00-644)

Noise Rejection Capability is provided by the following:

a) Electrical isolation of the sensors from the plant structure.

b) Loose part detection is at a high (27 Khz) frequency which is above the plant background noise spectrum.

c) Double-shielding of sensor to preamp cable through use of coaxial cable in 3/4 in. iron conduit.

d) High-gain charge preamp placed as close as practical to the sensor.

→(DRN 00-644)
e) Twisted shielded pair cables are used from the charge preamp to the cabinet. These cables are laid low-level trays. These trays are to have no 60-hz control signals and switching transients.
←(DRN 00-644)

f) Shielded MS connectors (supplied by ESG) are used.

g) Cabinet power is low noise. A line filter is included.

h) EMI filters are included on every channel.

→(DRN 03-1689, R13-A; 04-1780, R14; 06-871, R15)
The preamplifiers are mechanically protected in junction boxes and are located outside the biological shield, close to the sensors. They are tested and qualified for 10^7 rad., 0-100 percent humidity, 10g vibration, and 150°F temperature. Sensor to preamplifier cable runs are through rigid conduit inside the containment.
←(DRN 03-1689, R13-A; 04-1780, R14; 06-871, R15)

→(DRN 00-644)
The motion of the reactor core internals is detected by the core internals monitoring channel configuration using the Neutron Noise Monitor to detect the neutron noise signal from each channel and obtaining core motion information therefrom. The core internals channels use existing signals from the excore monitoring system through Class 1E buffers. Signals in the one to 25 Hz range can indicate the existence of fuel pin or core barrel motion.
←(DRN 00-644)

4.4.6.1.1 Sensor Location

The sensors and preamplifiers are located in the containment (as shown in Figure 4.4-11) at the following locations:

Channel No.	Location	Type of Accelerometer
1 (A&B)	Bottom of Reactor Vessel	Hi - Temp
2 (A&B)	Head of Reactor Vessel	Hi - Temp
3 (A&B)	RC Pump 1A	Hi - Temp
4 (A&B)	RC Pump 1B	Hi - Temp
5 (A&B)	RC Pump 2A	Hi - Temp
6 (A&B)	RC Pump 2B	Hi - Temp
7 (A&B)	Top of Steam Generator No. 1	Hi - Temp
8 (A&B)	Top of Steam Generator No. 2	Hi - Temp
9 (A&B)	Detector Module (Spare)	-
10 (A&B)	Detector Module (Spare)	-
13 (A&B)	Core Internals	Noise Channels
14 (A&B)	Core Internals	Noise Channels
15 (A&B)	Core Internals	Noise Channels
16 (A&B)	Core Internals	Noise Channels
17	Pressurizer Safety Relief Valve A	Hi - Temp
18	Pressurizer Safety Relief Valve B	Hi - Temp
19	Pressurizer Safety Relief Valve A	Hi - Temp
20	Pressurizer Safety Relief Valve B	Hi - Temp
21	Acoustic Valve Monitoring Module (Spare)	-
22	Acoustic Valve Monitoring Module (Spare)	-

All sensors with their signal processing loops have the capability of monitoring the signals emanating from the presence of loose parts within the system at the location where the sensors are installed.

4.4.6.1.2 Signal Processing and Monitoring Cabinet

This cabinet is located in the main control room and includes the system computer, mass storage, system interface module, graphics printer, modem, backup cartridge tape drive, software, and detector modules for the V&LPMS. The system has a test and reset capability for automatically testing the system. Alarm indication is provided for all the valve and loose parts sensing channels.

4.4.6.1.3 Training

A complete training program is provided. The training program includes the following, in either lecture or demonstration form:

- Theory of Operation
- System Description
- Hardware Description
- Installation
- Calibration
- Normal Operation
- Actions in the Event of an Alarm
- Trouble Shooting

SECTION 4.4: REFERENCES

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←(EC-9533. R302)

→(EC-13881, R304)

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-TABLE 4.4-1 (Sheet 1 of 2)

THERMAL AND HYDRAULIC PARAMETERS

Reactor Parameters	Waterford 3	Pilgrim Station Unit 2 (Docket 50-471)
Core Average Characteristics at Full Power:		
Total core heat output, MWt	3,390	3,456
Total core heat output, million Btu/hr	11,570	11,80
Average fuel rod energy deposition fraction	0.975	0.965
Hot fuel rod energy deposition fraction	0.975	0.960
Primary system pressure, psia	2,250	2,250
Reactor inlet coolant temperature, F	553	557.5
Reactor outlet coolant temperature, F	611	616
Core exit average coolant temperature, F	613	618
Average core enthalpy rise, Btu/lb_m	80.3	82.6
Design minimum primary coolant flow rate, gpm	396,000	396,000
Design maximum core bypass flow, % of primary	2.6	3.5
Design minimum core flow rate, gpm	385,700	382,140
Hydraulic diameter of nominal subchannel, in.	0.471	0.471
Core flow area, ft^2	54.7	54.8
Core avg mass velocity, million $lb_m/hr\text{-}ft^2$	2.64	2.60
Core avg coolant velocity, ft/sec	16.4	16.5
Core avg fuel rod heat flux, $Btu/hr\text{-}ft^2$	182,400	184,000
Total heat transfer area, ft^2	62,000	62,000
Average fuel rod linear heat rate, KW/ft	5.34	5.39
Power density, kW/liter	94.9	96.5

TABLE 4.4-1 (Sheet 2 of 2)

THERMAL AND HYDRAULIC PARAMETERS

Reactor Parameters	Waterford 3	Pilgrim Station Unit 2 (Docket 50-471)
No. of active fuel rods	49,580	49,476
Power Distribution Factors:		
Rod radial power factor	1.55	1.55
Nuclear power factor	2.28	2.28
Total heat flux factor	2.35	2.33
Maximum augmentation factor	1.041	1.076
Maximum gap length, in.	1.20	0.865
Engineering Factors:		
Engineering heat flux factor	1.03	1.03
Engineering enthalpy rise factor	1.03	1.03
Pitch and bow factor	1.05	1.05
Engineering factor on linear heat rate	1.03	1.03
Characteristics of Rod and Channel with Minimum DNBR:		
Maximum fuel rod heat flux, Btu/hr-ft^2	428,000	429,000
Maximum fuel rod linear heat rate, kW/ft	12.5	12.6
UO_2 maximum steady state temperature, F	3,180	3,420
Outlet temperature, F	642	651
Outlet enthalpy, Btu/lb$_m$	680	699
Minimum DNBR at nominal conditions	2.07[a]	2.26[b]

(a) Computed using the CE-1 CORRELATION
(b) Computed using the original W3 CORRELATION

TABLE 4.4-2

COMPARISON OF THE DEPARTURE FROM NUCLEATE BOILING RATIOS COMPUTED WITH DIFFERENT CORRELATIONS

	DNBRs for Nominal Reactor Conditions		DNBRs for Reactor Conditions Giving a 1.13 CE-1 Minimum DNBR	
Correlation	Matrix Subchannel	Subchannel Next to Guide Tube	Matrix Subchannel	Subchannel Next to Guide Tube
CE-1	2.29	2.07	1.13	1.14
Original W3(3)	2.36	2.50	1.03	1.13
Revised W3(5)	2.36	2.24	1.03	1.05
B&W-2(5)	2.76	3.01	1.35	1.63

TABLE 4.4-3

REACTOR COOLANT FLOWS IN BYPASS CHANNELS

Bypass Route	Percent of Total Vessel Flow
Outlet nozzle clearances	0.6
Alignment keyways	0.1
Support cylinder holes	0.3
Core shroud clearances	0.3
Guide tubes	0.8
Total bypass	2.1

TABLE 4.4-4

REACTOR VESSEL BEST ESTIMATE
PRESSURE LOSSES AND COOLANT TEMPERATURES

Component	Pressure Loss (psi)	Temperature (F)
Inlet nozzle and 90° turn	6.9	553
Downcomer, lower plenum, and support structure	11.1	553
Fuel assembly	15.7	583
Fuel assembly outlet to outlet nozzle	8.1	613
Total Pressure Loss	41.8	

TABLE 4.4-5

DESIGN STEADY STATE HYDRAULIC LOADS
ON VESSEL INTERNALS AND FUEL ASSEMBLIES (a)

	Component	Load Description	Load Value
1.	Core support barrel	Steady-state radial pressure differential directed inward opposite inlet duct.	84 psi
2.	Core support barrel and upper guide structure	Steady-state uplift load	1.2×10^6 lb
3.	Flow skirt	Steady-state radial drag load directed inward	3500 lb/ft of cir-cumference, average; 7000 lb/ft maximum
4.	Bottom plate	Steady-state drag load directed upward	58,000 lb
5.	Core support plate	Steady-state drag load directed upward	69,000 lb
6.	Fuel assembly	Steady-state uplift load	2,300 lb
7.	Core Shroud	Steady-state radial pressure differential directed outward	34 psi at bottom zero psi at top
8.	Upper guide structure	Steady-state load directed upward	490,000 lb
9.	Fuel alignment plate	Steady-state drag load directed upward	138,000 lb
10.	Upper guide plate	Steady-state load directed downward	66,000 lb
11.	CEA shrouds	Steady-state lateral drag load	5,700 lb
12.	CEA shrouds	Steady-state radial pressure differential directed outward	17 psi

(a) Loads listed are at 500 F, 120 percent of design minimum flow, core in place.

TABLE 4.4-6 (Sheet 1 of 3)

RCS VALVES AND PIPE FITTINGS

Pressure Boundary Valves

Valve	Valve No.	Size (in)	Quantity
Pressurizer safety	RC-200, RC-201	6 x 8	2
Spray control	RC-100E, RC-100F	3	2
Bypass needle	RC-236 RC-237	3/4	2
Letdown Stop	CH-515	2	1
Safety injection tank isolation check valve	SI-215 SI-225 SI-235 SI-245	12	4
Safety injection check valve leakage drain valves	SI-618 SI-628 SI-638 SI-648	1	4
Hot leg injection isolation check valves	SI-510A SI-512A SI-510B SI-512B	3	4
Hot leg injection isolation check valve leakage drain valves	SI-301 SI-302	1	2
Safety injection line isolation check valves	SI-217 SI-227 SI-237 SI-247	12	4
Low pressure safety injection isolation check valve	SI-114 SI-124 SI-134 SI-144	8	4
High pressure safety injection isolation check valves	SI-113 SI-123 SI-133	3	4
		SI-143	

TABLE 4.4-6 (Sheet 2 of 3) Revision 305 (11/11)

Pressure Boundary Valves (Cont'd)

Valve	Valve No.	Size (in)	Quantity
Safety injection tank isolation valve	SI-614 SI-624 SI-634 SI-644	12	4
Hot leg sample line isolation valve	RC-213	3/4	1
Pressurizer vent isolation valve	RC-239	3/4	1
Pressurizer vapor space sample isolation valve	RC-238	3/4	1
Surge line sample line isolation valve	RC-210	3/4	1
Refueling level indicator connection isolation valves	RC-214 RC-216	3/4	2
Reactor vessel head vent isolation valve	RC-212	3/4	1
Hot leg drain line isolation valves	RC-215 RC-215A	2	2
→(EC-14765, R305) Shutdown cooling isolation valves ←(EC-14765, R305)	SI-651 SI-652 SI-665 SI-666 SI-4052A SI-4052B	14 3/4	6
Shutdown cooling line thermal relief valves	SI-464 SI-469	1 x 1	2
Charging line Check valves	CH-423 CH-432	2	2
Refueling water level indicating system reference leg isolation valves	RC-217 RC-218	1/2	2
Auxiliary spray line check valve	CH-431	2	1
Charging isolation check	CH-432	2	1

TABLE 4.4-6 (Sheet 3 of 3)

Pressure Boundary Valves (Cont'd)

Valve	Valve No.	Size (in)	Quantity
Charging line bypass isolation valve	CH-434	2	1
Charging line bypass check valve	CH-435	2	1
Charging line isolation valves	CH-518 CH-519	2	2
Auxiliary spray line isolation valve	CH-517	2	1
Letdown line isolation valve	CH-516 CH-515	2	1
Cold leg drain isolation valves	RC-232 RC-332 RC-233 RC-333 RC-234 RC-334 RC-235 RC-335	2	8

RCS Pipe Fittings

Elbows	Size (in.)	Radius (in.)	Quantity
35°	42	63	2
45°	30	45	4
90°	30	45	8
34°	30	45	2
60°	30	45	2

TABLE 4.4-7

RCS DESIGN MINIMUM FLOWS

Flow Path	Flow (gal.min)
Total minimum RCS flow	396,000
Core bypass flow (design maximum)	10,300
Core flow	385,700
Hot leg flow	198,000
Cold leg flow	99,000

TABLE 4.4-8 (Sheet 1 of 2)

REACTOR COOLANT SYSTEM GEOMETRY

Component	Flow Path Length (ft)	Height and Liquid Level (ft) (e)	Bottom Elevation (ft) (d)	Minimum Flow Area (ft^2)	Volume (ft^3)
Hot leg	14.64	4.13	- 1.75	9.62	139.99
Suction leg	24.85	8.48	- 7.50	4.91	119.39
Discharge leg					
Parallel	16.16	2.50	- 1.25	4.91	79.01
Nonparallel	16.19	2.50	- 1.25	4.91	79.15
Pressurizer					
Liquid level (full power)	--	19.84	14.61	50.53[a]	1,500
Height	--	36.33	--	--	--
Surge line	66.11	12.33	2.38	0.57	36.97
Steam generator					
Inlet nozzle (ea.)	2.99	4.31	- 0.48	9.82	30.74
Outlet nozzle (ea.)	2.21	3.17	- 0.79	5.05	11.13
Inlet plenum	9.10[b]	6.01	0.13	9.82	249.22
Outlet plenum	9.10[b]	6.01	0.13	5.05	249.22
Tubes	60.51	33.66	6.13	0.002[c]	1,278.98
Reactor Vessel					
Inlet nozzle (ea.)	3.2	2.5	- 1.25	4.9	19.5
Downcomer	24.1	31.6	-25.5	33.3	1,111.0
Lower plenum	3.0	6.3	-28.6	43.7	519.0

a. For the cylinder.
b. Represents a geometrical rather than an actual flow path length.
c. Flow area per tube.
d. Reactor vessel nozzle center line is the reference elevation. It has an elevation of 0.0 ft.
e. Elevation difference between high and low point.

TABLE 4.4-8 (Sheet 2 of 2)

Component	Flow Path Length (ft)	Height and Liquid Level (ft) (e)	Bottom Elevation (ft) (d)	Minimum Flow Area (ft^2)	Volume (ft^3)
Lower support structure & lower inactive core	3.5	3.4	-22.3	28.0	300.0
Active core	12.5	12.5	-18.9	54.9	687.0
Upper inactive core	1.8	1.8	- 6.4	54.9	126.0
Outlet plenum	7.7	8.9	- 4.6	23.5	646.0
CEA shrouds	12.8	14.8	- 4.6	13.3	430.0
Upper head	4.2	8.9	4.3	0.5	652.0
Outlet nozzle (ea.)	3.7	4.0	- 2.0	9.6	52.5

TABLE 4.4-9 (Sheet 1 of 2) Revision 10 (10/99)

SAFETY INJECTION LINES LENGTHS

(X)* - (Y)*	LENGTH (FT)	LINE SIZE (IN)
(1) - (2)	56	24
(2) - (16)	55	20
(16) - (66)	10	8
(52) - (30)	30.5	8
(30) - (31)	103.5	10
(15) - (17)	54.5	20
(17) - (67)	10	8
(53) - (18)	27	8
(18) - (19)	161	10
(3) - (5)	9.5	10
(5) - (6)	32.5	10
(5) - (8)	87	10
(54) - (20)	63	4
(20) - (22)	155	4
(22) - (23)	4	4
(23) - (24)	4	4
(24) - (21)	0.5	4
(15) - (14)	7.5	24
(14) - (7)	9	10
(7) - (10)	89	10
(56) - (26)	58	4
(26) - (27)	13	4
(27) - (29)	2	4
(27) - (28)	2.5	4
(6) - (9)	119.5	10
(55) - (25)	86	4
(25) - (20)	21	4
(25) - (26)	26.5	4
(6) - (7)	38	10
(11) - (12)	20	24
(12) - (13)	54	24
(12) - (14)	28.5	24
(4) - (3)	142	24
(40) - (39)	102	12
(39) - (41)	39	12
(39) - (59)	2	12
(59) - (38)	49.5	8
(38) - (19)	201	8
(38) - (37)	143	3
(37) - (29)	142	2
(60) - (22)	32.5	2
(31) - (43)	130	8
(51) - (49)	24	12
(49) - (50)	97	12

→

* Indicating the sect's (1) - (2), (4) - (5) etc as shown in Figure 4.4-8 (for Figure 4.4-8, Sheet 3, refer to Drawing G167, Sheet 3)
←

TABLE 4.4-9 (Sheet 2 of 2) Revision 10 (10/99)

SAFETY INJECTION LINES LENGTHS

(X)* - (Y)*	LENGTH (FT)	LINE SIZE (IN)
(49) - (61)	1.5	12
(61) - (48)	28	8
(48) - (31)	179	8
(48) - (47)	97	3
(47) - (62)	12.5	2
(62) - (28)	38	2
(62) - (24)	86	2
(35) - (34)	24	12
(34) - (36)	75	12
(34) - (64)	1.5	12
(64) - (33)	52.5	8
(33) - (19)	120	8
(33) - (32)	99	3
(32) - (29)	104	2
(63) - (23)	24	2
(46) - (44)	35	12
(44) - (45)	94.5	12
(65) - (44)	2	12
(43) - (42)	92	3
(43) - (31)	160	8
(42) - (57)	59	2
(57) - (28)	29	2
(57) - (21)	94.5	2
(65) - (43)	45.5	8

→

* Indicating the sect's (1) - (2), (4) - (5) etc as shown in Figure 4.4-8 (for Figure 4.4-8, Sheet 3, refer to Drawing G167, Sheet 3)
←

TABLE 4.4-10 (Sheet 1 of 2)

REACTOR COOLANT SYSTEM COMPONENT
THERMAL AND HYDRAULIC DATA [(a)]

Component	Data
Reactor Vessel	
Rated core thermal power, MWt	3,390
Design pressure, psia	2,500
Operating pressure, psia	2,250
Coolant outlet temperature, F	611
Coolant inlet temperature, F	553
Coolant outlet state	Subcooled
Total coolant flow, 10^6 lbm/hr	148
Core average coolant enthalpy	
Inlet, Btu/lbm	551
Outlet, Btu/lbm	632
Average coolant density	
Inlet. lbm/ft^3	46.7
Outlet, lbm/ft^3	42.0
Steam Generators	
Number of units	2
Primary side (or tube sides)	
Design pressure/temperature, psia/F	2,500/650
Operating pressure, psia	2,250
Inlet temperature, F	611
Outlet temperature, F	553
Secondary (or shell side)	
Design pressure/temperature, psia/F	1,110/560
Full load steam pressure/temperature, psia/F	900/532
Zero load steam pressure, psia	1,000
Total steam flow per gen., lbm/hr	7.565×10^6
Full load steam quality, %	99.8
Feedwater temperature, full power, F	445
Pressurizer	
Design pressure, psia	2,500
Design temperature, F	700
Operating pressure, psia	2,250
Operating temperature, F	653
Internal volume, ft^3	1,500
Heaters	
Type and rating of heaters, kW	Immersion/50
Installed heater capacity, kW	1,500

(a) Full power conditions.

TABLE 4.4-10 (Sheet 2 of 2)

REACTOR COOLANT SYSTEM COMPONENT
THERMAL AND HYDRAULIC DATA [(a)]

Component	Data
Reactor Coolant Pumps	
Number of units	4
Type	Vert-Centfgl
Design capacity, gpm	99,000
Design pressure/temperature, psia F	2,250/650
Operating pressure, psia	2,250
Type drive	Squirrel cage induction motor
Total dynamic head, ft	310
Rating and power requirements, hp	7,200
Pump speed, rpm	1,180
Reactor Coolant Piping	
Flow per loop (106 lbm/hr)	
Hot leg	74
Cold leg	37
Pipe size (inside dia./wall thickness), in.	
Hot leg	42/4 1/8
Suction leg	30/2 7/8
Discharge leg	30/3 3/8
Elbow size (inside dia./wall thickness), in.	
Hot leg	42/4 3/4
Suction leg	30/3 5/8
Discharge leg	30/3 5/8
Pipe design press./temp, psia/F	
Pipe operating press./temp, psia/F	
Hot leg	2,250/611
Cold leg	2,250/553