4.4 THERMAL AND HYDRAULIC DESIGN

4.4.1 Design Basis

The overall objective of the thermal and hydraulic design of the reactor core is to provide adequate heat transfer which is compatible with the heat generation distribution in the core such that heat removal by the Reactor Coolant System (RCS) or the Emergency Core Cooling System (ECCS) (when applicable) assures that the following performance and safety criteria requirements are met:

1. Fuel damage* is not expected during normal operation and operational transients (Condition I) or any transient conditions arising from faults of moderate frequency (Condition II). It is not possible, however, to preclude a very small number of rod failures. These will be within the capability of the Plant Cleanup System and are consistent with the plant design bases.

2. The reactor can be brought to a safe state following a Condition III event with only a small fraction of fuel rods damaged* although sufficient fuel damage might occur to preclude resumption of operation without considerable outage time.

3. The reactor can be brought to a safe state and the core can be kept subcritical with acceptable heat transfer geometry following transients arising from Condition IV events.

---

* Fuel damage as used here is defined as penetration of the fission product barrier (i.e., the fuel rod clad).

In order to satisfy the above criteria, the following design bases have been established for the thermal and hydraulic design of the reactor core.

4.4.1.1 Departure From Nucleate Boiling Design Basis

Basis

Departure from nucleate boiling (DNB) will not occur on at least 95 percent of the limiting fuel rods during normal operation and operational transients and any transient conditions arising from faults of moderate frequency (Condition I and II events) at a 95 percent confidence level.

Discussion

The design method employed to meet the DNB design basis for the Vantage 5H, Vantage+, and RFA fuel assemblies is the Revised Thermal Design Procedure (RTDP), Reference 96. With the RTDP methodology, uncertainties in plant operating parameters, nuclear and thermal parameters, fuel fabrication parameters, computer codes and DNB correlation predictions are considered statistically to obtain DNB uncertainty factors. Based on the DNB uncertainty factors, RTDP design limit DNBR values are determined such that there is at least a 95 percent probability at a 95 percent confidence level that DNB will not occur on the most limiting fuel rod during normal operation and operational transients, and during transient conditions arising from faults of moderate frequency (Condition I and II events as defined in ANSI N18.2).

Uncertainties in the plant operating parameters (pressurizer pressure, primary coolant temperature, reactor power, and reactor coolant system flow) have been evaluated at Salem Units 1 and 2, Reference 114. Uncertainties in the power calorimetric at a 1.4% uprated reactor power have been evaluated at Salem Units 1 and 2, Reference 116. Only the random portion of the plant operating parameters uncertainties is included in the statistical combination. Instrument bias is treated as a direct DNBR penalty. Since the parameter uncertainties are considered in determining the RTDP design limit DNBR values, the plant safety analyses are performed using input parameters at their nominal values.

The RTDP design limit DNBR values are 1.24 for both typical and thimble cells of the Vantage 5H and Vantage+ fuels and 1.24 and 1.22 for the typical and thimble cells, respectively for RFA fuel.

The design limit DNBR values are used as a basis for the technical specifications and for consideration of the applicability of unreviewed safety questions as defined in 10 CFR 50.59.

To maintain DNBR margin to offset DNB penalties such as those due to fuel rod bow (see paragraph 4.4.2.3.5) and transition cores (see paragraph 4.4.2.3.6), safety analyses were performed to DNBR limits higher than the design limit DNBR values. The difference between the design limit DNBRs and the safety analysis limit DNBRs results in available DNBR margin. The net DNBR margin, after consideration of all penalties, is available for operating and design flexibility.

The option of thimble plug removal has been included in all of the DNBR analyses performed for the Vantage 5H, Vantage+, and RFA fuel. The primary impact of thimble plug removal on the thermal hydraulic analysis is an increase in core bypass flow. Bypass flow is assumed to be ineffective for core heat removal. The increased bypass flow is included in all of the flow and DNBR values presented in Table 4.4-1.

Operation with thimble plugs in place reduces the core bypass flow through the fuel assembly thimble tubes. The reduction in core bypass flow for operation with the thimble plugs in place is a DNBR benefit. The increased margin associated with the use of a full compliment of thimble plugs can be used to offset DNBR penalties.

The Standard Thermal Design Procedure (STDP) is used for those analyses where RTDP is not applicable. In the STDP methodology, the parameters used in the analysis are treated in a conservative way from a DNBR standpoint. The parameter uncertainties are applied directly to the plant safety analyses input values to give the lowest minimum DNBR. The DNBR limit for STDP is the appropriate DNB correlation limit increased by sufficient margin to offset the applicable DNBR penalties.

By preventing DNB, adequate heat transfer is assured between the fuel clad and the reactor coolant, thereby preventing clad damage as a result of inadequate cooling. Maximum fuel rod surface temperature is not a design basis as it will be within a few degrees of coolant temperature during operation in the nucleate boiling region. Limits provided by the nuclear control and protection systems are such that this design basis will be met for transients associated with Condition II events including overpower transients. There is an additional large DNBR margin at rated power operation and during normal operating transients.

4.4.1.2 Fuel Temperature Design Basis

Basis

During modes of operation associated with Condition I and Condition II events, the maximum fuel temperature shall be less than the melting temperature of uranium dioxide (UO_2). The UO_2

melting temperature for at least 95 percent of the peak kW/ft fuel rods will not be exceeded at the 95 percent confidence level. The melting temperature of UO_2 is taken as 5080□F (1) unirradiated and decreasing 58□F per 10,000 MWD/MTU. By precluding UO_2 melting, the fuel geometry is preserved and possible adverse effects of molten UO_2 on the cladding are eliminated. To preclude center melting and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700□F has been selected as the overpower limit. This provides sufficient margin for uncertainties in the thermal evaluations as described in Section 4.4.2.10.1.

Discussion

Fuel rod thermal evaluations are performed at rated power, maximum overpower and during transients at various burnups. These analyses assure that this design basis, as well as the fuel integrity design bases given in Section 4.2, are met. They also provide input for the evaluation of Condition III and IV faults given in Section 15.

4.4.1.3 Core Flow Design Basis

Basis

A minimum of 92.8 percent of the thermal flow rate will pass through the fuel rod region of the core and be effective for fuel rod cooling. Coolant flow through the thimble tubes as well as the leakage from the core barrel-baffle region into the core are not considered effective for heat removal.

Discussion

Core cooling evaluations are based on the thermal flow rate (minimum flow) entering the reactor vessel. A maximum of 7.2 percent of this value is allotted as bypass flow. This includes rod cluster control (RCC) guide thimble cooling flow,

head cooling flow, baffle leakage, and leakage to the vessel outlet nozzle.

The maximum bypass flow fraction of 7.2 percent assumes no plugging devices or burnable absorbers in the RCC guide thimble tubes, which do not contain RCC rods.

4.4.1.4 Hydrodynamic Stability Design Bases

Basis

Modes of operation associated with Condition I and II events shall not lead to hydrodynamic instability.

4.4.1.5 Other Considerations

The above design bases together with the fuel clad and fuel assembly design bases given in Section 4.2.1.1 are sufficiently comprehensive so additional limits are not required.

Fuel rod diametral gap characteristics, moderator-coolant flow velocity and distribution, and moderator void are not inherently limiting. Each of these parameters is incorporated into the thermal and hydraulic models used to ensure the above mentioned design criteria are met. For instance, the fuel rod diametral gap characteristics change with time (see Section 4.2.1.3.1) and the fuel rod integrity is evaluated on that basis. The effect of the moderator flow velocity and distribution (see Section 4.4.2.3) and moderator void distribution (see Section 4.4.2.5) are included in the core thermal (THINC) evaluation and thus affect the design bases.

Meeting the fuel clad integrity criteria covers possible effects of clad temperature limitations. As noted in Section 4.2.1.3.1, the fuel rod conditions change with time. A single clad temperature limit for Condition I or Condition II events is not appropriate since of necessity it would be overly conservative. A clad temperature limit is applied to the loss-of-coolant accident (LOCA) (Section 15.3.1), control rod ejection accident (2), and locked rotor accident (3).

4.4.2 Description

4.4.2.1 Summary Comparison

The Salem Unit 1 and Unit 2 reactors are designed to the appropriate DNBR limit as well as no fuel centerline melting during normal operation, operational transients, and faults of moderate frequency. The values of the thermal and hydraulic design parameters are presented in Table 4.4-1, with comparisons between Vantage 5H, Vantage+, and RFA. Values specific to Standard Thermal Design Procedure and Revised Thermal Design Procedure are provided where appropriate.

(The text on this page has been deleted)

The effects on DNB of fuel densification have been evaluated utilizing the methods and models described in detail in Reference 7 and summarized in the following sections. The net effect of fuel densification is a reduction of 0.2 percent in the DNBR due to a slight increase in the linear power generation rate as described in Section 4.4.2.2.

4.4.2.2 Fuel and Cladding Temperatures (Including Densification)

Consistent with the thermal-hydraulic design bases described in Section 4.4.1, the following discussion pertains mainly to fuel pellet temperature evaluation. A discussion of fuel clad integrity is presented in Section 4.2.1.3.1.

The thermal-hydraulic design assures that the maximum fuel temperature is below the melting point of UO_2 (melting point of 5080□F (1) unirradiated and decreasing 58□F per 10,000 MWD/MTU). To preclude center melting and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700□F has been selected as the overpower limit. This provides sufficient margin for uncertainties in the thermal evaluations as described in Section 4.4.2.10.1. The temperature distribution within the fuel pellet is predominantly a function of the local power density and the UO_2 thermal conductivity. However, the computation of radial fuel temperature distributions combines crud, oxide, clad, gap, and pellet conductances. The factors which influence these conductances, such as gap size (or contact pressure), internal gas pressure, gas composition, pellet density, and radial power distribution within the pellet, etc, have been combined into a semi-empirical thermal model (see Section 4.2.1. 3.1) which includes a model for time dependent fuel densification described in References 7 and 108. This thermal model enables the determination of these factors and their net

effects on temperature profiles. The temperature predictions have been compared to inpile fuel temperature measurements (8 through 14) and melt radius data (15,16) with good results.

Effect of Fuel Densification on Fuel Rod Temperatures

Fuel densification results in fuel pellet shrinkage. This affects the fuel temperatures in the following ways:

1. Pellet radial shrinkage increases the pellet diametral gap which results in increased thermal resistance of the gap, and thus, higher fuel temperatures (See Section 4.2.1.3.1).

2. Pellet axial shrinkage may produce pellet to pellet gaps which result in local power spikes and thus, higher total heat flux hot channel factor, F_Q, and local fuel temperatures.

3. Pellet axial shrinkage will result in a fuel stack height reduction and an increase in the linear power generation rate (kW/ft) for a constant core power level. Using the methods described in Section 5.3 of Reference 7, the increase in linear power for the fuel rod specifications listed in Table 4.3-1 is 0.2 percent.

As described in Reference 7, fuel rod thermal evaluations (fuel centerline, average and surface temperatures) are determined throughout the fuel rod lifetime with consideration of time dependent densification. Maximum fuel average and surface temperatures, shown on Figure 4.4-1 for standard fuel and Figure 4.4-1A for Vantage 5H, Vantage+, and RFA fuel as a function of linear power density (kW/ft), are peak values attained during the fuel lifetime. Figure 4.4-2 for standard fuel and Figure 4.4-2A for Vantage 5H, Vantage+, and RFA fuel present the peak value of fuel centerline temperature versus linear power density which is attained during the fuel lifetime.

The maximum pellet temperatures at the hot spot during full power steady state and at the maximum overpower trip point are shown in Table 4.4-1. The principal factors which are employed in the determination of the fuel temperature are discussed below.

4.4.2.2.1 Uranium Dioxide Thermal Conductivity

The thermal conductivity of UO_2 was evaluated from data reported in References 17 through 29.

At the higher temperatures, thermal conductivity is best obtained by utilizing the integral conductivity to melt which can be determined with more certainty. From an examination of the data, it has been concluded that the best estimate for the value of $\int_0^{2800°C} K\,dt$ is 93 watts/cm.

This conclusion is based on the integral values reported in References 15 and 29 through 33.

The design curve for the thermal conductivity is shown on Figure 4.4-3. The section of the curve at temperatures between 0°C and 1300°C is in excellent agreement with the recommendation of the International Atomic Energy Agency panel (34). The section of the curve above 1300°C is derived for an integral value of 93 watts/cm(15, 29, 33).

Thermal conductivity for UO_2 at 95 percent theoretical density can be represented best by the following equation:

$$K = \frac{1}{11.8 + 0.0238T} + 8.775 \times 10^{-13} T^3 \qquad (4.4\text{-}1)$$

where:

K = watts/cm-°C

T = □C.

4.4.2.2.2 Radial Power Distribution in UO_2 Fuel Rods

An accurate description of the radial power distribution as a function of burnup is needed for determining the power level for incipient fuel melting and other important performance parameters such as pellet thermal expansion, fuel swelling, and fission gas release rates.

This information on radial power distributions in UO_2 fuel rods is determined with the neutron transport theory code, LASER. The LASER code has been validated by comparing the code predictions on radial burnup and isotopic distributions with measured radial microdrill data (35,36). A "radial power depression factor", f, is determined using radial power distributions predicted by LASER. The factor f enters into the determination of the pellet centerline temperature, T_c, relative to the pellet surface temperature, T_s, through the expression:

$$\int_{T_s}^{T_c} k(T)\, dT = \frac{q'f}{4\square} \qquad (4.4\text{-}2)$$

where:

k(T) = the thermal conductivity for U0 with a uniform density distribution

q' = the linear power generation rate.

4.4.2.2.3 Gap Conductance

The temperature drop across the pellet-clad gap is a function of the gap size and the thermal conductivity of the gas in the gap. The gap conductance model is selected such that when combined with

the U0 thermal conductivity model, the calculated fuel centerline temperatures reflect the inpile temperature measurements.

The temperature drop across the gap is calculated by assuming an annular gap conductance model of the following form:

$$h = \frac{K_{gas}}{\frac{\delta}{2} + 14.4 \times 10^{-6}} \qquad (4.3 - 3)$$

or an empirical correlation derived from thermocouple and melt radius data:

$$h = 1500 \; K_{gas} + \frac{4.0}{0.006 + 12\delta} \qquad (4.4 - 3a)$$

where:

K_{gas} = thermal conductivity of the gas mixture including a correction factor (37) for the accommodation coefficient for light gases (e.g., helium), Btu/hr-ft-□F.

□ = diametral gap size, ft.

The larger gap conductance value from these two equations is used to calculate the temperature drop across the gap for finite gaps.

For evaluations in which the pellet-clad gap is closed, a contact conductance is calculated. The contact conductance between U0 and Zircaloy has been measured and found to be dependent on the contact pressure, composition of the gas at the interface and the surface roughness (37,38). This information together with the surface roughness found in Westinghouse reactors leads to the following correlation:

$$h = 0.6P + \frac{K_{gas}}{14.4 \times 10^{-6}} \qquad (4.4\text{-}4)$$

where:

h = contact conductance, Btu/hr-ft^2-□F

p = contact pressure, psi

K_{gas} = thermal conductivity of gas mixture at the interface including a correction factor (37) for the accommodation coefficient for light gases (e.g., helium, Btu/hr-ft-□F).

4.4.2.2.4 Surface Heat Transfer Coefficients

The fuel rod surface heat transfer coefficients during subcooled forced convection and nucleate boiling are presented in Section 4.4.2.8.1.

4.4.2.2.5 Fuel Clad Temperatures

The outer surface of the fuel rod at the hot spot operates at a temperature of approximately 660□F for steady state operation at rated power throughout core life due to the onset of nucleate boiling. Initially (beginning-of-life), this temperature is that of the clad metal outer surface.

During operation over the life of the core, the buildup of oxides and crud on the fuel rod surface causes the clad surface temperature to increase. Allowance is made in the fuel center melt evaluation for this temperature rise. Since the thermal-hydraulic design basis limits DNB, adequate heat transfer is provided between the fuel clad and the reactor coolant so that the core thermal output is not limited by considerations of the

clad temperature. Figure 4.4-4 shows the axial variation of average clad temperature for the average power rod both at beginning and end-of-life.

Treatment of Peaking Factors

The total heat flux hot channel factor, F_Q, is defined by the ratio of the maximum to core average heat flux. As presented in Table 4.3-2 and discussed in Section 4.3.2.2.1, the design value F_Q for normal operation is 2.40, including fuel densification effects.

This results in peak local power of 13.3 kW/ft at full power conditions. As described in Section 4.3.2.2.6, the peak local power at the maximum overpower trip point is $\leq$22.4 kW/ft. The centerline temperature at this kW/ft must be below the UO_2 melt temperature over the lifetime of the rod, including allowances for uncertainties. The melt temperature of unirradiated UO_2 is 5080□F (1) and decreases by 58□F per 10,000 MWD/MTU. From Figure 4.4-2 for standard fuel and Figure 4.4-2A for Vantage 5H, Vantage+, and RFA fuel, it is evident that the centerline temperatures at the maximum overpower trip points for both units are far below those required to produce melting. Fuel centerline temperature at the maximum overpower trip point is presented in Table 4.4-1.

4.4.2.3 Critical Heat Flux Ratio or Departure from Nucleate Boiling Ratio and Mixing Technology

The minimum DNBRs for the rated power, design overpower, and anticipated transient conditions are given in Table 4.4-1. The core average DNBR is not a safety-related item as it is not directly related to the minimum DNBR in the core, which occurs at some elevation in the limiting flow channel. Similarly, the DNBR at the hot spot is not directly safety-related. The minimum DNBR in the limiting flow channel will be downstream of the peak

heat flux location (hot spot) due to the increased downstream enthalpy rise.

DNBRs are calculated by using the correlation and definitions described in the following Sections 4.4.2.3.1 and 4.4.2.3.2. The THINC-IV computer code (discussed in Section 4.4.3.4.1) is used to determine the flow distribution in the core and the local conditions in the hot channel for use in the DNB correlation. The use of hot channel factors is discussed in Section 4.4.3.2.1 (nuclear hot channel factors) and in Section 4.4.2.3.4 (engineering hot channel factors).

4.4.2.3.1 Departure from Nucleate Boiling Technology

The primary DNB correlation used for the analysis of the Vantage 5H and Vantage+ fuel is the WRB-1 correlation (Reference 89). The Primary DNB correlation used for the analysis of the RFA fuel (with Intermediate flow Mixer grids) is the WRB-2 correlation (Reference 97).

The WRB-1 correlation was developed based exclusively on the large bank of mixing vane grid rod bundle CHF data (over 1100 points) that Westinghouse has collected. The WRB-1 correlation, based on local fluid conditions, represents the rod bundle data with better accuracy over a wide range of variables than the previous correlation used in design. This correlation accounts directly for both typical and thimble cold wall effects, uniform and non-uniform heat flux profiles, and variations in rod heated length and in grid spacing.

The Applicable range of parameters for the WRB-1 correlation is:

Pressure	:	$1440 \leq 2490$ psia
Local Mass Velocity	:	$0.9 \leq G_{loc}/10^6 \leq 3.7$ lb/ft^2-hr
Local Quality	:	$-0.2 \leq X_{loc} \leq 0.3$
Heated Length, Inlet to CHF Location	:	$L_h \leq 14$ feet
Grid Spacing	:	$13 \leq g_{sp} \leq 32$ inches
Equivalent Hydraulic Diameter	:	$0.37 \leq d_e \leq 0.60$ inches
Equivalent Heated Hydraulic Diameter	:	$0.46 \leq d_h \leq 0.59$ inches

Figure 4.4-5B shows measured critical heat flux plotted against predicted critical heat flux using the WRB-1 correlation.

A 95/95 correlation limit DNBR of 1.17 for the WRB-1 correlation has been approved by the NRC for Vantage 5H and Vantage+ fuel (Reference 92).

The WRB-2 DNB correlation was developed to take credit for the RFA Intermediate flow mixer (IFM) grid design. A limit of 1.17 is also applicable for the WRB-2 correlation. Figure 4.4-5C shows measured critical heat flux plotted against predicted critical heat flux using the WRB-2 correlation.

The applicable range of parameters for the WRB-2 correlation is

Pressure	:	$1440 \leq P \leq 2490$ psia
Local Mass Velocity	:	$0.9 \leq G_{loc}/10^6 \leq 3.7$ lb/ft^2-hr
Local Quality	:	$-0.1 \leq X_{loc} \leq 0.3$
Heated Length, Inlet to CHF Location	:	$L_h \leq 14$ feet
Grid Spacing	:	$10 \leq g_{sp} \leq 26$ inches
Equivalent Hydraulic Diameter	:	$0.33 \leq d_e \leq 0.51$ inches
Equivalent Heated Hydraulic Diameter	:	$0.45 \leq d_h \leq 0.66$ inches

The use of the WRB-2 correlation has been conservatively modified to utilize a penalty above a certain high quality threshold within approved ranges (Reference 117).

The W-3 correlation (References 4 and 98) is used for all fuel types where the primary DNBR correlations are not applicable. The WRB-1 and WRB-2 correlations were developed based on mixing vane data and therefore are only applicable in the heated rod spans above the first mixing vane grid. The W-3 correlation, which does not take credit for mixing vane grids, is used to calculate the DNBR value in the heated region below the first mixing vane grid. In addition, the W-3 correlation is applied in the analysis of accident conditions where the system pressure is below the range of the primary correlations. For system pressures in the range of 500 to 1000 psia, the W-3 correlation limit is 1.45, Reference 99. for system pressures greater than 1000 psia, the W-3 correlation limit is 1.30. A cold wall factor (Reference 100) is applied to the W-3 DNB correlation to account for the presence of the unheated thimble surfaces.

References 90 and 91 document the approval of the NRC that a 95/95 limit DNBR of 1.17 is appropriate for the STD and optimized fuel assemblies.

The use of the WRB-2 correlation with a 95/95 DNBR limit of 1.17 is applicable to the RFA fuel (References 111 and 112).

4.4.2.3.2 Definition of Departure from Nucleate Boiling Ratio

The DNB heat flux ratio (DNBR) as applied to the standard fuel utilizing the W-3 "R" grid DNB correlation when all flow cell walls are heated is:

$$DNBR = \frac{q''_{DNB,N} \times F'_S}{q''_{loc}} \qquad (4.4\text{-}5)$$

where:

$$q''_{DNB,N} = \frac{q''_{DNB,EU}}{F} \qquad (4.4\text{-}6)$$

and $q''_{DNB,EU}$ is the uniform DNB heat flux as predicted by the W-3 DNB correlation (42) when all flow cell walls are heated.

F is the flux shape factor to account for non-uniform axial heat flux distributions (42) with the "C" term modified as in Reference 39.

F'_S is the modified spacer factor described in Reference 5 using an axial grid spacing coefficient, K_S = 0.046, and a thermal diffusion coefficient (TDC) of 0.038, based on the 26-inch grid

spacing data. Since the actual grid spacing is approximately 20 inches, these values are conservative since the DNB performance was found to improve and TDC increases as axial grid spacing is decreased (References 40 and 43).

q''_{loc} is the actual local heat flux.

The DNBR as applied to this design when a cold wall is present is:

$$DNBR = \frac{q''_{DNB,N,CW} \times F'_S}{q''_{loc}} \qquad (4.4\text{-}7)$$

where:

$$q''_{DNB,N,CW} = \frac{q''_{DNB,EU,Dh} \times CWF}{F} \qquad (4.4\text{-}8)$$

where:

$q''_{DNB,EU,Dh}$ is the uniform DNB heat flux as predicted by the W-3 cold wall DNB correlation (39) when not all flow cell walls are heated (thimble cold wall cell)).

Values of minimum DNBR provided in Section 4.4.3.3 for STD fuel are the limiting value obtained by applying the above two definitions of DNBR to the appropriate cell (typical cell with all walls heated, or a thimble cold wall cell with a partial heated wall condition). Approximately 15 percent in DNBR margin has been retained in all DNB analyses performed for this application. Specifically, all DNBRs computed by Equations 4.4-5 and 4.4-7 have been multiplied by 0.86. Hence, if the value 1.30 is quoted, the actual calculated number (using either Equation 4.4-5 or 4.4-7) is 1.51. The basis for retaining this margin is discussed in detail in Section 4.4.2.1.

Histograms of both the "R" grid (15 x 15 geometry) data obtained from "R" grid rod bundle DNB tests (44,45) and the 17 x 17 geometry data obtained from similar tests (4,5) satisfy the criterion of being obtained from a normal distribution just as does the data used to develop the original W-3 DNB correlation with slight differences in the means and standard deviations from those of the W-3. However, the probability distribution curves for the "R" grid data and the 17 x 17 data (including the 0.88 multiplier) when compared to that of the W-3 correlation show that

the approach which was valid for the original W-3 DNB correlation is conservatively applicable for the "R" and 17 x 17 data (with the 0.88 multiplier for the 17 x 17 data).

Standard fuel will continue to be designed to a minimum DNBR of 1.30 for the peak rod or rods in the core. Based on the W-3 statistics, the proportion of such peak rods that will not reach DNB is 0.95 or greater at a 95 percent confidence level.

The DNB heat flux ratio (DNBR) as applied to the Vantage 5H and Vantage+ designs utilizing the WRB-1 DNB correlation, and as applied to the RFA design utilizing the WRB-2 DNB correlation is:

$$DNBR = \frac{q''_{DNB,\ N}}{q''_{loc}}$$

where:

$$q''_{DNB,N} = \frac{q''_{DNB,\ EU}}{F}$$

and $q''_{DNB,EU}$ is the uniform critical heat flux as predicted by the WRB-1 DNB correlation (Reference 89) or the WRB-2 correlation (Reference 97).

The procedures used in the evaluation of DNB margin for these applications show that the calculated minimum DNBR for the peak rod or rods in the core will be above the appropriate DNBR limit during Class I and II incidents, even when all the engineering hot channel factors described in Section 4.4.2.3.4 occur simultaneously in these channels. In reality the probability of this simultaneous occurrence is negligibly small and substantial increases in local heat flux or coolant temperature could be tolerated without violation of the design basis.

4.4.2.3.3 Mixing Technology

The rate of heat exchange by mixing between flow channels is proportional to the difference in the local mean fluid enthalpy of the respective channels, the local fluid density, and flow velocity. The proportionality is expressed by the dimensionless thermal diffusion coefficient, (TDC), which is defined as:

$$TDC = \frac{w'}{\square\square\square\square Va} \qquad (4.4\text{-}9)$$

where:

w' = flow exchange rate per unit length, lbs/ft-sec

□ = fluid density, lbm/ft^3

V = fluid velocity, ft/sec

THIS PAGE INTENTIONALLY BLANK

a = lateral flow area between channels per unit length, ft^2/ft

The application of the TDC in the THINC analysis for determining the overall mixing effect or heat exchange rate is presented in Reference 46.

Various mixing tests have been performed by Westinghouse at Columbia University (43). These series of tests, using the "R" mixing vane grid design on 13-, 26-, and 32-inch grid spacing, were conducted in pressurized water loops at Reynolds numbers similar to that of a PWR core under the following single and two phase (subcooled boiling) flow conditions:

Pressure	1500 to 2400 psia
Inlet temperature	332□F to 642□F
Mass velocity	1.0 to 3.5 x 10^6 lbm/hr-ft^2
Reynolds number	1.34 to 7.45 x 10^5
Bulk outlet quality	-52.1 to -13.5 percent

TDC is determined by comparing the THINC code predictions with the measured subchannel exit temperatures. Data for 26-inch axial grid spacing are presented on Figure 4.4-6 where the TDC is plotted versus the Reynolds number. The thermal diffusion coefficient is found to be independent of Reynolds number, mass velocity, pressure, and quality over the ranges tested. The two-phase data (local, subcooled boiling) fell within the scatter of the single phase data. The effect of two-phase flow on the value of TDC has been demonstrated by Cadek (43), Rowe and Angle (47, 48), and Gonzalez, Santalo, and Griffith (49). In the subcooled boiling region the values of TDC were indistinguishable from the single phase values. In the quality region, Rowe and Angle show that in the case with rod spacing similar to that in

pressurized water reactor (PWR) core geometry, the value of TDC increased with quality to a point and then decreased, but never below the single phase value. Gonzalez, Santalo, and Griffith showed that the mixing coefficient increased as the void fraction increased.

The data from these tests on the "R" grid showed that a design TDC value of 0.038 (for 26-inch grid spacing) can be used in determining the effect of coolant mixing in the THINC analysis.

A mixing test program similar to the one described above was conducted at Columbia University for the 17 x 17 geometry and mixing vane grids on 26-inch spacing (50). The mean value of TDC obtained from these tests was 0.059, and all data was well above the current design value of 0.038.

Since the actual reactor grid spacing is approximately 20 inches for the Vantage 5H and Vantage+ designs, and approximately 10 inches for the RFA design, additional margin is available, as the value of TDC increases as grid spacing decreases (43).

4.4.2.3.4 Engineering Hot-Channel Factors

The total hot-channel factors for heat flux and enthalpy rise are defined as the maximum-to-core average ratios of these quantities. The heat flux hot-channel factor considers the local maximum linear heat generation rate at a point (the "hot spot"), and the enthalpy rise hot-channel factor involves the maximum integrated value along a channel (the "hot-channel").

Each of the total hot-channel factors considers a nuclear hot-channel factor (See Section 4.4.3.2) describing the neutron power distribution and an engineering hot-channel factor, which allows for variations in flow conditions and fabrication tolerances. The engineering hot-channel factors are made up of subfactors which account for the influence of the variations of fuel pellet diameter, density, enrichment, and eccentricity; fuel

rod diameter, pitch, and bowing; inlet flow distribution; flow redistribution; and flow mixing.

Heat Flux Engineering Hot-Channel Factor, F_Q^E

The heat flux engineering hot channel factor is used to evaluate the maximum liners heat generation rate of the core. This subfactor is determined by statistically combining the fabrication variations for fuel pellet diameter, density, and enrichment has a value of 1.03 at the 95 percent probability level with 95 percent confidence. As shown in Reference 15, no DNB penalty need be taken for the short, relatively low-intensity heat flux spikes caused by variations in the above parameters, as well as fuel pellet eccentricity and fuel rod diameter variation.

Enthalpy Rise Engineering Hot-Channel Factor, $F_{\square H}^E$

The effect of variations in flow conditions and fabrication tolerances on the hot-channel enthalpy rise is directly considered in the THINC core thermal subchannel analysis (See Section 4.4.3.4.1) under any reactor operating condition. The items considered contributing to the enthalpy rise engineering hot-channel factor are discussed below:

Pellet Diameter, Density and Enrichment

Variations in pellet diameter, density, and enrichment are considered statistically in establishing the limit DNBRs (see paragraph 4.4.1.1) for the Revised Thermal Design Procedure (Reference 96) employed in this application. Uncertainties in these variables are determined from sampling of manufacturing data.

(This text has been deleted)

Inlet Flow Maldistribution

The consideration of inlet flow maldistribution in core thermal performances is discussed in Section 4.4.3.1.2. A design basis of 5 percent reduction in coolant flow to the hot assembly is used in the THINC-IV analysis.

Flow Redistribution

The flow redistribution accounts for the reduction in flow in the hot-channel resulting from the high flow resistance in the channel due to the local or bulk boiling. The effect of the nonuniform power distribution is inherently considered in the THINC analysis for every operating condition which is evaluated.

Flow Mixing

The subchannel mixing model incorporated in the THINC Code and used in reactor design is based on experimental data (51) discussed in Section 4.4.3.4.1. The mixing vanes incorporated in the spacer grid design induce additional flow mixing between the various flow channels in a fuel assembly as well as between adjacent assemblies. This mixing reduces the enthalpy rise in the hot-channel resulting from local power peaking or unfavorable mechanical tolerances.

4.4.2.3.5 Effects of Rod Bow on DNBR

The phenomenon of fuel rod bowing, as described in Reference 93, must be accounted for in the DNBR safety analysis of Condition I and Condition II events for each plant application. Applicable generic credits for margin resulting from retained conservatism in the evaluation of the DNBR and/or margin obtained from measured plant operating parameters (such as $F^N_{\square H}$ or core flow) -- which are less limiting than those required by the plant safety analysis -- can be used to offset the effect of rod bow.

For the safety analysis of the Salem units, sufficient DNBR margin was maintained (see Paragraph 4.4.1.1) to accommodate the full and low flow rod bow DNBR penalties identified in Reference 94. The referenced penalties are applicable to the Vantage 5H and Vantage+ fuel assembly analyses using the WRB-1 DNB correlation and to RFA assembly analyses using the WRB-2 DNB correlation.

This penalty is the maximum rod bow penalty at an assembly average burnup of 24,000 MWD/MTU. For burnups greater than 24,000 MWD/MTU, credit is taken for the effect of $F_{\square H}$ burndown, due to the decrease in fissionable isotopes and the buildup of fission product inventory. Therefore, no additional rod bow penalty is required at burnups greater than 24,000 MWD/MTU.

In the upper spans of the RFA fuel assembly, additional restraint is provided with the Intermediate Flow Mixer (IFM) grids such that the grid-to-grid spacing in those spans with IFM grids is approximately 10 inches compared to approximately 20 inches in the other spans. Using the NRC approved scaling factor results in predicted channel closure in the limiting 10 inch spans of less than 50 percent closure. Therefore, no rod bow DNBR penalty is required in the 10 inch spans of the RFA safety analyses.

The introduction of new fuel assembly design features in a core reload will result in mixed cores. It can typically take two or three cycles to completely transition to a full core of similar fuel assembly features. Generally, mixed (or transition) core impacts are a result of differences in pressure drop and localized flow distribution between adjacent assemblies with different grid types. The Westinghouse transition core DNB methodology is described in References 101, 102 and 103.

The transition from the 17 x 17 Standard design to Vantage 5H has been previously evaluated in References 104 and 105. With the implementation of the RFA design, IFM grids will be located in spans between the mid grids, where no grids exist in the Vantage 5H or Vantage+ assemblies. Test and analyses have confirmed that the RFA design is hydraulically compatible with the Vantage 5H and Vantage+ fuel designs (References 97, 111 and 115).

Transition cores are analyzed as if they were full cores of one assembly type (full core of Vantage 5H or RFA) and applying an applicable transition core DNBR penalty (Reference 106). The transition core DNBR penalty is a function of the number of new type fuel assemblies in the core. Therefore, the penalty is reduced each subsequent cycle. Since the Vantage+ assembly is dimensionally and hydraulically identical to Vantage 5H, introduction of this design resulted in no transition impact.

4.4.2.4 Flux Tilt Considerations

Significant quadrant power tilts are not anticipated during normal operation since this phenomenon is caused by some asymmetric perturbation. A dropped or misaligned rod cluster control assembly (RCCA) could cause changes in hot-channel factors; however, these events are analyzed separately in Section 15. This discussion will be confined to flux tilts caused by x-y xenon transients, inlet temperature mismatches, enrichment variations within tolerances, etc.

The design value of the enthalpy rise hot-channel factor F^N□H, which includes an 8 percent uncertainty (as discussed in Section 4.3.2.2.7) is assumed to be sufficiently conservative that flux tilts up to and including the alarm point (see Section 16.3.10, Technical Specifications) will not result in values of greater than that assumed in this submittal. The design value of F_Q does not include a specific allowance for quadrant flux tilts.

4.4.2.5 Void Fraction Distribution

The calculated core average and the hot-subchannel maximum and average void fractions are presented in Table 4.4-3 for operation at full power. The void fraction distribution in the core at various radial and axial locations is presented in Reference 52. The void models used in the THINC-IV computer code are described in Section 4.4.2.8.3.

Since void formation due to subcooled boiling is an important promoter of interassembly flow redistribution, a sensitivity study was performed with THINC-IV using the void model referenced above (52)

The results of this study showed that because of the realistic crossflow model used in THINC-IV, the minimum DNBR in the hot-channel is relatively insensitive to variations in this model.

The range of variations considered in this sensitivity study covered the maximum uncertainty range of the data used to develop each part of the void fraction correlation.

4.4.2.6 Core Coolant Flow Distribution

Assembly average coolant mass velocity and enthalpy at various radial and axial core locations are given below. Coolant enthalpy rise and normalized core flow distributions are shown for the 4-foot elevation (1/3 of core height) on Figure 4.4-7, and 8-foot elevation (2/3 of core height) on Figure 4.4-8, and at the core exit on Figure 4.4-9. These distributions are representative of a Westinghouse 4-loop plant. The THINC Code analysis for this case utilized a uniform core inlet enthalpy and inlet flow distribution.

4.4.2.7 Core Pressure Drops and Hydraulic Loads

4.4.2.7.1 Core Pressure Drops

The analytical model and experimental data used to calculate the pressure drops shown in Table 4.4-1 are described in Section 4.4.2.8. The core pressure drop includes the eight grid fuel assembly, core support plate, and holddown plate pressure drops. The full power operation pressure drop values shown in the tables are the unrecoverable pressure drops across the vessel, including the inlet and outlet nozzles, and across the core. These pressure drops are based on the best estimate flow (most likely value for actual plant operating conditions) as described in Section 5.1.1. Section 5.1.1 also defines and describes the thermal design flow (minimum flow) which is the basis for reactor core thermal performance and the mechanical design flow (maximum flow) which is used in the mechanical design of the reactor vessel internals and fuel assemblies. Since the best estimate flow is that flow which is most likely to exist in an operating plant, the

calculated core pressure drops in Table 4.4-1 are based on this best estimate flow rather than the thermal design flow.

Uncertainties associated with the core pressure drop values are discussed in Section 4.4.2.10.

4.4.2.7.2 Hydraulic Loads

The fuel assembly holddown springs, Figure 4.2-2, are designed to keep the fuel assemblies in contact with the lower core plate under all Condition I and II events with the exception of the turbine overspeed transient associated with a loss-of-external load. The holddown springs are designed to tolerate the possibility of an over deflection associated with fuel assembly liftoff for this case and provide contact between the fuel assembly and the lower core plate following this transient. More adverse flow conditions occur during a LOCA as discussed in Section 15.

Hydraulic loads at normal operating conditions are calculated considering the mechanical design flow which is described in Section 5.1 and accounting for the minimum core bypass flow based on manufacturing tolerances. Core hydraulic loads at cold plant startup conditions are adjusted to account for the coolant density difference. Conservative core hydraulic loads for a pump overspeed transient, which create flow rates 20 percent greater than the mechanical design flow, are evaluated to be greater than twice the fuel assembly weight.

Core hydraulic loads were measured during the prototype assembly tests described in Section 1.5. Reference 6 contains a detailed discussion of the results.

The Vantage 5H, Vantage+ and RFA designs have been shown to be hydraulically compatible in References 111 and 115.

4.4.2.8 Correlation and Physical Data

4.4.2.8.1 Surface Heat Transfer Coefficients

Forced convection heat transfer coefficients are obtained from the familiar Dittus-Boelter correlation (53), with the properties evaluated at bulk fluid conditions:

$$\frac{hD_e}{K} = 0.023\left(\frac{D_e G}{\mu}\right)^{0.8}\left(\frac{C_p \mu}{K}\right)^{0.4} \qquad (4.4\text{-}10)$$

where:

h	=	heat transfer coefficient, Btu/hr-ft^2-□F
De	=	equivalent diameter, ft
K	=	thermal conductivity, Btu/hr-ft^2-□F
G	=	mass velocity, lb/hr-ft^2
□	=	dynamic viscosity, lb/ft-hr
C_p	=	heat capacity, Btu/lb-□F

This correlation has been shown to be conservative (54) for rod bundle geometries with pitch to diameter ratios in the range used by PWRs.

The onset of nucleate boiling occurs when the clad wall temperature reaches the amount of superheat predicted by Thom's (55) correlation. After this occurrence the outer clad wall temperature is determined by:

$$\square T_{sat} = (0.072 \exp(-P/1260))\,(q'')^{0.5} \qquad (4.4\text{-}11)$$

where:

$\square T_{sat}$ = wall superheat, $T_w - T_{sat}$, □F

q'' = wall heat flux, Btu/hr-ft^2

P = pressure, psia

T_w = outer clad wall temperature, □F

T_{sat} = saturation temperature of coolant at P, □F

4.4.2.8.2 Total Core and Vessel Pressure Drop

Unrecoverable pressure losses occur as a result of viscous drag (friction) and/or geometry changes (form) in the fluid flow path. The flow field is assumed to be incompressible, turbulent, single-phase water. These assumptions apply to the core and vessel pressure drop calculations for the purpose of establishing the primary loop flow rate. Two-phase considerations are neglected in the vessel pressure drop evaluation because the core average void is negligible (See Section 4.4.2.5 and Table 4.4-3. Two phase flow considerations in the core thermal subchannel analyses are considered and the models are discussed in Section 4.4.3.1.3. Core and vessel pressure losses are calculated by equations of the form:

$$\square P_L = \left(K + F \frac{L}{D_e}\right) \frac{\square V^2}{2 g_c (144)} \qquad (4.4\text{-}12)$$

where:

$\square P_L$ = unrecoverable pressure drop, lb_f/in^2

$\square$ = fluid density, lb_m/ft^3

L = length, ft

D_e = equivalent diameter, ft

V = fluid velocity, ft/sec

$$g_c = 32.174 \frac{lb_m - ft}{lb_f - sec^2}$$

K = form loss coefficient, dimensionless

F = friction loss coefficient, dimensionless

Fluid density is assumed to be constant at the appropriate value for each component in the core and vessel. Because of the complex core and vessel flow geometry, precise analytical values for the form and friction loss coefficients are not available. Therefore, experimental values for these coefficients are obtained from geometrically similar models.

Values are quoted in Table 4.4-1 for unrecoverable pressure loss across the reactor vessel, including the inlet and outlet nozzles, and across the core. The results of full scale tests of core components and fuel assemblies were utilized in developing the core pressure loss characteristic. The pressure drop for the vessel was obtained by combining the core loss with correlation of 1/7th scale model hydraulic test data on a number of vessels (56,57) and from loss relationships (58). Moody (59) curves were used to obtain the single phase friction factors.

Tests of the primary coolant loop flow rates will be made (see Section 4.4.4.1) prior to initial criticality to verify that the flow rates used in the design, which were determined in part from the pressure losses calculated by the method described here, are conservative.

4.4.2.8.3 Void Fraction Correlation

There are three separate void regions considered in flow boiling in a PWR as illustrated on Figure 4.4-10. They are the wall void region (no bubble detachment), the subcooled boiling region (bubble detachment), and the bulk boiling region.

In the wall void region, the point where local boiling begins is determined when the clad temperature reaches the amount of superheat predicted by Thom's (55) correlation (discussed in Section 4.4.2.8.1). The void fraction in this region is calculated using Maurer's (60) relationship. The bubble detachment point, where the superheated bubbles break away from the wall, is determined by using Griffith's (61) relationship.

The void fraction in the subcooled boiling region (that is, after the detachment point) is calculated from the Bowring (62) correlation. This correlation predicts the void fraction from the detachment point to the bulk boiling region.

The void fraction in the bulk boiling region is predicted by using homogeneous flow theory and assuming no slip. The void fraction in this region is therefore a function only of the thermodynamic quality.

4.4.2.9 Thermal Effects of Operational Transients

DNB core safety limits are generated as a function of coolant temperature, pressure, core power, and axial power imbalance. Steady-state operation within these safety limits ensures that the minimum DNBR is not less than the appropriate DNBR limit. Figure 15.1-1 shows the DNBR limit lines and the resulting overtemperature □-T trip lines (which become part of the Technical Specifications), plotted as □-T vs T-average for various pressures.

This system provides adequate protection against anticipated operational transients that are slow with respect to fluid transport delays in the primary system. In addition, for fast transients, e.g., uncontrolled rod bank withdrawal at power incident (Section 15.1.2), specific protection functions are provided as described in Section 7.2 and the use of these protection functions is described in Section 15 (See Table 15.1-2).

The thermal response of the fuel rod is discussed in Section 4.4.3.7.

4.4.2.10 Uncertainties in Estimates

4.4.2.10.1 Uncertainties in Fuel and Clad Temperatures

As discussed in Section 4.4.2.2, the fuel temperature is a function of crud, oxide, clad, gap, and pellet conductances. Uncertainties in the fuel temperature calculation are essentially of two types: fabrication uncertainties such as variations in the pellet and clad dimensions and the pellet density; and model uncertainties such as variations in the pellet conductivity and the gap conductance. These uncertainties have been quantified by comparison of the thermal model to the inpile thermocouple measurements (8 through 14), by out-of-pile measurements of the fuel and clad properties (17 through 28), and by measurements of the fuel and clad dimensions during fabrication. The resulting uncertainties are then used in all evaluations involving the fuel temperature. The effect of densification on fuel temperature uncertainties is presented in Reference 7 and also included in the calculation of total uncertainty.

In addition to the temperature uncertainty described above, the measurement uncertainty in determining the local power and the effect of density and enrichment variations on the local power are considered in establishing the heat flux hot-channel factor. These uncertainties are described in Section 4.3.2.2.

Reactor trip setpoints as specified in the Technical Specifications include allowance for instrument and measurement uncertainties such as calorimetric error, instrument drift, and channel reproducibility, temperature measurement uncertainties, noise, and heat capacity variations.

Uncertainty in determining the cladding temperature results from uncertainties in the crud and oxide thicknesses. Because of the excellent heat transfer between the surface of the rod and the

coolant, the film temperature drop does not appreciably contribute to the uncertainty.

4.4.2.10.2 Uncertainties in Pressure Drops

Core and vessel pressure drops based on the best estimate flow, as discussed in Section 5.1, are quoted in Table 4.4-1. The uncertainties quoted are based on the uncertainties in both the test results and the analytical extension of these values to the reactor application.

A major use of the core and vessel pressure drops is to determine the primary system coolant flow rates as discussed in Section 5.1. In addition, as discussed in Section 4.4.4.1, tests on the primary system prior to initial criticality will be made to verify that a conservative primary system coolant flow rate has been used in the design and analyses of the plant.

4.4.2.10.3 Uncertainties Due to Inlet Flow Maldistribution

The effects of uncertainties in the inlet flow maldistribution criteria used in the core thermal analyses are discussed in Section 4.4.3.1.2.

4.4.2.10.4 Uncertainty in DNB Correlation

The uncertainty in the DNB correlation (Section 4.4.2.3) can be written as a statement on the probability of not being in DNB based on the statistics of the DNB data. This is discussed in Section 4.4.2.3.2.

4.4.2.10.5 Uncertainties in DNBR Calculations

The uncertainties in the DNBRs calculated by THINC analysis (see Section 4.4.3.4.1) due to uncertainties in the nuclear peaking factors are accounted for by applying conservatively high values of the nuclear peaking factors and including measurement error

Allowances in the statistical evaluation of the DNBR limit (see paragraph 4.4.1.1) using the Revised Thermal Design Procedure (Reference 96). In addition, conservative values for the engineering hot channel factors are used as discussed in Section 4.4.2.3.4.

The results of a sensitivity study (52) with THINC-IV show that the minimum DNBR in the hot channel is relatively insensitive to variations in the core-wide radial power distribution (for the same value of $F^N_{\square H}$).

The ability of the THINC-IV computer code to accurately predict flow and enthalpy distributions in rod bundles is discussed in Section 4.4.3.4.1 and in Reference 63. Studies have been performed (52) to determine the sensitivity of the minimum DNBR in the hot-channel to the void fraction correlation (also see Section 4.4.2.8.3); the inlet velocity and exit pressure distributions assumed as boundary conditions for the analysis; and the grid pressure loss coefficients. The results of these studies show that the minimum DNBR in the hot channel is relatively insensitive to variations in these parameters. The range of variations considered in these studies covered the range of possible variations in these parameters.

4.4.2.10.6 Uncertainties in Flow Rates

The uncertainties associated with loop flow rates are discussed in Section 5.1. For core thermal performance evaluations, a thermal design loop flow is used which is less than the best estimate loop flow (approximately 4 percent). In addition, another 7.2 percent of the thermal design flow is assumed to be ineffective for core heat removal capability because it bypasses the core through the various available vessel flow paths described in Section 4.4.3.1.1.

4.4.2.10.7 Uncertainties in Hydraulic Loads

As discussed in Section 4.4.2.7.2, hydraulic loads on the fuel assembly are evaluated for a pump overspeed transient which creates flow rates 20 percent greater than the mechanical design

flow. The mechanical design flow is greater than the best estimate or most likely flow rate value for the actual plant operating condition.

4.4.2.10.8 Uncertainty in Mixing Coefficient

The value of the mixing coefficient, TDC, used in THINC analyses for this application is 0.038. The mean value of TDC obtained in the "R" grid mixing tests described in Section 4.4.2.3.3 was 0.042 (for 26-inch grid spacing). The value of 0.038 is one standard deviation below the mean value, and ≅ 90 percent of the data gives values of TDC greater than 0.038 (43).

The results of the mixing tests done on 17 x 17 geometry, as discussed in Section 4.4.2.3.3, had a mean value of TDC of 0.059 and standard deviation of □ = 0.007. Hence the current design value of TDC is almost 3 standard deviations below the mean for 26-inch grid spacing.

4.4.2.11 Plant Configuration Data

Plant configuration data for the thermal-hydraulic and fluid systems external to the core are provided in the appropriate Sections 5, 6, and 9. Implementation of the ECCS is discussed in Section 15. Some specific areas of interest are the following:

1. Total coolant flow rates for the RCS are provided in Table 5.1-1. Flow rates employed in the evaluation of the core are presented in Section 4.4.

2. Total RCS volume including pressurizer and surge line, RCS liquid volume including pressurizer water at steady state power conditions are given in Table 5.1-1.

3. The flow path length through each volume may be calculated from physical data provided in the above referenced tables.

4. The height of fluid in each component of the RCS may be determined from the physical data presented in Section 5.5. The components of the RCS are water filled during power operation with the pressurizer being approximately 60 percent water filled.

5. The elevation of components of the RCS relative to the reactor vessel are shown in Section 5.1. Components of the ECCS are to be located so as to meet the criteria for net positive suction head (NPSH) described in Section 6.1.

6. Line lengths and sizes for the Safety Injection System are determined so as to guarantee a total system resistance which will provide, as a minimum, the fluid delivery rates assumed in the safety analyses described in Section 15.

4.4.3 Evaluation

4.4.3.1 Core Hydraulics

4.4.3.1.1 Flow Paths Considered in Core Pressure Drop and Thermal Design

The following flow paths or core bypass flow are considered:

1. Flow through the spray nozzles into the upper head for head cooling purposes

2. Flow entering into the RCC guide thimbles to cool the control rods

3. Leakage flow from the vessel inlet nozzle directly to the vessel outlet nozzle through the gap between the vessel and the barrel

4. Flow entering into the core from the baffle-barrel region through the gaps between the baffle plates

The above contributions are evaluated to confirm that the design value of core bypass flow is met. The design value of core bypass flow for Salem Units 1 and 2 is equal to 7.2 percent of the total vessel flow. Of the total allowance, 4.0 percent is associated with the core and the remainder associated with the internals. Calculations have been performed accounting for drawing tolerances and uncertainties in pressure losses. Based on these calculations, the core bypass flow for Salem Units 1 and 2 is no greater than the value quoted above.

Flow model test results for the flow path through the reactor are discussed in Section 4.4.2.8.2.

4.4.3.1.2 Inlet Flow Distributions

Data has been considered from several 1/7 scale hydraulic reactor model tests (56,57,64) in arriving at the core inlet flow maldistribution criteria to be used in the THINC analyses (See Section 4.4.3.4.1). THINC-I analyses made using this data have indicated that a conservative design basis is to consider a 5 percent reduction in the flow to the hot assembly (65). The same design basis of 5 percent reduction to the hot assembly inlet is used in THINC-IV analyses.

The experimental error estimated in the inlet velocity distribution has been considered as outlined in Reference 52 where the sensitivity of changes in inlet velocity distributions to hot channel thermal performance is shown to be small. Studies (52) made with the improved THINC model (THINC-IV) show that it is adequate to use the 5 percent reduction in inlet flow to the hot

assembly for a loop out of service based on the experimental data in References 56 and 57.

The effect of the total flow rate on the inlet velocity distribution was studied in the experiments of Reference 56. As was expected, on the basis of the theoretical analysis, no significant variation could be found in inlet velocity distribution with reduced flow rate.

(This text has been deleted)

4.4.3.1.3 Empirical Friction Factor Correlations

Two empirical friction factor correlations are used in the THINC-IV computer code (described in Section 4.4.3.4.1).

The friction factor in the axial direction, parallel to the fuel rod axis, is evaluated using the Novendstern-Sandberg correlation (66). This correlation consists of the following:

1. For isothermal conditions, this correlation uses the Moody (59) friction factor including surface roughness effects.

2. Under single-phase heating conditions a factor is applied based on the values of the coolant density and viscosity at the temperature of the heated surface and at the bulk coolant temperature.

3. Under two-phase flow conditions the homogeneous flow model proposed by Owens (67) is used with a modification to account for a mass velocity and heat flux effect.

The flow in the lateral directions, normal to the fuel rod axis, views the reactor core as a large tube bank. Thus, the lateral friction factor proposed by Idel'chik (58) is applicable. This correlation is of the form:

$$F_L = A \, \mathrm{Re}_L^{-0.2} \qquad (4.4\text{-}13)$$

where:

A is a function of the rod pitch and diameter as given in Reference 58.

Re_L is the lateral Reynolds number based on the rod diameter.

Extensive comparisons of THINC-IV predictions using these correlations to experimental data are given in Reference 63, and verify the applicability of these correlations in PWR design.

4.4.3.2 Influence of Power Distribution

The core power distribution which is largely established at beginning of life by fuel enrichment, loading pattern, and core power level is also a function of variables such as control rod worth and position, and fuel depletion throughout lifetime. Radial power distributions in various planes of the core are often illustrated for general interest; however, the core radial enthalpy rise distribution as determined by the integral of power up each channel is of greater importance for DNB analyses. These radial power distributions, characterized by $F^N_{\square H}$ (defined in Section 4.3.2.2.2) as well as axial heat flux profiles are discussed in the following two sections.

4.4.3.2.1 Nuclear Enthalpy Rise Hot Channel Factor, $F^N_{\Delta H}$.

Given the local power density q' (kw/ft) at a point x, y, z in a core with N fuel rods and height H,

$$F^N_{\Delta H} = \frac{\text{hot rod power}}{\text{average rod power}} = \frac{\text{Max}\int_0^H q'(x,y,z)\,dz}{\frac{1}{n}\sum_{\text{all rods}}\int_0^H q'(x,y,z)\,dz} \qquad (4.4\text{-}14)$$

where:

x_o, y_o are the position coordinates of the hot rod.

The way in which $F^N_{\Delta H}$ is used in the DNB calculation is important. The location of minimum DNBR depends on the axial profile and the value of DNBR depends on the enthalpy rise to that point. Basically, the maximum value of the rod integral is used to identify the most likely rod for minimum DNBR. An axial power profile is obtained which, when normalized to the design value of $F^N_{\Delta H}$, recreates the axial heat flux along the limiting rod. The surrounding rods are assumed to have the same axial profile with rod average powers which are typical of distributions found in hot assemblies. In this manner worst case axial profiles can be combined with worst case radial distributions for reference DNB calculations.

It should be noted again that $F^N_{\Delta H}$ is an integral and is used as such in the DNB calculations. Local heat fluxes are obtained by using hot channel and adjacent channel explicit power shapes which take into account variations in horizontal power shapes throughout the core. The sensitivity of the THINC-IV analysis to radial power shapes is discussed in Reference 52.

For operation at a fraction P of full power, the design $F^N_{\square H}$ used is given by:

$$F^N_{\square H} = F^{RTP}_{\square H} (1 + PF_{\square H} (1-P)) \qquad (4.4\text{-}15)$$

where $F^{RTP}_{\square H}$ is the limit at Rated Thermal Power (RTP) specified in the COLR

and $PF_{\square H}$ is the Power Factor Multiplier for $F^N_{\square H}$ specified in the COLR

The permitted relaxation of $F^N_{\square H}$ is included in the DNB protection setpoints and allows radial power shape changes with rod insertion to the insertion limits (68,88), thus allowing greater flexibility in the nuclear design.

4.4.3.2.2 Axial Heat Flux Distributions

As discussed in Section 4.3.2.2, the axial heat flux distribution can vary as a result of rod motion, power change, or due to spatial xenon transients which may occur in the axial direction. Consequently, it is necessary to measure the axial power imbalance by means of the ex-core nuclear detectors (as discussed in Section 4.3.2.2.7) and protect the core from excessive axial power imbalance. The Reactor Trip System provides automatic reduction of the trip setpoint in the overtemperature □T channels on excessive axial power imbalance; that is, when an extremely large axial offset corresponds to an axial shape which could lead to a DNBR which is less than that calculated for the reference DNB design axial shape.

The reference DNB design axial shape used for the Reactor Trip System application is a chopped cosine shape with a peak average value of 1.55.

4.4.3.3 Core Thermal Response

A general summary of the steady-state thermal-hydraulic design parameters including thermal output, flow rates, etc, is provided in Table 4.4-1 for all loops in operation.

As stated in Section 4.4.1, the design bases of the application are to prevent DNB and to prevent fuel melting for Condition I and II events. The protective systems described in Section 7 (Instrumentation and Controls) are designed to meet these bases. The response of the core to Condition II transients is given in Section 15.

4.4.3.4 Analytical Techniques

4.4.3.4.1 Core Analysis

The objective of reactor core thermal design is to determine the maximum heat removal capability in all flow subchannels and show that the core safety limits, as presented in the Technical Specifications, are not exceeded while compounding engineering and nuclear effects. The thermal design takes into account local variations in dimensions, power generation, flow redistribution, and mixing. THINC-IV is a realistic three-dimensional matrix model which has been developed to account for hydraulic and nuclear effects on the enthalpy rise in the core (52,63). The behavior of the hot assembly is determined by superimposing the power distribution among the assemblies upon the inlet flow distribution while allowing for flow mixing and flow distribution between assemblies. The average flow and enthalpy in the hottest assembly is obtained from the core-wide, assembly-by-assembly analysis. The local variations in power, fuel rod and pellet fabrication, and mixing within the hottest assembly are then superimposed on the average conditions of the hottest assembly in order to determine the conditions in the hot channel.

The following sections describe the use of the THINC Code in the thermal-hydraulic design evaluation to determine the conditions in the hot channel and to assure that the safety-related design bases are not violated.

Steady State Analysis

The THINC-IV computer program as approved by the Nuclear Regulatory Commission (NRC) (69) is used to determine coolant density, mass velocity, enthalpy, vapor void, static pressure, and DNBR distributions along parallel flow channels within a reactor core under all expected operating conditions. The core region being studied is considered to be made up of a number of contiguous elements in a rectangular array extending the full length of the core. An element may represent any region of the core from a single assembly to a subchannel.

The momentum and energy exchange between elements in the array are described by the equations for the conservation of energy and mass, the axial momentum equation, and two lateral momentum equations which couple each element with its neighbors. The momentum equations used in THINC-IV are similar to the Euler Equations (70) except that frictional loss terms have been incorporated which represent the combined effects of frictional and form drag due to the presence of grids and fuel assembly nozzles in the core. The crossflow resistance model used in the lateral momentum equations was developed from experimental data for flow normal to tube banks (58,71) The energy equation for each element also contains additional terms which represent the energy gain or loss due to the crossflow between elements.

The unique feature in THINC-IV is that lateral momentum equations, which include both inertial and crossflow resistance terms, have been incorporated into the calculational scheme. This differentiates THINC-IV from other thermal-hydraulic programs in which only the lateral resistance term is modeled. Another important consideration in THINC-IV is that the entire velocity field is solved, en masse, by a field equation while in other codes such as THINC-1 (46) and COBRA (72) the solutions are obtained by stepwise integration throughout the array.

The resulting formulation of the conservation equations is more rigorous for THINC-IV; therefore, the solution is more accurate. In addition, the solution method is complex and some simplifying techniques must be employed. Since the reactor flow is chiefly in the axial direction, the core flow field is primarily one-dimensional and it is reasonable to assume that the lateral velocities and the parameter gradients are larger in the axial direction than the lateral direction. Therefore, a perturbation technique can be used to represent the axial and lateral parameters in the conservation equations. The lateral velocity components are regarded as perturbed quantities which are smaller than the unperturbed and perturbed component with the unperturbed component equaling the core average value at a given elevation and the perturbed value is the difference between the local value and the unperturbed component. Since the magnitudes of the unperturbed and perturbed parameters are significantly different, they can be solved separately. The unperturbed equations are one-dimensional and can be solved with the resulting solutions becoming the coefficients of the perturbed equations. An iterative method is then used to solve the system of perturbed equations which couples all the elements in the array.

Three THINC-IV computer runs constitute one design run: a core wide analysis, a hot assembly analysis, and a hot subchannel analysis. While the calculational method is identical for each run, the elements which are modeled by THINC-IV change from run-to-run. In the core wide analysis, the computational elements represent full fuel assemblies. In the second computation the elements represent a quarter of the hot assembly. For the last computation, a quarter of the hot assembly is analyzed and each individual subchannel is represented as a computational element.

The first computation is a core-wide, assembly-by-assembly analysis which uses an inlet velocity distribution modeled from experimental reactor models (56,57,64) (see Section 4.4.3.1.2). In the core-wide analysis the core is considered to be made up of a number of contiguous fuel assemblies divided axially into

increments of equal length. The system of perturbed and unperturbed equations are solved for this array giving the flow, enthalpy, pressure drop, temperature, void fraction in each assembly. The system of equations is solved using the specified inlet velocity distribution and a known exit pressure condition at the top of the core. This computation determines the interassembly energy and flow exchange at each elevation for the hot assembly. THINC-IV stores this information then uses it for the subsequent hot assembly analysis.

In the second computation, each computational element represents one-fourth of the hot assembly. The inlet flow and the amount of momentum and energy interchange at each elevation are known from the previous core wide calculation. The same solution technique is used to solve for the local parameters in the hot one-quarter assembly.

While the second computation provides an overall analysis of the thermal and hydraulic behavior of the hot quarter assembly, it does not consider the individual channels in the hot assembly. The third computation further divides the hot assembly into channels consisting of individual fuel rods to form flow channels. The local variations in power, fuel rod and pellet fabrication, fuel rod spacing and mixing (engineering hot-channel factors) within the hottest assembly are imposed on the average conditions of the hottest fuel assembly in order to determine the conditions in the hot-channel. The engineering hot-channel factors are described in Section 4.4.2.3.4.

Experimental Verifications

An experimental verification (63) of the THINC-IV analysis for core wide, assembly-to-assembly enthalpy rises as well as enthalpy rise in a nonuniformly heated rod bundle have been obtained.

In these experimental tests, the system pressure, inlet temperature, mass flow rate, and heat fluxes were typical of present PWR core designs.

During the operation of a reactor, various core monitoring systems obtain measured data indicating the core performance. Assembly power distributions and assembly mixed mean temperature are measured and can be converted into the proper three-dimensional power input needed for the THINC programs. This data can then be used to verify the Westinghouse thermal-hydraulic design codes.

One standard startup test is the natural circulation test in which the core is held at a very low power (~2 percent) and the pumps are turned off. The core will then be cooled by the natural circulation currents created by the power differences in the core. During natural circulation, a thermal siphoning effect occurs resulting in the hotter assemblies gaining flow, thereby creating significant interassembly crossflow. As described in the preceding section the most important feature of THINC-IV is the method by which crossflow is evaluated. Thus, tests with significant crossflow are of more value in the code verification. Interassembly crossflow is caused by radial variations in pressure. Radial pressure gradients are in turn caused by variations in the axial pressure drops in different assemblies. Under normal operating conditions (subcooled forced convection) the axial pressure drop is due mainly to friction losses. Since all assemblies have the same geometry, all assemblies have nearly the same axial pressure drops and crossflow velocities are small. However, under natural circulation conditions (low flow) the axial pressure drop is due primarily to the difference in elevation head (or coolant density) between assemblies (axial velocity is low and therefore axial friction losses are small). This phenomenon can result in relatively large radial pressure gradients and, therefore, higher crossflow velocities than at normal reactor operating conditions.

The in-core instrumentation was used to obtain the assembly-by-assembly core power distribution during a natural circulation test. Assembly exit temperatures during the natural circulation test on a 157 assembly, three loop plant were predicted using THINC-IV. The predicted data points were plotted as assembly temperature rise versus assembly power and a least squares fitting program used to generate an equation which best fit the data. The result is the straight line presented on Figure 4.4-11. The measured assembly exit temperatures are reasonably uniform, as indicated on this figure, and are predicted closely by the THINC-IV code. This agreement verifies the lateral momentum equations and the crossflow resistance model used in THINC-IV. The larger crossflow resistance used in THINC-I reduces flow redistribution, so that THINC-IV gives better agreement with the experimental data.

Data has also been obtained for Westinghouse plants operating from 67 percent to 101 percent of full power. A representative cross section of the data obtained from a two-loop and a three-loop reactor were analyzed to verify the THINC-IV calculational method. The THINC-IV predictions were compared with the experimental data as shown on Figures 4.4-12 and 4.4-13. The predicted assembly exit temperatures were compared with the measured exit temperatures for each data run. The standard deviation of the measured and predicted assembly exit temperatures were calculated and compared for both THINC-IV and THINC-I and are given in Table 4.4-4. As the standard deviations indicate, THINC-IV generally fits the data somewhat more accurately than THINC-I. For the core inlet temperatures and power of the data examined, the coolant flow is essentially single phase. Thus, one would expect little inter-assembly crossflow and small differences between THINC-IV and THINC-I predictions as seen in the tables. Both codes are conservative and predict exit temperatures higher than measured values for the high powered assemblies.

An experimental verification of the THINC-IV subchannel calculation method has been obtained from exit temperature

measurements in a non-uniformly heated rod bundle (73). The inner nine heater rods were operated at approximately 20 percent more power than the outer rods to create a typical PWR intra-assembly power distribution. The rod bundle was divided into 36 subchannels and the temperature rise was calculated by THINC-IV using the measured flow and power for each experimental test.

Figure 4.4-14 shows, for a typical run, a comparison of the measured and predicted temperature rises as a function of the power density in the channel. The measurements represent an average of two to four measurements taken in various quadrants of the bundle. It is seen that the THINC-IV results predict the temperature gradient across the bundle very well. On Figure 4.4-15, the measured and predicted temperature rises are compared for a series of runs at different pressures, flows, and power levels.

Again, the measured points represent the average of the measurements taken in the various quadrants. It is seen that the THINC-IV predictions provide a good representation of the data.

Extensive additional experimental verification is presented in Reference 63.

The THINC-IV analysis is based on a knowledge and understanding of the heat transfer and hydrodynamic behavior of the coolant flow and the mechanical characteristics of the fuel elements. The use of the THINC-IV analysis provides a realistic evaluation of the core performance and is used in the thermal analyses as described above.

Transient Analysis

The THINC-IV thermal-hydraulic computer code does not have a transient capability. Since the third section of the THINC-I program (46) does have this capability, this code (THINC-III) continues to be used for transient DNB analysis.

The conservation equations needed for the transient analysis are included in THINC-III by adding the necessary accumulation terms to the conservation equations used in the steady-state (THINC-I) analysis. The input description must now include one or more of the following time-dependent arrays:

1. Inlet flow variation
2. Heat flux distribution
3. Inlet pressure history

At the beginning of the transient, the calculation procedure is carried out as in the steady-state analysis. The THINC-III code is first run in the steady-state mode to ensure conservatism with respect to THINC-IV and in order to provide the steady-state initial conditions at the start of the transient. The time is incremented by an amount determined either by the user or by the program itself. At each new time step the calculations are carried out with the addition of the accumulation terms which are evaluated using the information from the previous time step. This procedure is continued until a preset maximum time is reached.

At preselected intervals, a complete description of the coolant parameter distributions within the array as well as DNBR are printed out. In this manner the variation of any parameter with time can be readily determined.

At various times during the transient, steady state THINC-IV is applied to show that the application of the transient version of THINC-I is conservative.

The THINC-III code does not have the capability for evaluating fuel rod thermal response. This is treated by the methods described in Section 15.1.9.

4.4.3.4.2 Fuel Temperatures

As discussed in Section 4.4.2.2, the fuel rod behavior is evaluated utilizing a semi-empirical thermal model which considers, in addition to the thermal aspects, such items as clad creep, fuel swelling, fission gas release, release of absorbed gases, cladding corrosion and elastic deflection, and helium solubility.

A detailed description of the thermal model can be found in Reference 3 with the modifications for time-dependent densification given in Reference 7.

4.4.3.4.3 Hydrodynamic Instability

The analytical methods used to access hydraulic instability are discussed in Section 4.4.3.5.

4.4.3.5 Hydrodynamic and Flow Power Coupled Instability

Boiling flow may be susceptible to thermodynamic instabilities (74). These instabilities are undesirable in reactors since they may cause a change in thermo-hydraulic conditions that may lead to a reduction in the DNB heat flux relative to that observed during a steady flow condition or to undesired forced vibrations of core components. Therefore, a thermo-hydraulic design criterion was developed which states that modes of operation under Condition I and II events shall not lead to thermo-hydrodynamic instabilities.

Two specific types of flow instabilities are considered for Westinghouse PWR operation. These are the Ledinegg or flow excursion type of static instability and the density wave type of dynamic instability.

A Ledinegg instability involves a sudden change in flow rate from one steady state to another. This instability occurs (74) when

the slope of the reactor coolant system pressure drop-flow rate curve

$\left(\frac{\partial \Delta P}{\partial G}\Big|_{internal}\right)$ becomes algebraically smaller than the loop supply

(pump head) pressure drop-flow rate curve $\frac{\partial \Delta P}{\partial G}\Big|_{external}$) The

criterion for stability is thus $\frac{\partial \Delta P}{\partial G}_{internal} \geq \frac{\partial \Delta P}{\partial G}_{external}$.

The Westinghouse pump head curve has a negative slope ($\partial \Delta P/\partial G/external < 0$) whereas the Reactor Coolant System pressure drop-flow curve has a positive slope ($\partial \Delta P/\partial G/internal > 0$) over the Condition I and Condition II operational ranges. Thus, the Ledinegg instability will not occur.

The mechanism of density wave oscillations in a heated channel has been described by Lahey and Moody (75). Briefly, an inlet flow fluctuation produces an enthalpy perturbation. This perturbs the length and the pressure drop of the single phase region and causes quality or void perturbations in the two-phase regions which travel up the channel with the flow. The quality and length perturbations in the two-phase region create two-phase pressure drop perturbations. However, since the total pressure drop across the core is maintained by the characteristics of the fluid system external to the core, then the two-phase pressure drop perturbation feeds back to the single phase region. These resulting perturbations can be either attenuated or self-sustained.

A simple method has been developed by Ishii (76) for parallel closed channel systems to evaluate whether a given condition is stable with respect to the density wave type of dynamic instability. This method had been used to assess the stability of

typical Westinghouse reactor designs (77,78,79) under Condition I and II operation. The results indicate that a large margin to density wave instability exists, e.g., increases on the order of 200 percent of rated reactor power would be required for the predicted inception of this type of instability.

The application of the method of Ishii (76) to Westinghouse reactor designs is conservative due to the parallel open channel feature of Westinghouse PWR cores. For such cores, there is little resistance to lateral flow leaving the flow channels of high power density. There is also energy transfer from channels of high power density to lower power density channels. This coupling with cooler channels has led to the opinion that an open channel configuration is more stable than the above closed channel analysis under the same boundary conditions. Flow stability tests (80) have been conducted where the closed channel systems were shown to be less stable than when the same channels were cross connected at several locations. The cross connections were such that the resistance to channel crossflow and enthalpy perturbations would be greater than that which would exist in a PWR core which has a relatively low resistance to crossflow.

Flow instabilities which have been observed have occurred almost exclusively in closed channel systems operating at low pressures relative to the Westinghouse PWR operating pressures. Kao, Morgan, and Parker (81) analyzed parallel closed channel stability experiments simulating a reactor core flow. These experiments were conducted at pressures up to 2200 psia. The results showed that for flow and power levels typical of power reactor conditions, no flow oscillations could be induced above 1200 psia.

Additional evidence that flow instabilities do not adversely affect thermal margin is provided by the data from the rod bundle DNB tests. Many Westinghouse 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.

In summary, it is concluded that thermo-hydrodynamic instabilities will not occur under Condition I and II modes of operation for Westinghouse PWR reactor designs. A large power margin, greater than doubling rated power, exists due to predicted inception of such instabilities. Analysis has been performed which shows that minor plant-to-plant differences in Westinghouse reactor designs such as fuel assembly arrays, core power flow ratios, fuel assembly length, etc, will not result in gross deterioration of the above power margins.

4.4.3.6 Temperature Transient Effects Analysis

Waterlogging damage of a fuel rod could occur as a consequence of a power increase on a rod after water has entered the fuel rod through a clad defect. Water entry will continue until the fuel rod internal pressure is equal to the reactor coolant pressure. A subsequent power increase raises the temperature and, hence, could raise the pressure of the water contained within the fuel rod. The increase in hydrostatic pressure within the fuel rod then drives a portion of the water from the fuel rod through the water entry defect. Clad distortion and/or rupture can occur if the fuel rod internal pressure increase is excessive due to insufficient venting of water to the reactor coolant. This occurs when there is both a rapid increase in the temperature of the water within the fuel rod and a small defect. Zircaloy clad fuel rods which have failed due to waterlogging (82,83) indicate that very rapid power transients are required for fuel failure. Normal operational transients are limited to about 40 cal/gm-min. (peak rod) while the Spert tests (82) indicate that 120 to 150 cal/gm are required to rupture the clad even with very short transients (5.5 msec. period). Release of the internal fuel rod pressure is expected to have a minimal effect on the Reactor Coolant System (82) and is not expected to result in failure of additional fuel rods (83). Ejection of fuel pellet fragments into the coolant stream is not expected (82,83). A clad breech due to water logging is thus expected to be similar to any fuel rod failure mechanism which exposes fuel pellets to the reactor

coolant stream. Waterlogging has not been identified as the mechanism for clad distortion or perforation of any Westinghouse Zircaloy-4 clad fuel rods.

High fuel rod internal gas pressure could cause clad failure. One of the fuel rod design bases (Section 4.2.1.1.1) is that the fuel rod internal gas pressure is limited to a value below that which could cause (1) the diametral gap to increase due to outward cladding creep during steady-state operation, and (2) extensive DNB propagation to occur. During operational transients, fuel rod clad rupture due to high internal gas pressure is precluded by meeting the above design basis.

4.4.3.7 Potentially Damaging Temperature Effects During Transients

The fuel rod experiences many operational transients (intentional maneuvers) during its residence in the core. A number of thermal effects must be considered when analyzing the fuel rod performance.

The clad can be in contact with the fuel pellet at some time in the fuel lifetime. Clad-pellet interaction occurs if the fuel pellet temperature is increased after the clad is in contact with the pellet. Clad-pellet interaction is discussed in Section 4.2.1.3.1.

The potential effects of operation with waterlogged fuel are discussed in Section 4.4.3.6 which concluded that waterlogging is not a concern during operational transients.

Clad flattening, as noted in Section 4.2.1.3.1, has been observed in some operating power reactors. Thermal expansion (axial) of the fuel rod stack against a flattened section of clad could cause failure of the clad. This is no longer a concern because clad flattening is precluded during the fuel residence in the core (See Section 4.2.1.3.1).

There can be differential thermal expansion between the fuel rods and the guide thimbles during a transient. Excessive bowing of the fuel rods could occur if the grid assemblies did not allow axial movement of the fuel rods relative to the grids. Thermal expansion of the fuel rods is considered in the grid design so that axial loads imposed on the fuel rods during a thermal transient will not result in excessively bowed fuel rods (See Section 4.2.1.2.2).

4.4.3.8 Energy Release During Fuel Element Burnout

As discussed in Section 4.4.3.3 the core is protected from going through DNB over the full range of possible operating conditions. At full power nominal operation, the probability of a rod going through DNB is less than 0.1 percent at a 95 percent confidence level. In the extremely unlikely event that DNB should occur, the clad temperature will rise due to the steam blanketing at the rod surface and the consequent degradation in heat transfer. During this time there is a potential for a chemical reaction between the cladding and the coolant. However, because of the relatively good film boiling heat transfer following DNB, the energy release resulting from this reaction is insignificant compared to the power produced by the fuel.

DNB With Physical Burnout - Westinghouse (73) has conducted DNB tests in a 25-rod bundle where physical burnout occurred with one rod. After this occurrence, the 25-rod test section was used for several days to obtain more DNB data from the other rods in the bundle. The burnout and deformation of the rod did not affect the performance of neighboring rods in the test section during the burnout or the validity of the subsequent DNB data points as predicted by the W-3 correlation. No occurrences of flow instability or other abnormal operation were observed.

DNB With Return to Nucleate Boiling - Additional DNB tests have been conducted by Westinghouse (84) in 19 and 21 rod bundles. In these tests, DNB without physical burnout was experienced more than once on single rods in the bundles for short periods of time. Each time, a reduction in power of approximately 10 percent was sufficient to reestablish nucleate boiling on the surface of the rod. During these and subsequent tests, no adverse effects were observed on this rod or any other rod in the bundle as a consequence of operating in DNB.

4.4.3.9 Energy Release or Rupture of Waterlogged Fuel Elements

A full discussion of waterlogging, including energy release, is contained in Section 4.4.3.6. It is noted that the resulting energy release is not expected to affect neighboring fuel rods.

4.4.3.10 Fuel Rod Behavior Effects from Coolant Flow Blockage

Coolant flow blockages can occur within the coolant channels of a fuel assembly or external to the reactor core. The effects of fuel assembly blockage within the assembly on fuel rod behavior are more pronounced than external blockages of the same magnitude. In both cases the flow blockages cause local reductions in coolant flow. The amount of local flow reduction, where it occurs in the reactor, and how far along the flow stream the reduction persists are considerations which will influence the fuel rod behavior. The effects of coolant flow blockages in terms of maintaining rated core performance are determined both by analytical and experimental methods. The experimental data are usually used to augment analytical tools such as computer programs similar to the THINC-IV program. Inspection of the DNB correlation (Section 4.4.2.3 and References 42, 98, 97 and 113) shows that the predicted DNBR is dependent upon the local values of quality and mass velocity.

The THINC-IV code is capable of predicting the effects of local flow blockages on DNBR within the fuel assembly on a subchannel basis, regardless of where the flow blockage occurs. In Reference

63, it is shown that for a fuel assembly similar to the Westinghouse design, THINC-IV accurately predicts the flow distribution within the fuel assembly when the inlet nozzle is completely blocked. Full recovery of the flow was found to occur about 30 inches downstream of the blockage. With the reference reactor operating at the nominal full power conditions specified in Table 4.4-1 the effects of an increase in enthalpy and decrease in mass velocity in the lower portion of the fuel assembly would not result in the reactor reaching the DNBR limit.

From a review of the open literature it is concluded that flow blockage in "open lattice cores" similar to the Westinghouse cores causes flow perturbations which are local to the blockage. For instance, A. Oktsubo et al (85) show that the mean bundle velocity is approached asymptotically about 4 inches downstream from a flow blockage in a single flow cell. Similar results were also found for 2 and 3 cells completely blocked. Basmer et al (86) tested an open lattice fuel assembly in which 41 percent of the subchannels were completely blocked in the center of the test bundle between spacer grids. Their results show the stagnant zone behind the flow blockage essentially disappears after 1.65 L/De or about 5 inches for their test bundle. They also found that leakage flow through the blockage tended to shorten the stagnant zone or in essence the complete recovery length. Thus, local flow blockages within a fuel assembly have little effect on subchannel enthalpy rise. The reduction in local mass velocity is then the main parameter which affects the DNBR. If the Salem Units 1 and 2 were operating at full power and nominal steady state conditions as specified in Table 4.4-1, a reduction in local mass velocity of 80 percent in the Vantage 5H and Vantage+ fuel and 60 percent in the RFA fuel would be required to reduce the DNBR to the DNBR limit. The above mass velocity effect on the DNB correlation was based on the assumption of fully developed flow along the full channel length. In reality a local flow blockage is expected to promote turbulence and thus would likely not affect DNBR at all.

Coolant flow blockages induce local crossflows as well as promote turbulence. Fuel rod behavior is changed under the influence of a sufficiently high crossflow component. Fuel rod vibration could occur, caused by this crossflow component, through vortex shedding or turbulent mechanisms. If the crossflow velocity exceeds the limit established for fluid elastic stability, large amplitude whirling results. The limits for a controlled vibration mechanism are established from studies of vortex shedding and turbulent pressure fluctuations. Crossflow velocity above the established limits can lead to mechanical wear of the fuel rods at the grid support locations. Fuel rod wear due to flow induced vibration is considered in the fuel rod fretting evaluation (Section 4.2).

4.4.4 Testing and Verification

4.4.4.1 Tests Prior to Initial Criticality

A reactor coolant flow test, as noted in Item 5 of Table 13.3-1, is performed following fuel loading but prior to initial criticality. Coolant loop pressure drop data is obtained in this test. This data, in conjunction with coolant pump performance information, allow determination of the coolant flow rates at reactor operating conditions. This test verifies that proper coolant flow rates have been used in the core thermal and hydraulic analysis.

4.4.4.2 Initial Power and Plant Operation

Core power distribution measurements are made at several core power levels (see Section 4.3.2.2.7). These tests are used to ensure that conservative peaking factors are used in the core thermal and hydraulic analysis.

Additional demonstration of the overall conservatism of the THINC analysis was obtained by comparing THINC predictions to in-core thermocouple measurements. These measurements were performed on the Zion reactor (Reference 87). (Distribution of in-core

instrumentation for Salem Units 1 and 2 is shown on Figures 4.4-16 and 4.4-17, respectively). No further in-pile testing is planned.

4.4.4.3 Component and Fuel Inspections

Inspections performed on the manufactured fuel are delineated in Section 4.2.1.4. Fabrication measurements critical to thermal and hydraulic analysis are obtained to verify that the engineering hot channel factors employed in the design analyses (Section 4.2.3.4) are met.

4.4.4.4 Augmented Startup Test Program

Salem Unit No. 1 will participate in the augmented startup test program as necessary in accordance with the program outlined in the Westinghouse Topical Report, Augmented Startup and Cycle 1 Physics Program, WCAP-8575.

4.4.5 References for Section 4.4

1. Christensen, J. A.; Allio, R. J.; and Biancheria, A., "Melting Point of Irradiated UO_2," WCAP-6065, February 1965.

2. Risher, D. H., Jr., "An Evaluation of the Rod Ejection Accident in Westinghouse Pressurized Water Reactors Using Spatial Kinetics Methods," WCAP-7588, Revision 1, December 1971.

3. Supplemental information on fuel design transmitted from R. Salvatori, Westinghouse NES, to D. Knuth, AEC, as attachments to letters NS-SL-518 (12/22/72) (NS-SL-521 (12/29/72), NS-SL-524 (12/29/72) and NS-SL-543 (1/12/73), (Proprietary), and supplemental information on fuel design transmitted from R. Salvatori, Westinghouse NES, to D. Knuth, AEC, as attachments to letters NS-SL-527 (1/2/73) and NS-SL-544 (1/12/73).

4. Hill, K. W.; Motley, F. E.; Cadek, F. F.; and Wenzel, A. H., "Effect of 17 x 17 Fuel Assembly Geometry on DNB," WCAP-8296-P-A (Proprietary) and WCAP-8297-A (Nonproprietary), February 1975.

5. Motley, F. E.; Wenzel, A. H.; and Cadek, F. F., "Critical Heat Flux Testing of 17 x 17 Fuel Assembly Geometry with 22 Inch Grid Spacing," WCAP-8536 (Proprietary) and WCAP-8537 (Nonproprietary), May 1975.

6. Nakazato, S. and DeMario, E. E., "Hydraulic Flow Test of the 17 x 17 Fuel Assembly," WCAP-8278, February 1974 (Proprietary) and WCAP-8279, February 1974.

7. Hellman, J. M. (Ed.), "Fuel Densification Experimental Results and Model for Reactor Application," WCAP-8218-P-A (Proprietary) and WCAP-8219-A (Nonproprietary), March 1975.

8. Kjaerheim, G. and Rolstad, E., "In Pile Determination of U0 Thermal Conductivity, Density Effects and Gap Conductance," HPR-80, December 1967.

9. Kjaerheim, G., "In-Pile Measurements of Centre Fuel Temperatures and Thermal Conductivity Determination of Oxide Fuels," Paper IFA-175 presented at the European Atomic Energy Society Symposium on Performance Experience of Water-Cooled Power Reactor Fuel, Stockholm, Sweden, October 21-22, 1969.

10. Cohen, I.; Lustman, B.; and Eichenberg, J. D., "Measurement of the Thermal Conductivity of Metal-Clad Uranium Oxide Rods During Irradiation," WAPD-228, 1960.

11. Clough, D. J. and Sayers, J. B., "The Measurement of the Thermal Conductivity of $U0_2$ under Irradiation in the Temperature Range 150□-1600□C," AERE-R-4690, UKAEA Research Group, Harwell, December 1964.

12. Stora, J. P.; DeBernardy DeSigoyer B.; Delamas, R.; Deschamps, P.; Ringot, C.; and Lavaud, B., "Thermal Conductivity of Sintered Uranium Oxide under In-Pile Conditions," EURAEC-1095, 1964.

13. Devold, I., "A Study of the Temperature Distribution in $U0_2$ Reactor Fuel Elements," AE-318, Aktiebolaget Atomenergi, Stockholm, Sweden, 1968.

14. Balfour, M. G.; Christensen, J. A.; and Ferrari, H. M., "In-Pile Measurement of $U0_2$ Thermal Conductivity," Final Report, WCAP-2923, 1966.

15. Duncan, R. N., "Rabbit Capsule Irradiation of U0," CVTR Project, CVNA-142, June 1962.

16. Nelson, R. C.; Coplin, D. H.; Lyons, M. F.; and Weidenbaum, B., "Fission Gas Release from $U0_2$ Fuel Rods with Gross Central Melting," GEAP-4572, July 1964.

17. Howard, V. C. and Gulvin, T. G., "Thermal Conductivity Determinations of Uranium Dioxide by a Radial Flow Method," UKAEA IG-Report 51, November 1960.

18. Lucks, C. F. and Deem, H. W., "Thermal Conductivity and Electrical Conductivity of $U0_2$," In Progress Reports Relating to Civilian Applications, BMI-1448 (Rev.) for June 1960; BMI-1489 (Rev.) for December 1960 and BMI-1518 (Rev.) for May 1961.

19. Daniel, J. L.; Matolich, J., Jr.; and Deem, H. W., "Thermal Conductivity of $U0_2$," HW-69945, September 1962.

20. Feith, A. D., "Thermal Conductivity of $U0_2$ by a Radial Heat Flow Method," TID-21668, 1962.

21. Vogt, J.; Grandell, L.; and Runfors, U., "Determination of the Thermal Conductivity of Unirradiated Uranium Dioxide," AB Atomenergi Report RMB-527, 1964, Quoted by IAEA Technical Report Series No. 59, "Thermal Conductivity of Uranium Dioxide."

22. Nishijima, T.; Kawada, T.; and Ishihata, A., "Thermal Conductivity of Sintered $U0_2$ and $A1_2u_3$ at High Temperatures," J. American Ceramic Society, 48, 31-34, 1965.

23. Ainscough, J. B. and Wheeler, M.J., "The Thermal Diffusivity and Thermal Conductivity of Sintered Uranium Dioxide," in "Proceedings of the Seventh Conference on Thermal Conductivity," p. 467, National Bureau of Standards, Washington, 1968.

24. Godfrey, T. G.; Fulkerson, W.; Killie, J. P.; Moore, J. P.; and McElroy, D. L., "Thermal Conductivity of Uranium Dioxide and Armco Iron by an Improved Radial Heat Flow Technique," ORNL-3556, June 1964.

25. Stora, J. P. et al, "Thermal Conductivity of Sintered Uranium Oxide Under In-Pile Conditions," EURAEC-1095, August 1964.

26. Bush, A. J., "Apparatus for Measuring Thermal Conductivity to 2500□C," Westinghouse Research Laboratories Report 64-1P6-401-R3 (Proprietary), February 1965.

27. Asamoto, R. R.; Anselin, F. L.; and Conti, A. E., "The Effect of Density on the Thermal Conductivity of Uranium Dioxide," GEAP-5493, April 1968.

28. Kruger, O. L., "Heat Transfer Properties of Uranium and Plutonium Dioxide," Paper 11-N-68F presented at the Fall Meeting of Nuclear Division of the American Ceramic Society, Pittsburgh, September 1968.

29. Gyllander, J. A., "In-Pile Determination of the Thermal Conductivity of UO_2 in the Range 500-2500□C," AE-411, January 1971.

30. Lyons, M. F. et al, "UO_2 Powder and Pellet Thermal Conductivity During Irradiation," GEAP-5100-1, March 1966.

31. Coplin, D. H. et al, "The Thermal Conductivity of UO_2 by Direct In-Reactor Measurements," GEAP-5100-6, March 1968.

32. Bain, A. S., "The Heat Rating Required to Produce Center Melting in Various UO_2 Fuels," ASTM Special Technical Publication, No. 306, pp. 30-46, Philadelphia, 1962.

33. Stora, J. P., "In-Reactor Measurements of the Integrated Thermal Conductivity of UO_2 - Effect of Porosity," Trans. ANS, 13, 137-138, 1970.

34. International Atomic Energy Agency, "Thermal Conductivity of Uranium Dioxide," Report of the Panel held in Vienna, April 1965, IAEA Technical Reports Series, No. 59, Vienna, The Agency, 1966.

35. Poncelet, C. G., "Burnup Physics of Heterogeneous Reactor Lattices," WCAP-6069, June 1965.

36. Nodvick, R. J., "Saxton Core II Fuel Performance Evaluation," WCAP-3385-56, Part II, "Evaluation of Mass Spectrometric and Radiochemical Analyses of Irradiated Saxton Plutonium Fuel," July 1970.

37. Dean, R. A., "Thermal Contact Conductance Between U0 and Zircaloy-2," CVNA-127, May 1962.

38. Ross, A. M. and Stoute, R. L., "Heat Transfer Coefficient Between UO_2 and Zircaloy-2," AECL-1552, June 1962.

39. Tong, L. S., "Boiling Crisis and Critical Heat Flux," AEC Critical Review Series, TID-25887, 1972.

40. Motley, F. E. and Cadek, F. F., "DNB Test Results for New Mixing Vane Grids (R)," WCAP-7695-P-A (Proprietary) and WCAP-7958-A (Nonproprietary), January 1975.

41. Motley, F. E. and Cadek, F. F., "Application of Modified Spacer Factor to L. Grid Typical and Cold Wall Cell DNB," WCAP-7988-P-A (Proprietary) and WCAP-8030-A (Nonproprietary), January 1975.

42. Tong, L. S., "Prediction of Departure from Nucleate Boiling for an Axially Nonuniform Heat Flux Distribution," Journal of Nuclear Energy, 21, 241-248, 1967.

43. Cadek, F. F.; Motley, F. E.; and Dominicis, D. P., "Effect of Axial Spacing on Interchannel Thermal Mixing with the R Mixing Vane Grid," WCAP-7941-P-A (Proprietary) and WCAP-7959-A (Nonproprietary), January 1975.

44. Motely, F. E. and Cadek, F. F., "DNB Tests Results for New Mixing Vane Grids (R), "WCAP-7695-P-A, (Proprietary), and WCAP-7958-A, (Nonproprietary), January 1975.

45. Motely, F. E. and Cadek, F. F., "DNB Test Results for R Grid Thimble Cold Wall Cells," WCAP-7695-AI-P-A, Addendum I, (Proprietary), and WCAP-7958-A1-A Addendum I (Nonproprietary), January 1975.

46. Chelemer, H.; Weisman, J.; and Tong, L. S., "Subchannel Thermal Analysis of Rod Bundle Cores," WCAP-7015, Revision 1, January 1969.

47. Rowe, D. S. and Angle, C. W., "Crossflow Mixing Between Parallel Flow Channels During Boiling, Part II Measurement of Flow and Enthalpy in Two Parallel Channels," BNWL-371, Part 2, December 1967.

48. Rowe, D. S. and Angle, C. W., "Crossflow Mixing Between Parallel Flow Channels During Boiling, Part III Effect of Spacers on Mixing Between Two Channels," BNWL-371, Part 3, January 1969.

49. Gonzalez-Santalo, J. M. and Griffith, P., "Two-Phase Flow Mixing in Rod Bundle Subchannels," ASME Paper 72-WA/NE-19.

50. Motely, F. E.; Wenzel, A. H.; and Cadek, F. F., "The Effect of 17 x 17 Fuel Assembly Geometry on Interchannel Thermal Mixing," WCAP-8298-P-A (Proprietary) and WCAP-8299-A (Nonproprietary), January 1975.

51. Cadek, F. F., "Interchannel Thermal Mixing with Mixing Vane Grids," WCAP-7667-P-A (Proprietary), and WCAP-7755-A (Nonproprietary), January 1975.

52. Hochreiter, L. E. and Chelemer, H., "Application of the THINC IV Program to PWR Design," WCAP-8054 (Proprietary) and WCAP-8195 (Nonproprietary) September 1973.

53. Dittus, F. W. and Boetler, L. M. K., "Heat Transfer in Automobile Radiators of the Tubular Type," California University Publication in Eng., 2, No. 13, 443-461, 1930.

54. Weisman, J., "Heat Transfer to Water Flowing Parallel to Tube Bundles," Nuclear Science and Engineering, 6, 78-79, 1959.

55. Thom, J. R. S.;, Walker, W. M.; Fallon, T. A.; and Reising, G. F. S., "Boiling in Subcooled Water During Flowup Heated Tubes or Annuli," Proc. Instn. Mech. Engrs., 180, Pt. C., 226-246, 1965-66.

56. Hestroni, G., "Hydraulic Tests of the San Onofre Reactor Model," WCAP-3269-8, June 1964.

57. Hestroni, G., "Studies of the Connecticut-Yankee Hydraulic Model," NYO-3250-2, June 1965.

58. Idel'chik, I. E., "Handbook of Hydraulic Resistance," AEC-TR-6630, 1960.

59. Moody, L. F., "Friction Factors for Pipe Flow," Transaction of the American Society of Mechanical Engineers, 66, 671-684, 1944.

60. Mauer, G. W., "A Method of Predicting Steady State Boiling Vapor Fractions in Reactor Coolant Channels," WAPD-BT-19, pp. 59-70, June 1960.

61. Griffith, P.; Clark, J. A.; and Rohsenow, W. M., "Void Volumes in Subcooled Boiling Systems," ASME Paper No. 58-HT-19.

62. Bowring, R. W., "Physical Model, Based on Bubble Detachment, and Calculation of Steam Voidage in the Subcooled Region of a Heated Channel," HPR-10, December 1962.

63. Hochreiter, L. E.; Chelemer, H.; and Chu, P. T., "THINC-IV: An Improved Program for Thermal-Hydraulic Analysis of Rod Bundle Cores," WCAP-7956, June 1973.

64. Carter, F. D., "Inlet Orificing of Open PWR Cores," WCAP-9004, January 1969 (Proprietary) and WCAP-7836 (Nonproprietary), January 1972.

65. Shefcheck, J., "Application of the THINC Program to PWR Design," WCAP-7359-L, August 1969 (Proprietary) and WCAP-7838, January 1972.

66. Novendstern, E. H. and Sandberg, R. O., "Single Phase Local Boiling and Bulk Boiling Pressure Drop Correlations," WCAP-2850, April 1966 (Proprietary) and WCAP-7916 (Nonproprietary), April 1966.

67. Owens, W. L., Jr., "Two-Phase Pressure Gradient," In: International Developments in Heat Transfer, Part II pp. 363-368, ASME, New York, 1961.

68. McFarlane, A. F., "Power Peaking Factors," WCAP-7912-P-A, (Proprietary) and WCAP-7912-A (Nonproprietary), January 1975.

69. Letter from J. F. Stolz (NRC) to C. Eicheldinger (Westinghouse), Subject: Staff Evaluation of WCAP-7956, WCAP-8054, WCAP-8567, and WCAP-8762, April 19, 1978.

70. Vallentine, H. R., "Applied Hydrodynamics," Butterworth Publishers, London, 1959.

71. Kays, W. M. and London, A. L., "Compact Heat Exchangers," National Press, Palo Alto, 1955.

72. Rowe, D. W., "COBRA-III, a Digital Computer Program for Steady State and Transient Thermal-Hydraulic Analysis of Rod Bundle Nuclear Fuel Elements," BNWL-B-82, 1971.

73. Weissan, J.; Wenzel, A. H.; Tong, L. S.; Fitzsimmons, D.; Thorne, W.; and Batch, J., "Experimental Determination of the Departure from Nucleate Boiling in Large Rod Bundles at High Pressures," Chem. Eng. Prog. Symp. Ser. 64, No. 82, 114-125, 1968.

74. Boure, J. A.; Bergles, A. E., and Tong, L. S., "Review of Two Phase Flow Instability," Nuclear Engineering Design, 25 p. 165-192, 1973.

75. Lahey, R. T. and Moody, F. J., "The Thermal Hydraulics of a Boiling Water Reactor," American Nuclear Society, 1977.

76. Saha, P.; Ishii, M.; and Zuber, N., "An Experimental Investigation of the Thermally Induced Flow Oscillations in Two-Phase Systems," Journal of Heat Transfer, pp. 616-622, November 1976.

77. Virgil C. Summer FSAR, Docket No. 50-395.

78. Byron/Braidwood FSAR, Docket No. 50-456.

79. South Texas FSAR, Docket No. 50-498.

80. Kakac, S.; Verziroglu, T. N.; Akyuzlu, K.; and Berkol, O.; "Sustained and Transient Boiling Flow Instabilities in a Cross-Connected Four-Parallel-Channel Upflow System," Proc. of 5th International Heat Transfer Conference, Tokyo, September 3-7, 1974.

81. Kao, H. S.; Morgan, T. D.; and Parker, W. B., "Prediction of Flow Oscillation in Reactor Core Channel," Trans. ANS, Vol. 16, pp. 212-213, 1973.

82. Stephan, L. A., "The Effects of Cladding Material and Heat Treatment on the Response of Waterlogged UO Fuel Rods to Power Bursts," IN-ITR-111, January 1970.

83. Western New York Nuclear Research Center Correspondence with the AEC on February 11 and August 27, 1971, Docket 50-57.

84. Tong, L. S. et al, "Critical Heat Flux (DNB) in Square and Triangular Array Rod Bundles," presented at the Japan Society of Mechanical Engineers Semi-International Symposium held at Tokyo, Japan, September 4-8, 1967, pages 25-34.

85. Ohtsubo, A. and Uruwashi, S., "Stagnant Fluid due to Local Flow Blockage," Journal of Nuclear Science and Technology 9, No. 7, 433-434, 1972.

86. Basmer, P.; Kirsh, D.; and Schultheiss, G. F., "Investigation of the Flow Pattern in the Recirculation Zone Downstream of Local Coolant Blockages in Pin Bundles," Atomwirtschaft, 17, No. 8, 416-417, 1972. (In German)

87. Burke, T. M.; Meye, C. E.; and Shefcheck, J., "Analysis of Data from the Zion (Unit 1) THINC Verification Test," WCAP-8453-A, May 1976.

88. PSE&G Memorandum, E. A. Liden to S. A. Varga, November 18, 1982.

89. Motley, F. E., Hill, K. W., Cadek, F. F. and Shefcheck, J., "New Westinghouse Correlation WRB-1 for Predicting Critical Heat Flux in Rod Bundles with Mixing Vane Grids," WCAP-8762 (Proprietary) and WCAP-8763 (non-Proprietary), July 1984.

90. Letter, D. F. Ross, Jr. (NRC) to D. B. Vassala (NRC), "Topical Report Evaluation for WCAP-8762," April 10, 1978.

91. Letter, R. L. Tedesco (NRC) to T. M. Anderson (Westinghouse), "Acceptance for Referencing Topical Report, WCAP-9401 (Proprietary)/WCAP-9402 (Non-Proprietary)," May 1981.

92. Davidson, S. L. (ed.), et al, "Vantage 5H Fuel Assembly," WCAP-10444-P-A, Addendum 2, April 1988 and Letter from W. J. Johnson (Westinghouse) to M. W. Hodges (NRC), NS-NRC-88-3363, dated July 29, 1988; "Supplemental Information for WCAP-10444-P-A Addendum 2, "Vantage 5H Fuel Assembly."

93. Skaritka, J., (Ed.), "Fuel Rod Bow Evaluation," WCAP-8691 Revision 1, (Proprietary), July 1979.

94. "Partial Response to Request Number 1 for Additional Information on WCAP-8691, Revision 1," letter, E. P. Rahe, Jr. (Westinghouse to J. R. Miller (NRC), NS-EPR-2515, dated October 9, 1981; "Remaining Response to Request Number 1 for Additional Information on WCAP-8691, Revision 1," letter, E. P. Rahe, Jr. (Westinghouse) to J. R. Miller (NRC), NS-EPR-2572, dated March 16, 1982.

95. Letter from C. Berlinger (NRC) to E. P. Rahe, Jr. (Westinghouse), "Request for Reduction in Fuel Assembly Burnup Limit for Calculation of Maximum Rod Bow Penalty," June 18, 1986.

96. Friedland, A. J. and Ray, S., "Revised Thermal Design Procedure," WCAP-11397-P-A, April 1989.

97. Davidson, S. L. and Kramer, W. R. (Ed.), "Reference Core Report VANTAGE 5 Fuel Assembly," WCAP-10444-P-A, September 1985.

98. Tong, L. S., "Critical Heat Fluxes in Rod Bundles, Two Phase Flow and Heat Transfer in Rod Bundles," Annual Winter Meeting ASME, November 1968, p. 3146.

99. Scherder, W. J. (Ed.) and McHugh, C. J. (Ed.), "Reactor Core Response to Excessive Secondary Steam Releases," WCAP-9226-P-A, Revision 1, 1998.

100. Motley, F. E., Cadek, F. F., "DNB Test Results for R-Grid Thimble Cold Wall Cells," WCAP-7695-L, Addendum 1, October 1972.

101. Davidson, S. L. and Iorii, J. A., "Reference Core Report - 17x17 Optimized Fuel Assembly," WCAP-9500-A, May 1982.

102. Letter from E. P. Rahe (Westinghouse) to Miller (NRC) dated March 19, 1982, NS-EPR-2573, WCAP-9500 and WCAPS-9401/9402 NRC SER Mixed Core Compatibility Items.

103. Letter from C. O. Thomas (NRC) to Rahe (Westinghouse), "Supplemental Acceptance No. 2 for Referencing Topical Report WCAP-9500," January 1983.

104. Letter from W. J. Johnson (Westinghouse) to M. W. Hodges (NRC), " VANTAGE 5 DNB Transition Core Effects." NS-NRC-87-3268, October 2, 1987.a

105. Letter from M. W. Hodges (NRC) to W. J. Johnson (Westinghouse), NRC SER on VANTAGE 5 Transition Core Effects, February 24, 1998.

106. Schueren, P. and McAtee, K. R., "Extension of Methodology for Calculating Transition Core DNBR Penalties," WCAP-11837-P-A, January 1990.

107. Letter from C. Berlinger)NRC) to E. P. Rahe Jr. (Westinghouse), "Request for reduction in Fuel Assembly Burnup Limit Calculation of Maximum Rod Bow Penalty," June 18, 1986.

108. Weiner, R. A., et al., "Improved Fuel Performance Models for Westinghouse Fuel Rod Design and Safety Evaluations," WCAP-10851-P-A, August 1988.

109. Leech, W. J., et al., "Revised PAD Code Thermal Safety Model," WCAP-8720, Addendum 2, October 1982.

110. Friedland, A. J. and Ray, S., "Improved THINC IV Modeling for PWR Core Design," WCAP-12330-P-A.

111. Davidson, S. L., Nuhfer, D. L. (Eds.), "VANTAGE + Fuel Assembly Reference Core Report," WCAP-12610 and Appendices A through D, June 1990.

112. Davidson, S. L. and Ryan, T. L., "VANTAGE+ Fuel Assembly Reference Core Report," WCAP-12610-P-A, April 1995.

113. Motley, F. E., et al., "New Westinghouse Correlation WRB-1 for Predicting Critical Heat Flux in Rod Bundles with Mixing Vane Grids," WCAP-8762-P, July 1984.

114. C. F. Ciocca, "Westinghouse Revised Thermal Design Procedure Instrument Uncertainty Methodology for Salem Units 1 & 2," WCAP-13651 (Proprietary), August 1993.

115. Letter from W. J. Rinkacs (Westinghouse) to M. M. Mannion (PSE&G), "Westinghouse Generic Safety Evaluation for the 17x17 Standard Robust Fuel Assembly," October 1, 1998.

116. M. D. Coury, C. R. Tuley, T. P. Williams, "Power Calorimetric for the 1.4% Uprating for Public Service Electric & Gas Company, Salem Units 1 and 2", WCAP-15553 (Proprietary), September, 2000.

117. Letter from Gon Maxwell (Westinghouse) to Doug Tisdel (PSEG), PSE-15-61, "Westinghouse Resolution Plan and Technical Basis for NSAL-14-5, Lower Than Expected Critical Heat Flux Results Obtained During DNB Testing," December 3, 2015.

THIS PAGE INTENTIONALLY BLANK