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CHAPTER 4

4.0 REACTOR

4.1 SUMMARY DESCRIPTION

This chapter describes 1) the mechanical components of the reactor and reactor core including the fuel rods and fuel assemblies, 2) the nuclear design, and 3) the thermal-hydraulic design.

The reactor core is comprised of an array of fuel assemblies which are identical in mechanical design but different in fuel enrichment. The referenced three-region first-core design described herein results in a first cycle length of approximately one year for Unit 1 and 16 months for Unit 2.

Beginning with STP Unit 1 Cycle 8 and Unit 2 Cycle 7, the following changes were incorporated into the fresh fuel feed regions: ZIRLO intermediate spacer grid strap material, ZIRLO guide tubes and ZIRLO instrument tubes. As an extra measure of debris mitigation, a protective bottom grid was incorporated. Also incorporated into Unit 1 Cycle 8 were two spider-mounted, doubly encapsulated secondary source assemblies.

Annular pellets may be incorporated into the fresh fuel feed regions. These annular pellets have a hole through the long axis of the pellet. This void volume acts to increase the effective plenum volume in the fuel rod, thus reducing rod internal pressure at end-of-life. The annular pellets are used in the top and bottom axial regions of the fuel rod. Annular pellets may be natural, mid-, or fully-enriched and may be incorporated in both Integral Fuel Burnable Absorbers (IFBA) and non-IFBA fuel rods.

Beginning with STP Unit 2 Cycle 7 (and Unit 1 Cycle 9 to follow), the Robust Fuel Assembly (RFA) design was introduced. As with the VANTAGE+ fuel assembly, the RFA design incorporates ZIRLO material for mid-grids, guide thimble and instrument tubes. The mid-grid has been modified with radiused thimble cells to accept thicker walled guide thimble and instrument tubes. A redesigned guide thimble assembly incorporates a thicker wall tube (0.020" vs. 0.016") with a separate internal dashpot. This change replaces the VANTAGE+ double dashpot design. The instrument tube wall has also been increased from 0.016" to 0.020". The RFA and the VANTAGE+ designs are shown in Figure 4.2-2C.

Beginning with Unit 2, Cycle 19, and Unit 1, Cycle 21, fuel will be fabricated with the Robust Protective Grid (RPG). The RPG is a design improvement with respect to the Protective Grid in regard to Primary Water Stress Corrosion Cracking (PWSCC) and fatigue failure due to High Frequency Vibration (HFV). The RPG design is shown in Figure 4.2-2D. CN-3186 and CN-3203

Beginning with STP Unit 1 Cycle 12, eight lead use assemblies will be introduced with the XL-WIN, the Westinghouse Integral Nozzle for 14' fuel. The redesigned top nozzle incorporates changes to the nozzle and holdown springs and eliminates the use of screws to retain the springs.

Beginning with Unit 1 Cycle 18 and Unit 2 Cycle 17, the fuels will be fabricated using Optimized ZIRLO^{TM} High Performance Fuel Cladding Material (Reference 4.1-3).

The core is cooled and moderated by light water at a pressure of 2,250 psia in the Reactor Coolant System (RCS). The moderator coolant contains boron as a neutron absorber. The concentration of boron in the coolant is varied as required to control relatively slow reactivity changes including the effects of fuel burnup. Additional boron, in the form of IFBA and/or burnable absorber rods, is employed to establish the desired initial reactivity.

Each fuel assembly consists of 264 fuel rods mechanically joined in a square array. The fuel rods are supported at intervals along their length by grid assemblies which maintain the lateral spacing between the rods throughout the design life of the assembly. The grid assembly consists of an "egg-crate" arrangement of interlocked straps. The straps contain spring fingers and dimples for fuel rod support as well as coolant mixing vanes. The fuel rods consist of slightly enriched uranium dioxide ceramic cylindrical pellets contained in slightly cold worked or partially re-crystallized annealed Optimized ZIRLO™ High Performance Fuel Cladding Material of ZIRLO® High Performance Fuel Cladding Material tubing, which is plugged and seal welded at the ends to encapsulate the fuel. All fuel rods are pressurized with helium during fabrication to reduce stresses, strains, and to increase fatigue life.

A protective bottom grid has been added just above the bottom nozzle for the XL VANTAGE +, and a RPG has been added for the XL RFA fuel design. The grid straps intersect the nozzle flow holes, thus reducing the possibility of fuel rod damage due to debris-induced fuel rod fretting. In combination with the protective bottom grid, longer fuel rod end-plugs are used, such that any debris passing through the bottom nozzle and being trapped on the protective grid will fret against the solid end-plug instead of the fuel rod. CN-3186

The center position in the assembly is reserved for the incore instrumentation, while the remaining 24 positions in the array are equipped with guide thimbles joined to the grids and to the top and bottom nozzles. Depending upon the position of the assembly in the core, the guide thimbles are used as core locations for rod cluster control assemblies (RCCAs), neutron source assemblies, or burnable absorber assemblies.

The bottom nozzle is a box-like structure which serves as a bottom structural element of the fuel assembly and directs the coolant flow distribution to the assembly.

The top nozzle assembly functions as the upper structural support element of the fuel assembly in addition to providing a partial protective housing for the RCCA or other components.

The RCCAs each consist of a group of individual absorber rods fastened at the top end to a common hub or spider assembly. These assemblies have rods containing absorber material to control the reactivity of the core under operating conditions and to control axial power distribution.

The nuclear design analyses and evaluation establish physical locations for control rods and burnable absorbers, and physical parameters, such as fuel enrichments and boron concentration in the coolant. The nuclear design evaluation established that the reactor core has inherent characteristics which, together with corrective actions of the reactor control and protective systems, provide adequate reactivity control even if the highest reactivity worth RCCA is stuck in the fully withdrawn position.

The design also provides for inherent stability against diametral and azimuthal power oscillations and for control of induced axial power oscillation through the use of the control rods.

The thermal-hydraulic design analyses and evaluation establish coolant flow parameters which assure that adequate heat transfer is provided between the fuel clad and the reactor coolant. The thermal design takes into account local variations in dimensions, power generation, flow distribution, and mixing. The mixing vanes incorporated in the fuel assembly spacer grid design induce additional flow mixing between the various flow channels within a fuel assembly as well as between adjacent assemblies.

Instrumentation is provided in and out of the core to monitor the nuclear, thermal-hydraulic, and mechanical performance of the reactor and to provide inputs to automatic control functions.

Table 4.1-1 presents the current principal nuclear, thermal hydraulic, and mechanical design parameters.

The effects of fuel densification were evaluated with the methods described in Reference 4.1-2.

The analysis techniques employed in the core design are tabulated in Table 4.1-2. The loading conditions considered in general for the core internals and components are tabulated in Table 4.1-3. Specific or limiting loads considered for design purposes of the various components are listed as follows: fuel assemblies in Subsection 4.2.1.5, and the neutron absorber rods, burnable absorber rods, neutron source rods and thimble plug assemblies in Subsection 4.2.1.6. The dynamic analyses, input forcing functions, and response loadings are presented in Section 3.9.

REFERENCES

Section 4.1:

4.1-1 Hellman, J. M. (Ed.), “Fuel Densification Experimental Results and Model for Reactor Application”, WCAP-8218-P-A (Proprietary), WCAP-8219-A, March 1975.

4.1-2 Davidson, S. L. (Ed.), “VANTAGE + Fuel Assembly Reference Core Report, “WCAP-12610-P-A (Proprietary), April 1995.

4.1-3 WCAP-12610-P-A & CENPD-404-P-A, Addendum 1-A, “Optimized ZIRLO™”, July 2006.

TABLE 4.1-1

REACTOR DESIGN TABLE[g]

THERMAL AND HYDRAULIC DESIGN PARAMETERS

1.	Reactor Core Heat Output, MWt	3,853
2.	Reactor Core Heat Output 10^6 Btu/hr	13,147
3.	Heat Generated in Fuel, %	97.4
4.	System Pressure, Nominal, psia	2,250
5.	System Pressure, Minimum Steady State, psia	2,220
6.A	Minimum Departure from Nucleate Boiling Ratio at Nominal Conditions (Unit 1)	Typical flow channel 2.55 [h] / 2.190 [i] Thimble flow channel 2.47 [h] / 2.108 [i]
6.B	Minimum Departure from Nucleate Boiling Ratio at Nominal Conditions (Unit 2)	Typical flow channel 2.53 [h] / 2.190 [i] Thimble flow channel 2.45 [h] / 2.108 [i]
7.	Minimum Departure from Nucleate Boiling Ratio for Design Transients	Typical flow channel >1.24 [h] / 1.26 [i] Thimble flow channel > 1.23 [h] / 1.24 [i]
8.	DNB Correlation	WRB-2M [h] / WRB-1 [i]
PEAKING FACTORS TO PREVENT DNB		
9.	Nuclear Enthalpy Rise Hot Channel Factor, $F^N \Delta H$	1.62
10.	Axial Power Shape	See Section 4.4.4.3.2
COOLANT FLOW		
11.	Total Thermal Flow Rate, 10^6 lbm/hr	145.2
12.	Effective Flow Rate for Heat Transfer, 10^6 lbm/hr	132.9
13.	Effective Flow Area for Heat Transfer, ft^2	51.1
14.	Average Velocity Along Fuel Rods, ft/sec	16.5
15.	Average Mass Velocity, 10^6 lbm/hr-ft^2	2.60
COOLANT TEMPERATURE (°F) (Analyzed Range of Values at Hot Full Power)		
16.	Nominal Inlet	549.8 to 560.3 (L)
17.	Average Rise in Vessel	64.5 to 65.7 (L)
18.	Average Rise in Core	69.7 to 71.1 (L)
19.	Average in Core (based on average enthalpy)	587.4 to 597.4
20.	Average in Vessel	582.7 to 592.6

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TABLE 4.1-1 (Continued)

REACTOR DESIGN TABLE [g]

PRESSURE DROP [d]

1.	Across Core, psi	37.30 ± 3.8
2.	Across Vessel, including nozzle, psi	62.49 ± 9.12

HEAT TRANSFER

3.	Active Heat Transfer, Surface Area, ft^2	69,700
4.	Average Heat Flux, Btu/hr-ft^2	183,700
5.	Maximum Heat Flux for Normal Operation, Btu/hr-ft^2	496,000 [k]
6.	Average Linear Power, kW/ft	5.27
7.	Peak Linear Power for Normal Operation, kW/ft	14.2 [k]
8.	Peak Linear Power Resulting from Overpower Transients / Operator Errors (assuming a maximum overpower of 118%), kW/ft	22.45 [a]
9.	Heat Flux Hot Channel Factor, F_Q	2.55 [b]
10.	Peak Fuel Central Temperature at Peak Linear Power for Prevention of Centerline Melt, °F	4,700
11.	Fuel Rod Array	17 × 17
12.	Number of Fuel Assemblies	193
13.	UO Rods Per Assembly	264
14.	Rod Pitch, in.	0.496
15.	Overall Dimensions, in.	8.426 × 8.426
16.	Fuel Weight (as UO_2), lb	261,000 (Nominal) [e]
17.	Zircaloy/ZIRLO Weight, lb	57,120 (pre-VANTAGE+) 60,650 (VANTAGE+) 63,200 (RFA)
18.	Number of Grids per Assembly	10 (11) [c]
19.	Loading Technique	3 region nonuniform

TABLE 4.1-1 (Continued)

REACTOR DESIGN TABLE [g]

FUEL RODS		
20.	Number	50,952
21.	Outside Diameter, in.	0.374
22.	Diametral Gap, in.	0.0065
23.	Clad Thickness, in.	0.0225
24.	Clad Material	ZIRLO®/OPTIMIZED ZIRLO™
FUEL PELLETS		
25.	Material	UO_2 Sintered
26.	Density (% of theoretical)	95
27.	Diameter, in.	0.3225
28.	Length, in.	0.387 or 0.500 (Annular Pellets)
ROD CLUSTER CONTROL ASSEMBLIES		
29.	Neutron Absorber	Hafnium or Ag-In-Cd
30.	Cladding Material	Type 304 SS-Cold Worked
31.	Clad Thickness, in. [f]	0.0185
32.	Number of Clusters	57
33.	Number of Absorber Rods per Cluster	24
CORE STRUCTURE		
34.	Core Barrel, ID/OD, in.	148.0 / 152.5
35.	Thermal Shield	* The Unit 1 core contains 56 rod cluster control assemblies with no rod cluster control assembly installed at core location D-6.
STRUCTURE CHARACTERISTICS		
36.	Core Diameter, in. (Equivalent)	132.7
37.	Core Height, in. (Active Fuel)	168
REFLECTOR THICKNESS AND COMPOSITION		
38.	Top – Water plus Steel, in.	~10
39.	Bottom – Water plus Steel, in.	~10
40.	Side – Water plus Steel, in.	~15
41.	H_2O/U Molecular Ratio Core, Lattice (Cold)	2.41
FEED ENRICHMENT		
42.	Initial, wt. percent (typical)	3.8 to 4.4 (5.0 max.)

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TABLE 4.1-1 (Continued)

REACTOR DESIGN TABLE (g)

NOTES:

a. See Section 4.3.2.2.6.
b. This is the value of F_Q in the COLR for normal operation.
c. Includes a protective bottom grid.
d. Based on best-estimate reactor flow rate as discussed in Section 5.1.
e. This value will be reduced with the incorporation of annular pellets. Weight reduction is contingent upon the annular axial regional configuration.
f. In addition to SS 304 cladding, a chrome plating (nominally 0.2 to 0.75 mil) is applied to the rodlets in the Enhanced Performance RCCA design.
g. Based on a full core of RFA Fuel.
h. RFA Fuel.
i. Standard or V5H Fuel.
j. Based on 95.5% of theoretical.
k. Based on $F_Q = 2.70$.
L. Corresponds to thermal design flow and vessel average temperature listed. Higher flow will necessitate different values.

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TABLE 4.1-2

ANALYTICAL TECHNIQUES IN CORE DESIGN

Analysis	Technique	Computer Code	Section Referenced
Mechanical Design of Core Internals Loads, Deflections, and Stress Analysis	Static and Dynamic Modeling	Blowdown Code FORCE, Finite element structural analysis code, and others	3.7.2.1 3.9.2 3.9.3
Fuel Rod Design Fuel Performance Characteristics (temperature, internal pressure, clad stress, etc.)	Semi-empirical thermal model of fuel rod with consideration of fuel design changes, heat transfer, fission gas release, etc.	Westinghouse fuel rod design model	4.2.1.1 4.2.3.2 4.2.3.3 4.3.3.1 4.4.2.2 4.4.3.4.2
Nuclear Design			
1. Cross Sections and Group Constants	Microscopic data Macroscopic constants for homogenized core regions	Modified ENDF/B Library LEOPARD/CINDER type PHOENIX – P NEXUS/PARAGON	4.3.3.2 4.3.3.2 4.3.3.2 4.3.3.2
	Group constants for control rods with self-shielding	HAMMER-AIM PHOENIX – P NEXUS/PARAGON	4.3.3.2 4.3.3.2 4.3.3.2
2. X-Y Power Distributions, Fuel Depletion, Critical Boron Concentrations, X-Y Xenon Distributions, Reactivity Coefficients	2-D, 2-Group Diffusion Theory 2-D and 3-D Diffusion Theory – based Nodal Method	TURTLE TORTISE PALADON NEXUS/PARAGON/ANC	4.3.3.3 4.3.3.3 4.3.3.3
3. Axial Power Distributions, Control Rod Worths, and Axial Xenon Distribution	1-D, 2-Group Diffusion Theory 3-D, Diffusion Theory – based Nodal Method	PANDA APOLLO NEXUS/PARAGON/ANC	4.3.3.3 4.3.3.3
4. Fuel Rod Power	Integral Transport Theory	LASER	4.3.3.1
Effective Resonance Temperature	Monte Carlo Weighting Function	REPAD	

TABLE 4.1-2 (Continued)

ANALYTICAL TECHNIQUES IN CORE DESIGN

Analysis	Technique	Computer code	Section Referenced
Nuclear Design (Continued)			
5. Criticality of Reactor and Fuel Assemblies	2-D, 2-Groups Diffusion Theory	LEOPARD PDQ	4.3.2.6
	3-D Monte Carlo Theory	AMPX System Keno V.a	4.3.2.6 4.3.2.6
	2-D, Transport Theory	Phoenix – P	4.3.2.6
6. Vessel Irradiation	Multi-Group Spatial Dependent Transport Theory	DOT	4.3.2.8
Thermal – Hydraulic Design			
1. Steady-state	Subchannel analysis of local fluid conditions in rod bundles, including inertial and cross-flow resistance terms, solution progresses from core-wide to hot assembly to hot channel	THINC-IV	4.4.3.4.1
	Subchannel analysis of local fluid conditions in rod bundles, including inertial and cross-flow resistance terms, solution is based on a one pass model that simulates the core and hot channels	VIPRE-01	4.4.3.4.1
2. Transient Departure from Nucleate Boiling Analysis	Subchannel analysis of local fluid conditions in rod bundles during transients	THINC-I (THINC-III)	4.4.3.4.1
	Subchannel analysis of local fluid conditions in rod bundles during transients	VIPRE-01	4.4.3.4.1

TABLE 4.1-3

DESIGN LOADING CONDITIONS FOR REACTOR CORE COMPONENTS

Fuel Assembly Weight

Fuel Assembly Spring Forces

Internals Weight

Control Rod Trip (equivalent static load)

Differential Pressure

Spring Preloads

Coolant Flow Forces (static)

Temperature Gradients

Differences in Thermal Expansion
- Due to temperature differences
- Due to expansion of different materials

Interference Between Components

Vibration (mechanically or hydraulically induced)

One or More Loops Out of Service

All Operational Transients Listed in Table 5.2-1

Pump Overspeed

Seismic Loads (operation basis earthquake and safe shutdown earthquake)

Blowdown Forces (due to cold and hot leg break)

4.2 FUEL SYSTEM DESIGN

The plant conditions for design are divided into four categories in accordance with their anticipated frequency of occurrence and risk to the public: Condition I – Normal Operation; Condition II – Incidents of Moderate Frequency; Condition III – Infrequent Incidents; Condition IV – Limiting Faults. The bases and description of plant operation and events involving each Condition are given in the Accident Analysis Chapter 15.

The reactor is designed so that its components meet the following performance and safety criteria:

1. The mechanical design of the reactor core components and their physical arrangement, together with corrective actions of the reactor control, protection, and emergency cooling systems (when applicable) assure that:

   a. Fuel damage* is not expected during Condition I and Condition II events. It is not possible, however, to preclude a very small number of rod failures. These are within the capability of the plant clean-up system and are consistent with plant design bases.

   b. 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 immediate resumption of operation. The fraction of fuel rods damaged must be limited to meet the dose guidelines of 10 CFR 100.

   c. 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.

2. The fuel assemblies are designed to withstand, without exceeding the criteria of Section 4.2.1.5, loads induced during shipping, handling, and core loading.

3. The fuel assemblies are designed to accept control rod insertions in order to provide the required reactivity control for power operations and reactivity shutdown conditions.

4. All fuel assemblies have provisions for the insertion of incore instrumentation necessary for plant operation.

5. The reactor internals, in conjunction with the fuel assemblies and incore control components, direct reactor coolant through the core. This achieves acceptable flow distribution and restricts bypass flow so that the heat transfer performance requirements can be met for all modes of operation.

The following section provides the fuel system design bases and design limits. This information, augmented by the clarifying information submitted to the Nuclear Regulatory Commission (NRC) (Refs. 4.2-14 and 4.2-15) during their review of Westinghouse topical report WCAP-9500. Reference Core Report – 17 x 17 Optimized Fuel Assembly, is consistent with the acceptance criteria of the Standard Review Plan (SRP), Section 4.2.

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

4.2.1 Design Bases

The fuel rod and fuel assembly design bases are established to satisfy the general performance and safety criteria presented in this section. Other supplementary fuel design criteria/limits are given in Reference 4.2-22.

The revised fuel assembly structural component criterion is as follows: The Zircaloy-4 and ZIRLO®/ Optimized ZIRLO™ structural component stresses will be consistent with ASME Code Section III requirements after accounting for thinning due to corrosion.

The detailed fuel rod design establishes such parameters as pellet size and density, clad-pellet diametral gap, gas plenum size, and helium prepressurization level. The design also considers effects such as fuel density changes, fission gas release, clad creep, and other physical properties which vary with burnup. The integrity of the fuel rods is ensured by designing to prevent excessive fuel temperatures, excessive internal rod gas pressures due to fission gas releases, and excessive cladding stresses and strains. This is achieved by designing the fuel rods so that the conservative design bases in the following sections are satisfied during Condition I and Condition II events over the fuel lifetime. For each design basis, the performance of the limiting fuel rod must not exceed the limits specified by the design basis.

Structural integrity of the fuel assembly structure is assured by setting limits on stresses and deformations due to various loads and by determining that the assembly does not interfere with the functioning of other components. Three types of loads are considered:

1. Nonoperational loads such as those due to shipping and handling.

2. Normal and abnormal loads which are defined for Conditions I and II.

3. Abnormal loads which are defined for Conditions III and IV.

The design bases for the incore control components are described in Section 4.2.1.6.

4.2.1.1 Cladding

a. The desired fuel rod clad is a material which has a superior combination of neutron economy (low absorption cross section), high strength to resist deformation due to differential pressures and mechanical interaction between fuel and clad, high corrosion resistance to coolant, fuel and fission products, and high reliability. Zircaloy-4 and ZIRLO/Optimized ZIRLO[1] have this desired combination of clad properties. As shown in Reference 4.2-1 there is considerable pressurized water reactor (PWR) operating experience on the capability of ZIRLO and Optimized ZIRLO as clad materials. Information on the materials and mechanical properties of the cladding is given in Reference 4.2-2 and Reference 4.2-22.

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[1] Optimized ZIRLO™ is a trademark and ZIRLO* is a registered trademark of Westinghouse Electric Company LLC in the United States and may be registered in other countries throughout the world. All rights reserved. Unauthorized use is strictly prohibited.

b. Stress-strain Limits

1) Clad Stress

The von Mises criterion is used to calculate the effective stresses. The cladding stresses under Condition I and II events are less than the cladding 0.2% offset yield stress, with due consideration of temperature and irradiation effects. While the cladding has some capability for accommodating plastic strain, the yield stress has been accepted as a conservative design basis.

In addition, a revised clad stress criteria (Reference 4.2-C) can be used for standard ZIRLO cladding which is defined as:

- The maximum cladding stress intensities, excluding pellet cladding interaction but accounting for cladding corrosion as a loss-of-load carrying metal be less than the stress limit as defined based on the ASME code calculations.
- The 1% transient clad strain criterion is met.
- An additional steady-state clad strain criterion based on the total (plastic plus elastic strain) is met.
- No centerline fuel melting occurs, and
- The effect of the plastic deformation is accounted for in all fuel rod design criteria as appropriate.

2) Clad Tensile Strain

The total tensile creep strain is less than one percent from the unirradiated condition. The elastic tensile strain during a transient is less than 1 percent from the pretransient value. This limit is consistent with proven practice.

c. Vibration and Fatigue

1) Strain Fatigue

The cumulative strain fatigue cycles are less than the design strain fatigue life. This basis is consistent with proven practice.

2) Vibration

Potential fretting wear due to vibration is prevented assuring that the stress-strain limits are not exceeded during design life. Fretting of the clad surface can occur due to flow-induced vibration between the fuel rods and fuel assembly grid springs. Vibration and fretting forces vary during the fuel life due to clad diameter creep-down combined with grid spring relaxation.

d. Chemical Properties of the Cladding – This is discussed in References 4.2-2 and 4.2-22.

4.2.1.2 Fuel Material.

a. Thermal-Physical Properties

Fuel Pellet Temperatures – The center temperature of the hottest pellet is to be below the melting temperature of the UO_2 (melting point of 5080°F [Ref. 4.2-3] unirradiated and decreasing by 58°F per 10,000 MWD/MTU). While a limited amount of center melting can be tolerated, the design conservatively precludes center melting. A calculated fuel centerline temperature of 4700°F has been selected as an overpower limit to assure no fuel melting. This provides sufficient margin for uncertainties as described in Section 4.4.2.9.

The normal design density of the fuel is 95 percent of theoretical. Additional information on fuel properties is given in Reference 4.2-2.

b. Fuel Densification and Fission Product Swelling

The design bases and models used for fuel densification and swelling are provided in References 4.2-4 and 4.2-19. The same bases and models are used in Reference 4.2-A.

c. Chemical Properties

Reference 4.2-2 provides the basis for justifying that no adverse chemical interactions occur between the fuel and its adjacent material.

4.2.1.3 Fuel Rod Performance.

a. Fuel Rod Models

The basic fuel rod models and the ability to predict operating characteristics are given in Reference 4.2-19 and 4.2-A, and the Design Evaluation, Section 4.2.3.

b. Mechanical Design Limits

Fuel rod design methodology has been introduced that reduces the densification power spike factor to 1.0 and Reference 4.2-6 demonstrates that clad flattening will not occur in Westinghouse fuel designs.

The rod internal gas pressure shall remain below the value which causes the fuel-clad diametral gap to increase due to outward cladding creep during steady-state operation (Ref. 4.2-16). Rod pressure is also limited such that extensive departure from nucleate boiling (DNB) propagation shall not occur during normal operation and accident events.

4.2.1.4 Spacer Grids

a. Mechanical Limits and Materials Properties

Lateral loads resulting from seismic/loss-of-coolant accident (LOCA) events will not cause unacceptably high plastic grid deformation. Each fuel assembly's geometry will be maintained such that the fuel rods remain in an array amenable to cooling. The behavior of the grids under loading has been studied experimentally (Ref. 4.2-12).

The end-grid material and chemical properties are given in Reference 4.2-2. The Zircaloy mid-grids of the Vantage 5H fuel assembly are discussed in Reference 4.2-20. ZIRLO properties are discussed in Reference 4.2-22.

RPG chemical and material properties are identical to the P-grid, whose properties are discussed in Reference 4.2.2. CN-3186

b. Vibration and Fatigue

The grids shall provide sufficient fuel rod support to limit fuel rod vibration and maintain clad fretting wear to within acceptable limits (defined in Section 4.2.1.1).

4.2.1.5 Fuel Assembly

a. Structural Design

As previously discussed in Section 4.2.1, the structural integrity of the fuel assemblies is assured by setting design limits on stresses and deformations due to various nonoperational, operational and accident loads.

These limits are applied to the design and evaluation of the top and bottom nozzles, guide thimbles, grids, and the thimble joints. The design bases for evaluating the structural integrity of the fuel assemblies are:

1. Nonoperational – 4 g axial and 6 g lateral loading with dimensional stability.

2. For the normal operating and upset conditions, the fuel assembly component structural design criteria are established for the two primary material categories, namely austenitic steels and Zircaloy/ZIRLO. The stress categories and strength theory presented in the American Society of Mechanical Engineers Boiler and Pressure Vessel (ASME B&PV) Code, Section III, are used as a general guide. The maximum shear theory (Tresca criterion) for combined stresses is used to determine the stress intensities for the austenitic steel components. The stress intensity is defined as the numerically largest difference between the various principal stresses in a three-dimensional field. The allowable stress intensity value for austenitic steels, such as nickel-chromium-iron alloys, is given by the lowest of the following:

    a) One-third of the specified minimum tensile strength or two-thirds of the specified minimum yield strength at room temperature.

b) One-third of the tensile strength or 90 percent of the yield strength at temperature but not to exceed two-thirds of the specified minimum yield strength at room temperature.

The stress limits for the austenitic steel components are given below. All stress nomenclature is per the ASME B&PV Code, Section III.

Stress Intensity Limits

Categories	Limit
General Primary Membrane Stress Intensity	Sm
Local Primary Membrane Stress Intensity	1.5 Sm
Primary Membrane plus Bending Stress Intensity	1.5 Sm
Total Primary plus Secondary Stress Intensity	3.0 Sm

The Zircaloy/ZIRLO structural components, which consist of guide thimbles, the inner eight (8) grids and fuel tubes, are in turn subdivided into two categories because of material differences and functional requirements. The fuel tube design criteria are covered separately in Section 4.2.1.1. The maximum shear theories are used to evaluate the guide thimble design. For conservative purposes, the Zircaloy/ZIRLO unirradiated properties are used to define the stress limits.

3. Abnormal loads during Conditions III or IV – worst cases represented by combined seismic and blowdown loads.

   a) Deflections or failures of components cannot interfere with the reactor shutdown or emergency cooling of the fuel rods.

   b) The fuel assembly structural component stresses under faulted conditions are evaluated using primarily the methods outlined in Appendix F of the ASME B&PV Code, Section III. Since the current analytical methods utilize elastic analysis, the stress allowables are defined as the smaller value of 2.4 Sm or 0.70 Su for primary membrane and 3.6 Sm or 1.05 Su for primary membrane plus primary bending. For the austenitic steel fuel assembly components, the stress intensity is defined in accordance with the rules described in the previous section for normal operating conditions.

For the Zircaloy/ZIRLO components the stress intensity limits are set at two-thirds of the material yield strength, Sy, at reactor operating temperature. This results in Zircaloy/ZIRLO stress limits being the smaller of 1.6 Sy or 0.70 Su for primary membrane and 2.4 Sy or 1.05 Su for primary membrane plus bending. For conservative purposes, the Zircaloy/ZIRLO unirradiated properties are used to define the stress limits.

The grid component strength criteria are based on experimental tests. The limit is established at Pc where Pc is the experimental collapse load determined at the 95% confidence level on the true mean, as taken from the distribution of grid crush test measurements.

The material and chemical properties of the fuel assembly components are given in Reference 4.2-2 and 4.2-22.

b. Thermal-hydraulic Design

This topic is covered in Section 4.4.

4.2.1.6 Incore Control Components

The control components are sub-divided into two categories:

- Permanent devices used to control or monitor the core
- Temporary devices used to control or monitor the core

The permanent components are the rod cluster control assemblies (RCCAs), secondary neutron source assemblies, and thimble plug assemblies.

The temporary components are the burnable absorber assemblies and the primary neutron source assemblies, which are normally used only in the initial core.

Materials are selected for compatibility in a PWR environment, for adequate mechanical properties at room and operating temperature, for resistance to adverse property changes in a radioactive environment, and for compatibility with interfacing components. Materials properties are given in Reference 4.2-2 and 4.2-7.

1. Absorber Rods

The material properties and compatibilities are given in References 4.2-2 and 4.2-7. The design bases include a stress intensity limit, Sm, of two-thirds of the 0.2 percent offset yield stress for the 304 stainless steel clad tubing during the 15 year minimum RCCA design life.

The design basis of the absorber material is such that it does not exceed its minimum melting point of 3913°F (Ref. 4.2-7).

2. Burnable Absorber Rods

   The burnable absorber rod clad is designed as a Class 1 Component under Article NB-3000 of the ASME B&PV Code, Section III, 1973 for Conditions I and II. For abnormal loads during Conditions III and IV, code stresses are not considered limiting. Failures of the burnable absorber rods during these conditions must not interfere with reactor shutdown or cooling of the fuel rods.

   The burnable absorber absorber material is nonstructural. The structural elements of the burnable absorber rod are designed to maintain the absorber geometry even if the absorber material is fractured. The rods are designed so that the absorber material is below its softening temperature (1492°F*for reference 12.5 weight percent boron rods). In addition, the structural elements are designed to prevent excessive slumping.

3. Neutron Source Rods

   The neutron source rods are designed to withstand the following:

   a. The external pressure equal to the Reactor Coolant System (RCS) operating pressure with appropriate allowance for overpressure transients and,

   b. An internal pressure equal to the pressure generated by released gases over the source rod life.

4. Thimble Plug Assembly

   The thimble plug assembly is needed to restrict bypass flow through those thimbles not occupied by absorber, source, or burnable absorber rods.

   The thimble plug assemblies satisfy the following:

   a. Accommodate the differential thermal expansion between the fuel assembly and the core internals,

   b. Maintain positive contact with the fuel assembly and the core internals.

   c. Limits the flow through each occupied thimble to acceptable design values.

4.2.1.7 Surveillance Program

A testing and fuel surveillance operational experience program has been and is being conducted to verify the adequacy of the fuel performance and design bases. This program is discussed in Section 4.2.4.5 and Sections 8 and 23 of Reference 4.2-8. Fuel surveillance and testing results, as they become available, are used to improve fuel rod design and manufacturing processes and assure that the design bases and safety criteria are satisfied.

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* Borosilicate glass is accepted for use in burnable absorber rods if the softening temperature is 1510 ± 18°F. The softening temperature is defined in ASTM C338.

4.2.2 Design Description

The standard and upgraded fuel assembly, fuel rod, and incore control component design data are given in Table 4.3-1.

Two hundred and sixty-four (264) fuel rods, 24 guide thimble tubes and one instrumentation thimble tube are arranged within a supporting structure to form a fuel assembly. The instrumentation thimble is located in the center position and provides a channel for insertion of an incore neutron detector, if the fuel assembly is located in an instrumented core position. The guide thimbles provide channels for insertion of either an RCCA, a neutron source assembly, a burnable absorber assembly, or a plugging device, depending upon the position of the particular fuel assembly in the core. Figure 4.2-1 shows a cross section of the fuel assembly array, and Figures 4.2-2 and 4.2-2a show the standard and upgraded fuel assembly full length outlines. The fuel rods are loaded into the fuel assembly structure so that there is clearance between the fuel rod ends and the top and bottom nozzles.

Shown in Figure 4.2-2B is the XL VANTAGE 5H assembly design. The most significant design change associated with the VANTAGE 5H assembly is the use of Zircaloy grids to replace the eight intermediate STD Inconel grids. The guide thimbles and instrumentation tube diameters are reduced to accommodate this change. The VANTAGE 5H assembly incorporates the reconstitutable top nozzle and debris filter bottom nozzle designs as did the previous Upgrade assembly design (Reconstitutable STD assembly). The VANTAGE 5H assembly has the same cross-sectional envelope as the Upgrade assembly and the grid centerline elevations for the two designs are matched such that any integral contact between assemblies will be grid-to-grid. By matching grid elevation, any crossflow maldistribution between assemblies is minimized.

The most significant design change associated with the VANTAGE + assembly, Figure 4.2-2C, is the use of ZIRLO alloy for fuel rod cladding, guide thimble tubes, instrumentation tubes and mid-grids. The VANTAGE + assembly incorporates the reconstitutable top nozzle and debris filter bottom nozzle designs as did the previous Upgrade and XL VANTAGE 5H assembly designs. In addition, the XL VANTAGE + assembly design incorporates the protective bottom grid design feature. This feature, along with longer fuel rod end-plugs, adds additional debris resistance to the fuel assembly. The VANTAGE + assembly has the same cross-sectional envelope as the VANTAGE 5H and Upgrade assemblies and the grid centerline elevations for the three designs are consistent such that any integral contact between assemblies will be grid-to-grid. By matching grid elevations, any crossflow maldistribution between assemblies is minimized. The addition of the protective bottom grid in combination with placing the fuel pins on top of the bottom nozzle has no impact on assembly hydraulic characteristics.

The most significant design change associated with the RFA design is the guide thimble assembly. Where the previous design utilizes a 0.016” wall and a double dashpot configuration, the RFA design incorporates a 0.020” wall, constant OD/ID, guide thimble tube with a separate internal dashpot tube. Beginning in Unit 2, Cycle 19 and Unit 1, Cycle 21, the RFA design incorporates the RPG.

Each fuel assembly is installed vertically in the reactor vessel and stands upright on the lower core support, which is fitted with alignment pins to locate and orient the assembly. After all fuel assemblies are set in place, the upper support structure is installed. Alignment pins, built into the fuel assembly top nozzle, engage the upper core plate and locate the upper ends of the fuel assemblies.

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The upper core plate then bears downward against the fuel assembly top nozzle via the holddown springs to hold the fuel assemblies in place.

Improper orientation of fuel assemblies within the core is prevented by the use of an indexing hole in one corner of the top nozzle top plate. The assembly is oriented with respect to the handling tool and the core by means of a pin which is inserted into this indexing hole. Additionally, visual confirmation of orientation is provided by an engraved identification number on the opposite corner clamp.

4.2.2.1 Fuel Rods. The fuel rods consist of uranium dioxide ceramic pellets contained in slightly cold-worked and stress relieved ZIRLO or partially re-crystallized annealed Optimized ZIRLO tubing which is plugged and seal welded at the ends to encapsulate the fuel. A schematic of the standard and upgraded fuel rods is shown in Figure 4.2-3. The fuel pellets are right circular cylinders consisting of slightly enriched uranium dioxide powder which has been compacted by cold pressing and then sintered to the required density. The ends of each pellet are dished slightly to allow greater axial expansion at the center of the pellets and have a small chamfer at the outer cylinder surface.

Annular pellets may be incorporated into the fresh fuel feed regions. These annular pellets have a hole through the long axis of the pellet. This void volume acts to increase the effective plenum volume in the fuel rod, thus reducing rod internal pressure at end-of-life. The annular pellets are used in the top and bottom axial regions of the fuel rod.

The annular pellets are longer than the enriched solid pellets. The longer pellets aid in identification during fuel fabrication to help eliminate pellet loading errors. The annular pellets have the same outside diameter and pellet edge diameter as the enriched solid pellets but have no dish on the pellet ends.

The VANTAGE 5H fuel rods are the same design as the Upgrade fuel rods with the exception that a portion of the fuel rods within a given assembly may be Integral Fuel Burnable Absorber (IFBA) rods. The IFBA rod utilizes coated fuel pellets which are identical to the enriched uranium dioxide pellets except for the addition of a thin diboride coating on the pellet cylindrical surface. Coated pellets occupy the central portion of the fuel column (up to the full stack length of 168 inches) shown in the fuel rod schematic of Figure 4.2-3. The number and pattern of IFBA rods within an assembly may vary depending on specific application. Evaluation and test programs for the IFBA design features are given in Reference 4.2-21's Section 2.5.

The ZIRLO/Optimized ZIRLO fuel rod has a longer bottom end-plug incorporated to accommodate the protective bottom grid. To offset reduction in the plenum length, the ZIRLO/Optimized ZIRLO fuel rod has a variable pitch plenum spring. The spring has a smaller wire and coil diameter and a shorter free length. The variable pitch spring provides the same support as the regular plenum spring but with fewer turns which translates to less spring volume.

To avoid overstressing of the clad or seal welds, void volume and clearances are provided within the rods to accommodate fission gases released from the fuel, differential thermal expansion between the clad and the fuel, and fuel density changes during irradiation. Shifting of the fuel within the clad during handling or shipping prior to core loading is prevented by a stainless steel helical spring which bears on top of the fuel. At assembly the pellets are stacked in the clad to the required fuel height, the spring is then inserted into the top end of the fuel tube, and the end plugs pressed into the ends of the tube and welded.

All fuel rods are internally pressurized with helium during the welding process so that: 1) the internal gas pressure mechanical design limit given in Section 4.2.1.3 (b) is not exceeded and, 2) the cladding stress-strain limits (Section 4.2.1.1) are not exceeded for Condition I and II events, and 3) clad flattening will not occur during the fuel core life.

Fuel rods may be manufactured with a thin oxide coating on the outer surface of the lower ends of the rods. The oxide coating is applied to mitigate the effects of possible debris fretting.

4.2.2.2 Fuel Assembly Structure: The standard fuel assembly structure consists of a bottom nozzle, top nozzle, guide thimbles and grids, as shown in Figure 4.2-2. The upgraded fuel assembly structure consists of a bottom nozzle, thimble screws, top nozzle, guide thimbles, inserts, lock tubes, and grids as shown in Figure 4.2-2a. The VANTAGE 5H fuel assembly, which includes Zircalloy mid-grids and IFBA fuel rods, is shown in Figure 4.2-2B. The VANTAGE + and RFA fuel assembly shown in Figure 4.2-2C uses ZIRLO or Optimized ZIRLO alloy for fuel rod cladding, guide thimble tubes, instrumentation tubes and mid-grids and incorporates the protective bottom grid. The RFA fuel assembly depicted in Figure 4.2-2D incorporates the RPG. CN-3186

4.2.2.2.1 Standard Bottom Nozzle: The standard bottom nozzle serves as a bottom structural element of the fuel assembly and directs the coolant flow distribution to the assembly. The square nozzle is fabricated from Type 304 stainless steel and consists of a perforated plate and four angle legs with bearing plates as shown in Figure 4.2-2. The legs form a plenum for the inlet coolant flow to the fuel assembly. The plate also prevents accidental downward ejection of the fuel rods from the fuel assembly. The bottom nozzle is fastened to the fuel assembly guide tubes by locked screws which penetrate through the nozzle and mate with a threaded plug in each guide tube.

Coolant flow through the fuel assembly is directed from the plenum in the bottom nozzle upward through the penetrations in the plate to the channels between the fuel rods. The penetrations in the plate are positioned between the rows of the fuel rods.

Axial loads (holddown) imposed on the fuel assembly and the weight of the fuel assembly are transmitted through the bottom nozzle to the lower core support structure. Indexing and positioning of the fuel assembly is controlled by alignment holes in two diagonally opposite bearing plates which mate with locating pins in the lower core support. Any lateral loads on the fuel assembly are transmitted to the lower core support through the locating pins.

4.2.2.2.1a Upgrade, VANTAGE 5H, VANTAGE + , and RFA Bottom Nozzle: The Upgraded, VANTAGE 5H, VANTAGE +, and RFA assemblies use the skirted Debris Filter Bottom Nozzle (DFBN) to reduce the possibility of fuel rod damage due to debris-induced fretting. The relatively large flow holes in the STD nozzle are replaced with a new pattern of smaller flow holes. The holes are sized to minimize passage of debris particles large enough to cause damage while providing sufficient flow area, comparable pressure drop and continued structural integrity of the nozzle. Tests to measure pressure drop and demonstrate structural integrity verified that the 304 stainless steel DFBN is totally compatible with the current design.

Changes in design compared to the STD bottom nozzle design include: 1) a modified flow hole size and pattern as described above, 2) a decreased nozzle height with thinner top plate and skirted perimeter side plates to accommodate the extended burnup fuel rod and 3) increased lead-in chamferes for the core pin interface to improve handling.

4.2.2.2.2 Standard Top Nozzle: The top nozzle assembly functions as the upper structural element of the fuel assembly in addition to providing a partial protective housing for the RCCA or other components. It consists of an adapter plate, enclosure, top plate, and pads. The assembly has hold down springs and guide pins mounted on the top plate and pads respectively as shown in Figure 4.2-2. The springs and bolts are made of Inconel-718, whereas other components are made of Type 304 stainless steel.

The square adapter plate is provided with round penetrations and semicircular ended slots to permit the flow of coolant upward through the top nozzle. Other round holes are provided to accept the stainless steel sleeves which are welded to the adapter plate at their upper ends and mechanically attached to the thimble tubes at the lower ends. The ligaments in the plate cover the tops of the fuel rods and prevent their upward ejection from the fuel assembly. The enclosure is a box-like structure which sets the distance between the adapter plate and the top plate. The top plate has a large square hole in the center to permit access for the control rods and the control rod spiders. Holddown springs are mounted on the top plate and are fastened in place by bolts and clamps located at two diagonally opposite corners. On the other two corners integral pads are positioned which contain alignment pins for locating the upper end of the fuel assembly.

4.2.2.2.2a Upgrade, VANTAGE 5H, VANTAGE + , and RFA Top Nozzle: The Upgraded, VANTAGE 5H, VANTAGE +, and RFA assemblies use the Reconstitutable Top Nozzle (RTN) which differs from the STD design in two ways: 1) a groove is provided in each thimble thru-hole in the nozzle adaptor plate to facilitate attachment and removal; and 2) the nozzle adaptor plate thickness is reduced to provide additional axial space for fuel rod growth.

In the RTN, a stainless steel nozzle insert is mechanically connected to the top nozzle adaptor plate by means of pre-formed circumferential bulge near the top of the insert. The insert engages a mating groove in the wall of the adaptor plate thimble tube thru-hole. The insert has five equally spaced axial slots which allow the insert to deflect inwardly at the elevation of the bulge, thus permitting the installation or removal of the nozzle. The insert bulge is positively held in the adaptor plate mating groove by placing a lock tube with a uniform ID identical to that of the thimble tube into the insert.

To remove the top nozzle, a tool is first inserted through the lock tube and expanded radially to engage the bottom edge of the tube. An axial force is then exerted on the tool which overrides the local lock tube deformations and withdraws the lock tube from the insert. After the lock tubes have been withdrawn, the nozzle is removed by raising it off the upper slotted ends of the nozzle inserts which deflect inwardly under the axial lift load.

With the top nozzle removed, direct access is provided for the fuel rod examination or replacement. Reconstitution is completed by the remounting of the nozzle and the insertion of new lock tubes. The design bases and evaluation of the reconstitutable top nozzle are given in Reference 4.2-21's Section 2.3.2.

4.2.2.2.2b XL WIN Top Nozzle: The Westinghouse Integral Nozzle (WIN) 17x17 XL fourteen-foot fuel assembly products have been developed as a technical solution to the holddown spring retaining screw cracking problem. Rather than using screws to hold the springs in place, the tail end of the spring pack is slid into a blind pocket machined into a clamp that is an integral part of the top nozzle. A retaining pin is then pushed vertically through the clamp and welded in place to retain the spring pack. All other features of the above RTN design (see 4.2.2.2.2a) are retained. Eight Lead Use Assemblies incorporating XL WIN Top Nozzle are planned for South Texas Unit 1, Cycle 12.

4.2.2.2.3 Guide and Instrument Thimbles: The guide thimbles are structural members which also provide channels for the neutron absorber rods, burnable absorber rods, or neutron sources. The Standard Fuel, VANTAGE 5H, and VANTAGE+ guide thimble tubes use a double dashpot configuration with a 0.016" thick wall. The Standard and VANTAGE 5H designs use Zircaloy, whereas the VANTAGE+ and RFA designs use ZIRLO for the guide thimble tube. The RFA guide thimble design uses a 0.020" wall outer tube with a constant OD/ID and a separate inner ZIRLO tube to provide a dashpot function. The end plug of the dashpot tube is press-fitted onto the guide thimble end plug, which is secured to the bottom nozzle with thimble screws. As shown in Figure 4.2-7b, the dashpot tube is also bulged to the guide thimble tube. The tube diameter at the top section provides the annular area necessary to permit rapid control rod insertion during a reactor trip. Holes are provided on the thimble tube above the dashpot to reduce the rod drop time. For all designs except RFA, the lower portion of the guide thimble is swaged to a smaller diameter to reduce diametral clearances and produce a dashpot action near the end of the control rod travel during normal trip operation. The dashpot is closed at the bottom by means of an end plug which is provided with a small flow port to avoid fluid stagnation in the dashpot volume during normal operation. The top end of the guide thimble is fastened to a tubular sleeve by three expansion swages. In the STD assembly, the sleeve fits into and is welded to the top nozzle adapter plate. The lower end of the guide thimble is fitted with an end plug which is then fastened to the bottom nozzle by a locked screw.

In the Upgrade assemblies, the top end of the guide thimbles are fastened to a tubular nozzle insert sleeve by three expansion swages. The inserts engage into mating grooves in the top nozzle adapter plate as shown in Figure 4.2-6a. In the STD assembly design, the guide thimbles are similarly fastened to the top grid sleeves which are then welded to the top nozzle adapter plate as shown in Figure 4.2-6.

Guide thimbles of the VANTAGE 5H and VANTAGE + designs are identical to those of the STD and the Upgrade design with the exception of a reduction in diameter (relative to both STD and Upgrade) and length above the dashpot (relative to STD). The diametral reduction is required to allow for the thicker straps of the mid Zircaloy/ZIRLO grids; the length reduction is required by the RTN design. The VANTAGE 5H/VANTAGE + guide thimble tube ID provides an adequate nominal diametral clearance of 0.061 inches for the control rods. The VANTAGE 5H/VANTAGE + thimble tube ID also provides sufficient diametral clearance for burnable absorber rods, source rods and thimble plugs. The RFA, while incorporating thicker wall guide thimble and instrument tubes, maintains the same inside diameter in and above the dashpot region as the VANTAGE 5H and VANTAGE+ designs. The RFA, while incorporating thicker wall guide thimble and instrument tubes, maintains the same inside diameter in and above the dashpot region as the VANTAGE 5H and VANTAGE+ designs. The thimble plugs used in the South Texas Units are the dually compatible type and can be inserted into the STD, Upgrade, VANTAGE 5H, VANTAGE + and RFA assembly guide thimbles.

For the STD, Upgrade, VANTAGE 5H, VANTAGE + and RFA assembly designs, each grid is fastened to the guide thimble assemblies to create an integrated structure. The fastening method depicted in Figures 4.2-5a and 4.2-5b is used for the mid-grid joints of the VANTAGE

5H/VANTAGE + and RFA assembly. Shown in Figures 4.2-4 and 4.2-5 is the fastening method for the mid-grids of the Upgrade and STD assemblies. The top grid joint connection for all assemblies is similar and is shown in Figure 4.2-5; the RFA top grid has one bulge below the grid.

An expanding tool is inserted into the inner diameter of the Zircaloy/ZIRLO thimble tube at the elevation of stainless steel sleeves that have been brazed into the mid-grid assembly. In the VANTAGE 5H design, these mid-grid sleeves are made of Zircaloy and are laser welded to the Zircaloy grid assemblies. In the VANTAGE + and RFA design, these mid-grid sleeves are made of ZIRLO and are laser welded to the ZIRLO grid assemblies. The four-lobed tool forces the thimble and sleeve outward to a predetermined diameter, thus joining the two components.

For the STD, Upgrade and VANTAGE 5H designs the bottom grid assembly is joined to the assembly as shown in Figure 4.2-7. The stainless steel insert is spotwelded to the bottom grid and later captured between the guide thimble end plug and the bottom nozzle by means of a stainless steel thimble screw.

In the VANTAGE+ design the Protective grid (P-grid) and bottom grid are joined to the assembly as shown in Figure 4.2-7A. The bottom grid insert has been lengthened to accept the P-grid which, like the bottom grid, is attached to the insert with spot welds.

In the RFA design, the bottom grid insert is replaced with a sleeve which is bulged to the guide thimble tube as shown in Figure 4.2-7B. At four locations the P-grid is supported by and welded to spacers which are captured between the guide thimble end plug and bottom nozzle by thimble screws.

Beginning with Unit 2 Cycle 19, and Unit 1, Cycle 21, the Robust P-Grid (RPG) replaces the P grid to correct fatigue failure and stress corrosion cracking observed in the P-grid design. A description of
RFA using RPG is shown in Figure 4.2.7C. At eight locations the RPG is supported by and welded to spacers which are captured between the guide thimble end plug and bottom nozzle by thimble screws.

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The described methods of grid fastening are standard and have been used successfully since the introduction of Zircaloy guide thimbles in 1969.

The central instrumentation tube of each fuel assembly is constrained by seating in counterbores in each nozzle. This tube is a constant diameter and guides the incore neutron detectors. The instrumentation tube of the VANTAGE 5H/VANTAGE + /RFA assembly has a reduced diameter as compared to that of the STD and Upgraded assembly designs. The RFA Instrument Tube has an increased O.D. Sufficient diametral clearance exists for the flux thimble to traverse the tube without binding. This thimble is expanded at the top and mid grids in the same manner as the previously discussed expansion of the guide thimbles to the grids.

4.2.2.2.4 Grid Assembly: The fuel rods, as shown in Figure 4.2-2, are supported at intervals along their length by structural grid assemblies which maintain the lateral spacing between the rods. Each fuel rod is supported laterally within each grid cell by a combination of support dimples and springs (six support locations per cell: four dimples and two springs). The magnitude of grid spring force on the fuel rods is set high enough to minimize possible fretting, without overstressing the cladding at the contact points.

All grid assemblies allow axial thermal expansion of the fuel rods without imposing restraint sufficient to develop buckling or distortion.

The top, bottom and protective grids are made of Inconel 718 strap material, chosen for its strength and high corrosion resistance. These non-mixing vane grids are nearly identical in the STD, VANTAGE 5H, VANTAGE + and RFA designs. VANTAGE 5H, VANTAGE + and RFA differences are: 1) a snag resistant design minimizes assembly interaction during core loading/unloading, 2) dimples are rotated 90 degrees to minimize fuel rod fretting and dimple cocking, 3) grid heights are increased to accommodate rotated dimples, 4) the top grid spring force has been reduced to minimize rod bow and 5) top grid sleeves are made of 304L stainless steel instead of 304 stainless steel in the STD design.

The eight intermediate (mixing vane) grids of the VANTAGE 5H are made of Zircaloy material, the VANTAGE+ and RFA mid grids use Zirlo strip material (both materials chosen for their low neutron absorption properties), and Inconel is used for the mid-grids of the STD and Upgrade design. Inner straps of all designs include mixing vanes which project into the coolant stream and promote mixing of the coolant in the high heat flux region of the assemblies. Relative to the STD and Upgrade Inconel mid-grids, the VANTAGE 5H/VANTAGE +/RFA grids include: (1) increased strap thickness and strap height for structural performance, 2) the anti-snag feature noted above, 3) chamfered upstream strap edges and 4) grid springs positioned diagonally to further improve pressure drop. RFA mid-grids are embossed with a slight radius in thimble cells to accommodate the larger guide tubes. The VANTAGE 5H Zircaloy and VANTAGE + and RFA ZIRLO mid-grids and are designed for approximately the same pressure drop as the STD Inconel mid-grids and are designed for approximately the same pressure drop as the STD Inconel mid-grids and have superior structural performance relative to it as discussed in Refiner 4.2-20. All grid assemblies consist of individual slotted straps assembled in an interlocking “egg-crate” arrangement. Zircaloy and ZIRLO grid strap as well as the protective grid strap joints and grid/sleeve joints are fabricated by laser welding, whereas the top and bottom Inconel grid joints are brazed. The outside straps on all grids contain mixing vanes which, in addition to their mixing function, aid in guiding the grids and fuel assemblies past projecting surfaces during handling or during loading and unloading of the core.

4.2.2.3 Incore Control Components. Reactivity control is provided by neutron absorbing rods and a soluble chemical neutron absorber (boric acid). The boric acid concentration is varied to control long-term reactivity changes such as:

1. Fuel depletion and fission product buildup
2. Cold to hot, zero power reactivity change
3. Reactivity change produced by intermediate-term fission products such as xenon and samarium
4. Burnable absorber depletion

Chemical and volume control is discussed in Chapter 9.

The RCCAs provide reactivity control for:

1. Shutdown

2. Reactivity changes due to coolant temperature changes in the power range

3. Reactivity changes associated with the power coefficient of reactivity

4. Reactivity changes due to void formation

It is desirable to have a non-positive moderator temperature coefficient at rated thermal power throughout the entire cycle in order to reduce possible deleterious effects caused by a positive coefficient during loss of coolant or loss of flow accidents.

The most effective reactivity control components are the RCCAs and their corresponding control rod drive mechanisms (CRDMs) which are the only moving parts in the reactor. Figure 4.2-8 identifies the rod cluster control and CRDM assembly, in addition to the arrangement of these components in the reactor relative to the interfacing fuel assembly and guide tubes. In the following paragraphs, each reactivity control component is described in detail. The CRDM assembly is described in Section 3.9.4.

4.2.2.3.1 Rod Cluster Control Assembly: The RCCAs are divided into two categories: control and shutdown. The control groups compensate for reactivity changes due to variations in operating conditions of the reactor; i.e., power and temperature variations. Two nuclear design criteria have been employed for selection of the control group. First, the total reactivity worth must be adequate to meet the nuclear requirements of the reactor. Second, because these rods may be partially inserted at power operation, the total power peaking factor should be low enough to ensure that the power capability is met. The control and shutdown group provides adequate shutdown margin.

A rod cluster control assembly is comprised of a group of individual neutron absorber rods fastened at the top end to a common spider assembly, as illustrated in Figure 4.2-9.

The absorber materials used in the control rod design are either: (1) Ag-In-Cd alloy extruded rods, or; 2) solid Hafnium bar. The absorber materials are essentially “black” to thermal neutrons and have sufficient additional resonance absorption to significantly increase their worth. For both the Ag-In-Cd alloy and the Hafnium design the material is sealed in cold-worked type 304 stainless steel tubes to prevent the absorber material from coming in direct contact with the coolant (Figure 4.2-10). Sufficient diametral and end clearances are provided to accommodate relative thermal expansions and material swelling, as shown in Section 4.2.3.6.

In the Enhanced Performance RCCA design a chrome plating (nominally 0.2 to 0.75 mil) is applied to the rodlets, in order to reduce control rod wear.

The bottom plugs are made bullet-nosed to reduce the hydraulic drag during reactor trip and to guide smoothly into the dashpot section of the fuel assembly guide thimbles.

The material used in the absorber rod end plugs is Type 308 stainless steel. The design stresses used for the Type 308 material are the same as those defined in the ASME Code, Section III, for Type 304 stainless steel. At room temperature the yield and ultimate stresses per ASTM 580 are exactly the same for the two alloys. In view of the similarity of the alloy composition, the temperature dependence of strength for the two materials is also assumed to be the same.

The allowable stresses used as a function of temperature are listed in Table 1.1-2 of Section III of the ASME B&PV Code. The fatigue strength for the Type 308 material is based on the S-N curve for austenitic stainless steels in Figure 1-9.2 of section III.

The spider assembly is in the form of a central hub with radial vanes containing cylindrical fingers from which the absorber rods are suspended. Handling detents and detents for connection to the drive rod assembly are machined into the upper end of the hub. Coil springs inside the spider body absorb the impact energy at the end of a trip insertion. The radial vanes are joined to the hub by tack weld and braze and the fingers are joined to the vanes by brazing. A spring retainer bolt holds the springs and spring retainer and is threaded into the hub within the skirt and welded to prevent loosening in service. All components of the spider assembly are made from Types 304 and 308 stainless steel except for the spring retainer which is of 17-4 PH material and the springs which are Inconel-718 alloy.

The absorber rods are fastened securely to the spider to assure trouble-free service. The rods are first threaded into the spider fingers and then pinned to maintain joint tightness, after which the pins are welded in place. The end plug below the pin position is designed with a reduced section to permit flexing of the rods to correct for small operating or assembly misalignments.

The overall length is such that when the assembly is withdrawn through its full travel the tips of the absorber rods remain engaged in the guide thimbles so that alignment between rods and thimbles is always maintained. Since the rods are long and slender, they are relatively free to conform to any small misalignments with the guide thimble.

4.2.2.3.2 Standard Burnable Absorber Assembly: Each standard burnable absorber assembly consists of burnable absorber rods attached to a hold down assembly. A standard burnable absorber assembly is shown in Figure 4.2-11. When needed for nuclear considerations, burnable absorber assemblies are inserted into selected thimbles within fuel assemblies.

The absorber rods consist of borosilicate glass tubes contained within Type 304 stainless steel tubular cladding which is plugged and seal welded at the ends to encapsulate the glass. The glass is also supported along the length of its inside diameter by a thin wall tubular inner liner. The top end of the liner is open to permit the diffused helium to pass into the void volume and the liner overhangs the glass. The liner has an outward flange at the bottom end to maintain the position of the liner with the glass. A typical standard burnable absorber rod is shown in longitudinal and transverse cross sections in Figure 4.2-12.

The absorber rods in each fuel assembly are grouped and attached together at the top end of the rods to a hold down assembly by a flat perforated retaining plate (baseplate) which fits within the fuel assembly top nozzle and rests on the adaptor plate. The baseplate and the absorber rods are held down and restrained against vertical motion through a spring pack which is attached to the plate and is compressed by the upper core plate when the reactor upper internals assembly is lowered into the reactor. This arrangement ensures that the absorber rods cannot be ejected from the core by flow forces. Each rod is permanently attached to the baseplate by a nut which is lock welded into place.

The clad in the rod assemblies is slightly cold-worked Type 304 stainless steel. All other structural materials are Types 304 or 308 stainless steel except for the springs, which are Inconel-718. The borosilicate glass tube provides sufficient boron content to meet the criteria discussed in Section 4.3.1.

4.2.2.3.2a Updated Burnable Absorber Assembly: Each updated burnable absorber assembly consists of burnable absorber rods attached to a hold-down assembly. An updated burnable absorber assembly is shown in Figure 4.2-11a. When needed for nuclear considerations, updated burnable absorber assemblies are inserted into selected thimbles with fuel assemblies.

The absorber rods consist of borosilicate glass tubes contained within Type 304 stainless steel tubular cladding which is plugged and seal welded at the ends to encapsulate the glass. The glass is also supported along the length of its inside diameter by a thin-wall tubular liner. The top end of the liner is open to permit the diffused helium to pass into the void volume and the liner overhangs the glass. The liner has an outward flange at the bottom end to maintain the position of the liner with the glass. A spacer fits over the top end of the liner. A spring clip, located at the top end of the spacer, maintains the axial positioning of the spacer and glass. A typical updated burnable absorber rod is shown in longitudinal cross sections in Figure 4.2-12a.

The absorber rods in each fuel assembly are grouped and attached together at the top end of the rods to a hold-down assembly by a flat perforated baseplate which fits within the fuel assembly top nozzle and rests on the adapter plate. The baseplate and the absorber rods are held down and restrained against vertical motion through a spring pack which is attached to the plate and is compressed by the upper core plate when the reactor upper internals assembly is lowered into the reactor. This arrangement ensures that the absorber rods cannot be ejected from the core by flow forces. Each rod is permanently attached to the baseplate by either a crimp nut which is deformed into the flats of the absorber rod threaded upper fitting, or by a nut which is lock welded into place.

The clad in the updated rod assemblies is slightly cold-worked Type 304 stainless steel. All other structural materials are Types 304 or 308 stainless steel except for the springs, which are Inconel-718. The borosilicate glass tube provides sufficient boron content to meet the criteria discussed in Section 4.3.1.

The updated burnable absorber assembly design is implemented in Unit 1 Cycle 4 and Unit 2 Cycle 2. This revised design will be used in subsequent reload cores for both units. Burnable absorber assemblies of both the standard and updated designs may be present at the same time in a given reload core.

4.2.2.3.3 Neutron Source Assembly: The purpose of a neutron source assembly is to provide a base neutron level to ensure that the detectors are operational and responding to core multiplication neutrons. Since there is very little neutron activity during loading, refueling, shutdown, and approach to criticality, a neutron source is usually placed in the reactor to provide a positive neutron count of approximately two counts per second on the source range detectors attributable to core neutrons. The detectors, called source range detectors, are used primarily when the core is subcritical and during special subcritical modes of operations.

The source assembly also permits detection of changes in the core multiplication factor during core loading refueling, and approach to criticality. This can be done since the multiplication factor is related to an inverse function of the detector count rate. Therefore a change in the multiplication factor can be detected during addition of fuel assemblies while loading the core, a change in control rod positions, and changes in boron concentration.

Both primary and secondary neutron source rods may be used. The primary source rod, containing a radioactive material, spontaneously emits neutrons during initial core loading and reactor startup.

After the primary source rod decays beyond the desired neutron flux level, neutrons are then supplied by the secondary source rod. The secondary source rod contains a stable material, which must be activated by neutron bombardment during reactor operation. The activation results in the subsequent release of neutrons. This becomes a source of neutrons during periods of low neutron flux, such as during refueling and subsequent startups.

The initial reactor core employed four source assemblies: two primary and two secondary. Each primary source assembly contained one primary source rod and a number of burnable absorber rods. Each secondary source assembly contains a symmetrical grouping of six secondary source rods with thimble plugs in the remaining locations. The source assemblies are shown in Figures 4.2-13 and 4.2-14. Subsequent cores usually do not contain primary sources. Secondary sources may be present in reload cores, however, they are not required. Refer to Section 4.2.2.3.3a and 4.2.2.3.3.b for a description of refueling and reactor startup operations when secondary sources are not used.

Neutron source assemblies are employed at opposite sides of the core. The assemblies are inserted into the rod cluster control guide thimbles in fuel assemblies at selected unrodded locations.

As shown in Figure 4.2-13 the primary source assembly contains a holddown assembly identical to that of the burnable absorber assembly. The secondary source assembly shown in Figure 4.2-14 contains a spider assembly. The spider assembly is in the form of a central hub with radial vanes containing cylindrical fingers from which the secondary source rods and thimble plugs are suspended.

The primary and secondary source rods utilize the same cladding material as the absorber rods. The secondary source rods contain antimony-beryllium pellets stacked to a height of approximately 88 inches. The primary source rods contain capsules of californium (plutonium-beryllium possible alternate) source material and alumina spacer pellets to position the source material within the cladding.

The other structural members are constructed of Type 304 stainless steel except for the springs. The springs exposed to the reactor coolant are Inconel 718.

4.2.2.3.3a Refueling Operations Without Secondary Source Assemblies: Core refueling operations (e.g., core offload, reload, incore shuffle) may be conducted without secondary source assemblies present in the reactor vessel. Plant procedures ensure that an adequate neutron level exists to ensure that the core monitoring neutron detectors are operational and responding to core multiplication.

4.2.2.3.3b Reactor Startup Without Secondary Source Assemblies: Reactor startup from cold shutdown may be performed without secondary source assemblies in the reactor core. Sufficient neutrons from fission fragments present in irradiated fuel exist to provide an adequate neutron level to ensure that the source range neutron detectors are operational and responding to neutrons resulting from core multiplication.

4.2.2.3.4 Thimble Plug Assembly: In order to limit bypass flow through the rod cluster control guide thimbles for a fuel assembly that does not contain either control rods, source rods, or burnable absorber rod assemblies, the fuel assembly may be fitted with a thimble plug assembly. The thimble plug assembly, as shown in Figure 4.2-15, consists of a flat base plate with short rods suspended from the bottom surface and a spring pack assembly. The 24 short rods, called thimble CN-3213 plugs, project into the upper ends of the guide thimbles to reduce the bypass flow. Each thimble plug is permanently attached to the baseplate by a nut which is lock-welded to the threaded end of the plug. Similar short rods are also used on the source assemblies and burnable absorber assemblies to plug the ends of all vacant fuel assembly guide thimbles. Upon installation in the core, the thimble plug assembly interfaces with both the upper core plate and with the fuel assembly top nozzles by resting on the adaptor plate. The spring pack is compressed by the upper core plate when the upper internals assembly is lowered into place.

All components in the thimble plug assembly, except for the springs, are constructed from Type 304 stainless steel. The springs are Inconel 718.

Thimble plug assemblies are used with Delta 94 steam generators. CN-3213

4.2.3 Design Evaluation

The fuel assemblies and fuel rods are designed to satisfy the performance and safety criteria of Section 4.2, the mechanical design bases of Section 4.2.1, and other interfacing nuclear and thermal-hydraulic design bases specified in Sections 4.3 and 4.4. Effects of Condition II, III, IV or anticipated transients without trip (ATWT) on fuel integrity are presented in Chapter 15 or supporting topical reports.

The initial step in fuel rod design evaluation for a region of fuel is to determine the limiting rod(s). Limiting rod(s) are defined as those rod(s) whose predicted performance provides the minimum margin to each of the design criteria. For a number of design criteria the limiting rod is the lead burnup rod of a fuel region. In other instances it may be the maximum power or the minimum burnup rod. For the most part, no single rod will be limiting with respect to all design criteria.

After identifying the limiting rod(s), a worst-case evaluation is made which utilizes the limiting rod(s) design basis power history and considers the effects of model uncertainties and dimensional variations. Furthermore, to verify adherence to the design criteria, the conservative case evaluation also considers the effects of postulated transient power increases which are achievable during operation consistent with Conditions I and II. These transient power increases can affect both rod average and local power levels. The analytical methods used in the evaluation result in performance parameters which demonstrate the fuel rod behavior. Examples of parameters considered include rod internal pressure, fuel temperature, clad stress, and clad strain. In fuel rod design analyses these performance parameters provide the basis for comparison between expected fuel rod behavior and the corresponding design criteria limits.

Fuel rod assembly models used for the various evaluations are documented and maintained under an appropriate control system. Materials properties used in the design evaluations are given in Reference 4.2-2.

4.2.3.1 Cladding.

1. Vibration and Wear

Fuel rod vibrations are flow induced. The effect of the vibration on the fuel assembly and individual fuel rods is minimal. The cyclic stress range associated with deflections of such small magnitude is insignificant and has no effect on the structural integrity of the fuel rod.

The reaction force on the grid supports due to rod vibration is also small and is much less than the spring preload. Firm fuel clad spring contact is maintained. No significant wear of the clad or grid supports is expected during the life of the fuel assembly.

Clad fretting and fuel vibration have been experimentally investigated as shown in Reference 4.2-9. Reference 4.2-18 provides selected results of the hydraulic testing of the anti-snag grid design including clad fretting.

2. Fuel Rod Internal Pressure and Cladding Stresses

The burnup dependent fission gas release model (Ref. 4.2-19 and used in Ref. 4.2-A) is used in determining the internal gas pressures as a function of irradiation time. The plenum height of the fuel rod has been designed to ensure that the maximum internal pressure of the fuel rod will not exceed the value which would cause the fuel-clad diametral gap to increase during steady-state operation.

The clad stresses at a constant local fuel rod power are low. Compressive stresses are created by the pressure differential between the coolant pressure and the rod internal gas pressure. Because of the prepressurization with helium, the volume average effective stresses are always less than approximately 11,000 psi at the pressurization level used in this fuel rod design. Stresses due to the temperature gradient are not included in this average effective stress because thermal stresses are, in general, negative at the clad inside diameter and positive at the clad outside diameter, and their contribution to the clad volume average stress is small. Furthermore, the thermal stress decreases with time due to stress relaxation during steady-state operation. The stress due to pressure differential is highest in the minimum power rod at the beginning of life due to low internal gas pressure and the thermal stress is highest in the maximum power rod due to steep temperature gradient.

The internal gas pressure at beginning of life is approximately 1,000 psia for a typical lead burnup fuel rod. The total tangential stress at the clad inside diameter at beginning of life is approximately 15,800 psi compressive (~14,600 psi due to ΔP and ~1,200 psi due to ΔT) for a low power rod operating at 5 kW/ft and approximately 15,100 psi compressive (~11,900 psi due to ΔP and ~3,200 psi due to ΔT) for a high power rod operating at 15 kW/ft. However, the volume average effective stress at beginning of life is between approximately 8,000 psi (high power rod) and approximately 11,000 psi (low power rod). These stresses are substantially below even the unirradiated clad strength (~55,500 psi) at a typical clad mean operating temperature of 700°F.

Tensile stresses could be created once the clad has come in contact with the pellet. These stresses would be induced by the fuel pellet swelling during irradiation. Fuel swelling can result in small clad strains (< 1 percent) for expected discharge burnups, but the associated clad stresses are very low because of clad creep (thermal- and irradiation-induced creep). Furthermore, the 1 percent strain criterion is extremely conservative for fuel-swelling driven clad strain because the strain rate associated with solid fission product swelling is very slow.

3. Materials and Chemical Evaluation

Zircaloy-4/ZIRLO clad has a high corrosion resistance to the coolant, fuel, and fission products. As shown in Reference 4.2-1, there is considerable PWR operating experience on the capability of Zircaloy/ZIRLO as a clad material. Optimized ZIRLO has superior corrosion performance to ZIRLO and the resulting hydrogen content in the rods will be reduced correspondingly (Reference 4.2-B). Controls on fuel fabrication specify very low maximum moisture levels to preclude clad hydriding.

Metallographic examination of irradiated commercial fuel rods have shown occurrences of fuel/clad chemical interaction. Reaction layers of <1 mil in thickness have been observed between fuel and clad at limited points around the circumference. Metallographic data indicates that this interface layer remains very thin even at high burnup. Thus, there is no indication of propagation of the layer and eventual clad penetration.

Stress corrosion cracking is another postulated phenomenon related to fuel/clad chemical interaction. Out of pile tests have shown that in the presence of high clad tensile stresses, large concentrations of iodine can chemically attack the Zircaloy/ZIRLO/Optimized ZIRLO tubing and can lead to eventual clad cracking. Extensive post-irradiation examination has produced no in pile evidence that this mechanism is operative in commercial fuel (Reference 4.2-8).

4. Rod Bowing

Reference 4.2-10 presents the model used for evaluation of fuel rod bowing. Based on this approved methodology, a comparison of L^2/I (where I = the fuel rod bending moment of inertia and L = span length) shows that the South Texas XL assembly span length and rod bow are less than a corresponding 12 ft LOPAR 17 x 17 assembly. The effects of rod bow on departure from nucleate boiling ratio (DNBR) are described in Section 4.4.2.2.5.

5. Consequences of Power-Coolant Mismatch

This subject is discussed in Chapter 15.

6. Creep Collapse and Creepdown

This subject and the associated irradiation stability of cladding have been evaluated in Reference 4.2-6. It has been established that the design basis of no clad collapse during planned core life can be satisfied by limiting fuel densification.

4.2.3.2 Fuel Materials Considerations. Sintered, high density uranium dioxide fuel reacts only slightly with the clad at core operating temperatures and pressures. In the event of clad defects, the high resistance of uranium dioxide to attack by water products against fuel deterioration although limited fuel erosion can occur. As has been shown by operating experience and extensive experimental work, the thermal design parameters conservatively account for changes in the thermal performance of the fuel elements due to pellet fracture which may occur during power operation (Refs. 4.2-1 and 4.2-8). The consequences of defects in the clad are greatly reduced by the ability of uranium dioxide to retain fission products including those which are gaseous or highly volatile. Observations from several operating Westinghouse PWRs (Ref. 4.2-8) have shown that fuel pellets can densify under irradiation to a density higher than the manufactured values. Fuel densification and subsequent settling of the fuel pellets can result in local and distributed gaps in the fuel rods. Fuel densification has been minimized by improvements in the fuel manufacturing process and by specifying a nominal 95 percent initial fuel density.

The evaluation of fuel densification effects and their consideration in fuel design are described in References 4.2-4 and 4.2-19. The treatment of fuel swelling and fission gas release are described in Reference 4.2-19. The same effects, models, and treatment of fuel densification, fuel swelling, and fission gas release are used in Reference 4.2-A.

Waterlogging considerations on fuel behavior are discussed in Section 4.2.3.3.

4.2.3.3 Fuel Rod Performance. In calculating the steady-state performance of a nuclear fuel rod, the following interacting factors are considered:

1. Clad creep and elastic deflection.

2. Pellet density changes, thermal expansion, gas release, and thermal properties as a function of temperature and fuel burnup.

3. Internal pressure as a function of fission gas release, rod geometry, and temperature distribution.

These effects are evaluated using a fuel rod design model (Ref. 4.2-19 and 4.2-A). The model modifications for time dependent fuel densification are given in References 4.2-4, 4.2-5, and 4.2-19 and are used in Reference 4.2-A. With these interacting factors considered, the model determines the fuel rod performance characteristics for a given rod geometry, power history, and axial power shape. In particular, internal gas pressure, fuel and clad temperatures, and clad deflections are calculated. The fuel rod is divided into several axial sections and radially into a number of annular zones. Fuel density changes are calculated separately for each segment. The effects are integrated to obtain the internal rod pressure. The initial rod internal pressure is selected to delay fuel/clad mechanical interaction. It is limited, however, by the design criteria for the rod internal pressure (Section 4.2.1.3[b]).

The gap conductance between the pellet surface and the clad inner diameter is calculated as a function of the composition, temperature, and pressure of the gas mixture, and the gap size of contact pressure between clad and pellet. After computing the fuel temperature for each pellet annular zone, the fractional fission gas release is assessed using an empirical model derived from experimental data (Ref. 4.2-19 and used in Reference 4.2-A). The total amount of gas released is based on the average fractional release within each axial and radial zone and the gas generation rate which in turn is a function of burnup. Finally, the gas released is summed over all zones and the pressure is calculated.

The code shows good agreement in fit for a variety of published and proprietary data on fission gas release, fuel temperatures, and clad deflections (Ref. 4.2-19 and 4.2-A). Included in this spectrum are variations in power, time, fuel density, and geometry. Inpile fuel temperature measurement comparisons are shown in Reference 4.2-19 and 4.2-A.

1. Fuel-Cladding Mechanical Interaction

   One factor in fuel element duty is potential mechanical interaction of fuel and clad. This fuel/clad interaction produces cyclic stresses and strains in the clad, and these in turn consume clad fatigue life. The reduction of fuel/clad interaction is therefore a goal of design. In order to achieve this goal, and to enhance the cyclic operational capability of the fuel rod, the technology for using prepressurized fuel rods in Westinghouse PWRs has been developed.

   Initially the gap between the fuel and clad is sufficient to prevent hard contact between the two. However, during power operation a gradual compressive creep of the clad onto the fuel pellet occurs due to the external pressure exerted on the rod by the coolant. Clad compressive creep eventually results in the fuel/clad contact. During this period of fuel/clad contact changes in power level could result in changes in clad stresses and strains. By using prepressurized fuel rods to partially offset the effect of the coolant external pressure, the rate of clad creep toward the surface of the fuel is reduced. Fuel rod prepressurization delays the time at which fuel/clad interaction and contact occur and hence significantly reduces the number and extent of cyclic stresses and strains experienced by the clad both before and after fuel/clad contact. These factors result in an increase in the fatigue life margin of the clad and lead to greater clad reliability. If gaps should form in the fuel stacks, clad flattening will be prevented by the rod prepressurization so that the flattening time will be greater than the fuel core life.

   A two-dimensional (r,θ) finite element model has been established to investigate the effects of radial pellet cracks on stress concentrations in the clad. Stress concentration, herein, is defined as the difference between the maximum clad stress in the θ-direction and the mean clad stress. The first case has the fuel and clad in mechanical equilibrium and as a result the stress in the clad is close to zero. In subsequent cases the pellet power is increased in steps and the resultant fuel thermal expansion imposes tensile stress in the clad. In addition to uniform clad stresses, stress concentrations develop in the clad adjacent to radial cracks in the pellet. These radial cracks have a tendency to open during a power increase but the frictional forces between fuel and clad oppose the opening of these cracks and result in localized increases in clad stress. As the power is further increased, large tensile stresses exceed the ultimate tensile strength of uranium dioxide, and additional cracks in the fuel are created which limit the magnitude of the stress concentration in the clad.

As part of the standard fuel rod design analysis, the maximum stress concentration evaluated from finite element calculations is added to the volume averaged effective stress in the clad as determined from one-dimensional stress/strain calculations. The resultant clad stress is then compared to the temperature dependent Zircaloy/ZIRLO/Optimized ZIRLO yield stress in order to assure that the stress/strain criteria are satisfied. Additionally, a revised clad stress criteria (Reference 4.2-C) can be used for standard ZIRLO cladding which is defined as:

- The maximum cladding stress intensities excluding pellet cladding interaction but accounting for cladding corrosion as a loss-of-load carrying metal be less than the stress limit as defined based on the ASME code calculations.
- The 1% transient clad strain criterion is met.
- An additional steady-state clad strain criterion based on the total (plastic plus elastic strain) is met.
- No centerline fuel melting occurs, and
- The effect of the plastic deformation is accounted for in all fuel rod design criteria as appropriate.

Transient Evaluation Method

Pellet thermal expansion due to power increases is considered the only mechanism by which significant stresses and strains can be imposed on the clad. Power increases in commercial reactors can result from fuel shuffling (e.g., Region 3 positioned near the center of the core for Cycle 2 operation after operating near the periphery during Cycle 1), reactor power escalation following extended reduced power operation, and control rod movement. In the mechanical design model, lead rods are depleted using best estimate power histories as determined by core physics calculations. During the depletion, the amount of diametral gap closure is evaluated based upon the pellet expansion-cracking model, clad creep model, and fuel swelling model. At various times during depletion the power is increased locally on the rod to the burnup dependent attainable power density as determined by core physics calculations. The radial, tangential, and axial clad stresses resulting from the power increase are combined into a volume average effective clad stress.

The Von Mises criterion is used to evaluate if the clad yield stress has been exceeded. This criterion states that an isotropic material in multi-axial stress will begin to yield plastically when the effective stress exceeds the yield stress as determined by an axial tensile test. The yield stress correlation is that for irradiated cladding since fuel/clad interaction occurs at high burnup. Furthermore, the effective stress is increased by an allowance, which accounts for stress concentrations in the clad adjacent to radial cracks in the pellet, prior to the comparison with the yield stress. This allowance was evaluated using a two-dimensional (r,θ) finite element model.

Slow transient power increases can result in large clad strains without exceeding the clad yield stress because of clad creep and stress relaxation. Therefore, in addition to the yield stress criterion, a criterion on allowable clad strain is necessary. Based upon high strain rate burst and tensile test data on irradiated tubing, 1 percent strain was determined to be a conservative lower limit on irradiated clad ductility and thus adopted as a design criterion.

A comprehensive review of the available strain-fatigue models was conducted by Westinghouse as early as 1968.

This included the Langer-O'Donnell model (Ref. 4.2-11), the Yao-Munse model, and the Manson-Halford model. Upon completion of this review and using the results of the Westinghouse experimental programs discussed below, it was concluded that the approach defined by Langer-O'Donnell would be retained and the empirical factors of their correlation modified in order to conservatively bound the results of the Westinghouse testing program.

The Langer-O'Donnell empirical correlation has the following form:

$$S_a = \frac{E}{4N_f} \ln \frac{(100)}{100-RA} + S_e$$

where:

S_a = 1/2 E $\Delta\varepsilon_t$ = pseudo – stress amplitude which causes failure in N_f cycles, lb/in.2
$\Delta\varepsilon_t$ = total strain range, in./in.
E = Young's Modulus, lb/in.2
N_f = number of cycles to failure
RA = reduction in area at fracture in an uniaxial tensile test, %
S_e = endurance limit, lb/in.2

Both RA and S_e are empirical constants which depend on the type of material, the temperature and irradiation. The Westinghouse testing program was subdivided into the following subprograms:

a. A rotating bend fatigue experiment on unirradiated Zircaloy-4 specimens at room temperature and at 725°F (Ref. 4.2-2). Both hydrided and nonhydrided Zircaloy-4 cladding were tested.

b. A biaxial fatigue experiment in gas autoclave on unirradiated Zircaloy-4 cladding both hydrided and nonhydrided.

c. A fatigue test program on irradiated cladding from the Carolina-Virginia Tube Reactor and Yankee Core V conducted at Battelle Memorial Institute.

The results of these test programs provided information on different cladding conditions including the effect of irradiation, of hydrogen level, and of temperature.

The design equations followed the concept for the fatigue design criterion according to the ASME B&PV Code, Section III; namely:

a. The calculated pseudo-stress amplitude (S_a) has to be multiplied by a factor of 2 in order to obtain the allowable number of cycles (N_f).

b. The allowable cycles for a given S_a is 5 percent of N_f, or a safety factor of 20 on cycles.

The lesser of the two allowable number of cycles is selected. The cumulative fatigue life fraction is then computed as:

$$\sum_{1}^{k} \frac{n_k}{N_{fk}} \leq 1$$

where:

n_k = number of diurnal cycles of mode k.
N_{fk} = number of allowable cycles

It is recognized that a possible limitation to the satisfactory behavior of the fuel rods in a reactor which is subjected to daily load follow is the failure of the clad by low cycle strain fatigue. During their normal residence time in reactor, the fuel rods may be subjected to 1,000 cycles with typical changes in power level from 50 to 100 percent of their steady-state values.

The assessment of the fatigue life of the fuel rod clad is subject to a considerable uncertainty due to the difficulty of evaluating the strain range which results from the cyclic interaction of the fuel pellets and clad. This difficulty arises, for example, from such highly unpredictable phenomena as pellet cracking, fragmentation, and relocation. Nevertheless, since early 1968, this particular phenomenon has been investigated analytically and experimentally. Strain fatigue tests on irradiated and nonirradiated hydrided Zirconium-4 claddings were performed which permitted a definition of a conservative fatigue life limit and recommendation on a methodology to treat the strain fatigue evaluation of the Westinghouse reference fuel rod designs.

It is believed that the final proof of the adequacy of a given fuel rod design to meet the load follow requirements can only come from incore experiments performed on actual reactors. Experience in load follow operation dates back to early 1970 with the load follow operation of the Saxton reactor. Successful load follow operation has been performed on reactor A (400 load follow cycles) and reactor B (500 load follow cycles). In both cases, there was no significant coolant activity increase that could be associated with the load follow mode of operation.

2. Irradiation Experience

   Westinghouse fuel operational experience is presented in Reference 4.2-1. Additional test assembly and test rod experience are given in Sections 8 and 23 of Reference 4.2-8.

3. Fuel and Cladding Temperature

   The methods used for evaluation of fuel rod temperatures are presented in Section 4.4.2.11.

4. Waterlogging

Local cladding deformations typical of water-logging* bursts have never been observed in commercial Westinghouse fuel.

Experience has shown that the small number of rods which have acquired clad defects, regardless of primary mechanism, remain intact and do not progressively distort or restrict coolant flow. In fact, such small defects are normally observed through reductions in coolant activity to be progressively closed upon further operation due to the buildup of zirconium oxide and other substances. Secondary failures which have been observed in defected rods are attributed to hydrogen embrittlement of the cladding. Post-irradiation examinations point to the hydriding failure mechanism rather than a water logging mechanism; the secondary failures occur as axial cracks in the cladding and are similar regardless of the primary failure mechanism. Such cracks do not result in flow blockage. Hence the presence of such fuel, the quantity of which must be maintained below technical specification limits, does not in any way exacerbate the effects of any postulated transients.

5. 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 4.2.3.3.

The potential effects of operation with waterlogged fuel are discussed in Section 4.2.3.3 which concludes that waterlogging is not a concern during operational transients.

Clad flattening, as shown in Reference 4.2-6, 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 (Section 4.2.3.1).

Potential differential thermal expansion between the fuel rods and the guide thimbles during a transient is considered in the design. Excessive bowing of the fuel rods is precluded because the grid assemblies allow axial movement of the fuel rods relative to the grids. Specifically, 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.

6. Fuel Element Burnout and Potential Energy Release

As discussed in Section 4.4.2.2, the core is protected from DNB over the full range of possible operating conditions. 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

* Water-logging damage of a previously defected fuel rod has occasionally been postulated as a mechanism for subsequent rupture of the cladding. Such damage has been postulated as a consequence of a power increase on a rod after water has entered such a rod through a clad defect of appropriate size. Rupture is postulated upon power increase if the rod internal pressure increase is excessive due to insufficient venting of water to the reactor coolant.

degradation in heat transfer. During this time there is a potential for 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.

7. Coolant Flow Blockage Effects on Fuel Rods

   This evaluation is presented in Section 4.4.4.7.

4.2.3.4 Spacer Grids. The coolant flow channels are established and maintained by the structure composed of grids and guide thimbles. The lateral spacing between fuel rods is provided and controlled by the support dimples of adjacent grid cells. Contact of the fuel rods on the dimples is maintained through the clamping force of the grid springs. Lateral motion of the fuel rods is opposed by the spring force and the internal moments generated between the spring and the support dimples. Grid testing is discussed in Reference 4.2-12.

As shown in Reference 4.2-12 and 4.2-21, grid crushing tests and seismic and LOCA evaluations show that the grids will maintain a geometry that is capable of being cooled under the worst-case accident Condition IV event.

4.2.3.5 Fuel Assembly.

4.2.3.5.1 Stresses and Deflections: The fuel assembly component stress levels are limited by the design. For example, stresses in the fuel rod due to thermal expansion and Zircaloy and ZIRLO/Optimized ZIRLO irradiation growth are limited by the relative motion of the rod as it slips over the grid spring and dimple surfaces. Clearances between the fuel rod ends and nozzles are provided so that Zircaloy and ZIRLO/Optimized ZIRLO irradiation growth does not result in rod end interferences. Stresses in the fuel assembly caused by tripping of the RCCA have little influence on fatigue because of the small number of events during the life of an assembly. Assembly components and prototype fuel assemblies made from production parts have been subjected to structural tests to verify that the design bases requirements are met.

As discussed in Sections 4.2.1 and 4.2.1.5, the fuel assembly design loads for shipping and handling have been established at 6 g lateral and 4 g axial while maintaining dimensional stability. Accelerometers are permanently placed into the shipping cask to monitor and detect fuel assembly accelerations that would exceed the criteria. Past history and experience has indicated that loads which exceed the allowable limits rarely occur. Exceeding the limits requires reinspection of the fuel assembly for damage. Tests on various fuel assembly components such as the grid assembly, sleeves, inserts, and structure joints have been performed to assure that the shipping design limits do not result in impairment of fuel assembly function. As discussed in Section 9.1.4, the Fuel Handling System is designed such that the inertial loads imparted to the fuel assemblies during handling operations are less than the loads which could cause damage.

4.2.3.5.2 Dimensional Stability: A prototype fuel assembly has been subjected to column loads in excess of those expected in normal service and faulted conditions (Ref. 4.2-12).

Fuel rod swelling, thermal expansion, or fuel rod/thimble tube bowing will not prevent the reactivity control systems from assuring that the capability to cool the core is maintained under postulated accident conditions and appropriate margin for stuck rods. In the early phase of the transient following the coolant pipe break, the high axial loads, which could be generated by the difference in thermal expansion between fuel clad and thimbles, are relieved by slippage of the fuel rods through the grids. The relatively low drag force restraint on the fuel rods will induce only minor thermal bowing, which is insufficient to close the fuel rod-to-thimble tube gap.

References 4.2-12 and 4.2-17 show that the fuel assemblies will maintain a geometry amenable to cooling during a combined seismic and LOCA.

4.2.3.5.3 Seismic and LOCA Loads: An analysis has also been performed to demonstrate that the fuel assemblies maintain a geometry that is capable of being cooled under the worst-case accident.

The fuel assembly response resulting from a safe shutdown earthquake and the limiting auxiliary line breaks was analyzed using time-history numerical techniques. The vessel motion for this type of accident primarily causes lateral loads on the reactor core. Consequently, the finite element seismic model as described in References 4.2-12, 4.2-17, and 4.2-21 was used to assess the fuel assembly deflections and impact forces. The input parameters were modified to appropriately represent the 17 x 17 14-ft fuel assemblies.

The time-history motions of the upper and lower core plates and the barrel at the upper core plate elevation, which are simultaneously applied to the simulated reactor core model as input motion, were obtained from the time-history analysis of the reactor vessel and internals. The fuel assembly displacements and impact forces were obtained with the reactor core model by using the motions resulting from a seismic event or LOCA. The auxiliary line LOCAs are the accumulator and surge line breaks that produced the limiting structural loads for the fuel assembly.

4.2.3.5.3.1 Grid Analyses – The maximum grid impact forces obtained from the break and seismic analyses were less than the allowable grid strength. A calculation of the grid maximum impact forces, combined with the square root sum of the squares method (in accordance with SRP Section 4.2, Appendix A), also results in values less than the allowable grid strength for both Unit 1 and Unit 2. CN-3204

4.2.3.5.3.2 Nongrid Analyses – The stresses included in the various fuel assembly nongrid components were assessed based on the most limiting seismic and LOCA conditions. The fuel assembly axial forces resulting from the LOCA are the primary source of the stresses in the thimble guide tube and fuel assembly nozzles. The fuel rod accident induced stresses, which are generally very small, are caused by bending due to the fuel assembly deflections during the seismic accident. The component stresses, which include normal operating stresses, are substantially below the established allowable limits. Consequently, the structural designs of the fuel assembly components are acceptable under the postulated accident design conditions.

4.2.3.6 Incore Control Components. The components are analyzed for loads corresponding to normal, upset, emergency, and faulted conditions. The analysis performed depends on the mode of operation under consideration.

The scope of the analysis requires many different techniques and methods, both static and dynamic.

Some of the loads that are considered on each component where applicable are as follows:

1. Control rod trip (equivalent static load)
2. Differential pressure
3. Spring preloads
4. Coolant flow forces (static)
5. Temperature gradients
6. Differences in thermal expansion
   a. Due to temperature differences
   b. Due to expansion of different materials
7. Interference between components
8. Vibration (mechanically or hydraulically induced)
9. Operational transients
10. Pump overspeed
11. Seismic loads (operation basis earthquake and safe shutdown earthquake)
12. Blowdown forces (due to cold or hot leg break)
13. Material swelling and gas generation pressure

The main objective of the analysis is to satisfy allowable stress limits, to assure an adequate design margin, and to establish deformation limits which are concerned primarily with the functioning of the components. The stress limits are established not only to assure that peak stresses will not reach unacceptable values, but also limit the amplitude of the oscillatory stress component in consideration of fatigue characteristics of the materials. Standard methods of strength of materials are used to establish the stresses and deflections of these components.

Sufficient diametral and end clearances have been provided in the neutron absorber, burnable absorber, and source rods to accommodate the relative thermal expansions and material swelling between the enclosed material and the surrounding clad and end plugs. There is no bending or warping induced in the rods although the clearance offered by the guide thimble would permit a postulated warpage to occur if there were no restraint on the rods. Bending, therefore, is not considered in the analysis of the rods.

Experience with incore control components is discussed in Reference 4.2-1. Materials data and evaluations are given in Reference 4.2-2. A mechanical and materials evaluation of the hafnium absorber rods is presented in Sections 4 and 5.1 of Reference 4.2-7.

1. Mechanical Strength Evaluation

The design of incore component rods provides a sufficient cold void volume within the burnable absorber and source rods to limit the internal pressures to a value which satisfies the criteria in Section 4.2.1.6. A gas plenum at the top of the absorber rods provides void volume for the pressure buildup. The void volume for the helium in the burnable absorber rods is also obtained through the use of glass in tubular form which provides a central void along the length of the rods. Helium gas is not released by hafnium neutron absorber rod material. The internal pressure of source rods continues to increase from ambient until end of life. The stress analysis of reactivity component rods assumes 100 percent gas release to the rod void volume and satisfies the criteria in Section 4.2.1.6.

Based on available data for properties of the borosilicate glass and on nuclear and thermal calculations for the burnable absorber rods, gross swelling or cracking of the glass tubing is not expected during operation. Some minor creep of the glass at the hot spot on the inner surface of the tube could occur but would continue only until the glass came in contact with the inner liner. The wall thickness of the inner liner is sized to provide adequate support in the event of slumping and to collapse locally before rupture of the exterior cladding if unexpected large volume changes due to swelling or cracking should occur. The top of the inner liner is open to allow communication to the central void by helium which diffuses out of the glass.

2. Thermal Evaluation

The radial and axial temperature profiles have been determined by considering gap conductance, thermal expansion, and neutron or gamma heating of the contained material as well as gamma heating of the clad. The maximum hafnium absorber material temperature was found to be less than 1,070°F which occurs axially at only the highest flux region. The maximum borosilicate glass temperature was calculated to be about 1200 °F and takes place following the initial rise to power. The glass temperature then decreases rapidly for the following reasons: 1) reduction in power generation due to boron-10 depletion; 2) better gap conductance as the helium produced diffuses to the gap; and 3) external gap reduction due to borosilicate glass creep. Rod, guide thimble, and dashpot flow analysis indicates that the flow is sufficient to prevent coolant boiling and maintain clad temperatures at which the clad material has adequate strength to resist coolant operating pressures and rod internal pressures.

Coolant temperatures for thimbles at the bottom of the fuel assemblies range from approximately 530°F to 553°F. Mid-assembly temperatures reach a high of about 593°F while the maximum temperatures at the top of the assemblies are about 641°F.

3. Irradiation and Chemical Evaluation

The materials selected are considered to be the best available from the standpoint of resistance to irradiation damage and compatibility to the reactor environment. The materials selected partially dictate the reactor environment; e.g., chloride ion control in the coolant.

With regard to the materials of construction exhibiting satisfactory resistance to adverse property changes in a radioactive environment, it should be noted that work on breeder reactors in current design, similar materials are being applied. At high fluences the austenitic materials increase in strength with corresponding decreased ductility (as measured by tensile tests) but energy absorption (as measured by impact tests) remains quite high. Corrosion of the materials exposed to the coolant is quite low and proper control of chloride ions and oxygen in the coolant will prevent the occurrence of stress corrosion. Many of the austenitic stainless steel base materials used are processed and fabricated by furnace brazing. The procedure used requires that the pieces be rapidly cooled so that the time-at-temperature is minimized. The time that is spent by the control rod spiders in the sensitization range, 800-1500°F, during fabrication is controlled to preclude sensitization. The 17-4 PH parts are all aged at the highest standard aging temperature of 1,100°F to avoid stress corrosion problems exhibited by aging at lower temperatures.

Based on the preceding considerations, it is judged that the potential for interference with rod cluster control movement due to possible corrosion phenomena is very low. Additional information on irradiation and chemical effects is given in Reference 4.2-2.

4. Failure Evaluations

Analysis of the RCCAs show that if the drive mechanism housing ruptures, the RCCA will be ejected from the core by the pressure across the drive rod assembly. The ejection is also predicted on the failure of the drive mechanism to retain the drive rod/RCCA position.

It should be emphasized that a drive mechanism housing rupture will cause the ejection of only one RCCA with the other assemblies remaining in the core. Analysis also showed that a pressure drop in excess of 4,000 psi must occur across a two-fingered vane to break the vane/spider body joint causing ejection of two neutron absorber rods from the core. Since the greatest possible pressure drop in the system is only 2,250 psi, a pressure drop in excess of 4,000 psi is not credible. Thus, the ejection of the neutron absorber rods due to pressure drop is not possible.

Ejection of a burnable absorber or thimble plug assembly is conceivable based on the postulation that the hold down bar fails and that the baseplate and burnable absorber rods are severely deformed. In the unlikely event that failure of the holddown bar occurs, the upward displacement of the burnable absorber assembly only permits the baseplate to contact the upper core plate. Since this displacement is small, the major portion of the borosilicate glass tubing remains positioned within the core. In the case of the thimble plug assembly, the thimble plugs will partially remain in the fuel assembly guide thimbles thus maintaining a majority of the desired flow impedance. Further displacement or complete ejection would necessitate the square baseplate and burnable absorber rods be forced, thus plastically deformed, to fit up through a smaller diameter hole. It is expected that this condition requires a substantially higher force or pressure drop than that of the hold down bar failure.

The mechanical design of the reactivity control components provides for the protection of the active elements to prevent the loss of control capability and functional failure of critical components. The components have been reviewed for potential failure and consequences of a functional failure of critical parts. The results of the review are summarized below.

1. The basic absorbing materials are sealed from contact with the primary coolant and the fuel assembly and guidance surfaces by a high quality stainless steel clad. Potential loss of absorber mass or reduction in reactivity control material due to mechanical or chemical erosion or wear is, therefore, reliably prevented.

2. A breach of the cladding for a limited number of absorber rods for any postulated reason does not result in serious consequences. The hafnium absorber material is relatively inert and still

remains remote from high coolant velocity regions. Rapid loss of the hafnium would not occur, since corrosion rates for hafnium are extremely low (Ref. 4.2-7).

3. The individually clad absorber rods are doubly secured to the retaining spider vane by a threaded joint and a welded lock pin.

   It should also be noted that in several instances of control rod jamming (of a similar design) caused by foreign particles, the individual rods at the site of the jam have borne the full capacity of the CRDM and higher impact loads to dislodge the jam without failure. The conclusion to be drawn from this experience is that this joint is extremely insensitive to potential mechanical damage. A failure of the joint would result in the insertion of the individual rod into the core resulting in reduced reactivity.

4. The radial vanes are attached to the spider body by a welded and brazed joint.

   It is a feature of the design that the guidance of the rod cluster control is accomplished by the inner fingers of these vanes. They are, therefore, the most susceptible to mechanical damage. Since these vanes carry two rods, failure of the vane-to-hub joint does not prevent the free insertion of the rod pair. Neither does such a failure interfere with the continuous free operation of the drive line.

   Failure of the vane-to-hub joint of a single rod vane could potentially result in failure of the separated vane and rod to insert. This could occur only at withdrawal elevations where the spider is above the continuous guidance section of the guide tube (in the upper internals). A rotation of the disconnected vane could cause it to hang on one of the guide cards in the intermediate guide tube. Such an occurrence would be evident from the failure of the rod cluster control to insert below a certain elevation but with free motion above this point.

   This possibility is considered extremely remote because the single rod vanes are subjected to only vertical loads and very light lateral reactions from the rods. The consequences of such a failure are not considered critical since only one drive line of the reactivity control system would be involved. This condition is readily observed and can be corrected at shutdown.

5. The spider hub being of single unit cylindrical construction is very rugged and of extremely low potential for damage. It is difficult to postulate a condition to cause failure. Should some unforeseen event cause fracture of the hub above the vanes, the lower portion with the vanes and rods attached would insert by gravity into the core causing a reactivity decrease. The rod could not then be removed by the drive line. Fracture below the vanes cannot be postulated since all loads, including scram impact, are taken above the vane elevation.

6. The guide thimbles of the fuel assemblies provide a clear channel for insertion of the rod cluster control rods. In the event that control rods are subject to incomplete insertion (as was first observed during Unit 1 Cycle 6), bounding scenarios will be evaluated to demonstrate that an unreviewed safety question will not exist. Fuel rod failure due to postulated control rod contact is prevented by providing this physical barrier between the fuel rod and the intended insertion channel. Distortion of the fuel rods by bending cannot apply sufficient force to damage or significantly distort the guide thimble. Fuel rod distortion by swelling, though precluded by design, would be terminated by fracture before contact with the guide thimble occurs. If such were not the case, it would be expected that a force reaction at the

point of contact would cause a slight deflection of the guide thimble. The radius of curvature of the deflected shape of the guide thimbles would be sufficiently large to have a negligible influence on rod cluster control insertion.

Burnable Absorber Assemblies

The burnable absorber assemblies are static temporary reactivity control elements. The axial position is assured by the hold down assembly which bears against the upper core plate. Their lateral position is maintained by the guide thimbles of the fuel assemblies.

The individual rods are shouldered against the underside of the baseplate. In the standard burnable absorber assembly, the rods are securely fastened at the top of the baseplate by a threaded nut which is then locked into place with a welded pin. The updated burnable absorber assembly uses crimp nuts to securely fasten the individual rods to the baseplate. The nuts are deformed into the flats of the upper fitting of the individual rods, thereby locking the rods to the baseplate. The diagonal dimension of the baseplate is larger than the diameter of the flow holes through the core plate. Failure of the hold-down bar or spring pack, therefore, does not result in ejection of the burnable absorber rods from the core.

The only incident that could potentially result in ejection of the burnable absorber rods is a multiple fracture of the baseplate. This is not considered credible because of the light loads borne by this component. During normal operation the loads borne by the plate are distributed at the points of attachment. Even a multiple fracture of the baseplate plate would result in jamming of the plate segments against the upper core plate, again preventing ejection. Excessive reactivity increase due to burnable absorber ejection is, therefore, prevented.

4.2.4. Testing and Inspection Plan

4.2.4.1 Quality Assurance Program. The Westinghouse Electric Company Quality Management System (QMS) described in Reference 4.2-13 is the Westinghouse Electric Company top-level quality assurance program description. It applies to activities that affect the quality of items and services provided by all Westinghouse worldwide operations. The QMS describes the Westinghouse commitments to the quality assurance requirements of ISO 9001; 10CFR50, Appendix B; and ASME NQA-1.

Westinghouse drawings and product, process, and material specifications identify the inspection to be performed.

4.2.4.2 Quality Control. Quality Control (QC) philosophy is generally based on the following inspections being performed to a 95 percent confidence that at least 95 percent of the product meets specification, unless otherwise noted.

1. Fuel System Components and Parts

The characteristics inspected depend upon the component parts and include dimensional, visual, check audits of test reports, material certification, and nondestructive examination such as X-ray and ultrasonic.

All materials used in this core is accepted and released by QC.

2. Pellets

Inspection is performed for dimensional characteristics such as diameter, density, length, and squareness of ends. Additional visual inspections are performed for cracks, chips, and surface conditions according to approved standards.

Density is determined in terms of weight per unit length and is plotted on zone charts used in controlling the process. Chemical analyses are taken on a specified sample basis throughout pellet production.

3. Rod Inspection

Fuel rod, control rodlet, burnable absorber, and source rod inspection consists of the following nondestructive examination techniques and methods, as applicable:

a. Leak Testing

Each rod is tested using a calibrated mass spectrometer with helium being the detectable gas.

b. Enclosure Welds

Rod welds are inspected by ultrasonic test or X-ray in accordance with a qualified technique and Westinghouse specifications.

c. Dimensional

All rods are dimensionally inspected prior to final release. The requirements include such items as length, camber, and visual appearance.

d. Plenum Dimensions

All fuel rods are inspected by gamma scanning or other approved methods as discussed in Section 4.2.4.4 to ensure proper plenum dimensions.

e. Pellet-to-Pellet Gaps

All fuel rods are inspected by gamma scanning or other approved methods as discussed in Section 4.2.4.4 to ensure that no significant gaps exist between pellets.

f. Gamma Scanning

All fuel rods are active gamma scanned to verify enrichment control prior to acceptance for assembly loading.

g. Traceability

Traceability of rods and associated rod components is established by QC.

4. Assemblies

Each fuel, control rod, burnable absorber, and source rod assembly is inspected for drawing and/or specification requirements. Other incore control component inspection and specification requirements are given in Section 4.2.4.3.

5. Other inspections

The following inspections are performed as part of the routine inspection operation:

a. Tool and gauge inspection and control including standardization to primary and/or secondary working standards. Tool inspection is performed at prescribed intervals on all serialized tools. Complete records are kept of calibration and conditions of tools.

b. Audits are performed of inspection activities and records to assure that prescribed methods are followed and that records are correct and properly maintained.

c. Surveillance inspections where appropriate, as well as audits of outside contractors, are performed to ensure conformance with specified requirements.

6. Process Control

To prevent the possibility of mixing enrichments during fuel manufacture and assembly, strict enrichment segregation and other process controls are exercised.

The uranium dioxide powder is kept in sealed containers. The contents are fully identified both by descriptive tagging and preselected color coding. A Westinghouse identification tag completely describing the contents is affixed to the containers before transfer to powder storage. Isotopic content is confirmed by analysis.

Powder withdrawal from storage can be made by only one authorized group, which directs the powder to the correct pellet production line. All pellet production lines are physically separated from each other and pellets of only a single nominal enrichment and density are produced in a given production line at any given time.

Finished pellets are placed on trays identified with the same color code as the powder containers and transferred to segregated storage racks within the confines of the pelleting area. Samples from each pellet lot are tested for isotopic content and impurity levels prior to acceptance by QC. Physical barriers prevent mixing of pellets of different nominal densities and enrichments in this storage area. Unused powder and substandard pellets are returned to storage in the color coded containers.

Loading of pellets into the clad is performed in isolated production lines and again only one density and enrichment is loaded on a line at a time.

A serialized traceability code is placed on each fuel tube which identifies the contract and enrichment. The end plugs are inserted, and then inert welded to seal the tube. The fuel tube remains coded, and traceability identified until just prior to installation in the fuel assembly.

At the time of installation into an assembly, the traceability codes are removed and a matrix is generated to identify each rod in its position within a given assembly. The top nozzle is inscribed with a permanent identification number providing traceability to the fuel contained in the assembly.

Similar traceability is provided for burnable absorber, source rods, and control rodlets as required.

4.2.4.3 Incore Control Component Testing and Inspection. During the component manufacturing phase, the following requirements apply to the reactivity control components to assure the proper functioning during reactor operation:

1. All materials are procured to specifications to attain the desired standard of quality.

2. A spider from each braze lot is proof tested by applying a load to the spider body.

3. All rods are checked for integrity by the methods described in Section 4.2.4.2.3.

4. To assure proper fitup with the fuel assembly, the rod cluster control, burnable absorber and source assemblies are installed in the fuel assembly without restriction or binding in the dry condition. Also, a straightness of 0.01 in./ft is required on the entire inserted length of each rod assembly.

   The RCCAs are functionally tested following core loading but prior to criticality to demonstrate reliable operation of the assemblies. Each assembly is operated (and tripped) one time at no flow/cold conditions, one time at full flow/cold conditions, and one time at full flow/hot conditions. Control rods whose trip times exceed the two-sigma limit of the trip times for all control rods will be retested a minimum of three times. Thus each assembly is tested a minimum of three times or up to a maximum of 12 times to ensure the assemblies are properly functioning.

In order to demonstrate continuous free movement of the RCCAs and to ensure acceptable core power distributions during operations, partial movement checks are performed on every RCCA during reactor critical operation at the frequency specified by the Technical Specifications. In addition, periodic drop tests of the RCCAs are performed at each refueling shutdown to demonstrate continued ability to meet trip time requirements, to ensure core subcriticality after reactor trip, and to limit potential reactivity insertions from a hypothetical RCCA ejection.

If an RCCA cannot be moved by its mechanism, adjustments in the boron concentration ensure that adequate shutdown margin would be achieved following a trip. Thus inability to move one RCCA can be tolerated. More than one inoperable RCCA could be tolerated, but would impose additional demands on the plant operator. Therefore, the number of inoperable RCCA has been limited to one.

4.2.4.4 Tests and Inspections by Others. If any tests and inspections are to be performed on behalf of Westinghouse, Westinghouse will review and approve the quality control procedures, inspection plans, etc., to be utilized to ensure that they are equivalent to the description provided above and are performed properly to meet all Westinghouse requirements.

4.2.4.5 Inservice Surveillance. Westinghouse is conducting a program to examine detailed aspects of the 17 x 17 fuel assembly. This program is described in Section 23 of Reference 4.2-8. Reference 4.2-1 is periodically updated in order to provide recent results of operating experience with Westinghouse fuel and incore control components.

A surveillance program for the inspection of post-irradiated fuel assemblies will be established. This program will consist of a qualitative visual examination of a representative sample of discharged fuel assemblies during each refueling. This examination will include items such as visual checks for the presence of foreign material, fuel assembly damage, rod to top nozzle mechanical interference, and gross differences as compared to other fuel assemblies. The surveillance program will provide for additional inspection of other fuel assemblies if unusual characteristics are observed during the initial visual inspection for each cycle. The post-irradiated fuel surveillance program will address disposition of any fuel assemblies receiving an unsatisfactory visual examination. These assemblies will not be reinserted into the core until a more detailed inspection or evaluation can be performed, and if required, corrective action will be taken.

4.2.4.6 Onsite Inspection. Detailed written procedures are used by station staff for the post shipment inspection and handling of all new fuel and associated components such as control rods and other inserts. The procedures are specific and have been field tested. Quality Assurance audits the data or information compiled as a result of the use of these procedures.

Loaded fuel containers, when received onsite, are externally inspected to ensure that labels and markings are intact and the seals are unbroken. After the containers are opened, the shock indicators attached to the suspended internals are inspected to determine if the contents were subjected to unacceptable acceleration during transit.

Following removal of the fuel assembly from the container in accordance with detailed procedures, the fuel assembly polyethylene wrapper is examined for evidence of damage. The polyethylene wrapper is then removed and a visual inspection of the fuel assembly is performed.

Control rod assemblies which are shipped in fuel assemblies are inspected after the removal of the fuel assembly from the container. The control rod assembly is withdrawn a few inches from the fuel assembly to ensure free and unrestricted movement. The exposed section is then visually inspected for mechanical integrity. The RCCA is reinserted in the fuel assembly and stored with the fuel assembly. If control rod assemblies are shipped separately from fuel assemblies, they shall be visually inspected for mechanical integrity.

4.2.4.7 Online Fuel Failure Monitoring. The function of the Chemical and Volume Control System (CVCS) letdown monitor is to monitor the CVCS letdown liquid process and to provide indication of abnormal activity levels in the RCS. This monitor can act as a means of failed fuel warning because failed fuel would be a cause of an increase in activity. However, confirmation of the cause of any abnormal activity levels will be made by laboratory analysis of primary coolant. For a discussion of CVCS letdown monitor, refer to information provided on liquid process and effluent monitors presented in Section 11.5.2.4 and Table 11.5-1.

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TABLE 4.2-1

FUEL ASSEMBLY COMPONENT STRESSES (Percent of Allowable)

Component	Uniform Stresses (Direct/Membrane)	Combined Stresses (Membrane + Bending)
Thimble	77.8	58.0
Fuel Rod*	33.5	26.3
Top Nozzle Plate	- - -	7.2
Bottom Nozzle Plate	- - -	45.3
Bottom Nozzle Leg	28.0	26.5

* Including primary operating stresses
\- - - A negligible value

4.3 NUCLEAR DESIGN

4.3.1 Design Bases

This section describes the design bases and functional requirements used in the nuclear design of the fuel and reactivity control system and relates these design bases to the General Design Criteria (GDC) in 10CFR50 Appendix A. Where appropriate, supplemental criteria such as the Appendix K to 10CFR50 for Emergency Core Cooling Systems (ECCSs) are addressed. Before discussing the nuclear design bases it is appropriate to briefly review the four major categories ascribed to conditions of plant operation.

The full spectrum of plant conditions is divided into four categories, in accordance with the anticipated frequency of occurrence and risk to the public:

1. Condition I - Normal Operation
2. Condition II - Incidents of Moderate Frequency
3. Condition III - Infrequent Faults
4. Condition IV - Limiting Faults

In general the Condition I occurrences are accommodated with margin between any plant parameter and the value of that parameter which would require either automatic or manual protective action. Condition II incidents are accommodated with, at most, a shutdown of the reactor with the plant capable of returning to operation after corrective action. Fuel damage (fuel damage as used here is defined as penetration of the fission product barrier; i.e., the fuel rod clad) is not expected during Condition I and Condition II events. It is not possible, however, to preclude a very small number of rod failures. These are within the capability of the plant cleanup system and are consistent with the plant design basis.

Condition III incidents shall not cause more than a small fraction of the fuel elements in the reactor to be damaged, although sufficient fuel element damage might occur to preclude immediate resumption of operation. The release of radioactive material due to Condition III incidents should not be sufficient to interrupt or restrict public use of these areas beyond the exclusion radius. Furthermore, a Condition III incident shall not, by itself generate a Condition IV fault or result in a consequential loss of function of the reactor coolant or reactor Containment barriers.

Condition IV occurrences are faults that are not expected to occur but are defined as limiting faults which must be designed against. Condition IV faults shall not cause a release of radioactive material that results in an undue risk to public health and safety.

The core design power distribution limits related to fuel integrity are met for Condition I occurrences through conservative design and maintained by the action of the control system. The requirements for Condition II occurrences are met by providing an adequate protection system which monitors reactor parameters. The control and protection systems are described in Chapter 7 and the consequences of Conditions II, III, and IV occurrences are given in Chapter 15.

4.3.1.1 Fuel Burnup

Basis

The fuel rod design basis is described in Section 4.2. the nuclear design basis is to install sufficient reactivity in the fuel to attain a region average discharge burnup of approximately 45,000 MWD/MTU. The above, along with the design basis in Section 4.3.1.3, Control of Power Distribution, satisfies GDC 10.

Discussion

Fuel burnup is a measure of fuel depletion which represents the integrated energy output of the fuel (MWD/MTU) and is a convenient means for quantifying fuel exposure criteria.

The core design lifetime or design discharge burnup is achieved by installing sufficient initial excess reactivity in each fuel region and by following a fuel replacement program (such as that described in Section 4.3.2) that meets all safety-related criteria in each cycle of operation.

Initial excess reactivity installed in the fuel, although not a design basis, must be sufficient to maintain core criticality at full power operating conditions throughout cycle life with equilibrium xenon, samarium, and other fission products present. The end of design cycle life is defined to occur when the chemical shim concentration is essentially zero with control rods present to the degree necessary for operational requirements (e.g., the controlling bank at the “bite” position). In terms of chemical shim boron concentration this represents approximately 10 ppm with no control rod insertion.

A limitation on initial installed excess reactivity is not required other than as is quantified in terms of other design bases such as core negative reactivity feedback and shutdown margin discussed below.

4.3.1.2 Negative Reactivity Feedbacks (Reactivity Coefficient).

Basis

The fuel temperature coefficient will be negative and the moderator temperature coefficient of reactivity will be nonpositive for operation at rated thermal power (RTP), thereby providing negative reactivity feedback characteristics. The design basis meets GDC 11.

Discussion

When compensation for a rapid increase in reactivity is considered, there are two major effects. These are the resonance absorption effects (Doppler) associated with changing fuel temperature and the spectrum effect resulting from changing moderator density. These basic physics characteristics are often identified by reactivity coefficients. The use of slightly enriched uranium ensure that the Doppler coefficient of reactivity is negative. This coefficient provides the most rapid reactivity compensation. The core is also designed to have an overall negative moderator temperature coefficient of reactivity when operated at rated thermal power (RTP) so that average coolant temperature or void content provides another, slower compensatory effect.

Nominal power operation is permitted only in a range of overall negative moderator temperature coefficient. The negative moderator temperature coefficient can be achieved through use of fixed burnable absorber and/or control rods by limiting the reactivity held down by soluble boron.

Burnable absorber content (quantity and distribution) is not stated as a design basis other than as it relates to accomplishing a nonpositive moderator temperature coefficient at power operating conditions discussed above.

4.3.1.3 Control of Power Distribution.

Basis

The nuclear design basis is that, with at least a 95 percent confidence level:

1. The fuel will not be operated at greater than 14.3 kW/ft under normal operating conditions including an allowance of 0.6 percent for calorimetric.
2. Under abnormal conditions including the maximum overpower condition, the fuel peak power will not cause melting as defined in Section 4..4.1.2.
3. The fuel will not operate with a power distribution that violates the departure from nucleate boiling (DNB) design basis (i.e., the DNBR shall not be less than the design limit DNBR, as discussed in Section 4.4.1) under Condition I and II events including the maximum overpower condition.
4. Fuel management will be such as to produce rod powers and burnups consistent with the assumptions in the fuel rod mechanical integrity analysis of Section 4.2.

The above basis meets GDC 10.

Discussion

Calculation of extreme power shapes which affect fuel design limits is performed with proven methods and verified frequently with measurements from operating reactors. The conditions under which limiting power shapes are assumed to occur are chosen conservatively with regard to any permissible operating state.

Even though there is good agreement between measured peak power calculations and measurements, a nuclear uncertainty margin (Section 4.3.2.2.7) is applied to calculated peak local power. Such a margin is provided both for the analysis for normal operating states and for anticipated transients.

4.3.1.4 Maximum Controlled Reactivity Insertion Rate.

Basis

The maximum reactivity insertion rate due to withdrawal of rod cluster control assemblies (RCCAs) at power or by boron dilution is limited. During normal at-power operation, the maximum controlled reactivity rate change is less than 30 pcm/sec*.

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* 1 pcm = (percent millirho) = $10^{-5}\ \Delta\rho$ (see Note ++ in Table 4.3-2)

A maximum reactivity change rate of 75 pcm/sec for accidental withdrawal of control banks is set such that peak heat generation rate and departure from nucleate boiling ratio (DNBR) do not exceed the maximum allowable at overpower conditions. This satisfies GDC 25.

The maximum reactivity worth of control rods and the maximum rates of reactivity insertion employing control rods are limited to preclude rupture of the coolant pressure boundary or disruption of the core internals to a degree which impair core cooling capacity due to a rod withdrawal or ejection accident (Chapter 15).

Following any Condition IV event (rod ejection, steamline break, etc.) the reactor can be brought to the shutdown condition and core will maintain acceptable heat transfer geometry. This satisfies GDC 28.

Discussion

Reactivity addition associated with an accidental withdrawal of a control bank (or banks) is limited by the maximum rod speed (or travel rate) and by the worth of the bank(s). The maximum control rod speed is 45 in./min and the maximum rate of reactivity change considering two control banks moving is less than 75 pcm/sec. During normal operation at power and with normal control rod overlap, the maximum reactivity change rate is less than 30 pcm/sec.

The reactivity change rates are conservatively calculated assuming unfavorable axial power and xenon distributions. The peak xenon burnout rate is 25 pcm/min, significantly lower than the maximum reactivity addition rate of 30 pcm/sec for normal operation and 75 pcm/sec for accidental withdrawal of two banks.

4.3.1.5 Shutdown Margins.

Basis

Minimum shutdown margin as specified in the Technical Specifications is required at any power operating condition, in the hot standby shutdown condition, and in the cold shutdown condition.

In all analyses involving reactor trip, the single, highest worth rod cluster control assembly is postulated to remain untripped in its full-out position (stuck rod criterion). This satisfies GDC 26.

Discussion

Two independent reactivity control systems are provided, namely control rods and soluble boron in the coolant. The control rod system can compensate for the reactivity effects of the fuel and water temperature changes accompanying power level changes over the range from full-load to no-load. In addition, the control rod system provides the minimum shutdown margin under Condition I events and is capable of making the core subcritical rapidly enough to prevent exceeding acceptable fuel damage limits assuming that the highest worth control rod is stuck out upon trip.

The boron system can compensate for all xenon burnout reactivity changes and will maintain the reactor in the cold shutdown. Thus, backup and emergency shutdown provisions are provided by a mechanical and a chemical shim control system which satisfies GDC 26.

Basis

When fuel assemblies are in the pressure vessel and the vessel head is not in place, k_{eff} will be maintained at or below 0.95 with soluble boron.

Discussion

American National Standards Institute (ANSI) Standard N18.2 specifies a k_{eff} not to exceed 0.95 in spent fuel storage racks and transfer equipment flooded with pure water and a k_{eff} not to exceed 0.98 in new fuel storage racks assuming optimum moderation. No criterion is give for the refueling operation, however a 5 percent margin, which is consistent with spent fuel storage and transfer and the new fuel storage, is adequate for the controlled and continuously monitored operations involved.

The boron concentration required to meet the refueling shutdown criteria will be specified in the Technical Specifications. Verification that this shutdown criteria is met, including uncertainties, is achieved using standard Westinghouse design methods. The subcriticality of the core is continuously monitored as will be described in the Technical Specifications.

4.3.1.6 Stability.

Basis

The core will be inherently stable to power oscillations at the fundamental mode. This satisfies GDC 12. Spatial power oscillations within the core with a constant core power output, should they occur, can be reliably and readily detected and suppressed.

Discussion

Oscillations of the total power output of the core, from whatever cause, are readily detected by the loop temperature sensors and by the nuclear instrumentation. The core is protected by these systems and a reactor trip would occur if power increased unacceptably, preserving the design margins to fuel design limits. The stability of the turbine/steam generator/core systems and the reactor control system is such that total core power oscillations are not normally possible. The redundancy of the protection circuits ensures an extremely low probability of exceeding design power levels.

The core is designed so that diametral and azimuthal oscillations due to spatial xenon effects are self-damping and no operator action or control action is required to suppress them. The stability to diametral oscillations is so great that this exception is highly improbable. Convergent azimuthal oscillations can be excited by prohibited motion of individual control rods. Such oscillations are readily observable and alarmed, using the excore long ion chambers. Indications are also continuously available from incore thermocouples and loop temperature measurements. Moveable incore detectors can be activated to provide more detailed information. In all proposed cores these horizontal plane oscillations are self-damping by virtue of reactivity feedback effects designed into the core.

However, axial xenon spatial power oscillations may occur during core life. The control banks and excore detectors are provided for control and monitoring of axial power distributions. Assurance that fuel design limits are not exceeded is provided by reactor overpower ΔT and overtemperature ΔT trip functions which use the measured axial power imbalance as an input.

4.3.1.7 Anticipated Transients Without Trip (ATWT).

The effects of anticipated transients with failure to trip are not considered in the design bases of the plant. Analysis has shown that the likelihood of such a hypothetical event is negligibly small (Ref. 4.3-1). Furthermore, analysis of the consequences of a hypothetical failure to trip following anticipated transients has shown that no significant core damage would result, system peak pressures would be limited to acceptable values, and no failure of the Reactor Coolant System (RCS) would result. These analyses were documented (Ref. 4.3-2) in November 1974 in accordance with the Atomic Energy Commission (AEC) policy outlined in WASH-1270 “Technical Report on Anticipated Transients Without Scram for Water-Cooled Power Reactors”, September 1973.

4.3.2 Description

4.3.2.1 Nuclear Design Description. The reactor core consists of a specified number of fuel rods which are held in bundles by spacer grids and top and bottom fittings. The fuel rods are constructed of Zircaloy or ZIRLO cylindrical tubes containing uranium dioxide fuel pellets. The bundles, known as fuel assemblies, are arranged in a pattern which approximates a right circular cylinder.

Each fuel assembly contains a 17 x 17 rod array composed of 264 fuel rods, 24 rod cluster control thimbles and an incore instrumentation thimble. Figure 4.2-1 shows a cross sectional view of a 17 x 17 fuel assembly and the related rod cluster control locations. Further details of the fuel assembly are given in Section 4.2.

Prior to Unit 1 Cycle 10 and Unit 2 Cycle 8, the fuel rods within a given assembly have the same uranium enrichment in both the radial and axial planes. Fuel assemblies of three different enrichments are used in the initial core loading to establish a favorable radial power distribution. Two regions consisting of the two lower enrichments are interspersed so as to from a checkerboard pattern in the central portion of the core. The third region is arranged around the periphery of the core and contains the highest enrichment. The enrichments for the first core are shown in Table 4.3-1. Starting with Unit 2 Cycle 4 and Unit 1 Cycle 6, upgraded fuel utilizing Integral Fuel Burnable Absorbers (IFBA) and Zircalloy mid-grids is introduced.

Annular pellets may be incorporated into the fresh fuel feed regions. These annular pellets provide additional void volume to accommodate IFBA helium release and increased fission gas release due to extended burnups. The natural, mid-, or fully enriched axial blanket pellet design (using solid or annular pellets) also reduces neutron leakage and improves fuel utilization.

Beginning with Unit 1 Cycle 10 and Unit 2 Cycle 8, axial blankets may be used in the core design. An axial blanket is the designation for any of the several fuel rod designs. Axial blanket designs call for multiple types, or zones, of fuel pellets over the axial length of the fuel rod. The zones can be distinguished by one or more of the following: fuel enrichment, IFBA concentration, or the use of annular fuel pellets. Axial blankets are used to reduce the fuel loading at the top and bottom of the rod, while maintaining the peaking factors within their allowed limits. The use of annular fuel pellets, even of the same enrichment as the conventional pellets, is one type of axial blanket design. Analyses using annular pellets in an axial blanket configuration bound the results of the same analyses with solid pellets.

The reference reloading pattern is typically similar to Figure 4.3-1 with depleted fuel interspersed checkerboard style in the center and new fuel mixed with depleted fuel on the periphery. The core will normally operate on a nominal eighteen-month cycle, accumulating approximately 17,000 MWD/MTU burnup. The exact reloading pattern, initial and final positions of assemblies, number of fresh assemblies and their placement are dependent on the energy requirement for the next cycle and burnup and power histories of the previous cycles. The core average burnup is approximately 40,000 MWD/MTU.

The core average enrichment is determined by the amount of fissionable material required to provide the desired core lifetime and energy requirements, namely a region average discharge burnup of approximately 45,000 MWD/MTU. The physics of the burnup process is such that operation of the reactor depletes the amount of fuel available due to the absorption of neutrons by the uranium-235 atoms and their subsequent fission. The rate of uranium-235 depletion is directly proportional to the power level at which the reactor is operated. In addition, the fission process results in the formation of fission products, some of which readily absorb neutrons. These effects, depletion and the buildup of fission products, are partially offset by the buildup of plutonium shown on Figure e4.3-2 for the 17 x 17 fuel assembly, which occurs due to the nonfission absorption of neutrons in uranium-238. Therefore, at the beginning of any cycle a reactivity reserve equal to the depletion of the fissionable fuel and the buildup of fission product poisons over the specified cycle life must be “built” into the reactor. This excess reactivity is controlled by removable neutron absorbing material in the form of boron dissolved in the primary coolant and by burnable absorber rods and/or IFBA.

The concentration of boric acid in the primary coolant is varied to provide control and to compensate for long-term reactivity requirements. The concentration of the soluble neutron absorber is varied to compensate for reactivity changes due to fuel burnup, fission product poisoning including xenon and samarium, burnable absorber depletion, and the cold-to-operating moderator temperature change. Using its normal makeup path, the CVCS is capable of inserting negative reactivity at a rate of approximately 60 pcm/min when the reactor coolant boron concentration is 1000 ppm and approximately 70 pcm/min when the reactor coolant boron concentration is 100 ppm. The peak burnout rate for xenon is 25 pcm/min (Section 9.3.4.3.1 discusses the capability of the CVCS to counteract xenon decay). Rapid transient reactivity requirements and safety shutdown requirements are met with control rods.

As the boron concentration is increased, the moderator temperature coefficient becomes less negative. The use of a soluble absorber alone could result in a positive moderator coefficient at beginning of life (BOL) for the cycle. Therefore, burnable absorber rods and/or IFBA are used in the core to reduce the soluble boron concentration sufficiently to ensure that the moderator temperature coefficient is negative for power operating conditions. During operation the absorber content in these rods is depleted thus adding positive reactivity to offset some of the negative reactivity from fuel depletion and fission product buildup. The depletion rate of the burnable absorber rods is not critical since chemical shim is always available and flexible enough to cover any possible deviations in the expected burnable absorber depletion rate. Figure 4.3-3 is a graph of a typical core depletion with and without burnable absorber rods. Note that even at end-of-life (EOL) conditions some residual absorber remains in the burnable absorber rods resulting in a net decrease in the cycle lifetime.

In addition to reactivity control, the burnable absorber rods are strategically located to provide a favorable radial power distribution. Figure 4.3-4 and 4.3-4a show the burnable absorber distribution within a fuel assembly for the several burnable absorber patterns used in a 17 x 17 array. Typical burnable absorber loading patterns are shown on Figures 4.3-5 and 4.3-5a.

Tables 4.3-1 through 4.3-3 contain a summary of the reactor core design parameters for the first fuel cycle, included reactivity coefficients, delayed neutron fraction, and neutron lifetime. Sufficient information is included to permit an independent calculation of the nuclear performance characteristics of the core.

4.3.2.2 Power Distributions. The accuracy of power distribution calculations has been confirmed through approximately 1,000 flux maps during some 20 years of operation under conditions similar to those expected for the South Texas Project Electric Generating Station (STPEGS). Details of this confirmation are given in Reference 4.3-3 and in Section 4.3.2.2.6.

4.3.2.2.1 Definitions: Power distributions are quantified in terms of hot channel factors. Theses factors are a measure of the peak pellet power within the reactor core and the total energy produced in a coolant channel and are expressed in terms of quantities related to the nuclear or thermal design namely:

Power density is the thermal power produced per unit volume of the core (kW/liter).

Linear power density is the thermal power produced per unit length of active fuel (kW/ft). Since fuel assembly geometry is standardized this is the unit of power density most commonly used. For all practical purposes it differs from kW/liter by a constant factor which includes geometry and the fraction of the total thermal power which is generated in the fuel rod.

Average linear power density is the total thermal power produced in the fuel rods divided by the total active fuel length of all rods in the core.

Local heat flux is the heat flux at the surface of the cladding (BTU-ft^{-2}-hr^{-1}). For nominal rod parameters this differs from linear power density by a constant factor.

Rod power or rod integral power is the length integrated linear power density in one rod (kW).

Average rod power is the total thermal power produced in the fuel rods divided by the number of fuel rods (assuming all rods have equal length).

The hot channel factors used in the discussion of power distributions in this section are defined as follows:

F_Q, Heat Flux Hot Channel Factor, is defined as the maximum local heat flux on the surface of a fuel rod divided by the average fuel rod heat flux, allowing for manufacturing tolerances on fuel pellets and rods.

F_Q^N, Nuclear Heat Flux Hot Channel Factor, is defined as the maximum local fuel rod linear power density divided by the average fuel rod linear power density, assuming nominal fuel pellet and rod parameters.

F_Q^E, Engineering Heat Flux Hot Channel Factor, is the allowance on heat flux required for manufacturing tolerances. The engineering factor allows for local variations in enrichment, pellet density and diameter, surface area of the fuel rods, and eccentricity of the gap between pellet and clad. Combined statistically, the net effect is a factor of 1.03 to be applied to fuel rod surface heat flux.

$F^N_{\Delta H}$, Nuclear Enthalpy Rise Hot Channel Factor, is defined as the ratio of the integral of linear power along the rod with the highest integrated power to the average rod power.

Manufacturing tolerances, hot channel power distribution, and surrounding channel power distributions are treated explicitly in the calculation of the DNB ratio described in Section 4.4.

It is convenient for the purposes of discussion to define subfactors of F_Q, however, design limits are set in terms of the total peaking factor.

F_Q = Total peaking factor or heat flux hot-channel factor

$$= \frac{\text{Maximum kW/ft}}{\text{Average kW/ft}}$$

$$F_Q = F^N_Q \text{ x } F^E_Q$$

$$= F^N_{XY} \text{ x } F^N_Z \text{ x } F^N_U \text{ x } F^E_Q$$

where

F^N_Q and F^E_Q are defined above.

F^N_U = factor for conservatism, assumed to be 1.05.

F^N_{XY} = ratio of peak power density to average power density in the horizontal plane of peak local power.

F^N_Z = ratio of the power per unit core height in the horizontal plane of peak local power to the average value of power per unit core height. If the plane of peak local power coincides with the plane of maximum power per unit core height then F^N_Z is the core average axial peaking factor.

4.3.2.2.2 Radial Power Distributions: The power shape in horizontal sections of the core at full power is a function of the fuel and burnable absorber loading patterns and the presence or absence of a single bank of control rods. Thus, at any time in the cycle a horizontal section of the core can be characterized as unrodded or rodded with group D control rods. These two situations combined with burnup effects determine the radial power shapes which can exist in the core at full power. Typical values F_{XY}^{N} are given in Table 4.3-2. The effect on radial power shapes of power level, xenon, samarium and moderator density effects are considered also but these are quite small. The effect of nonuniform flow distribution is negligible. While radial power distributions in various planes of the core are often illustrated, the core radial enthalpy rise distribution as determined by the integral of power up each channel is of greater interest. Figures 4.3-6 through 4.3-11 show typical radial power distributions for one-eighth of the core for representative operating conditions. These conditions are 1) Hot Full Power (HFP) at BOL – unrodded – no xenon, 2) HFP at BOL – unrodded – equilibrium xenon, 3) HFP near BOL – Bank D in – equilibrium xenon, 4) HFP near middle-of-life (MOL) – unrodded – equilibrium xenon, and 5) HFP at EOL – unrodded – equilibrium xenon, and 6) HFP at EOL – Bank D in – equilibrium xenon.

Since the position of the hot channel varies from time to time a single reference radial design power distribution is selected for DNB calculations. This reference power distribution is chosen conservatively to concentrate power in one area of the core, minimizing the benefits of flow redistribution. Assembly powers are normalized to core average power. The radial power distribution within a fuel rod and its variation with burnup as utilized in thermal calculations and fuel rod design is discussed in Section 4.2.

4.3.2.2.3 Assembly Power Distributions: For the purpose of illustration, assembly power distributions from the BOL and EOL conditions corresponding to Figures 4.3-7 and 4.3-10, respectively, are given for the same assembly on Figures 4.3-12 and 4.3-13, respectively.

Since the detailed power distribution surrounding the hot channel varies from time to time, a conservatively flat assembly power distribution is assumed in the DNB analysis, described in Section 4.4, with the rod of maximum integrated power artificially raised to the design value of $F_{\Delta H}^{N}$. Care is taken in the nuclear design of all fuel cycles and all operating conditions to ensure that a flatter assembly power distribution does not occur with limiting values of $F_{\Delta H}^{N}$.

4.3.2.2.4 Axial Power Distributions: The shape of the power profile in the axial or vertical direction is largely under the control of the operator either through the manual operation of the control rods or automatic motion of rods responding to manual operation of the Chemical Volume and Control System (CVCS). Nuclear effects which cause variations in the axial power shape include moderator density, Doppler effect on resonance absorption, spatial xenon, and burnup. Automatically controlled variations in total power output and rod motion are also important in determining the axial power shape at any time. Signals are available to the operator from the excore ion chambers which are long ion chambers outside the reactor vessel running parallel to the axis of the core. Separate signals are taken from the top and bottom halves of the chambers. The difference between top and bottom signals from each of four pairs of detectors is displayed on the control panel and called the flux difference, ΔI. Calculations of core average peaking factor for many plants and measurements from operating plants under many operating situations are associated with either ΔI or axial offset in such a way that an upper bound can be placed on the peaking factor. For these correlations axial offset is defined as:

$$\text{axial offset} = \frac{\theta_t - \theta_b}{\theta_t + \theta_b}$$

and θ_t and θ_b are the top and bottom detector readings.

Representative axial power shapes from Reference 4.3-4 for BOL, MOL, and EOL conditions are shown on Figures 4.3-14 through 4.3-16. These figures cover a wide range of axial offset including values not permitted at full power.

The radial power distributions shown in Figures 4.3-8 and 4.3-11 involving the partial insertion of control rods represent a synthesis of the radial power shapes from the rodded and unrodded axial planes with each power shape weighted in proportion to the power in the corresponding axial plane. The applicability of this procedure is assured through extensive three-dimensional calculations of possible rodded conditions. As an example, Figure 4.3-17 compares the axial power distribution for several assemblies at different distances from inserted control rods with the core average distribution.

The only significant difference from the average occurs in the low power peripheral assemblies, thus confirming the validity of the separability assumption.

4.3.2.2.5 DELETED

4.3.2.2.6 Limiting Power Distributions: According to the ANSI classification of plant conditions (Chapter 15), Condition I occurrences are those which are expected frequently or regularly in the course of power operation, maintenance, or maneuvering of the plant. As such, Condition I occurrences are accommodated with margin between any plant parameter and the value of that parameter which would require either automatic or manual protective actions. Inasmuch as Condition I occurrences occur frequently or regularly, they must be considered from the point of view of affecting the consequences of fault conditions (Conditions II, III, and IV). In this regard, analysis of each fault condition described is generally based on a conservative set of initial conditions corresponding to the most adverse set of conditions which can occur during Condition I operation.

The list of steady-state and shutdown conditions, permissible deviations and operational transients is given in Section 15.0. Implicit in the definition of normal operation is proper and timely action by the reactor operator. That is, the operator follows recommended operating procedures for maintaining appropriate power distributions and takes any necessary remedial actions when altered to do so by the plant instrumentation. Thus, as stated above, the worst or limiting power distribution which can occur during normal operation is to be considered as the starting point for analysis of ANSI Conditions II, III, and IV events.

Improper procedural actions or errors by the operator are assumed in the design as occurrences of moderate frequency (ANSI Condition II). Some of the consequences which might result are discussed in Chapter 15. Therefore, the limiting power shapes which result from such Condition II events are those power shapes which deviate from the normal operating condition at the recommended axial offset band; e.g. due to lack of proper action by the operator during a xenon transient following a change in power level brought about by control rod motion. Power shapes which fall in this category are used for determination of the reactor protection system setpoints so as to maintain margin to overpower or DNB limits.

The means for maintaining power distributions within the required hot channel factor limits are described in the technical specifications. A complete discussion of power distribution control in pressurized water reactors (PWRs) is included in Reference 4.3-8. Detailed background information on the design constraints on local power density in a Westinghouse PWR with defined operating procedures and the measures taken to preclude exceeding design limits, is presented in the Westinghouse topical report on power distribution control and load following procedures. The following paragraphs summarize these reports and describe the calculations used to establish the upper bound on peaking factors. The calculations used to establish the upper bound on peaking factors, F_Q and $F^N_{\Delta H}$, include all of the nuclear effects which influence the radial and/or axial power distributions throughout the core life for various modes of operation including load follow, reduced power operation, and axial xenon transients.

Radial power distributions are calculated for the full power condition and fuel and moderator temperature feedback effects are included for the average enthalpy plane of the reactor. The steady state nuclear design calculations are done for normal flow with the same mass flow in each channel and flow redistribution effects neglected. The effect of flow redistribution is calculated explicitly where it is important in the DNB analysis of accidents. The effect of xenon on radial power distribution is small (compare Figures 4.3-6 and 4.3-7) but is included as part of the normal design process. Radial power distributions are relatively fixed and easily bounded with upper limits.

The core average axial profile, however, can experience significant changes which can occur rapidly as a result of rod motion and load changes and more slowly due to xenon distribution. For the study of points of closest approach to axial power distribution limits, several thousand cases are examined. Since the properties of the nuclear design dictate what axial shapes can occur, boundaries on the limits of interest can be set in terms of the parameters which are readily observed on the plant. Specifically, the nuclear design parameters which are significant to the axial power distribution analysis are:

1. Core power level
2. Core height
3. Coolant temperature and flow
4. Coolant temperature program as a function of reactor power
5. Fuel cycle lifetimes
6. Rod banks worths
7. Rod bank overlaps

Normal operation of the plant assumes compliance with the following conditions:

1. Control rods in a single bank move together with no individual rod insertion differing by more than 12 steps (indicated) from the bank demand position;

2. Control banks are sequenced with overlapping banks;

3. The control bank insertion limits are not violated;

4. Axial power distribution procedures, which are given in terms of flux difference control and control bank position, are observed.

The axial power distribution procedures referred to above are part of the required operating procedures which are followed in normal operation. Briefly, they require control of the axial offset (flux difference divided by fractional power) at all power levels within a permissible operating band of a target value corresponding to the equilibrium full power value. In the first cycle, the target value changes from about –15 to –1 percent linearly through the life of the cycle. This minimizes xenon transient effects on the axial power distribution, since the procedures essentially keep the xenon distribution in phase with the power distribution.

Calculations are performed for normal operation of the reactor including load following maneuvers. Beginning, middle, and end of cycle conditions are included in the calculations. Different histories of operation are assumed prior to calculating the effect of load follow transients on the axial power distribution. These different histories assume base loaded operation and extensive load following. For a given plant and fuel cycle, a finite number of maneuvers are studied to determine the general behavior of the local power density as a function of core elevation.

These cases represent many possible reactor states in the life of one fuel cycle and they have been chosen as sufficiently definitive of the cycle by comparison with much more exhaustive studies performed on some 20 or 30 different, but typical, plant and fuel cycle combinations. The cases are described in detail in Reference 4.3-9 and are considered to be necessary and sufficient to generate a local power density limit which, when increased by 5 percent for conservatism, will not be exceeded with a 95 percent confidence level. Many of the points do not approach the limiting envelope, however they are part of the time histories which lead to the hundreds of shapes which do define the envelope. They also serve as a check that the reactor studied is typical of those studied more exhaustively.

Thus it is not possible to single out any transient or steady-state condition which defines the most limiting case. It is not even possible to separate out a small number which form an adequate analysis. The process of generating a myriad of shapes is essential to the philosophy that leads to the required level of confidence. A maneuver which provides a limiting case for one reactor fuel cycle, defined as approaching the F_Q envelope which is given in the Core Operating Limits Report (COLR), is not necessarily a limiting case for another reactor or fuel cycle with different control bank worths, enrichments, burnup, coefficients, etc. Each shape depends on the detailed history of operation up to the time and on the manner in which the operator conditioned xenon in the days immediately prior to the time at which the power distribution is calculated.

The calculated points are synthesized from axial calculations combined with radial factors appropriate for rodded and unrodded planes in the first cycle. In these calculations the effects on the unrodded radial peak of xenon redistribution that occurs following the withdrawal of a control bank (or banks) from a rodded region is obtained from two-dimensional XY calculations. A 1.03 factor to be applied on the unrodded radial peak was obtained from calculations in which xenon distribution was preconditioned by the presence of control rods and then allowed to redistribute for several hours. A detailed discussion of this effect may be found in Reference 4.3-9. The calculated values have been increased by a factor of 1.05 for conservatism and a factor of 1.03 for the engineering factor F_Q^E . The envelope drawn over the calculated (max [F_Q • Power]) points shown in the COLR represents an upper bound envelope on local power density versus elevation in the core. It should be emphasized that this envelope is a conservative representation of the bounding values of local power density. Expected values are considerably smaller and, in fact, less conservative bounding values may be justified with additional analysis or surveillance requirements. For example, the envelope bounds both BOL and EOL conditions, but without consideration of radial power distribution flattening with burnup; i.e., both BOL and EOL points presume the same radial peaking factor. Inclusion of the burnup flattening effect would reduce the local power densities corresponding to EOL conditions which may be limiting at the higher core elevations.

Finally, as previously discussed, this upper bound envelope is based on procedures of load follow which require operation within an allowed deviation from a target equilibrium value of axial flux difference. These procedures are detailed in the Technical Specifications and are followed by relying only upon excore surveillance supplemented by the normal monthly full core map requirement, and by computer based alarms on deviation and time of deviation from the allowed flux difference band.

The average linear power at 3,853 MWt is 5.27 kW/ft. From the F_Q envelope shown in the COLR, the conservative upper bound valve of normalized local power density, including uncertainty allowances, is 2.55 corresponding to a peak linear power of 13.5 kW/ft at 100.6 percent power.

To determine Reactor Protection System (RPS) setpoints with respect to power distributions, three categories of events are considered, namely rod control equipment malfunctions, operator errors of commission, and operator errors of omission. In evaluating these three categories of events the core is assumed to be operating within the four constraints described above.

The first category comprises uncontrolled rod withdrawal (with rods moving in the normal bank sequence). Also included are motions of the rod banks below their insertion limits which could be caused, for example, by uncontrolled dilution or primary coolant cooldown. Power distributions were calculated throughout these occurrences assuming short-term corrective action, that is , no transient xenon effects were considered to result from the malfunction. The event was assumed to occur from typical normal operating situations which include normal xenon transients. It was further assumed in determining the power distributions that total core power level would be limited by reactor trip to below 118 percent. Since the study is to determine protection limits with respect to power and axial offset, no credit was taken for trip setpoint reduction due to flux difference. Results for the first core are given on Figure 4.3-22 in units of kW/ft. The peak power density which can occur in such events, assuming reactor trip at or below 118 percent, is less than that required for center-line melt including uncertainties.

The second category, also appearing in Figure 4.3-22, assumes that the operator mispositions the rod bank in violation of the insertion limits and creates short-term conditions not included in normal operating conditions.

The third category assumes that the operator fails to take action to correct a flux difference violation. The first core (3,800 MWt) results shown on Figure 4.3-23 are F_Q (based on F_Q = 2.7) multiplied by 102 percent power including an allowance for calorimetric error. The figure shows that provided the assumed error in operation does not continue for a period which is long compared to the xenon time constant, the peak linear power does not exceed 22.45 kW/ft including the above factors. Since the peak kW/ft is below that which results in fuel center-line melt, no flux difference penalties are required for overpower protection. It should be noted that a reactor overpower accident is not assumed to occur coincident with an independent operator error. Additional detailed discussion of these analyses is presented in Reference 4.3-9.

Analyses of possible operating power shapes show that the appropriate hot channel factors F_Q and $F^N_{\Delta H}$ for peak local power density and for DNB analysis at full power are the values given in Table 4.3-2 and addressed in the Technical Specifications.
Analyses F_Q can be increased with decreasing power as shown in the Technical Specifications. Increasing allowable $F^N_{\Delta H}$ with decreasing power is permitted by the DNB protection setpoints and allows radial power shape changes with rod insertion to the insertion limits as described in Section 4.4.4.3. The allowance for increased $F^N_{\Delta H}$ permitted is $F^N_{\Delta H}$ - $F^{RTP}_{\Delta H}$ [1 + 0.3 (1-P)], where $F^{RTP}_{\Delta H}$ is the $F^N_{\Delta H}$ limit at rated thermal power specified in the COLR and P is the fraction of full power. This becomes a design basis criterion which is used for establishing acceptable control rod patterns and control bank sequencing. Likewise, fuel loading patterns for each cycle are selected with consideration of this design criterion. The worst values of $F^N_{\Delta H}$ for possible rod configurations occurring in normal operation are used in verifying that this criterion is met. Typical radial factors and radial power distributions are shown on Figures 4.3-6 through 4.3-11. The worst values generally occur when the rods are assumed to be at their insertion limits. Maintenance of constant axial offset control establishes rod positions which are above the allowed rod insertion limits thus providing increased margin to the $F^N_{\Delta H}$ criterion. As discussed in Section 3.2 of Reference 4.3-10 it has been determined that, provided the above conditions 1 through 4 are observed, the Technical Specification limits will be met. These limits are taken as input to the thermal-hydraulic design basis as described in Section 4.4.4.3.1.

When a situation is possible in normal operation which could result in local power densities in excess of those assumed as the precondition for a subsequent hypothetical accident, but which would not itself cause fuel failure, administrative controls and alarms are provided for returning the core to a safe condition. These alarms are described in Chapter 7 and 16.

4.3.2.2.7 Experimental Verification of Power Distribution Analysis: This subject is discussed in depth in Reference 4.3-3. A summary of this report is given below. It should be noted that power distribution related measurements are incorporated into the evaluation of calculated power distribution information using the INCORE code described in Reference 4.3-11. A detailed description of this code's input and output is included in this reference. The measured power distribution is compared with the calculated power distribution periodically throughout the cycle lifetime of the reactor as required by Technical Specifications.

For cores with strong three dimensional characteristics (i.e., cores with axial blankets), either a three dimensional version of the INCORE code or the BEACON code system (Reference 4.3-43) is used to calculate and evaluate core power distribution information. The discussion below is applicable to INCORE (2D), INCORE(3D), and BEACON.

In a measurement of the heat flux hot channel factor, F_Q, with the movable detector system described in Sections 7.7.1 and 4.4.6, the following uncertainties have to be considered:

1. Reproducibility of the measured signal

2. Errors in the calculated relationship between detector current and local flux

3. Errors in the calculated relationship between detector flux and peak rod power some distance from the measurement thimble

The appropriate allowance for category 1 above has been quantified by repetitive measurements made with several intercalibrated detectors by using the common thimble features of the incore detector system. This system allows more than one detector to access any thimble. Errors in category 2 above are quantified to the extent possible, by using the fluxes measured at one thimble location to predict fluxes at another location which is also measured. Local power distribution predictions are verified in critical experiments on arrays of rods with simulated guide thimbles, control rods, burnable absorbers, etc. These critical experiments provide quantification of errors of category 2 and 3, above.

Reference 4.3-3 describes critical experiments performed at the Westinghouse Reactor Evaluation Center and measurements taken on two Westinghouse plants with incore systems of the same type as used in this plant. The report concludes that the uncertainty associated with F_Q (heat flux) is 4.58 percent at the 95 percent confidence level with only 5 percent of the measurements greater than the inferred value. This is the equivalent of a 1.645σ limit on a normal distribution and is the uncertainty to be associated with a full core flux map with movable detectors reduced with a reasonable set of input data incorporating the influence of burnup on the radial power distribution. The uncertainty is usually rounded up to 5 percent.

In comparing measured power distributions (or detector currents) against the calculations for the same situation it is not possible to subtract the detector reproducibility. Thus a comparison between measured and predicted power distributions has to include some measurement error. Such a comparison is given on Figure 4.3-24 for one of the maps used in Reference 4.3-3. Since the first publication of the report, hundreds of maps have been taken on these and other reactors. The results confirm the adequacy of the 5 percent uncertainty allowance on the calculated F_Q.

A similar analysis was performed for the uncertainty in $F^N_{\Delta H}$ (rod integral power) measurements.

Calculational and measurement uncertainties are included in this uncertainty analysis. The calculational uncertainty is 3.9% which, when combined with the measurement uncertainty, yields a total uncertainty of 8% which is allowed in the nuclear design (i.e., the predicted rod integrals must not exceed the design $F^N_{\Delta H}$ less 8 percent).

A recent measurement in the second cycle of a 121 assembly core is compared with a simplified one dimensional core average axial calculational on Figure 4.3-25. This calculation does not give explicit representation to the fuel grids.

The accumulated data on power distribution in actual operation is basically of three types:

1. Much of the data is obtained in steady-state operation at constant power in the normal operating configuration.

2. Data with unusual values of axial offset are obtained as part of the excore detector calibration exercise which is performed monthly.

3. Special tests have been performed in load follow and other transient xenon conditions which have yielded useful information on power distributions.

These data are presented in detail in Reference 4.3-10. Figure 4.3-26 contains a summary of measured values of F_Q as a function of axial offset for five plants from that report.

4.3.2.2.8 Testing:

A very extensive series of physics tests is performed on first cores. These tests and the criteria for satisfactory results are described in detail in Chapter 14. Since not all limiting situations can be created at BOL, the main purpose of the tests is to provide a check on the calculational methods used in the predications for the conditions of the test. Tests performed at the beginning of each reload cycle are limited to verification of steady-state power distributions.

4.3.2.2.9 Monitoring Instrumentation:

The adequacy of instrument numbers, spatial deployment, required correlations between readings and peaking factors, calibration, and errors are described in References 4.3-3, 4.3-8, and 4.3-10. The relevant conclusions are summarized in Sections 4.3.2.2.7 and 4.4.6.

Provided the limitations given in Section 4.3.2.2.6 on rod insertion and flux difference are observed, the excore detector system provides adequate online monitoring of power distributions. Further details of specific limits on the observed rod positions and flux difference are given in the Technical Specifications together with a discussion of their bases.

Limits for alarms, reactor trip, etc. are given in the Technical Specifications. Descriptions of the systems provided are given in Section 7.7.

4.3.2.3 Reactivity Coefficients.

The kinetic characteristics of the reactor core determine the response of the core to changing plant conditions or to operator adjustments made during normal operation, as well as the core response during abnormal or accidental transients. These kinetic characteristics are quantified in reactivity coefficients. The reactivity coefficients reflect the changes in the neutron multiplication due to varying plant conditions such as power, moderator or fuel temperatures, or less significantly due to a change in pressure or void conditions. Since reactivity coefficients change during the life of the core, ranges of coefficients are employed in transient analysis to determine the response of the plant throughout life. The results of such simulations and the reactivity coefficients used are presented in Chapter 15. The reactivity coefficients are calculated on a core-wise basis by radial and axial diffusion theory methods. The effect of radial and axial power distribution on core average reactivity coefficients is implicit in those calculations and is not significant under normal operating conditions. For example, a skewed xenon distribution which results in changing axial offset by 5 percent changes the moderator and Doppler temperature coefficients by less than 0.01 pcm/°F and 0.03 pcm/°F, respectively. An artificially skewed xenon distribution which results in changing the radial $F_{\Delta H}^{N}$ by 3 percent changes the moderator and Doppler temperature coefficients by less than 0.03 pcm/°F and 0.001 pcm/°F, respectively. The spatial effects are accentuated in some transient conditions such as the postulated rupture of the main steam line break and rupture of RCCA mechanism housing described in Section 15.1.5 and 15.4.8, and are included in these analyses.

The analytical methods and calculational models used in calculating the reactivity coefficients are given in Section 4.3.3. these models have been confirmed through extensive testing of more than 30 cores similar to the plant described herein; results of these tests are discussed in Section 4.3.3.

Quantitative information for calculated reactivity coefficients, including fuel Doppler coefficient, moderator coefficients (density, temperature, pressure, void), and power coefficient is given in the following sections.

4.3.2.3.1 Fuel Temperature (Doppler) Coefficient: The fuel temperature (Doppler) coefficient is defined as the change in reactivity per degree change in effective fuel temperature and is primarily a measure of Doppler broadening of uranium-238 and plutonium-240 resonance absorption peaks. Doppler broadening of other isotopes such as uranium-236, neptunium-237, etc., are also considered but their contributions to the Doppler effect are small. An increase in fuel temperature increases the effective resonance absorption cross sections of the fuel and produces a corresponding reduction in reactivity.

The fuel temperature coefficient is calculated by performing two-group X-Y calculations using TORTISE, an updated version of the TURTLE (Ref. 4.3-12) Code. Moderator temperature is held constant and the power level is varied. Spatial variation of fuel temperature is taken into account by calculating the effective fuel temperature as a function of power density as discussed in Section 4.3.3.1.

The Doppler temperature coefficient is shown on Figure 4.3-27 as a function of the effective fuel temperature (at BOL and EOL conditions). The effective fuel temperature is lower than the volume averaged fuel temperature since the neutron flux distribution is nonuniform through the pellet and gives preferential weight to the surface temperature. The Doppler-only contribution to the power coefficient, defined later, is shown on Figure 4.3-28 as a function of relative core power. The integral of the differential curve on Figure 4.3-28 is the Doppler contribution to the power defect and is shown on Figure 4.3-29 as a function of relative power. The Doppler coefficient becomes more negative as function of life as the plutonium-240 content increases, thus increasing the plutonium resonance absorption, but overall becomes less negative since the fuel temperature changes with burnup as described in Section 4.3.3.1. The upper and lower limits of the Doppler coefficient used in accident analyses are given in Chapter 15.

Starting with Unit 1, Cycle 2 and Unit 2, Cycle 1, fuel temperature coefficients are calculated with the two-group 3D code ANC (Ref. 4.3-34).

Starting with Unit 1, Cycle 4 and Unit 2, Cycle 3, fuel temperature coefficients are calculated with PHOENIX-P/ANC (Ref. 4.3-38).

Starting with Unit 2, Cycle 17 and Unit 1, Cycle 19, fuel temperature coefficients are calculated with NEXUS/PARAGON/ANC (Ref. 4.3-6).

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4.3.2.3.2 Moderator Coefficients:

The moderator coefficients are a measure of the change in reactivity due to change in specific coolant parameters such as density, temperature, pressure, or void. The coefficients as obtained are moderator density, temperature, pressure, and void coefficients.

Moderator Density and Temperature Coefficients

The moderator temperature (density) coefficient is defined as the change in reactivity per degree change in the moderator temperature. Generally, the effect of the changes in moderator density as well as the temperature are considered together. A decrease in moderator density means less moderation which results in a negative moderator coefficient. An increase in coolant temperature, keeping the density constant, leads to a hardened neutron spectrum and results in an increase in resonance absorption in uranium-238, plutonium-240, and other isotopes. The hardened spectrum also causes decrease in the fission to capture ratio in uranium-235 and plutonium-239. Both of these effects make the moderator coefficient more negative. Since water density changes more rapidly with temperature as temperature increases, the moderator temperature coefficient becomes more negative with increasing temperature.

The soluble boron used in the reactor as a means of reactivity control also has an effect on moderator temperature coefficient since the soluble boron absorber density as well as the water density is decreased when the coolant temperature rises. An increase in the soluble absorber concentration introduces a positive component in the moderator temperature coefficient.

Thus, if the concentration of soluble absorber is large enough, the net value of the coefficient may be positive. With the burnable absorber rods present, however, the initial hot boron concentration is sufficiently low that the moderator temperature coefficient is negative at rated thermal power. The effect of control rods is to make the moderator temperature coefficient more negative by reducing the required soluble boron concentration and by increasing the “leakage” of the core.

With burnup, the moderator temperature coefficient becomes more negative primarily as a result of boric acid dilution but also to a significant extent from the effects of the buildup of plutonium and fission products.

The moderator temperature coefficient is calculated for the various plant conditions discussed above by performing two-group X-Y calculations, varying the moderator temperature by approximately ±5°F about each of the mean temperatures. The moderator temperature coefficient is shown as function of core temperature and boron concentration for the unrodded and rodded core on Figures 4.3-30 through 4.3-32. The temperature range covered is from cold (68°F) to about 600°F. the contribution due to Doppler coefficient (because of change in moderator temperature) has been subtracted from these results. Figure 4.3-33 shows the hot, full power moderator temperature coefficient plotted as function of first cycle lifetime for the just critical boron concentration condition based on Figure 4.3-3.

The moderator temperature (density) coefficients presented here are calculated on a core-wide basis, since they are used to describe the core behavior in normal and accident situations when the moderator temperature changes can be considered to affect the entire core. Moderator temperature coefficient and moderator density coefficient are used interchangeably according to which is more appropriate as input for the codes used.

Starting with Unit 1, Cycle 2 and Unit 2, Cycle 1, moderator temperature coefficients are calculated with the two-group 3D code ANC (Ref. 4.3-34).

Starting with Unit 1, Cycle 4 and Unit 2, Cycle 3, moderator temperature coefficients are calculated with PHOENIX-P/ANC (Ref. 4.3-38).

Starting with Unit 2, Cycle 17 and Unit 1, Cycle 19, moderator temperature coefficients are calculated with NEXUS/PARAGON/ANC (Ref. 4.3-6).

Moderator Pressure Coefficient

The moderator pressure coefficient relates the change in moderator density, resulting from a reactor coolant pressure change, to the corresponding effect on neutron production. This coefficient is of much less significance in comparison with the moderator temperature coefficient. A change of 50 psi in pressure has approximately the same effect of reactivity as a half-degree change in moderator temperature. This coefficient can be determined from the moderator temperature coefficient by relating change in pressure to the corresponding change in density. The moderator pressure coefficient is negative over a portion of the moderator temperature range at BOL (-0.004 pcm/psi, BOL) but is always positive at rated thermal power and becomes more positive during life (to 0.3 pcm/psi, EOL) due principally to the change in boron concentration of the moderator with cycle burnup.

Moderator Void Coefficient

The moderator void coefficient relates the change in neutron multiplication to the presence of voids in the moderator. In a PWR this coefficient is not very significant because of the low void content in the coolant. The core void content is less than one-half of one percent and is due to local or statistical boiling. The void coefficient varies from 50 pcm/percent void at BOL and at low temperature to –250 pcm/percent void at EOL and at operating temperatures. The negative void coefficient at operating temperature becomes more negative with fuel burnup.

4.3.2.3.3 Power Coefficient:

The combined effect of moderator temperature and fuel temperature change as the core power level changes is called the total power coefficient and is expressed in terms of reactivity change per percent power change. An example of the power coefficient at BOL and EOL conditions is given on Figure 4.3-34. It becomes more negative with burnup reflecting the combined effect of moderator and fuel temperature coefficients with burnup. An example of the power defect (integral reactivity effect) at BOL and EOL is given on Figure 4.3-35.

4.3.2.3.4 Comparison of Calculated and Experimental Reactivity Coefficient:

Section 4.3.3 describes the comparison of calculated and experimental reactivity coefficients in detail. Based on the data presented there, the accuracy of the current analytical model is:

±0.2 percent $\Delta\rho$ for Doppler and power defect

± 2 pcm/°F for the moderator coefficient.

Experimental evaluation of the calculated coefficients will be completed during the physics startup tests described in Chapter 14.

4.3.2.3.5 Reactivity Coefficients Used in Transients Analysis:

Table 4.3-2 gives the limiting values as well as the best estimate values for the reactivity coefficients. The limiting values are used as design limits in the transient analysis. The exact values of the coefficient used in the analysis depend on whether the transient of interest is examined at BOL or EOL, whether the most negative or the most positive (least negative) coefficients are appropriate, and whether spatial nonuniformity must be considered in the analysis. Conservative values of coefficients, considering various aspects of analysis, are used in the transient analysis. This is described in Chapter 15.

The reactivity coefficients shown on Figures 4.3-27 through 4.3-35 are best estimate values calculated for this cycle and apply to the core described in Table 4.3-1. The limiting values shown in Table 4.3-2 are chosen to encompass the best estimate reactivity coefficients, including the uncertainties given in Section 4.3.3.3 over appropriate operating conditions calculated for this cycle and the expected values for the subsequent cycles. The most positive as well as the most negative values are selected to from the design basis range used in the transient analysis. A direct comparison of the best estimate and design limit values shown in Table 4.3-2 can be misleading since, in many instances, the most conservative combination of reactivity coefficients is used in the transient analysis even though the extreme coefficients assumed may not simultaneously occur at the conditions of lifetime, power level, temperature, and boron concentrations assumed in the analysis. The need for a reevaluation of any accident in a subsequent cycle is contingent upon whether or not the coefficients for that cycle fall within the identified range used in the analysis presented in Chapter 15 with due allowance for the calculational uncertainties given in Section 4.3.3.3. Control rod requirements are given in Table 4.3-3 for the core described and for hypothetical equilibrium cycles since these are markedly different. These latter numbers are provided for information only and their validity in a particular cycle would be an unexpected coincidence.

4.3.2.4 Control Requirements. To ensure the shutdown margin stated in the Technical Specifications under conditions where a cooldown to ambient temperature is required, concentrated soluble boron is added to the coolant. Boron concentrations for several core conditions are listed in Table 4.3-2. For all core conditions including refueling, the boron concentration is well below the solubility limit. The RCCAs are employed to bring the reactor to the hot shutdown condition. The minimum required shutdown margin are given in the Technical Specifications.

The ability to accomplish the shutdown for hot conditions is demonstrated in Table 4.3-3 by comparing the difference between the RCCA assembly reactivity available with an allowance for the worst stuck rod with that required for control and protection purposes. The shutdown margin includes an allowance of 10 percent for analytic uncertainties (Section 4.3.2.4.9). The largest reactivity control requirements appears at the EOL when the moderator temperature coefficient reaches its peak negative value as reflected in the larger power defect.

The control rods are required to provide sufficient reactivity to account for the power defect from full power to zero power and to provide the required shutdown margin. The reactivity addition resulting from power reduction consists of contributions from Doppler, variable average moderator temperature, flux redistribution, and reduction in void content as discussed below.

4.3.2.4.1 Doppler: The Doppler effect arises from the broadening of uranium-238 and plutonium-240 resonance peaks with an increase in effective pellet temperature. This effect is most noticeable over the ranges of zero power to full power due to the large pellet temperature increase with power generation.

4.3.2.4.2 Variable Average Moderator Temperature: When the core is shut down to the hot zero power (HZP) condition, the average moderator temperature changes from the equilibrium full load value determined by the steam generator and turbine characteristics (steam pressure, heat transfer, tube fouling, etc.) to the equilibrium no load value, which is based on the steam generator shell side design pressure. The design change in temperature in conservatively increased by 5°F to account for the control dead band and measurement errors.

Since the moderator temperature coefficient is negative, there is a reactivity addition with power reduction. The moderator temperature coefficient becomes more negative as the fuel depletes because the boron concentration is reduced. This effect is the major contributor to the increased requirement at EOL.

4.3.2.4.3 Redistribution: During full power operation the coolant density decreases with core height, and this, together with partial insertion of control rods, results in less fuel depletion near the top of the core. Under steady-state conditions, the relative power distribution will be slightly asymmetric towards the bottom of the core. On the other hand, at HZP conditions, the coolant density is uniform up the core, and there is no flattening due to Doppler. The result will be a flux distribution which at zero power can be skewed toward the top of the core. The reactivity insertion due to the skewed distribution is calculated with an allowance for effects of xenon distribution.

4.3.2.4.4 Void Content: A small void content in the core is due to nucleate boiling at full power. The void collapse coincident with power reduction makes a small reactivity contribution.

4.3.2.4.5 Rod Insertion Allowance: At full power, the control bank is operated within a prescribed band of travel to compensate for small periodic changes in boron concentration, changes in temperature and very small changes in the xenon concentration not compensated for by a change in boron concentration. When the control bank reaches either limit of this band, a change in boron concentration is required to compensate for additional reactivity changes. Since the insertion limit is set by a rod travel limit, a conservatively high calculation of the inserted worth is made which exceeds the normally inserted reactivity.

4.3.2.4.6 Burnup: Excess reactivity of approximately 10 percent $\Delta\rho$ (hot) is installed at the beginning of each cycle to provide sufficient reactivity to compensate for fuel depletion and fission products throughout the cycle. This reactivity is controlled by the addition of soluble boron to the coolant and by burnable absorbers. The soluble boron concentration for several core configurations, the unit boron worth, and burnable absorber worth are given in Tables 4.3-1 and 4.3-

2. Since the excess reactivity for burnup is controlled by soluble boron and/or burnable absorbers it is not included in control rod requirements.

4.3.2.4.7 Xenon and Samarium Poisoning and pH Effects: Changes in xenon and samarium concentrations in the core occur at a sufficiently slow rate, even following rapid power level changes, that the resulting reactivity change is controlled by changing the soluble boron concentration. Changes in reactivity due to a change in coolant pH, if any, are sufficiently small in magnitude and occur slowly enough to be controlled by the boron system. Further details are provided in Reference 4.3-13.

4.3.2.4.8 Combined Control Requirements: The reactivity requirements at EOL of a typical cycle for a 168-inch and a 144-inch 17 x 17 four-loop core are listed on a comparable basis in Table 4.3-4. The Doppler defect is slightly less for the 168-inch core due to the lower average linear power density (5.20 vs. 5.44 kW/ft). The moderator defect is higher due to the slightly more negative moderator temperature coefficient at the higher temperature of the 168-inch core. The redistribution requirement is greater for the longer core (1.20 percent $\Delta\rho$ vs. .085 percent $\Delta\rho$). More excess margin is available to the 168-inch core than the 12-foot core due to the use of 57 rather than 53 control rods in this example. Both cores operate in the same range of expected reactivity parameters as shown in Table 4.3-5.

4.3.2.4.9 Experimental Confirmation: Following a normal shutdown, the total core reactivity change during cooldown with a stuck rod has been measured on a 121 assembly, 10-foot-high core and 121 assembly, 12-foot-high core. In each case, the core was allowed to cool down until it reached criticality simulating the steam line break accident. For the 10-ft core, the total reactivity change associated with the cooldown is overpredicted by about 0.3 percent $\Delta\rho$ with respect to the measured result. This represents an error of about 5 percent in the total reactivity change and is about half the uncertainty allowance for this quantity. For the 12-ft core, the difference between the measured and predicted reactivity change was an even smaller 0.2 percent $\Delta\rho$. These measurements and others demonstrate the capability of the methods described in Section 4.3.3.

4.3.2.4.10 Control: Core reactivity is controlled by means of a chemical absorber dissolved in the coolant, RCCAs, and burnable absorber rods as described below.

4.3.2.4.11 Chemical Absorber: Boron in solution as boric acid is used to control relatively slow reactivity changes associated with:

1. The moderator temperature defect in going from cold shutdown at ambient temperature to the hot operating temperature at zero power.
2. The transient xenon and samarium poisoning, such as that following power changes or changes in rod cluster control position.
3. The excess reactivity required to compensate for the effects of fissile inventory depletion and buildup of long-life fission products.
4. The burnable absorber depletion.

The boron concentrations for various core conditions are presented in Table 4.3-2.

4.3.2.4.12 Rod Cluster Control Assemblies: Only full-length assemblies are employed in this reactor. The number of assemblies is shown in Table 4.3-1. The RCCAs are used for shutdown and control purposes to offset fast reactivity changes associated with:

1. The required shutdown margin in the HZP, stuck rod condition.
2. The reactivity compensation as a result of an increase in power above hot zero power (power defect including Doppler, and moderator reactivity changes).
3. Unprogrammed fluctuations in boron concentration, coolant temperature, or xenon concentration (with rods not exceeding the allowable rod insertion limits).
4. Reactivity ramp rates resulting from load changes.

The allowed control bank reactivity insertion is limited at full power to maintain shutdown capability. As the power level is reduced, control rod reactivity requirements are also reduced and more rod insertion is allowed. The control bank position is monitored and the operator is notified by an alarm if the limit is approached. The determination of insertion limit uses conservative xenon distributions and axial power shapes. In addition, the RCCA withdrawal pattern determined from these analyses is used in determining power distribution factors and in determining the maximum worth of an inserted RCCA ejection accident. Further discussion is provided in the Technical Specifications on rod insertion limits.

Power distribution, rod ejection, and rod misalignment analyses are based on the arrangement of the shutdown and control groups of the RCCAs shown on Figure 4.3-36. All shutdown RCCAs are withdrawn before withdrawal of the control banks is initiated. In going from zero to 100 percent power control banks A, B, C, and D are withdrawn sequentially. The limits of rod positions and further discussion on the basis for rod insertion limits are provided in the Technical Specifications.

4.3.2.4.13 Reactor Coolant Temperature: Reactor coolant (or moderator) temperature control has added flexibility in reactivity control of the Westinghouse PWR. This feature takes advantage of the negative moderator temperature coefficient inherent in a PWR to:

1. Maximize return to power capabilities.
2. Provide ± 5 percent power load regulation capabilities without requiring control rod compensation.
3. Extend the time in cycle life to which daily load follow operations can be accomplished.

Reactor coolant temperature control supplements the dilution capability of the plant by lowering the reactor coolant temperature to supply positive reactivity through the negative moderator temperature coefficient of the reactor. After the transient is over, the system automatically recovers the reactor coolant temperature to the programmed value.

Moderator temperature control of reactivity, like soluble boron control, has the advantage of not significantly affecting the core power distribution. However, unlike boron control, temperature control can be rapid enough to achieve reactor power change rates of 5 percent/minute.

4.3.2.4.14 Burnable Absorber Rods: The burnable absorber rods provide partial control of the excess reactivity available at the beginning of the fuel cycle. In doing so, these rods prevent the moderator temperature coefficient from being positive at normal operating conditions at rated thermal power. They perform this function by reducing the requirement for soluble absorber in the moderator at the beginning of the first fuel cycle as described previously. For purposes of illustration, typical burnable absorber rod patterns in the core, together with the number of rods per assembly, are shown on Figure 4.3-5 while the arrangements within an assembly are displayed on Figures 4.3-4 and 4.3-4a. Typical values of the reactivity worth of these rods in the first core are shown in Table 4.3-1. The boron in the rods is depleted with burnup but at a sufficiently slow rate so that the resulting critical concentration of soluble boron is such that the moderator temperature coefficient remains negative at all times for power operating conditions at rated thermal power.

4.3.2.4.15 Peak Xenon Startup: Compensation for the peak xenon buildup is accomplished using the boron control system. Startup from the peak xenon condition is accomplished with a combination of rod motion and boron dilution. The boron dilution may be made at any time, including during the shutdown period, provided the shutdown margin is maintained.

4.3.2.4.16 Load Follow Control and Xenon Control: During load follow maneuvers power changes are accomplished using control rod motion and dilution or boration by the boron system as required. Control rod motion is limited by the control rod insertion limits in the Technical Specifications and discussed in Section 4.3.2.4.12 and 4.3.2.4.13. The power distribution is maintained within acceptable limits through the location of the rod bank. Reactivity changes due to the changing xenon concentration can be controlled by rod motion and/or changes in the soluble boron concentration.

Late in cycle life, extended load follow capability is obtained by augmenting the limited boron dilution capability at low soluble boron concentrations by temporary moderator temperature reductions.

Rapid power increases (5 percent/minute) from partial power during load follow operation are accomplished with a combination of rod motion, moderator temperature reduction, and boron dilution. Compensation for the rapid power increase is accomplished initially by a combination of rod withdrawal and moderator temperature reduction. As the slower boron dilution takes effect after the initial rapid power increase, the moderator temperature returns to the programmed value.

4.3.2.4.17 Burnup: Control of the excess reactivity for burnup is accomplished using soluble boron and/or burnable absorbers. The boron concentration must be limited at rated thermal power to ensure the moderator temperature coefficient is negative. Sufficient burnable absorbers are installed at the beginning of a cycle to give the desired cycle lifetime without exceeding the boron concentration limit. The practical minimum boron concentration is 10 ppm.

4.3.2.5 Control Rod Patterns and Reactivity Worth. The RCCAs are designated by function as the control groups and the shutdown groups. The terms “group” and “bank” are used synonymously throughout this report to describe a particular grouping of control assemblies. The rod cluster assembly pattern is displayed on Figure 4.3-36. The control banks are labeled A, B, C, and D and the shutdown banks are labeled SA, SB, etc., as applicable. Each bank, although operated and controlled as a unit, is comprised of two subgroups. The axial position of the RCCAs may be controlled manually or automatically. The RCCAs are all dropped into the core following actuation of reactor trip signals.

Two criteria have been employed for selection of the control groups. First, the total reactivity worth must be adequate to meet the requirements specified in Table 4.3-3. Second, since these rods may be partially inserted at power operation, the total power peaking factor should be low enough to ensure that the power capability requirements are met. Analyses indicate that the first requirement can be met either by a single group or by two or more banks whose total worth equals at least the required amount. The axial power shape would be more peaked following movement of a single group of rods worth three to four percent $\Delta\rho$ than following movement of more banks each worth less; therefore, four banks (described as A, B, C, and D on Figure 4.3-36) each worth approximately one percent $\Delta\rho$ have been selected. Typical control bank worths are shown in Table 4.3-2.

The position of control banks for criticality under any reactor condition is determined by the concentration of boron in the coolant. On an approach to criticality, boron is adjusted to ensure that criticality will be achieved with control rods above the insertion limit set by shutdown and other considerations which are given in the Technical Specifications.

Ejected rod worths are given in Section 15.4.8 for several different conditions.

Allowable deviations due to misaligned control rods are discussed in the Technical Specifications.

A representative calculation for two banks of control rods withdrawn simultaneously (rod withdrawal accident) is given on Figure 4.3-37.

Calculation of control rod reactivity worth versus time following reactor trip involves both control rod velocity and differential reactivity worth. The bounding rod position versus time of travel after rod release assumed in accident analysis is given on Figure 15.0-3. For nuclear design purposes, the reactivity worth versus rod position is calculated by a series of steady-state calculations at various control rod positions assuming all rods out of the core as the initial position in order to minimize the initial reactivity insertion rate. Also, to be conservative, the rod with the highest worth is assumed stuck out of the core and the flux distribution (and thus reactivity importance) is assumed to be skewed to the bottom of the core. The bounding curve used in accident analysis is shown on Figure 15.0-4.

The shutdown groups provide additional negative reactivity to assure an adequate shutdown margin. Shutdown margin is defined as the amount by which the core would be subcritical at hot shutdown if all RCCAs are tripped, but assuming that the highest worth assembly remains fully withdrawn and no changes in xenon or boron take place. The loss of control rod worth due to the material irradiation is negligible since only bank D may be in the core under normal operating conditions.

The values given in Table 4.3-3 show that the available reactivity in withdrawn RCCAs provides the design bases minimum shutdown margin allowing for the highest worth cluster to be at its fully withdrawn position. An allowance for the uncertainty in the calculated worth of N-1 rods is made before determination of the shutdown margin.

4.3.2.6 <u>Criticality of the Reactor During Refueling and Criticality of Fuel Assemblies</u>. The basis for maintaining the reactor subcritical during refueling is presented in Section 4.3.1.5 and a discussion of how control requirements are met is given in Section 4.3.2.4 and 4.3.2.5.

Criticality of fuel assemblies outside the reactor is precluded by adequate design of fuel transfer and fuel storage facilities and by administrative control procedures. This section identifies those criteria important to criticality safety analyses.

4.3.2.6.1 <u>New Fuel Storage</u>: New fuel is stored in 21-inch center-to-center racks in the new fuel storage facilities in a dry condition. For the flooded condition and for the low water density optimum moderator condition (with unborated water assuming fuel of the highest anticipated enrichment of 5.0 w/o U-235), the effective multiplication factor does not exceed 0.95. CN-3230

In the analysis for the storage facilities, the fuel assemblies are assumed to be in their most reactive condition, namely fresh or undepleted and with no control rods or removable neutron absorbers present. Credit is taken for the inherent neutron-absorbing effect of the construction materials of the racks. Assemblies cannot be closer together than the design separation provided by the storage facility, except in special cases such as in fuel shipping containers where analyses are carried out to establish the acceptability of the design.

In the case of an accident that would increase reactivity, such as an assembly drop in the normal dry condition ($k_{eff} \leq 0.70$), the maximum k_{eff} will be less than 0.95. This includes allowances for biases and uncertainties (Reference 4.3-40).

4.3.2.6.2 Spent Fuel Storage:

4.3.2.6.2.1 Analysis Methodology – The reactivity of the spent fuel rack for new and irradiated fuel is analyzed in Reference 4.3-41. To provide safety margin in the criticality analysis of the spent fuel racks, credit is taken for the soluble boron present in the Region 1 and 2 spent fuel pool. This parameter provides significant negative reactivity in the criticality analysis of the spent fuel rack and will be used here in conjunction with administrative controls to offset the reactivity increase when ignoring the presence of the spent fuel rack Boraflex poison panels. Soluble boron credit provides sufficient relaxation in the enrichment limits of the spent fuel racks. Reference 4.3-42 provides the evaluation of spent fuel pool dilution. It is shown that there is no credible event which would result in a spent fuel pool dilution from the required soluble boron concentration (2500 ppm) to the minimum soluble boron concentration that assures $K_{eff} < 0.95$ (700 ppm). CN-3196

The design basis for preventing criticality in the spent fuel pool is:

1. the effective neutron multiplication factor, K_{eff}, of the fuel rack array will be less than 1.00 in pure, unborated water, with a 95 percent probability at a 95 percent confidence level, including uncertainties; and,

2. the effective neutron multiplication factor, K_{eff}, of the fuel rack array will be ≤ 0.95 in the pool containing borated water, with a 95 percent probability at a 95 percent confidence level, including uncertainties.

The purpose of this section is to present the storage requirements, including maximum nominal enrichments, minimum burnup values, minimum decay times, minimum IFBA content, storage configurations, and the minimum pool soluble boron concentration.

With the simplifying assumptions employed in this analysis (no grids, sleeves, axial blankets, etc.), the various types of 17x17XL fuel do not contribute to any increase in the basic assembly reactivity. This includes small changes in guide tube and instrumentation tube dimensions. Therefore, future fuel assembly upgrades do not require a criticality analysis if the fuel diameter continues to be 0.374 inches.

The criticality analysis was performed at a nominal temperature of 68°F (Reference 4.3-41). However, a reactivity bias was applied to account for the effect of the normal range of the spent fuel pool water temperature of 50°F to 160°F.

The fuel rod, guide tube and instrumentation tube claddings are modeled with zircaloy in this analysis. This is conservative with respect to the Westinghouse ZIRLO product which is a zirconium alloy containing additional elements including niobium. Niobium has a small absorption cross section which causes more neutron capture in the cladding regions resulting in a lower reactivity. Therefore, this analysis is conservative with respect to fuel assemblies containing ZIRLO cladding in fuel rods, guide tubes, and the instrumentation tube.

Empty water cells may be substituted for fresh or burned fuel assemblies at any location. Any positive reactivity effect due to the additional water in the 2x2 analytical cell is offset by the absence of uranium.

When storing fuel with an initial nominal enrichment greater than the maximum all-cell enrichment for the respective rack region, a rack K_{eff} of less than or equal to 0.95 is ensured by the maintenance of a minimum amount of soluble boron and the use of administrative procedures to control the placement of burned and fresh fuel and RCCAs. A rack K_{eff} of less than 1.00 in pure, unborated water, is ensured by the use of administrative procedures to control the placement of burned and fresh fuel and RCCAs. Guidance on the close packed storage of fresh and burned fuel, and fuel containing IFBA's, is provided in Section 5.6 of the Technical Specifications.

The licensing basis for the racks is met by the combination of the physical design and center-to-center spacing of the storage cells, the required presence of soluble boron, and the use of administrative procedures to guide the placement of fuel assemblies and RCCAs.

4.3.2.6.2.2 Region 1 Rack Design – The Region 1 racks have a 10.95 inch-nominal center-to-center spacing. This region is conservatively designed to accommodate close packed storage of unirradiated fuel enriched to 2.50 nominal weight percent uranium-235.

Storage of close packed fuel with nominal enrichment of greater than 2.50 nominal w/o is achievable by the use of reactivity equivalencing for burnup credit and the presence of IFBA pins, as discussed in Reference 4.3-41.

Guidance on the close packed storage of fresh and burned fuel, and fuel containing IFBA's, is provided in Section 5.6 of Technical Specifications. Similar guidance is provided on the storage of fresh and burned fuel in a checkerboard patter in Region 1 racks. Empty water cells may be substituted for fuel assemblies in all cases.

4.3.2.6.2.3 Region 2 Rack Design -The Region 2 racks have a 9.15 inch-nominal center-to-center spacing with fixed absorber material surrounding each cell. However, the criticality analysis does not take credit for the presence of the fixed absorber material. This region is conservatively designed to accommodate unirradiated fuel enriched to 1.20 nominal weight percent uranium-235.

Storage of close packed fuel with nominal enrichment of greater than 1.20 nominal w/o is achievable by the use of reactivity equivalencing for burnup credit, as discussed in Reference 4.3-41.

Guidance on the close packed storage of burned fuel is provided in Section 5.6 of the Technical Specifications. Similar guidance is provided on the storage of fresh and burned fuel in a checkerboard pattern in Region 2 racks. Empty water cells may be substituted for fuel assemblies in all cases.

4.3.2.6.2.4 Storage Configuration Interface Requirements – When the two storage areas meet at an interface, the type of interface is explicitly defined to ensure the rack K_{eff} limit is met. Interface requirements are defined in Section 5.6 of the Technical Specifications. Technical Specification Section 5.6 also defines the interface requirements between fuel and non-fuel items (non-fissile material). CN-3196

4.3.2.6.2.5 Rack Utilization – Section 5.6 of the Technical Specifications describes the storage of fresh, burned, and IFBA-containing fuel in the spent fuel storage racks. Both close packed and checkerboarded storage of fuel is allowed in Region 1 and Region 2 racks, depending on the reactivity of the fuel assembly.

The reactivity characteristics of fuel assemblies which are to be placed in the spent fuel storage racks are determined and the assemblies are categorized by reactivity. Alternately, if necessary, all assemblies may be treated as if each assembly is of the highest reactivity class until the actual assembly reactivity classification is determined. Section 5.6 of the Technical Specifications provides the definitions of the reactivity classifications and the allowed storage patterns.

The boron concentration of the water in the spent fuel pool is maintained at or above the minimum value needed to ensure that the rack K_{eff} is less than or equal to 0.95 in the event of a single misplaced assembly.

4.3.2.6.3 In-Containment Storage of Fresh and Spent Fuel: The in-containment storage racks provide for the temporary storage of both fresh and burned fuel during refueling operations. The fuel is stored in 16 inch center-to-center stainless steel racks in the in-containment storage area (ICSA). The ICSA is flooded with borated refueling water when fuel is present.
The methodology used to analyze the ICSA is the same as that used for the Spent Fuel Storage Racks, as described in Reference 4.3-41 The ICSA racks have a 16 inch-nominal center-to-center spacing. This region is conservatively designed to accommodate close packed storage of unirradiated fuel enriched to 4.5 weight percent uranium-235. The following equation is used to develop the maximum K_{eff} for the ICSA fuel storage racks:

$$K_{eff} = K_{worst} + B_{method} + \sqrt{\left[(ks)^2 worst + (ks)^2 method\right]}$$

where:

K_{worst} = worst case KENO K_{eff} that includes material, mechanical, and enrichment tolerances

B_{method} = method bias determined from benchmark critical comparisons

ks_{worst} = 95/95 uncertainty in the worst case KENO K_{eff}

ks_{method} = 95/95 uncertainty in the method bias

Substituting calculated values in the order listed above, the result is:

$$K_{eff} = 0.9315 + 0.0074 + \sqrt{\left[(0.0064)^2 + (0.0029)^2\right]} = 0.9459$$

Since K_{eff} is less than 0.95 including uncertainties at a 95/95 probability/confidence level, the acceptance criteria for criticality is met for ICSA storage of fuel assemblies enriched to a nominal 4.5 w/o uranium-235.

Storage of close packed fuel with nominal enrichment of greater than 4.5 w/o is achievable by taking credit for the presence of IFBA pins, as discussed in Reference 4.3-41.

Reactivity equivalencing for IFBA credit allows fuel with an initial nominal enrichment of greater than 4.5 w/o to be stored in a close packed array if the fuel assembly reference K_{inf} is less than or equal to 1.484. A figure reflecting this constant with K_{inf} is given in Section 5.6 of the Technical Specifications. This curve reflects the minimum number of IFBA pins required in an assembly for close packed storage. The curve starts at 4.5 w/o and no IFBA pins and ends at 5.0 w/o and 36 IFBA pins. Note that fuel assemblies categorized as Category 2, 3, or 4 (as defined in Technical Specification 5.1.6.2), have a lower reactivity than fuel with an initial nominal enrichment of greater than 4.5 w/o and no IFBA's. therefore, such assemblies may be stored in close packed array in the ICSA.

The IFBA absorber material is a zirconium diboride (ZrB_2) coating on the outside of the fuel pellet (Reference 4.3-40). Each IFBA pin has a nominal poison material loading of 1.57 milligram B^{10} per inch, which is the minimum standard loading offered by Westinghouse for 17 x 17XL/STD fuel assemblies. The IFBA B^{10} loading is reduced by 5% to conservatively account for manufacturing tolerances and then by an additional 28.5% to conservatively model a minimum poison length of 120 inches.

Additional IFBA credit calculations were performed to examine the reactivity effects of higher IFBA linear B^{10} loadings (2.36 and 3.14 mg/in). These calculations confirm that the assembly reactivity remains constant provided the net B^{10} material per assembly is preserved. Therefore with higher IFBA B^{10} loadings, the required number of IFBA pins per assembly can be reduced by the ratio of the higher loading to the nominal 1.57 mg/in loading.

The IFBA requirements were developed based on the standard IFBA patterns used by Westinghouse. However, since the worth of individual IFBA pins can change depending on the position within the assembly (due to local variations in thermal flux), studies were performed to evaluate the effect and a conservative reactivity margin was included in the development of the IFBA requirement to account for this effect. This assures that the IFBA requirement remains valid at intermediate enrichments where standard IFBA patterns may not be available. In addition, to conservatively account for calculational uncertainties, the IFBA requirements also include a conservatism of approximately 10% on the total number of IFBA pins at the 5.0 w/o end.

Empty water cells may be substituted for fresh or burned fuel assemblies at any location. Any positive reactivity effect due to the additional water in the 2x2 analytical cell is offset by the absence of uranium. This is illustrated by the use of empty water holes in the checkerboard pattern used in the Region 2 spent fuel storage racks, discussed in Section 4.3.2.6.2.3. In Region 2 racks, fresh, no IFBA, 5.0 w/o assemblies may be stored in a checkerboard pattern with empty water holes. A similar checkerboard storage pattern in the ICSA racks is allowable since the ICSA racks have a greater cell to cell spacing than Region 2 racks. The wider assembly spacing would serve to further reduce the rack reactivity.

When storing fuel with an initial nominal enrichment of greater than 4.5 w/o, a rack K_{eff} of less than or equal to 0.95 in the ICSA racks is ensured by the use of administrative procedures to control the IFBA content of the fuel. Guidance on the close packed storage of fuel is provided in Section 5.6 of the Technical Specifications. Empty water cells may be substituted for fuel assemblies in all cases.

4.3.2.7 Stability.

4.3.2.7.1 Introduction: The stability of the PWR cores against xenon-induced spatial oscillations and the control of such transients are discussed extensively in References 4.3-8, 4.3-16, 4.3-17, and 4.3-18. A summary of these reports is given in the following discussion and the design bases are give in Section 4.3.1.6.

In a large reactor core, xenon-induced oscillations can take place with no corresponding change in the total power of the core. The oscillation may be caused by a power shift in the core which occurs rapidly by comparison with the xenon-iodine time constants. Such a power shift occurs in the axial direction when a plant load change is made by control rod motion and results in a change in the moderator density and fuel temperature distributions. Such a power shift could occur in the diametral plane of the core as a result of abnormal control action.

Due to the negative power coefficient of reactivity, PWR cores are inherently stable to oscillations in total power. Protection against total power instabilities is provided by the control and protection system as described in Section 7.7. Hence, the discussion on the core stability will be limited here to xenon-induced spatial oscillations.

4.3.2.7.2 Stability Index: Power distributions, either in the axial direction or in the X-Y plane, can undergo oscillations due to perturbations introduced in the equilibrium distributions without changing the total core power. The overtones in the currents PWRs and the stability of the core against xenon-induced oscillation can be determined in terms of the eigen-values of the first flux overtones. Writing, either in the axial direction or in the X-Y plane, the eigen value ξ of the first flux harmonic as:

$$\xi = b + ic \qquad \text{(Eq. 4.3-1)}$$

then b is defines as the stability index and $T = 2\pi/c$ as the oscillation period of the first harmonic. The time-dependence of the first harmonic in the power distribution can now be represented as:

$$\delta \o (t) = A e^{\xi t} = ae^{bt} \cos ct, \qquad \text{(Eq. 4.3-2)}$$

where A and A_{n+1} are constants. The stability index can also be obtained approximately by:

$$b = \frac{1}{T} \ln \frac{A_{n+1}}{A_n} \qquad \text{(Eq. 4.3-3)}$$

where A_n, A_{n+1} are the successive peak amplitudes of the oscillation and T is the time period between the successive peaks.

4.3.2.7.3 Prediction of the Core Stability: The core described in this report has an active fuel length 24 in. longer (nominal) than previous Westinghouse PWRs containing 193 fuel assemblies. For this reason it is expected that the core will be as stable as previous designs with respect to radial and diametral xenon oscillation since the radial core dimensions have not changed. However, the core will be slightly less stable than previous cores with respect to axial xenon oscillations because the fuel length has been increased by 24 inches. The effect of this will be to decrease the burnup at which the axial stability index becomes zero (Section 4.3.2.7.4b, below). The moderator temperature coefficients and Doppler temperature coefficients of reactivity will be similar to those of previous plants. Control banks present in the core are sufficient to dampen any xenon oscillations present. Free axial xenon oscillations are not allowed to occur for any length core, except during special tests as discussed in Section 4.3.2.7.4.

Analysis of both the axial and X-Y xenon transient tests, discussed in Section 4.3.2.7.5, shows that the calculational model is adequate for the prediction of core stability.

4.3.2.7.4 Stability Measurements:

1. Axial Measurements

Two axial xenon transient tests conducted in a PWR with a core height of 12 ft and 121 fuel assemblies are reported in Reference 4.3-19, and will be briefly discussed here. The tests were performed at approximately 10 percent and 50 percent of cycle life.

Both a free-running oscillation test and a controlled test were performed during the first test. The second test at mid-cycle consisted of a free-running oscillation test only. In each of the free-running oscillation tests, a perturbation was introduced to the equilibrium power distribution through an impulse motion of the control Bank D and the subsequent oscillation period was measured. In the controlled test conducted early in the cycle, the part length rods were used to follow the oscillations to maintain an axial offset within the prescribed limits. The axial offset of power was obtained from the excore ion chamber readings (which had been calibrated against the incore flux maps) as a function of time for both free-running tests as shown on Figure 4.3-40.

The total core power was maintained constant during these spatial xenon tests, and the stability index and the oscillation period were obtained from a least-square fit of the axial offset data in the from of Equation 4.3-2. The axial offset of power is the quantity that properly represents the axial stability in the sense that it essentially eliminates any contribution from even order harmonics, including the fundamental mode. The conclusions of the test are:

a. The core was stable against induced axial xenon transients both at the core average burnups of 1550 MWD/MTU and 7700 MWD/MTU. The measured stability indices are -0.041 hr^{-1} for the first test (Curve 1 of Figure 4.3-40) and -0.014 hr^{-1} for the second test (Curve 2 of Figure 4.3-40). The corresponding oscillation periods are 32.4 hrs. and 27.2 hrs., respectively.

b. The reactor core becomes less stable as fuel burnup progresses and the axial stability index was essentially zero at 12,000 MWD/MTUMeasurements in the X-Y Plane

Two X-Y xenon oscillation tests were performed at a PWR plant with a core height of 12 ft and 157 fuel assemblies. The first test was conducted at a core average burnup of 1,540 MWD/MTU and the second at a core average burnup of 12,900 MWD/MTU. Both of the X-Y xenon tests show that the core was stable in the X-Y plane at both burnups. The second test shows that the core became more stable as the fuel burnup increased and all Westinghouse PWRs with 121 and 157 assemblies are expected to be stable throughout their burnup cycles.

In each of the two X-Y tests, a perturbation was introduced to the equilibrium power distribution through an impulse motion of one rod cluster control unit located along the diagonal axis. Following the perturbation, the uncontrolled oscillation was monitored using the moveable detector and thermocouple system and the excore power range detectors. The quadrant tilt difference (QTD) is the quantity that properly represents the diametral oscillation in the X-Y plane of the reactor core in that the differences of the quadrant average powers over two symmetrically opposite quadrants essentially eliminates the contribution to the oscillation from the azimuthal mode. The QTD data were fitted in the from of Equation 4.3-2 through a least-square method. A stability index of –0.076 hr^{-1} with a period of 29.6 hours was obtained from the thermocouple data shown on Figure 4.3-41.

It was observed in the second X-Y xenon test that the PWR core with 157 fuel assemblies had become more stable due to an increased fuel depletion and the stability index was not determined.

4.3.2.7.5 Comparison of Calculations with Measurements: The analysis of the axial xenon transient tests was performed in an axial slab geometry using a flux synthesis technique. The direct simulation of the axial offset data was carried out using the PANDA Code (Ref. 4.3-20) for Unit 1 and APOLLO, an updated version of the PANDA code for Unit 2. The analysis of the X-Y xenon transient tests was performed in an X-Y geometry using TORTISE, a modified TURTLE (Ref. 4.3-12) Code. Both the PANDA/APOLLO and TURTLE/TORTISE codes solve the two-group time-dependent neutron diffusion equation with time-dependent xenon and iodine concentrations. The fuel temperature and moderator density feedback is limited to a steady-state model. All the X-Y calculations were performed in an average enthalpy plane.

The basic nuclear cross sections used in this study were generated from a unit cell depletion program which has evolved from the codes LEOPARD (Ref. 4.3-21) and CINDER (Ref. 4.3-22). The detailed experimental data during the tests including the reactor power level, enthalpy rise, and the impulse motion of the control rod assembly, as well as the plant follow burnup data, were closely simulated in the study.

The results of the stability calculation for the axial tests are compared with the experimental data in Table 4.3-7. The calculations show conservative results for both of the axial tests with a margin of approximately –0.01 hr^{-1} in the stability index.

An analytical simulation of the first X-Y xenon oscillation test shows a calculated stability index of –0.081 hrs^{-1}, in good agreement with the measured value of –0.076 hr^{-1}. As indicated earlier, the second X-Y xenon test showed that the core had become more stable compared to the first test and no evaluation of the stability index was attempted. This increase in the core stability in the X-Y plane due to increased fuel burnup is due mainly to the increased magnitude of the negative moderator temperature coefficient.

Previous studies of the physics of xenon oscillations, including three-dimensional analysis, are reported in the series of topical reports, References 4.3-16, 4.3-17, and 4.3-18. A more detailed description of the experimental results and analysis of the axial and X-Y xenon transient tests is presented in Reference 4.3-19 and Section 1 of Reference 4.3-23.

4.3.2.7.6 Stability Control and Protection: The excore detector system is utilized to provide indications of xenon-induced spatial oscillations. The readings from the excore detectors are available to the operator and also from part of the protection system

1. Axial Power Distribution

For maintenance of proper axial power distributions, the operator is instructed to maintain an axial offset within a prescribed operating band, based on the excore detector readings. Should the axial offset be permitted to move far enough outside this band, the protection limit will be reached and power will be automatically reduced.

Both 12- and 14-ft PWR cores become less stable to axial xenon oscillations as fuel burnup progresses. However, free xenon oscillations are not allowed to occur except for special tests. The control rod banks are sufficient to dampen and control any axial xenon oscillations present. Should the axial offset be inadvertently permitted to move far enough outside the control band due to an axial xenon oscillation, or any other reason, the protection limit on axial offset will be reached and power will be automatically reduced.

At BOL (150 MWD/MTU) stability indexes of about -0.47 hrs^{-1} and -0.020 hrs^{-1} were obtained, respectively, for 12-ft and 14-ft cores. The axial stability index is essentially zero in the 11,000 to 12,000 MWD/MTU range for 12-ft cores and in the 8,000 to 9,000 MWD/MTU range for 14-ft cores. At extended burnup (~15,000 MWD/MTU) both 12-ft and 14-ft cores have essentially the same stability index of about 0.02 hrs^{-1} or less. The axial oscillation period for both 12-ft and 14-ft cores increases with burnup. A period of 27 to 28 hours is obtained for both 12-ft and 14-ft cores at BOL. At EOL periods of about 32 and 34 hours area obtained, respectively, for the 12-ft and 14-ft cores. These values depend upon the core design as well as burnup, and the stability index can be positive throughout core life for both 12-ft and 14-ft cores. However, long periods and vertical control rod systems make axial xenon transients easily controllable in modern PWRs at all times of life.

2. Radial Power Distribution

The core described herein is calculated to be stable against X-Y xenon induced oscillations at all times in life.

The X-Y stability of large PWRs has been further verified as part of the startup physics test program for cores with 193 fuel assemblies. The measured X-Y stability of the cores with 157 and 193 assemblies was in good agreement with the calculated stability as discussed in Sections 4.3.2.7.4 and 4.3.2.7.5. In the unlikely event that X-Y oscillations occur, back-up actions are possible and would be implemented, if necessary, to increase the natural stability of the core. This is based on the fact that several actions could be taken to make the moderator temperature coefficient more negative, which would increase the stability of the core in the X-Y plane.

Provisions for protection against non-symmetric perturbations in the X-Y power distribution that could result from equipment malfunctions are made in the protection system design. Such malfunctions include control rod drop, rod misalignment and asymmetric loss of coolant flow.

A more detailed discussion of the power distribution control in PWR cores is presented in Reference 4.3-8 and 4.3-9.

4.3.2.8 Vessel Irradiation. A brief review of the methods and analyses used in the determination of neutron and gamma ray flux attenuation between the core and the pressure vessel is given below. A more complete discussion of the pressure vessel irradiation and surveillance program is given in Section 5.3.

The materials that serve to attenuate neutrons originating in the core and gamma rays from both the core and structural components consist of the core baffle, core barrel, neutron pads, and associated water annuli all of which are within the region between the core and the pressure vessel.

In general, few group neutron diffusion theory codes are used to determine fission power density distributions within the active core and the accuracy of these analyses is verified by incore measurements on operating reactors. Region and rodwise power sharing information from the core calculations is then used as source information in two-dimensional S_n transport calculations which compute the flux distributions throughout the reactor.

The neutron flux distribution and spectrum in the various structural components varies significantly from the core to the pressure vessel. Representative values of the neutron flux distribution and spectrum are presented in Table 4.3-8. The values listed are based on time averaged equilibrium cycle reactor core parameters and power distributions, and thus are suitable for long-term neutron flux projections and for correlation with radiation damage estimates.

As discussed in Section 5.3, the irradiation surveillance program utilizes actual test samples to verify the accuracy of the calculated fluxes at the vessel.

4.3.3 Analytical Methods

Calculations required in nuclear design consist of three distinct types, which are performed in sequence:

1. Determination of effective fuel temperatures

2. Generation of macroscopic few-group parameters

3. Space-dependent, few-group diffusion and nodal calculations

These calculations are carried out by computer codes which can be executed individually; however, at Westinghouse most of the codes required have been linked to from an automated design sequence which minimizes design time, avoids errors in transcription of data, and standardizes the design methods.

4.3.3.1 Fuel Temperature (Doppler) Calculations. Temperatures vary radially within the fuel rod, depending on the heat generation rate in the pellet, the conductivity of the materials in the pellet, gap, and clad; and the temperature of the coolant.

The fuel temperatures for use in most nuclear design Doppler calculations are obtained from a simplified version of the Westinghouse fuel rod design model described in Section 4.2.1.3 which considers the effect of radial variation of pellet conductivity, expansion-coefficient and heat generation rate, elastic deflection of the clad, and a gap conductance which depends on the initial fill gap, the hot open gap dimension, and the fraction of the pellet over which the gap is closed. The fraction of the gap assumed closed represents an empirical adjustment used to produce good agreement with observed reactivity data at BOL. Further gap closure occurs with burnup and accounts for the decrease in Doppler defect with burnup which has been observed in operating plants. For detailed calculations of the Doppler coefficient, such as for use in xenon stability calculations, a more sophisticated temperature model is used which accounts for the effects of fuel swelling, fission gas release, and plastic clad deformation.

Radial power distributions in the pellet as a function of burnup are obtained from LASER (Ref. 4.3-24) calculations.

The effective uranium-238 temperature for resonance absorption is obtained from the radial temperature distribution by applying a radially dependent weighting function. The weighting function was determined from REPAD (Ref. 4.3-25) Monte Carol calculations of resonance escape probabilities in several steady-state and transient temperature distributions. In each case a flat pellet temperature was determined which produced the same resonance escape probability as the actual distribution. The weighting function was empirically determined from these results.

The effective plutonium-240 temperature for resonance absorption is determined by a convolution of the radial distribution of plutonium-240 densities from LASER burnup calculations and the radial weighting function. The resulting temperature is burnup dependent, but the difference between uranium-238 and plutonium-240 temperatures, in terms of reactivity effects, is small.

The effective pellet temperature for pellet dimensional change is that value which produces the same outer pellet radius in a virgin pellet as that obtained from the temperature model. The effective clad temperature for dimensional change is its average value.

The temperature calculational model has been validated by plant Doppler defect data as shown in Table 4.3-9 and Doppler coefficient data as shown on Figure 4.3-42. Stability index measurements also provide a sensitive measure of the Doppler coefficient near full power (Section 4.3.2.7). Doppler defect data is typically within 0.2 percent $\Delta\rho$ of prediction.

4.3.3.2 Macroscopic Group Constants. Macroscopic few-group constants (analogous microscopic cross sections needed for feedback and microscopic depletion calculations) are generated for fuel cells by a recent version of the LEOPARD (Ref. 4.3-21) and CINDER (Ref. 4.3-22) codes, which are linked internally and provide burnup dependent cross sections. Normally a simplified approximation of the main fuel chains is used; however, where needed, as complete solution for all the significant isotopes in the fuel chains from thorium-232 to curium-244 is available (Ref. 4.3-26). Fast and thermal cross section library tapes contain microscopic cross sections taken for the most part from the ENDF/B (Ref. 4.3-27) library, with a few exceptions where other data provided better agreement with critical experiments, isotopic measurements, and plant critical boron values. The effect on the unit fuel cell of non-lattice components in the fuel assembly is obtained by supplying an appropriate volume fraction of these materials in an extra region which is homogenized with the unit cell in the fast (MUFT) and thermal (SOFOCATE) flux calculations. In the thermal calculation, the fuel rod, clad, and moderator are homogenized by energy-dependent disadvantage factors derived from an analytical fit to integral transport theory results.

Group constants for burnable absorber cells, guide thimbles, instrument thimbles, and interassembly gaps are generated in a manner analogous to the fuel cell calculations. Reflector group constants are taken from infinite medium LEOPARD calculation. Baffle group constants are calculated from an average of core and radial reflector microscopic group constants for stainless steel.

Group constants for control rods are calculated in a linked version of the HAMMER (Ref. 4.3-28) and AIM (Ref.4.3-29) codes which provide an improved treatment of self-shielding in the broad resonance structure of these isotopes at epithermal energies than is available in LEOPARD. The Doppler broadened cross sections of the control rod materials are represented as smooth cross sections in the 54-group LEOPARD fast group structure and in 30 thermal groups. The four-group constants in the rod cell and appropriate extra region are generated in the coupled space-energy transport HAMMER calculation. A corresponding AIM calculation of the homogenized rod cell with extra region is used to adjust the absorption cross sections of the rod cell to match the reaction rates in HAMMER. These transport-equivalent group constants are reduced to two-group constants for use in space-dependent diffusion calculations. In discrete X-Y calculations only one mesh interval per cell is used, and the rod group constants are further adjusted for use in this standard mess by reaction rate matching the standard mesh unit assembly to a fine mesh unit assembly calculation.

Nodal group constants are obtained by a flux-volume homogenization of the fuel cells, burnable absorber cells, guide thimbles, instrumentation thimbles, interassembly gaps, and control rod cells from one mesh interval per cell X-Y unit fuel assembly diffusion calculations for Unit 1, and from one mesh interval per cell X-Y diffusion calculations for Unit 2.

Validation of the cross section method is based on analysis of critical experiments as shown in Table 4.3-6, isotopic data as shown in Table 4.3-10, plant critical boron (C_B) values at HZP, BOL as shown in Table 4.3-11 and at HFP as a function of burnup as shown on Figures 4.3-43 through 4.3-45. Control rod worth measurements are shown in Table 4.3-12.

Confirmatory critical experiments on burnable absorbers are described in Reference 4.3-30.

Beginning with Unit 1 Cycle 4 and Unit 2 Cycle 3, macroscopic few-group constants and analogous microscopic cross sections (needed for feedback and microscopic depletion analysis) are generated by PHOENIX-P (Ref. 4.3-38). PHOENIX-P is a two dimensional, multi-group transport theory code which has been approved by the USNRC. The nuclear cross section library used by PHOENIX-P contains cross section data based on a 42 energy-group structure derived from ENDF/ B-V files (Reference 4.3-39). The solution of the flux distribution is divided into two major steps in PHOENIX-P:

1. Solve for two-dimensional, 42 energy-group nodal fluxes which couple individual subcell regions (pellet, clad, moderator) as well as surrounding pins using a method based on collision probabilities and heterogeneous response flux which has been described in detail in Reference 4.3-38.

2. Solve for a coarse energy-group flux distribution using a standard S4 discrete ordinates calculation and use these fluxes to normalize the detailed 42 group nodal fluxes from step 1.

PHOENIX-P is capable of modeling all cell types necessary for PWR design applications. Nodal group constants (two group) are obtained by flux-volume homogenization of the fuel cell (including IFBA pins), guide thimbles, instrumentation thimbles, and interassembly gaps using the PHOENIX-P coarse energy-group flux distribution. Group constants for control rods are based on analysis of critical experiments, isotopic data, and plant critical boron values at HZP and at HFP conditions as a function of burnup as discussed in detail in Reference 4.3-38. Control rod worth measurements are also discussed in the reference.

PARAGON has been approved by the NRC as the new generation of Westinghouse lattice code (Ref. 4.3-5). PARAGON is a replacement for PHOENIX-P and its primary use will be to provide the same types of input data that PHOENIX-P generates for use in three dimensional core simulator codes. This includes macroscopic cross sections, macroscopic cross section for feedback to adjustments to the macroscopic cross sections, pin factors for pin power reconstruction calculations, discontinuity factors for a nodal method solution, and other data needed for safety analysis or other downstream applications.

PARAGON is based on collision probability – interface current cell coupling method. PARAGON provides flexibility in modeling that was not available in PHOENIX-P including exact cell geometry representation instead of cylinderization, multiple rings and regions within the fuel pin and the moderator cell geometry, and variable cell pitch. The solution method permits flexibility in choosing the quality of the calculation through both increasing the number of regions modeled within the cell and the number of angular current directions tracked at the cell interfaces.

The calculation scheme in PARAGON is based on the conventional lattice modules: resonance calculation, flux solution, leakage correction and depletion. This detailed theory of these modules is described in Ref. 4.3-5. The cross-section resonance calculation module is based on the space dependent Dancoff method (Ref. 4.3-5); it is a generalization of the PHOENIX-P methodology that permits to subdivide the fuel pin into many rings and therefore generates space dependent self-shielded isotopic cross-sections. The flux solution module uses the interface current collision probability method and permits a detailed representation of the fuel cells (Ref. 4.3-5). The other two modules (leakage and depletion) are similar to the ones used in PHOENIX-P.

The current PARAGON cross section library is a 70-group library, based on the ENDF/B basic nuclear data, with the same group structure as the library currently used with PHOENIX-P. The PARAGON qualification library has been improved through the addition of more explicit fission products and fissi0n product chains (Ref. 4.3-5). PARAGON is however designed to employ any number of energy groups.

The new NEXUS cross-section generation system uses PARAGON as the lattice code (Ref. 4.3-6).

4.3.3.3 Spatial Few-Group Diffusion Calculations: For Unit 1, Cycle 1 spatial few-group calculations consist primarily of two-group diffusion X-Y calculations using an updated version of the TURTLE Code, two-group X-Y nodal calculation using the PALADON (Ref. 4.3-33) code, and two-group axial calculations using an updated version of the PANDA Code.

Discrete X-Y calculations (one mesh per cell) are carried out to determine critical boron concentrations and power distributions in the X-Y plane. An axial average in the X-Y plane is obtained by synthesis from unrodded and rodded planes. Axial effects in unrodded depletion calculations are accounted for by the axial buckling, which varies with burnup and is determined by radial depletion calculations which are matched in reactivity to the analogous R-Z depletion calculation. The moderator coefficient is evaluated by varying the inlet temperature in the same X-Y calculations used for power distribution and reactivity predictions.

Validation of TURTLE reactivity calculations is associated with the validation of the group constants themselves, as discussed in Section 4.3.3.2. validation of the Doppler calculations is associated with the fuel temperature validation discussed in Section 4.3.3.1. Validation of the moderator coefficient calculations is obtained by comparison with plant measurements at HZP conditions as shown in Table 4.3-13.

PALADON is used in two-dimensional and three-dimensional calculations. PALADON can be used in safety analysis calculations, and to determine critical boron concentrations, control rods worths, and reactivity coefficients.

Axial calculations are used to determine differential control rod worth curves (reactivity versus rod insertion) and axial power shapes during steady-state and transient xenon conditions (flyspeck curve). Group constants and the radial buckling used inn the axial calculation are obtained from the three-dimensional TURTLE calculation from which group constants are homogenized by flux-volume weighting.

For Unit 1 Cycles 2 and 3 and Unit 2 Cycles 1 and 2, spatial few-group calculations consist of two group diffusion X-Y calculations using TORTISE, an updated version of the TURTLE code, and nodal calculations using the PALADON (Ref. 4.3-33) and ANC (Ref. 4.3-34) codes. Two-group axial calculations utilize APOLLO, an updated version of the PANDA code.

Two-dimensional calculations are carried out to determine critical boron concentrations and power distributions in the X-Y plane. An axial average in the X-Y plane is obtained by synthesis from rodded and unrodded planes. Axial effects are accounted for by an input axial buckling, which varies with burnup and was determined by radial depletion calculations matched in reactivity to an analogous R-Z depletion calculation. The moderator coefficient is evaluated by varying the inlet temperature in the same X-Y calculations used for power distribution and reactivity predictions.

Validation of TORTISE reactivity calculations is associated with the validation of the group constants themselves, as discussed in Section 4.3.3.2. PALADON and ANC have been qualified with respect to TORTISE results. Validation of the Doppler calculations is associated with the fuel temperature validation discussed in Section 4.3.3.1. Validation of the moderator coefficient calculations is obtained by comparison with plant measurements at hot zero power conditions as shown in Table 4.3-13.

PALADON and ANC are used in both two-and three-dimensional calculations. They can be used in safety analysis calculations, and to determine critical boron concentrations, control rod worths, and reactivity coefficients.

Axial calculations are used to determine differential control rod worth curves (reactivity versus rod insertion) and axial power shapes during steady-state and transient conditions (flyspeck curve). Group constants and the radial buckling used in the axial calculation are obtained from three-dimensional calculations from which group constants are homogenized by flux-volume weighting.

Validation of the spatial codes for calculating power distributions involves the use of incore and excore detectors and is discussed in Section 4.3.2.2.7.

Based on comparison with measured data it is estimated that the accuracy of current analytical methods is:

± 0.2 percent $\Delta\rho$ for Doppler defect

± 2 x 10^{-5}/°F for moderator coefficient

± 50 ppm for critical boron concentration with depletion

± 3 percent for power distribution

± 0.2 percent $\Delta\rho$ for rod bank worth

±4 pcm/step for differential rod worth

± 0.5 pcm/ppm for boron worth

± 0.1 percent $\Delta\rho$ for moderator defect

Beginning with Unit 1 Cycle 2 and Unit 2 Cycle 1, the reference code used for spatial calculations is ANC (Ref. 4.3-34), beginning with Unit 1 Cycle 4 and Unit 2 Cycle 3, PHOENIX-P (Ref. 4.3-38) generated cross-section sets are implemented in ANC.

Beginning with Unit 2 Cycle 17 and Unit 1 Cycle 19, the reference code used for spatial calculations is ANC with NEXUS/PARAGON (Ref. 4.3-6) generated cross-section sets.

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TABLE 4.3-1

REACTOR CORE DESCRIPTION

Active Core		
Equivalent diameter, in.		132.7
Active Fuel Height, First Core, in.		168
Height – to – Diameter Ratio		1.27
Total Cross Section Area, ft^2		96.06
H_2O/U Molecular Ratio, lattice (cold)		2.41
Reflector Thickness and Composition		
Top – Water plus Steel, in.		~10
Bottom – Water plus Steel, in		~10
Side – Water plus Steel, in.		~15
Fuel Assemblies		
Number		193
Rod Array		17 x 17
Rods per Assembly		264
Rod Pitch, in.		0.496
Overall Transverse Dimensions, in.		8.426 x 8.426
Fuel Weight (as UO_2), lb		261,000 (nominal)***
	**V5H	
Zircaloy Weight, lbs (active core)	57,120	54,840
Number of Grids per Assembly		10 – Type R
Composition of Grids	Zirc-4 or ZIRLO	Inconel 718
Weight of Grids (effective in core), lb	3685	2,979 (3,237*)
Number of Guide Thimbles per Assembly		24
Composition of Guide Thimbles		Zircaloy-4 or ZIRLO
Diameter of Guide Thimbles (upper part), in	0.442 0.474	0.450 ID 0.482 OD
Diameter of Guide Thimbles (lower part), in.		0.397 ID 0.429 OD
Diameter of Instrument Guide Thimbles, in.	0.442 0.474	0.450 ID 0.482 OD

* Applicable following change – over to Anti Snag Grid Design
Change-over begins: Unit 1, Cycle 3, Reload 2
Unit 2, Cycle 2, Reload 1

** Applicable following change to Vantage 5H Design
Change-over begins: Unit 1, Cycle 6, Reload 5
Unit 2, Cycle 4, Reload 3

*** This value will be reduced with the incorporation of annular pellets.
Weight reduction is contingent upon the annular axial region configuration.

TABLE 4.3-1 (Continued)

REACTOR CORE DESCRIPTION

Fuel Rods

Number		50,952
Outside Diameter, in.		0.374
Diametral Gap, in.		0.0065
Clad Thickness, in.		0.0225
Clad Material		Zircaloy-4 or ZIRLO

Fuel Pellets

Material		UO_2 Sintered
Density (percent of theoretical)		95
Fuel Enrichments, wt %		
Region 1	Unit 1	1.50
	Unit 2	2.10
Region 2	Unit 1	2.20
	Unit 2	2.60
Region 3		2.90
Diameter, in.		0.3225
Length, in.		0.387 0.462 (Annular Pellets)
Mass of UO_2 per Foot of Fuel Rod, lb/ft		0.364**

Rod Cluster Control Assemblies (Cycle 1)

Neutron Absorber	Hafnium
Composition	9.53% min
Diameter, in.	0.366
Density, lb/in.3	0.470 (min)
Cladding Material	Type 304, Cold-Worked Stainless Steel
Clad Thickness, in.	0.0185
Number of Clusters	57
Number of Absorber Rods per Cluster	24

* Applicable following change – over to Anti Snag Grid Design
Change-over begins: Unit 1, Cycle 3, Reload 2
Unit 2, Cycle 2, Reload 1

** Mass of UO_2 per foot of fuel rod is for solid pellets. This value is reduced for annular pellets in the axial regions where used.

TABLE 4.3-1 (Continued)

REACTOR CORE DESCRIPTION

Burnable Absorber Rods (First Core)	
Number	
Unit 1	946
Unit 2	1,470
Material	Borosilicate Glass
Clad, OD, in.	0.381
Inner Tube, OD, in.	0.1815
Clad Material	Stainless Steel
Inner Tube Material	Stainless Steel
Boron Loading (w/o $B_2 0_3$ in glass rod)	12.5
Weight of boron-10 per foot of rod, lb/ft	.000419
Initial Reactivity Worth, % $\Delta\rho$	
Unit 1	4.65 (HFP), 4.65 (HZP) 3.40 (cold)
Unit 2	7.23 (HFP), 7.23 (HZP) 5.28 (cold)
Excess Reactivity (Cycle 1)	
Maximum Fuel Assembly $k\infty$ (cold, clean, unborated water)	1.39
Maximum Core Reactivity (cold, zero power, beginning of cycle)	
Unit 1	1.22
Unit 2	1.29

* Applicable following change – over to Anti Snag Grid Design

Change-over begins: Unit 1, Cycle 3, Reload 2
Unit 2, Cycle 2, Reload 1

TABLE 4.3-2

NUCLEAR DESIGN PARAMETERS

(First Cycle)

Core Average Linear Power, kW/ft,	5.20		
Total Heat Flux Hot Channel Factor, F_Q	2.50		
Nuclear Enthalpy Rise Hot Channel Factor, $F^N_{\Delta H}$	1.52		
Reactivity Coefficients+	Design Limits	Best Estimate	
		Unit 1	Unit 2
Doppler-only Power, Upper Curve	-19.4 to -12.6	-15.5 to -11.5	-13.8 to -10.0
β Coefficients, pcm/% Power (See Figure 15.0-2), Lower Curve	-10.2 to -6.7	-12.5 to -9	-11.9 to -8.1
Doppler Temperature Coefficient pcm/°F++	-2.9 to -1.1	-2.5 to -1.8	-2.2 to -1.4
Moderator Temperature Coefficient, pcm/°F++	0 to -40	-6.0 to -30.0	-6.9 to -35.4
Boron Coefficient, pcm/ppm++	-16 to -7	-14 to -9	-14 to -8
Rodded Moderator Density Coefficient, pcm/gm/cm^3++	≤0.43 x 10^5	≤0.34 x 10^5	≤0.39 x 10^5

Delayed Neutron Fraction and Lifetime	Unit 1	Unit 2
β_{eff} BOL (EOL)	0.0075 (0.0044)	0.0075 (0.0047)
ℓ*, BOL (EOL) μ sec	25.0 (16.0)	25.0 (17.0)

\+ Uncertainties are given in Section 4.3.3.3

++ Note: 1 pcm = (percent millirho) = 10^{-5} $\Delta\rho$ where $\Delta\rho$ is calculated from two statepoint values of k_{eff} by ln (k_2\\k_1).

** Gigawatt Day (GWD) = 1000 Megawatt Day (1000 MWD). During the first cycle, fixed burnable poison rod are present which significantly reduce the boron depletion rate compared to reload cycles.

TABLE 4.3-2 (Continued)

NUCLEAR DESIGN PARAMETERS

(First Cycle)

Control Rods	Unit 1	Unit 2
Rod Requirements	See Table 4.3-3	See Table 4.3-3
Maximum Bank Worth, pcm	< 2000	< 2000
Maximum Ejected Rod Worth	See Chapter 15	See Chapter 15
Bank Worth, pcm++ (BOL)	BOL, Xe free	BOL, Xe free
Bank D	650	746
Bank C	1250	1308
Bank B	1200	1315
Bank Worth, pcm++ (EOL)	EOL Eq. Xe	EOL Eq. Xe
Bank D	750	748
Bank C	1450	1308
Bank B	1400	1199
Radial Factor (BOL to EOL)		
Unroded	1.41 to 1.28	1.36 to 1.24
D bank	1.50 to 1.45	1.44 to 1.35
D + C	1.60 to 1.45	1.59 to 1.34
D + C + B	1.80 to 1.55	1.66 to 1.56
Boron Concentrations, BOL, ppm		
Zero Power, K_{eff} = 0.99, Cold Rod Cluster Control Assemblies Out, clean	1080	1375
Zero Power, K_{eff} = 0.99, Hot, Rod Cluster Control Assemblies Out, clean	1030	1325

\+ Uncertainties are given in Section 4.3.3.3.

++ Note: 1 pcm = (percent millirho) = 10^{-5} $\Delta\rho$ where $\Delta\rho$ is calculated from two statepoint values of k_{eff} by ln ($K_2\backslash K_1$).

** Gigawatt Day (GWD) = 1000 Megawatt Day (1000 MWD). During the first cycle, fixed burnable poison rod are present which significantly reduce the boron depletion rate compared to reload cycles.

TABLE 4.3-2 (Continued)

NUCLEAR DESIGN PARAMETERS

(First Cycle)

Boron Concentrations (Cont'd)	Unit 1	Unit 2
Design Basis Refueling Boron Concentration	2500	2500
Zero Power, K_{eff} = 0.95, Cold Rod Cluster Control Assemblies In, Clean	910	1205
Zero Power, K_{eff} = 1.00, Hot Rod Cluster Control Assemblies Out, Clean	930	1222
Full Power, No Xenon, k_{eff} = 1.0, Hot, Rod Cluster Control Assemblies Out	835	1114
Zero Power, No Xenon k_{eff} = .99, Cold, Rod Cluster Control Assemblies In, Less	730	1009
Full Power, Equilibrium Xenon, k_{eff} = 1.0, Hot, Rod Cluster Control Assemblies Out	590	807
Reduction with Fuel Burnup First Cycle ppm/GWD/MTU**	See Figure 4.3-3	See Figure 4.3-3
Most Reactive Rod Stuck in Full Out Position		

\+ Uncertainties are given in Section 4.3.3.3.

++ Note: 1 pcm = (percent millirho) = 10^{-5} $\Delta\rho$ where $\Delta\rho$ is calculated from two statepoint values of k_{eff} by ln ($K_2 \backslash K_1$).

** Gigawatt Day (GWD) = 1000 Megawatt Day (1000 MWD). During the first cycle, fixed burnable poison rod are present which significantly reduce the boron depletion rate compared to reload cycles.

TABLE 4.3-3

REACTIVITY REQUIREMENTS FOR ROD CLUSTER CONTROL ASSEMBLIES

	Reactivity Effects, percent	Beginning-of Life (First Cycle)		End-of-Life (First Cycle)		End-of-Life (Equilibrium Cycle)
		Unit 1	Unit 2	Unit 1	Unit 2	Units 1 & 2
1.	Control requirements					
	Fuel temperature (Doppler), %Δρ	1.35	1.16	1.10	0.97	1.00
	Moderator Temperature *, %Δρ	0.20	0.30	1.15	1.05	1.30
	Redistribution, %Δρ	0.50	0.50	1.20	1.20	1.16
	Rod Insertion Allowance, %Δρ	0.60	0.60	0.60	0.60	0.85
2.	Total Control, %Δρ	2.65	2.56	4.05	3.82	4.31
3.	Estimated Rod Cluster Control Assembly Worth (57 Rods)					
	a. All assemblies inserted, %Δρ	9.50	9.09	9.50	8.68	7.58
	b. All but one (highest worth) assembly inserted, %ρ	8.00	7.78	8.00	7.41	6.62
4.	Estimated Rod Cluster Control Assembly credit with 10 percent adjustment to accommodate uncertainties (3b – 10 percent),%Δρ	7.20	7.00	7.20	6.67	5.95
5.	Shutdown margin available (4-2), %Δρ	4.55**	4.44**	3.15**	2.85**	1.64***

* Includes void effects

** The design basis minimum shutdown is 1.75%Δρ

*** The design basis minimum shutdown is 1.30%Δρ

TABLE 4.3-4

COMPARISON OF REACTIVITY REQUIREMENTS

		End of Life Typical 12 mo. Cycle		End of Life (Typical 18 mo. Cycle)	
		3853 MWt 168-inch fuel	3411 MWt 144 - inch fuel	3853 MWt 168 – inch fuel	3411 MWt 144 – inch fuel
1.	Control Requirements				
	a. Fuel Temperature (Doppler), %Δρ + Moderator Temperature, %Δρ + Void, %Δρ + Rod Insertion Allowance, %Δρ	2.95	2.94	3.15	2.56
	b. Redistribution, %Δρ	1.20	0.85	1.16	0.85
2.	Total Control, %Δρ	4.15	3.79	4.31	3.41
3.	Estimated Rod Cluster Control Assembly Worth				
	a. Number of Control Rod Clusters	57	53	57	53
	b. Worth of all assemblies, %Δρ	8.50	7.30	7.58	6.92
	c. Worth of all but one Assembly (highest worth), %Δρ	6.90	6.20	6.62	6.04
4.	Estimated Rod Cluster Control Assembly credit with 10 percent adjustment to accommodate uncertainties (3c – 10 percent), %Δp	6.20	5.58	5.95	5.44
5.	Shutdown Margin Available (4-2), %Δρ	2.05[a]	1.79[b]	1.64[©]	2.03[b]

a. The design basis minimum shutdown is 1.75%Δρ

b. The design basis minimum shutdown is 1.60%Δρ

c. The design basis minimum shutdown is 1.30%Δρ

TABLE 4.3-5

EXPECTED REACTIVITY PARAMETER RANGE

	3853 MWt 168-inch Active Length	3411 MWt 144 – inch Active Length
β_{eff}	0.0075 to 0.0044	0.0075 to 0.0044
1*, μsec	25.0 to 16.0	19.4 to 18.1
Doppler, pcm/°F	-2.9 to –1.1	-2.9 to –1.1
Moderator, pcm/°F	0 to –40	0 to -40
Boron Worth, pcm/ppm	-16 to –7	-16 to -7

TABLE 4.3-6

BENCHMARK CRITICAL EXPERIMENTS

Description of Experiments*	Number of Experiments	LEOPARD k_{eff} Using Experimental Bucklings
UO_2		
Al clad	14	1.0012
SS clad	19	0.9963
Berated H_2O	7	0.9989
Subtotal	40	0.9985
U-Metal		
Al clad	41	0.9995
Unclad	20	0.9990
Subtotal	61	0.9993
Total	101	0.9990

* Reported in Reference 4.3-14

TABLE 4.3-7

AXIAL STABILITY INDEX PRESSURIZED WATER REACTOR CORE WITH A 12-FOOT HEIGHT

Burnup (MWD/MTU)	F_Z	C_B (ppm)	Stability Index (hr^{-1}) Exp	Calc
1,550	1.34	1,065	-0.041	-0.032
7,700	1.27	700	-0.014	-0.006
		Difference:	+0.027	+0.026

TABLE 4.3-8

TYPICAL NEUTRON FLUX LEVELS (n/cm^2-sec) AT FULL POWER

	E> 1.0MeV	0.111MeV < E <1.0MeV	0.3eV ≤ E < 0.111 MeV	E < 0.3eV
Core Center	9.98×10^{13}	1.11×10^{14}	2.17×10^{14}	5.36×10^{13}
Core Outer Radius At Mid-Height	4.24×10^{13}	4.85×10^{13}	9.52×10^{13}	2.21×10^{13}
Core Top, on Axis	2.62×10^{13}	2.13×10^{13}	1.31×10^{14}	$4.35 \times 10^{13+}$
Core Bottom, on Axis	2.70×10^{13}	2.25×10^{13}	1.33×10^{14}	4.74×10^{13}
Pressure Vessel Inner Diameter Aximuthal Peak, Core Mid-Height	2.08×10^{10}	2.83×10^{10}	6.18×10^{10}	1.20×10^{11}

TABLE 4.3-9

COMPARISON OF MEASURED AND CALCULATED DOPPLER DEFECTS

Fuel Type	Core Burnup (MWD/MTU)	Measured (pcm)*	Calculated (pcm)
Air-filled	1,800	1,700	1,710
Air-filled	7,700	1,300	1,440
Air and helium-filled	8,460	1,200	1,210

* pcm = 10^{-5} x 1n (k_1/k_2)

TABLE 4.3-10

SAXTON CORE II ISOTOPICS ROD MY, AXIAL ZONE 6

Atom Ratio	Measured*	2σPrecision (%)	LEOPARD Calculation
U-234/U	4.65×10^{-5}	± 29	4.60×10^{-5}
U-235/U	5.74×10^{-3}	± 0.9	5.73×10^{-3}
U-236/U	3.55×10^{-4}	± 5.6	3.74×10^{-4}
U-238/U	0.99386	± 0.01	0.99385
Pu-238/Pu	1.32×10^{-3}	± 2.3	1.222×10^{-3}
Pu-239/Pu	0.73971	± 0.03	0.74497
Pu-240/Pu	0.19302	± 0.2	0.19102
Pu-241/Pu	6.014×10^{-2}	± 0.3	5.74×10^{-2}
Pu-242/Pu	5.81×10^{-3}	± 0.9	5.38×10^{-3}
Pu/U**	5.938×10^{-2}	± 0.7	5.970×10^{-2}
Np-237/U-238	1.14×10^{-4}	± 15	0.86×10^{-4}
Am-241/Pu-239	1.23×10^{-2}	± 15	1.08×10^{-2}
Cm-242/Pu-239	1.05×10^{-4}	± 10	1.11×10^{-4}
Cm-244/Pu-239	1.09×10^{-4}	± 20	0.98×10^{-4}

* Reported in Reference 4.3-29

** Weight ratio

TABLE 4.3-11

CRITICAL BORON CONCENTRATIONS, HZP, BOL

Plant Type	Measured	Calculated
2-Loop, 121 Assemblies 10 foot core	1,583	1,589
2-Loop, 121 Assemblies 12 foot core	1,625	1,624
2-Loop, 121 Assemblies 12 foot core	1,517	1,517
3-Loop, 157 Assemblies 12 foot core	1,169	1,161

TABLE 4.3-12

COMPARISON OF MEASURED AND CALCULATED ROD WORTH

2-Loop, Plant
121 Assemblies

10 foot core	Measured (pcm)	Calculated (pcm)
Group B	1,885	1,893
Group A	1,530	1,649
Shutdown Group	3,050	2,917

ESADA-Critical*, 0.69 in. Pitch,
2 w/o PuO_2, 8% Pu-240,

9 Control Rods		
6.21 in. rod separation	2,250	2,250
2.07 in. rod separation	4,220	4,160
1.38 in. rod separation	4,010	4,010

* Reported in Reference 4.3-30

TABLE 4.3-13

COMPARISON OF MEASURED AND CALCULATED MODERATOR COEFFICIENTS AT HZP, BOL

Plant Type/ Control Bank Configuration	Measured α_{iso}* (pcm/°F)	Calculated α_{iso} (pcm/°F)
3-Loop, 157 Assemblies,		
12 foot core		
D at 160 steps	-0.50	-0.50
D in, C at 190 steps	-3.01	-2.75
D in, C at 28 steps	-7.67	-7.02
B, C, and D in	-5.16	-4.45
2-Loop, 121 Assemblies		
12 foot core		
D at 180 steps	+0.85	+1.02
D in, C at 180 steps	-2.40	-1.90
C and D in, B at 165 steps	-4.40	-5.58
B, C, and D in A at 174 steps	-8.70	-8.12

* Isothermal coefficients, which include the Doppler effect in the fuel.

$$\alpha_{iso} = 10^5 \ln \left(\frac{k_2}{k_1}\right) / \Delta T \text{ °F}$$

4.4 THERMAL AND HYDRAULIC DESIGN

4.4.1 Design Bases

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 performances and safety criteria requirements are met:

1. Fuel damage (defined as penetration of the fission product barrier; i.e., the fuel rod clad) 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 (see above definition) although sufficient fuel damage might occur to preclude immediate resumption of operation.

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.

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

There will be at least a 95 percent probability that departure from nucleate boiling (DNB) will not occur on the limiting fuel rods during normal operation, operational transients, and any transient conditions arising from faults of moderate frequency (Conditions I and II events), at a 95 percent confidence level. For this application, the design limit departure from nucleate boiling ratio (DNBR) values for 17x17XL STD, V5H and VANTAGE+ fuel are 1.26 for typical cells and 1.24 for thimble cells based on the Revised Thermal Design Procedure (RTDP) and the WRB-1 DNB correlation. For use in the DNB safety analyses, the limit DNBR is conservatively increased to provide DNB margin to offset the effect of rod bow and any other DNB penalties that may occur, and to provide flexibility in design and operation of the plant. Safety analysis limit DNBR values of 1.43 for typical cells and 1.38 for thimble cells are employed in the analysis.

The design limit DNBR values for RFA fuel are 1.24 for typical cells and 1.23 for thimble cells based on RTDP (Ref. 4.4-3b) and WRB-2M DNB correlation. For use in the DNB safety analyses, the limit DNBR is conservatively increased to provide DNB margin to offset the effect of rod bow and any other DNB penalties that may occur, and to provide flexibility in design and operation of the plant. Safety analysis limit DNBR values of 1.52 for typical cells and 1.52 for thimble cells are employed in the analysis.

Discussion

The thermal-hydraulic analysis for 17x17XL STD, V5H and VANTAGE+ fuel uses the WRB-1 DNB correlation (Ref. 4.4-5), and incorporates RTDP (Ref 4.4-3b) and Improved THINC-IV Modeling (Ref. 4.4-3c). The thermal-hydraulic analysis for Robust Fuel Assemblies (RFA) uses the VIPRE-01 computer code (Ref. 4.4-88 and 4.4-89) with the WRB-2M DNB correlation (Ref. 4.4-90) and the RTDP. The W-3 correlation (Ref. 4.4-3 and 4.4-31) and standard methods are used when conditions are outside the range of the WRB-1 or WRB-2M correlation and the RTDP.

The WRB-1 DNB correlation is based entirely on rod bundle data and takes credit for the significant improvement in the accuracy of the critical heat flux predications over previous DNB correlations. The WRB-1 DNB correlation is applicable to VANTAGE 5H fuel since, from a DNB perspective, the VANTAGE 5H assembly is virtually identical to the 17x17 Inconel R-Grid design. As documented in Ref. 4.4-3d the use of the WRB-1 DNB correlation with a 95/95 DNBR limit of 1.17 is applicable to the VANTAGE 5H fuel assembly. The 1.17 DNBR limit is the same as the 95/95 limit for STD fuel.

The WRB-2M DNB correlation (Ref. 4.4-90) was developed based on DNB testing of the RFA. This correlation is used to predict DNBR for the RFAs whenever the conditions are in the applicable range of the parameters. The DNBR correlation limit is established based on the variance of the correlation data such that there is 95 percent probability with a 95 percent confidence level that DNB will not occur when the calculated DNBR is at this limit. The WRB-2M correlation limit is equal to 1.14.

With RTDP methodology, variations in plants operating parameters, nuclear and thermal parameters, fuel fabrication parameters, and DNB correlation predictions are considered statistically to obtain the overall DNBR uncertainty factor which is used to define the design limit DNBR that satisfies the DNB design criterion. This criterion is that the probability that DNB will not occur on the most limiting fuel rod is at least 95 percent (at 95 percent confidence level) for any Condition I or II event. Since the uncertainties are all included in the uncertainty factor, the accident analysis is done with input parameters at their nominal or best estimate values. RTDP analyses use minimum measured flow (MMF) equal to thermal design flow (TDF) plus a flow uncertainty. Analyses by standard methods continue to use TDF.

Any 17X17XL STD, V5H or VANTAGE+ fuel that is present in the uprate (3853 MWt) cores will have a significant amount of burnup and thus reduced F-delta-H values. These fuel assemblies will be analyzed using RTDP and the WRB-1 correlation, with DNBR design limits equal to 1.26 for thimble cells and 1.24 for typical cells, DNBR safety analysis limits of 1.43 for typical cells and 1.38 for thimble cells, the Improved THINC-IV modeling (Ref. 4.4-3c), and a value of F-delta-H in the COLR that gives DNBRs that meet the DNBR design limits.

The improved THINC IV modeling scheme improves the accuracy of the solution by minimizing the inaccuracies, which result from the use of the perturbation technique in the solution of the governing equations.

For conditions outside the range of parameters for the WRB-2M correlation (refer to Section 4.4.2.2.1), the WRB-1 or W-3 correlation is used. For the W-3 correlation, a DNBR design limit of

1.30 applies for pressures equal to or greater than 1,000 psia. For low pressure (500-1,000 psia) applications of the W-3 correlation, a design limit DNBR of 1.45 applies (Ref. 4.4-9).

By preventing DNB, adequate heat transfer is assured between the fuel clad and the reactor coolant, thereby preventing clad damage as a result in 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.

Steam generator tube plugging combined with the effect of hot leg streaming on precision calorimetric flow measurement, has the potential to reduce minimum measured flow below the required 392,300 gpm. Consequently, the South Texas Project has implemented a change to the Technical Specifications allowing a 3 percent reduction in the minimum measured flow requirement on an as-needed basis. To allow continued operation at currently licensed power level with the indicated flow reduced by 3 percent, existing margins are used. However, to offset the decrease in DNB margin:

- The upper end of the nominal RCS average temperature range is reduced from 593°F to 590°F;

- The K1 and K4 terms in the OTDT and the OPDT reactor trip setpoints are reduced;

- The LCO maximum average RCS temperature is reduced from 598°F to 595°F;

- The lower end of the nominal RCS average temperature is raised from 582.3°F to 583.2°F; and

- The start of the axial offset penalty f (ΔI) is moved from 8 percent to 6 percent.

This alternative operating condition with 3 percent reduction in RCS flow will not appreciably affect the normal plant operating parameters. The reduction will not affect system actuation, accident mitigating capabilities, or assumptions important to the analyses. Because post-accident assumptions are not impacted, the proposed change will not create conditions more limiting that those assumed in the analyses. This condition is bounded by the design basis of the plant.

This provision is not only to be used for the affected unit in the event that Technical Specification RCS flow requirements for normal operation can not be met, while maintaining the licensed power output level.

4.4.1.2 Fuel Temperature Design Basis.

Basis

During modes of operation associated with Condition I and Condition II events, there is at least a 95 percent probability that the peak kW/ft fuel rods will not exceed the uranium dioxide melting temperature at the 95 percent confidence level. The melting temperature of uranium dioxide is taken as 5080°F (Ref. 4.4-1), unirradiated and decreasing 58°F per 10,000 MWD/MTU. By precluding uranium dioxide melting, the fuel geometry is preserved and possible adverse effects of molten uranium dioxide 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 over power limit. This provides sufficient margin for uncertainties in the thermal evaluation as described in Section 4.4.2.9.1.

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 events given in Chapter 15.

4.4.1.3 Core Flow Design Basis.

Basis

A minimum of 91.5 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 8.5 percent of this value is allotted as bypass flow. This includes rod cluster control guide thimble cooling flow, head cooling flow, baffle leakage, and leakage to the vessel outlet nozzle.

4.4.1.4 Hydrodynamic Stability Design Basis.

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 are sufficiently comprehensive that 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 design criteria are met. For instance, the fuel rod diametral gap characteristics change with time (Section 4.2.3.3) and the fuel rod integrity is evaluated on that basis. The effect of the moderator flow velocity and distribution (Section 4.4.2.2) and moderator void distribution (Section 4.4.2.4) 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 4.2.3.3, 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 (Section 15.6.5), control rod ejection accident, and locked rotor accident.

4.4.2 Description

HISTORICAL

4.4.2.1 Summary Comparison. Table 4.4-1 provides a comparison of the design parameters for the core described herein with those given in the W. B. McGuire Units 1 and 2 design.

Examination of the tables demonstrates that in progressing from the standard 12 ft core to the current model a relatively small perturbation has been made in the core hardware.

The fundamental difference in core geometry between this application and previous 17 x 17 submittals is an increase of 24 inches in the nominal active fuel length from 144 to 168 inches. Note that the actual fuel rod is 176.64 inches long (upgraded fuel rod length is 176.86 inches and RFA fuel rod length is 177.61 inches).

This increase in the active fuel length allows the average and linear heat generation rate (kW/ft) and heat flux to remain approximately the same for the 168-in. core with 3800 MWth power rating as that of a 144-in., 3411 MWth core, (Table 4.4-1). A slightly higher peak linear heat generation rate and heat flux results from a higher design peaking factor, $F_Q = 2.7$. The 14-ft core uses two more grids (10) than the 12-ft core (8).

HISTORICAL

4.4.2.2 Critical Heat Flux Ratio or DNBR and Mixing Technology. The minimum DNBR for the rated power, design overpower, and anticipated transient conditions are given in Table 4.1-1. 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.

The DNBRs are calculated by using the correlation and definitions described in Sections 4.4.2.2.1 and 4.4.2.2.2. The THINC-IV (Refs. 4.4-18 and 4.4-49) or VIPRE-01 (Refs. 4.4-88 and 4.4-89) computer code 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 4.4.4.3.1 (nuclear hot channel factors) and in Section 4.4.2.2.4 (engineering hot channel factors).

4.4.2.2.1 DNB Technology: Early experimental studies of DNB were conducted with fluid flowing inside single heated tubes or channels and with single annulus configurations with one or both walls heated. The results of the experiments were analyzed using many different physical models for describing the DNB phenomenon, but all resultant correlations are highly empirical in nature. The evolution of these correlations is given by Tong (Refs. 4.4-2 and 4.4-3), including the W-3 correlation which is in wide use in the pressurized water reactor (PWR) industry.

As testing methods progressed to the use of rod bundles instead of signal channels, it became apparent that the bundle average flow conditions cannot be used in DNB correlations. As outlined by Tong (Ref. 4.4-4) test results showed that correlations based on average conditions were not accurate predictors of DNB heat flux. This indicated that a knowledge of the local subchannel conditions within the bundle is necessary.

It is also noted that in this power capability evaluation, there has not been any change in the design basis. The reactor is designed to a minimum DNBR greater than or equal to the design limit DNBR as well as no fuel centerline melting during normal operation, operational transients, and faults of moderate frequency.

Fuel densification has been considered in the DNB and fuel temperature evaluations utilizing the methods and models described in detail in References 4.4-6 and 4.4-86.

In order to determine the local subchannel conditions, the THINC (Ref. 4.4-7) computer code was developed. In the THINC code, a rod bundle is considered to be an array of subchannels each of which includes the flow area formed by four adjacent rods. The subchannels are also divided into axial steps such that each may be treated as a control volume. By solving simultaneously the mass, energy, and momentum equations, the local fluid conditions in each control volume are calculated. The W-3 correlation, developed from single channel data, can be applied to rod bundles by using the subchannel local fluid conditions calculated by the THINC code.

The VIPRE-01 computer code (Ref. 4.4-88 and 4.4-89) 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 expected steady state operating conditions. VIPRE-01, which replaces the THINC-IV computer code for analysis of RFA at uprate (3,853 MWt) conditions, has had extensive experimental verification and comparisons with other licensed codes, and is considered a best estimate code. The DNBR predictions are very close to those predicted by THINC-IV. VIPRE-01 is licensed with the NRC as an acceptable model for performing thermal-hydraulic calculations. The W-3 correlation, developed from single channel data, can be applied to rod bundles by using the subchannel local fluid conditions calculated by the VIPRE-01 code.

It was shown by Tong (Ref. 4.4-4) that the above approach yielded conservative predictions, particularly in rod bundles with mixing vane grid spacers. The WRB-1 (Ref. 4.4-5) correlation was developed based exclusively on the large bank of mixing vane grid rod bundle CHF data (over 1,100 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 cell effects, uniform and nonuniform heat flux profiles, and variations in rod heated length and in grid spacing.

The applicable range of WRB-1 variables is:

Pressure	$1,440 \leq P \leq 2,490$ 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	$L_h \leq 14$ ft
Grid Spacing	$13 \leq g_{sp} \leq 32$ in.
Equivalent Hydraulic Diameter	$0.37 \leq d_e \leq 0.60$ in.
Equivalent Heated Hydraulic Diameter	$0.46 \leq d_h \leq 0.58$ in.

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

The WRB-1 correlation is applicable for the South Texas 17 x 17 fuel assembly with "R" mixing vane grid design, as well as for the V5H VANTAGE+ and RFA design (Ref. 4.4-91).

The WRB-2M DNB correlation is applicable to the RFA. This correlation gives improved DNBR predictions compared to the WRB-1-DNB correlation. The applicable range of WRB-2M variables is:

Pressure	$1{,}495 \leq P \leq 2{,}495$ psia
Local Mass Velocity	$0.97 \leq G_{loc}/10^6 \leq 3.1$ lb/ft^2-hr
Local Quality	$-0.1 \leq X_{loc} \leq 0.29$ [1]
Heated Length, Inlet to CHF Location	$L_H \leq 14$ ft
Grid Spacing	$10 \leq g_{sp} \leq 20.6$ in.
Equivalent Hydraulic Diameter	$0.37 \leq D_e \leq 0.46$ in.
Equivalent Heated Hydraulic Diameter	$0.46 \leq D_H \leq 0.54$ in.

[1] Use of the WRB-2M correlation has been conservatively modified to utilize a penalty above a certain high local quality threshold within approved ranges (Reference 4.4-92).

As stated in Section 4.4.1.1 Westinghouse has chosen the design criterion that DNB will not occur at a 95 percent probability with a 95 percent confidence level.

4.4.2.2.2 Definition of Departure from Nucleate Boiling Ratio (DNBR): The DNB heat flux ratio (DNBR) as applied to this design when all flow cell walls are heated is:

$$DNBR = \frac{q''_{DNB,N}}{q''_{loc}} \qquad \text{(Eq. 4.4-4)}$$

where:

$q''_{DNB,N}$ is the heat flux predicted by the applicable DNB correlation.

For the W-3 correlation,

$$q''_{DNB,N} = \frac{q''_{DNB,EU}}{F} \qquad \text{(Eq. 4.4-5)}$$

and $q''_{DNB,Eu}$ is the uniform DNB heat flux as predicted by the W-3 DNB correlation, (Ref. 4.4-10) all flow cell walls are heated.

F is the flux shape factor to account for nonuniform axial heat flux distributions, (Ref. 4.4-10), with the “C” term modified as in Reference 4.4-3.

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

The DNB heat flux ratio as applied to this design when a cold wall is present is:

$$\mathrm{DNBR} = \frac{q''_{\mathrm{DNB,N,CW}}}{q''_{\mathrm{loc}}} \qquad \text{(Eq. 4.4-6)}$$

where:

$$q''_{\mathrm{DNB,N,CW}} = \frac{q''_{\mathrm{DNB,EU,Dh}} {}^{x\,\mathrm{CWF}}}{\mathrm{F}} \qquad \text{(Eq. 4.4-7)}$$

where:

$q''_{DNB,EU,Dh}$ is the uniform DNB heat flux as predicted by the W-3 cold wall DNB correlation, (Ref. 4.4-3), when not all flow cell walls are heated (thimble cold wall cell).

$$CWF\ (\mathrm{Re}\,f.\,4.4-3) = 1.0 - Ru\left[13.76 - 1.37e^{1.78x} - 4.732\left(\frac{(G)}{10^{6}}\right)^{0.0535} - 0.0619\frac{(p)}{1000}^{0.e14} - 8.509Dh^{0.107}\right] \qquad \text{(Eq. 4.4-8)}$$

and Ru = 1 – De/Dh

For the WRB-1 correlation,

$$q''_{\mathrm{DNB,N}} = \frac{q''_{\mathrm{WRB-1}}}{\mathrm{F}} \qquad \text{(Eq. 4.4-8a)}$$

where F is the same flux shape factor that is used with the W-3 correlation.

For the WRB-2M correlation,

$$q''_{\mathrm{DNB,N}} = \frac{q''_{\mathrm{WRB-2M}}}{\mathrm{F}} \qquad \text{(Eq. 4.4-8b)}$$

where F is the same flux shape that is used with the W-3 correlation.

4.4.2.2.3 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 of the local fluid density and flow velocity. The proportionality is expressed by the dimensionless thermal diffusion coefficient (TDC) which is defined as:

$$\mathrm{TDC} = \frac{\mathrm{W}'}{\rho \mathrm{Va}}. \qquad \text{(Eq. 4.4-9)}$$

where:

w′	= flow exchange rate per unit length, lb_m/ft-sec
ρ	= fluid density, lb_m/ft^3
V	= fluid velocity, ft/sec
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 4.4-7. The application of the TDC in the VIPRE-01 analysis is presented in Reference 4.4-89.

As a part of a research and development program, Westinghouse has sponsored and directed mixing tests at Columbia University (Ref. 4.4-12). 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 to 642°F
Mass velocity	1.0 – 3.5 x 10^6 $lb_m/hr\text{-}ft^2$
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-in. axial grid spacing are presented on Figure 4.4-4 where the TDC is plotted versus the Reynolds number. TDC 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 (Ref. 4.4-12), Rowe and Angle (Refs. 4.4-13 and 4.4-14) and Gonzalez – Santalo and Griffith Ref. 4.4-15). 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 PWR reactor 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 or VIPRE –01 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 (Ref. 4.4-16). 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 19.8 inches, additional margin is available for this design, as the value of TDC increases as grid spacing decreases (Ref. 4.4-12).

4.4.2.2.4 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 factor (Section 4.4.4.3) 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; 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 heat flux. This subfactor is determined by statistically combining the tolerances for the fuel pellet diameter, density enrichment, eccentricity, and the fuel rod diameter, and has a value of 1.03. Measured manufacturing data on recent Westinghouse 17 x 17 fuel were used to verify that this value was not exceeded for 95 percent of the limiting fuel rods at a 95 percent confidence level. Thus, it is expected that a statistical sampling of the fuel assemblies of this plant will yield a value no larger than 1.03.

Enthalpy Rise Engineering Hot Channel Factor. $F_{\Delta H}^E$

The effect of variations in flow conditions and fabrication tolerances on the hot channel enthalpy rise is directly considered in the THINC or VIPRE-01 core thermal subchannel analysis (Section 4.4.4.5.1) under any reactor operating condition. The items considered contributing to the enthalpy rise engineering hot channel factor are discussed below:

1. Pellet diameter, density, and enrichment

Design values employed in the THINC or VIPRE-01 analysis related to the above fabrication variations are based on applicable limiting tolerances such that these design values are met for 95 percent of the limiting channels at a 95 percent confidence level. Measure manufacturing data on Westinghouse 17 x 17 fuel show the tolerances used in this evaluation are conservative. The effect of variations in pellet diameter, enrichment, and density is employed in the THINC or VIPRE-01 analysis as a direct multiplier on the hot channel enthalpy rise.

2. Inlet Flow Maldistribution

The consideration of inlet flow maldistribution in core thermal performances is discussed in Section 4.4.4.2.2. A design basis of 5 percent reduction in coolant flow to the hot assembly is used in the THINC-IV or VIPRE-01 analysis.

3. 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 or VIPRE-01 analysis for every operating condition which is evaluated.

4. Flow Mixing

The subchannel mixing model incorporated in the THINC or VIPRE-01 Code and used in reactor design is based on experimental data (Ref. 4.4-17) discussed in Section 4.4.4.5.1. 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.

4.4.2.2.5 Effect of Rod Bow on DNBR: The phenomenon of fuel rod bowing, as described in Reference 4.4-84, 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 DNBR and/or margin obtained from measured plant operating parameters (such as $F^N_{\Delta H}$ 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.

The safety analysis for South Texas cores maintained sufficient margin between the safety analysis DNBR limit and the design DNBR limit to accommodate full and low flow DNBR penalties identified in Reference 4.4-85 with the incorporation of the L^2/I scaling factor (I = fuel rod bending moment of inertia, L = span length) to account for 17 x 17 XL span lengths.

The maximum rod bow penalties accounted for in the design safety analysis are based on an assembly average burnup of 24,000 MWD/MTU. At burnups greater than 24,000 MWD/MTU, credit is taken for the effect of $F^N_{\Delta H}$ burndown, due to the decrease in fissionable isotopes and the buildup of fission product inventory, and no additional rod bow penalty is required (Ref. 4.4-8).

4.4.2.2.6 Effect of Loop Temperature Asymmetry on DNBR: The phenomenon of loop temperature asymmetry, as described in Reference 4.4-87, 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 DNBR and/or margin obtained from measured plant operating parameters (such as $F_{\Delta H}$ or core flow), which are less limiting than those by the plant safety analysis, can be used to offset the effect of loop temperature assymetry.

If generic DNBR margin is not available and if the plant surveillance for FDH or core flow indicates that there is insufficient margin ($F_{\Delta H}$ or core flow margin) available, then loop-specific Tavg calibration may be implemented. If loop-specific Tavg calibrations are implemented, then the affected DNBR safety analyses must allow for a 1°F deviation between measured and calibrated loop Tavg values when at the reference condition.

Parameter limits will be documented in the cycle-specific Core Operating Limits Report. The margins will be evaluated on a cycle-specific basis and will be documented as part of the cycle reload safety evaluation.

4.4.2.3 Linear Heat Generation Rate. The core average and maximum linear powers are given in Table 4.1-1. The method of determining the maximum linear powers is given in Section 4.3.2.2.

4.4.2.4 Void Fraction Distribution. The void fraction distribution in the core at various radial and axial locations is presented in Reference 4.4-18, based on THINC-IV predictions. The void models used in the THINC-IV and VIPRE-01 computer codes are described in Section 4.4.2.7.3. Normalized core flow and enthalpy rise distributions based on THINC-IV predictions are shown on Figures 4.4-5 through 4.4-7 for the first core.

4.4.2.5 Core Coolant Flow Distribution. Assembly average coolant mass velocity and enthalpy at various radial and axial core locations based on THINC-1V are given below. Coolant enthalpy rise and flow distributions are shown for the one-third core height elevation of Figure 4.4-5, and two-thirds core height elevation on Figure 4.4-6 and at the core exit on Figure 4.4-7. These distributions are for the full power conditions as given in Table 4.1-1 and for the radial power density distribution shown on Figure 4.4-7. The THINC code analysis for this case utilized a uniform core inlet enthalpy and inlet flow distribution. No orificing is employed in the reactor design.

4.4.2.6 Core Pressure Drops and Hydraulic Loads.

4.4.2.6.1 Core Pressure Drops: The analytical model and experimental data used to calculate the pressure drops shown in Table 4.1-1 are described in Section 4.4.2.7. The core pressure drop includes the fuel assembly, lower core plate, and upper core plate pressure drops. The full power operation pressure drop values shown in Table 4.1-1 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 for actual plant operating conditions as described in Section 5.1.1. This section 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 4.1-1 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 4.4.2.9.2.

4.4.2.6.2 Hydraulic Loads: The fuel assembly hold down 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 hold down 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 loss of coolant accident. These conditions are presented in Section 15.6.5.

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 based on the cold mechanical design flow, but are adjusted to account for the coolant density difference. The cold shutdown liftoff force calculation supporting the implementation of XL-WIN top nozzles assumes that the RCS temperature is at or above 140°F prior to starting the fourth reactor coolant pump. Conservative core hydraulic loads for a pump overspeed transient, which could possibly create flow rates 18 percent greater than the mechanical design flow, are evaluated to be approximately twice the fuel assembly weight.

Core hydraulic loads were measured during the prototype assembly tests described in Section 1.5. Reference 4.4-19 contains a detailed discussion of the results.

4.4.2.7 Correlation and Physical Data.

4.4.2.7.1 Surface Heat Transfer Coefficients: Forced convection heat transfer coefficients are obtained from the familiar Dittus-Boelter correlation (Ref. 4.4-20) with the properties evaluated at a bulk fluid conditions:

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

where:

h	= heat transfer coefficient, Btu/hr-ft^2-°F
D_e	= equivalent diameter, ft
K	= thermal conductivity, Btu/hr-ft-°F
G	= mass velocity, lbm/hr-ft^2
μ	= dynamic velocity, lbm/ft-hr
Cρ	= heat capacity, Btu/lbm-°F

This correlation has been shown to be conservative (Ref. 4.4-21) 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 correlation (Ref. 4.4-22). After this occurrence the outer clad wall temperature is determined by:

$$\Delta T_{sat} = \left[0.072 \exp\left(-P/1260\right)\right]\left(q''\right)^{0.5} \quad \text{(Eq. 4.4-11)}$$

where:

ΔT_{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

4.4.2.7.2 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. Two-phase flow considerations in the core thermal subchannel analyses are considered and the models are discussed in Section 4.4.4.2.3. Core and vessel pressure losses are calculated by equations of the form:

$$\Delta P_L = \left(K + F \left(\frac{L}{De} \right) \right) \frac{\rho v^2}{2 g_c (144)} \quad \text{(Eq. 4.4-12)}$$

Where:

ΔP_L = unrecoverable pressure drop, lb/in^2
ρ = fluid density, lbm/ft^3
L = length, ft
D_e =equivalent diameter, ft
V = fluid velocity, ft/sec
g_c = $32.174 \dfrac{\text{lbm - ft}}{\text{lb - sec}^2}$
K = from 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 from and friction loss coefficients are not available. Therefore, experimental values for these coefficients are obtained from geometrically similar models.

Values are quoted in Table 4.1-1 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 (Refs. 4.4-23 and 4.4-24) and from loss relationships (Ref. 4.4-25). Moody curves (Ref. 4.4-26) were used to obtain the single phase friction factors.

Tests of the primary coolant loop flow rates will be made (Section 4.4.5.1) 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.

4.4.2.7.3 Void Fraction Correlation: For THINC-IV there are three separate void regions considered in flow boiling in a PWR as illustrated on Figure 4.4-8. They are the wall void region (no bubble detachment), the subcooled boiling region (bubble detachment), and the bulk boiling region.

In the THINC-IV wall void region, the point where local boiling begins is determined when the clad temperature reaches the amount of superheat predicted by Thom's correlation (Ref. 4.4-22) discussed in Section 4.4.4.2.7.1. The void fraction in this region is calculated using Maurer's relationship (Ref. 4.4-27). The bubble detachment point, where the superheated bubbles break away from the wall, is determined by using Griffith's relationship (Ref. 4.4-28).

The THINC-IV void fraction in the subcooled boiling region (that is, after the detachment point) is calculated from the Bowring correlation (Ref. 4.4-29). This correlation predicts the void fraction from the detachment point to the bulk boiling region.

VIPRE-01 considers two-phase flow in two steps. First, a quality model is used to compute the flowing vapor mass fraction (true quality) including the effects of subcooled boiling. Then, given the true void quality, a bulk void model applied to compute the vapor volume fraction (void fraction).

VIPRE-01 uses a profile fit model (Ref. 4.4-89) for determining subcooled quality. It calculates the local vapor volumetric fraction in forced convection boiling by 1) predicting the point of bubble departure from the heated surface and 2) postulating a relationship between the true local vapor fraction and the corresponding thermal equilibrium value.

The void fraction for THINC-IV or VIPRE 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.

4.4.2.8 Thermal Effects of Operational Transients. The 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 safety limit DNBR. Figure 15.0-1C shows limit lines for DNBR equal to or greater than the safety limit DNBR and the resulting overtemperature ΔT trip lines (which become part of the Technical Specifications), plotted as ΔT versus T_{avg} 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 15.4.2]) specific protection functions are provided as described in Section 7.2 and the use of these protection functions is described in Chapter 15.

4.4.2.9 Uncertainties in Estimates.

4.4.2.9.1 Uncertainties in Fuel and Clad Temperature: As discussed in Section 4.4.2.11, 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 (Refs. 4.4-30 through 4.4-36), by out-of-pile measurements of the fuel and clad properties (Refs. 4.4-37 through 4.4-48), 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 References 4.4-6 and 4.4-86.

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 4.3.2.2.1.

The reactor trip setpoints 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.

4.4.2.9.2 Uncertainties in Pressure Drops: Core and vessel pressure drops based on the best estimate flow, as described in Section 5.1, are quoted in Table 4.1-1. 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 4.4.5.1, tests on the primary system prior to initial criticality verified that a conservative primary system coolant flow rate has been used in the design and analyses of the plant.

4.4.2.9.3 Uncertainties due to Inlet Flow Maldistribution: The effects of uncertainties in the inlet flow maldistribution criteria used in the core thermal analyses is discussed in Section 4.4.4.2.2.

4.4.2.9.4 Uncertainty in DNB Correlation: The uncertainty in the DNB correlation (Section 4.4.2.2) can be written as a statement of the probability of not being in DNB based on the statistics of the DNB data. This is discussed in Section 4.4.2.2.2.

4.4.2.9.5 Uncertainties in DNBR Calculations: The uncertainties in the DNBRs calculated by THINC or VIPRE-01 analysis (Section 4.4.4.5.1) 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 addition, conservative values for the engineering hot channel factors are used as discussed in Section 4.4.2.2.4. The results of a sensitivity study (Ref. 4.4-18) with THINC-IV, a VIPRE-01 equivalent code, 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 $F^{N}_{\Delta H}$).

The ability of the THINC-IV computer code to accurately predict flow and enthalpy distributions in rod bundles is discussed in Section 4.4.4.5.1 and in Reference 4.4-49. Studies have been performed (Ref. 4.4-18) to determine the sensitivity of the minimum DNBR in the hot channel to the void fraction correlation (Section 4.4.2.7.3); 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.

The ability of the VIPRE-01 computer code to accurately predict flow and enthalpy distributions in rod bundles is discussed in 4.4.4.5.1 and in Ref. 4.4-89. Studies (Ref. 4.4-88) have been performed to determine the sensitivity of the minimum DNBR to the void fraction correlation (see also Section 4.4.2.7.3) and the inlet flow distributions. The results of these studies show that the minimum DNBR is relatively insensitive to variation in these parameters. Furthermore, the VIPRE-01 flow field model for predicting conditions in the hot channels is consistent with that used in the derivation of the DNB correlation limits including void/quality modeling, turbulent mixing and crossflow, and two phase flow (Ref. 4.4-89).

4.4.2.9.6 Uncertainties in Flow Rate: 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. In addition, another 8.5 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 4.4.4.2.1.

4.4.2.9.7 Uncertainties in Hydraulic Loads: As discussed in Section 4.4.2.6.2, hydraulic loads on the fuel assembly are evaluated for a pump overspeed transient which create flow rates 18 percent greater than the mechanical design flow. The mechanical design flow as stated in Section 5.1 is greater than the best estimate or most likely flow rate value for the actual plant operating condition.

4.4.2.9.8 Uncertainty in Mixing Coefficient: The value of the mixing coefficient, TDC, used in THINC mixing analyses for this application is 0.038. VIPRE-01 uses a value equivalent to the THINC value. The mean value of TDC obtained in the “R” grid mixing tests described in Section 4.4.2.2.1 was 0.042 (for 26-inch-grid spacing). The value 0.038 is one standard deviation below the mean value; and approximately 90 percent of the data gives values of TDC greater than 0.038 (Ref. 4.4-12).

The results of the mixing tests done on 17 x 17 geometry, as discussed in Section 4.4..2.2.3, had a mean value of TDC of 0.059 and standard deviation of $\sigma = 0.007$. Hence the current THINC mixing analysis value of TDC is almost 3 standard deviations below the mean for 26-inch-grid spacing.

4.4.2.10 Flux Tilt Considerations: Significant quadrant power tilts are not anticipated during steady-state, full power operation since this phenomenon is caused by some symmetric perturbation. A dropped or misaligned rod cluster control assembly (RCCA) could cause changes in hot channel factors; however, these events are analyzed separately in Chapter 15. This discussion will be confined to flux tilts caused by conditions such as x-y xenon transients, inlet temperature mismatches, and enrichment variations within tolerances.

The design value of the enthalpy rise hot channel factor $F_{\Delta H}^{N}$, which includes a 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 (described in the Technical Specifications) will not result in values of $F_{\Delta H}^{N}$ greater than that assumed in this submittal. The design value of F_Q does not include a specific allowance for quadrant flux tilts.

When the indicated quadrant power tilt ratio exceeds 1.02, corrective action (e.g., power reduction) must be taken. The procedure to be followed is explained in detail in the Technical Specifications. The quadrant power tilt ratio limit assures that the radial power distribution satisfies the design values used in the power capability analysis.

Experience at STPEGS and other large, 4-loop, Westinghouse PWRs has shown that quadrant power tilts in excess of 1.02 may occur during reactor power changes during reactor startup, load follow operations, and shutdown. Studies on several of these large PWRs, similar to STPEGS, indicate that the cause of these quadrant power tilts are the result of differences in symmetric fuel assembly burnups induced by differences in symmetric fuel assembly average powers. The results also indicate that the cause of these incore power imbalances is consistent with the presence of a combination of core inlet flow and temperature imbalances at full power which cause the average temperatures and powers in symmetric fuel assemblies to be slightly different. Experience has shown that these quadrant power tilts decrease to below 1.02 as power is increased to 100% Rated Thermal Power.

4.4.2.11 Fuel and Cladding Temperatures. 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 4.2.3.1.

The thermal-hydraulic design assures that the maximum fuel temperature is below the melting point of uranium dioxide (melting point of 5080°F (Ref. 4.4-1) unirradiated and decreasing by 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 4.4.2.9.1. The temperature distribution within the fuel pellet is predominantly a function of the local power density and the uranium dioxide thermal conductivity. However, the computation of radial fuel temperature distributions combines curd, 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 (Section 4.2.3.3) with the model modifications for time dependent fuel densification given in Reference 4.4-6 and 4.4-86. 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, References 4.4-30 through 4.4-36 and melt radius data, References 4.4-50 and 4.4-51, with good results.

As described in Reference 4.4-6 and 4.4-86, fuel rod thermal evaluations (fuel centerline, average and surface temperatures) are determined throughout the fuel rod life time with consideration of time dependent densification. To determine the maximum fuel temperatures, various burnup rods, including the highest burnup rod, are analyzed over the rod linear power range of interest.

The principal factors which are employed in the determination of the fuel temperature are discussed below.

4.4.2.11.1 <u>UO_2 Thermal Conductivity</u>: The thermal conductivity of uranium dioxide was evaluated from data reported by Howard, et al. (Ref. 4.4-37); Lucks, et al. (Ref. 4.4-37); Danial, et al. (Ref. 4.4-39); Feith (Ref 4.4-40); Vogt, et al. (Ref. 4.4-41); Nishijima, et al. (Ref. 4.4-42); Wheeler, et al. (Ref. 4.4-43); Godfrey, et al. (Ref. 4.4-44); Stora, et al. (Ref. 4.4.-45); Bush (Ref. 4.4-46); Asamoto, et al. Kruger (Ref. 4.4-48); and Gyllander (Ref. 4.4-52).

At the higher temperatures, thermal conductivity is best obtained by utilizing the integral conductivity to melt can be determined with more certainty.

From an examination of the data, it has been concluded that the best estimate for the value of $\int_0^{2800°C} Kdt$ is 93 watts/cm. This conclusion is based on the integral values reported by Gyllander (Ref. 4.4-52), Lyons, et al. (Ref. 4.4-53), Coplin, et al. (Ref. 4.4-54), Duncan (Ref. 4.4-50), Bain (Ref. 4.4-55), and Stora (Ref. 4.4-56).

The design curve for the thermal conductivity is shown on Figure 4.4-9. The section of the curve at temperatures between 0°C and 1300°C is in excellent agreement with the recommendation of the IAEA panel (Ref. 4.4-57). The section of the curve above 1300°C is derived for an integral value of 93 watts/cm (Refs. 4.4-50, 4.4-52, and 4.4-56).

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

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

where:

k = watts/cm-°C
T = °C

4.4.2.11.2 <u>Radial Power Distribution in UO_2 Fuel Rods</u>: 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 is uranium dioxide 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, References 4.4-58 and 4.4-59. 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:

$$\int_{T_s}^{T_c} k(T)dt = \frac{q'f}{4\pi} \qquad \text{(Eq. 4.4-14)}$$

where:

K(T) = the thermal conductivity for UO_2 with a uniform density distribution
q′ = the linear power generation rate

4.4.2.11.3 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 uranium dioxide thermal conductivity model, the calculated fuel centerline temperatures reflect the inpile temperature measurements.

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

$$h = \frac{K_{gas}}{\frac{\delta}{2} + \delta_r} \qquad \text{(Eq. 4.4-15)}$$

where:

h = gap conductance, Btu/hr-ft^2-°F
K_{gas} = thermal conductivity of the gas mixture including a correction factor (Ref. 4.4-60) for the accommodation coefficient for light gases (e.g., helium), Btu/hr-ft-°F
δ = diametral gap size, ft
δ_r = effective gap spacing due to surface roughness, ft

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

The larger gap conductance value from these two methods 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 UO_2 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, References 4.4-60 and 4.4-61. This information together with the surface roughness found in Westinghouse reactors leads to the following correlation:

$$h = 0.6P + \frac{K_{gas}}{\delta_r} \qquad \text{(Eq. 4.4-16)}$$

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 (Ref. 4.4-60 for the accommodation coefficient for light gases (e.g., helium), Btu/hr-ft-°F

4.4.2.11.4 Surface Heat Transfer Coefficients: The fuel rod surface heat transfer coefficients during subcooled forced convection and nucleate boiling are presented in Section 4.4.2.7.1.

4.4.2.11.5 Fuel Clad Temperature: 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 clad temperature.

4.4.2.11.6 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. The design value of F_Q for normal operation is 2.55. This results in a peak linear power of 13.5 kW/ft at full power conditions.

As described in Section 4.3.2.2.6 the peak linear power resulting from overpower transients/operator errors (assuming a maximum overpower of 118 percent) is less than 22.45 kW/ft. The centerline temperature kW/ft must be below the uranium dioxide melt temperature over the lifetime of the rod, including allowances for uncertainties. The fuel temperature design basis is discussed in Section 4.4.1.2 and results in a maximum allowable calculated centerline temperature of 4700°F. The peak linear power for prevention of centerline melt is >22.45 kW/ft. The centerline temperature at the peak linear power resulting from overpower transients/overpower errors (assuming a maximum overpower of 118 percent) is below that required to produce melting.

4.4.3 Description of the Thermal and Hydraulic Design of the Reactor Coolant System

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

1. Total coolant flow rates for the RCS and each loop 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, and RCS liquid volume, including pressurizer water at no load 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 5.4. The components of the RCS are water-filled during power operation with the pressurizer being approximately 60 percent water-filled.

5. Components of the ECCS are to be located so as to meet the criteria for net positive suction head (NPSH) described in Section 6.3.

6. Line lengths and sizes for the Safety Injection System (SIS) 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 Chapter 15.

7. The parameters for components of the RCS presented in Section 5.4.

8. The steady state pressure drops and temperature distributions through the RCS are presented in Table 5.1-1.

4.4.3.2 Operating Restrictions on Pumps. The minimum NPSH and minimum seal injection flow rate must be established before operating the reactor coolant pumps. With the minimum 6 gal/min labyrinth seal injection flow rate established, the operator will have to verify that the system pressure satisfies NPSH requirements.

4.4.3.3 Power-Flow Operating Map (BWR). Not applicable to STPEGS.

4.4.3.4 Temperature-Power Operating Map. The relationship between RCS temperature and power is shown on Figure 4.4-21.

The effects of reduced core flow due to inoperative pumps are discussed in Sections 5.4.1, 15.2.5, and 15.3.4. Natural circulation capability of the system is shown in Table 15.2-2.

4.4.3.5 Load Following Characteristics. The RCS is designed on the basis of steady-state operation at full power heat load. The reactor coolant pumps utilize constant speed drives as described in Section 5.4 and the reactor power is controlled to maintain average coolant temperature at a value which is a linear function of load, as described in Section 7.7.

4.4.3.6 Thermal and Hydraulic Characteristics Summary Table. The thermal and hydraulic characteristics are given in Table 4.3-1 and Table 4.1-1.

4.4.4 Evaluation

4.4.4.1 Critical Heat Flux. The critical heat flux correlation utilized in the core thermal analysis is discussed in Section 4.4.2.

4.4.4.2 Core Hydraulics.

4.4.4.2.1 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 rod cluster control guide thimbles to cool the control rods.

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

4. Flow introduced between the baffle and the barrel for the purpose of cooling these components and which is not considered available for core cooling.

5. Flow in the gaps between the fuel assemblies on the core periphery and the adjacent baffle wall.

The above contributions are evaluated to confirm that the design value of the core bypass flow is met. The design value of core bypass flow is equal to 8.5 percent of the total vessel flow.

Of the total allowances, 4.5 percent is associated with the internals (items 1, 3, 4, and 5 above) and 4.0 percent is for the core. Calculations have been performed using drawing tolerances on a worst case basis and accounting for uncertainties in pressure losses. Based on these calculations, the core bypass flow is less than the design value of 8.5 percent.

Flow model test results for the flow path through the reactor are discussed in Section 4.4.2.7.2.

4.4.4.2.2 Inlet Flow Distributions: Data has been considered from several 1/7th scale hydraulic reactor model tests (Refs. 4.4-23, 4.4-24, and 4.4-62) in arriving at the core inlet flow maldistribution criteria used in the THINC or VIPRE-01 analyses (Section 4.4.4.5.1). THINC-I analyses made using this data have indicated that a conservative design basis is to consider 5 percent reduction in the flow to the hot assembly (Ref. 4.4-63). The same design basis of 5 percent reduction to the hot assembly inlet is used in THINC-IV or VIPRE-01 analyses.

The experimental error estimated in the inlet velocity distribution has been considered as outlined in Reference 4.4-18 where the sensitivity of changes in inlet velocity distributions to hot channel thermal performance is shown to be small. Studies (Ref. 4.4-18) 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 4.4-23 and 4.4-24.

The effect of the total flow rate on the inlet velocity distribution was studied in the experiments of Reference 4.4-23. As we expected, on the basis of the theoretical analysis, no significant variation could be found in inlet velocity distribution with reduced flow rate.

Subsequent tests conducted at STPEGS have shown that a further reduction in the inlet flow to the hot assembly may occur when a phenomenon knows as the RCS Flow Anomaly is present.

4.4.4.2.3 Empirical Friction Factor Correlations: Empirical friction factor correlations are used in the THINC-IV and VIPRE-01 computer codes (described in Section 4.4.4.5.1).

For the THINC-IV computer code, the friction factor in the axial direction, parallel to the fuel rod axis, is evaluated using the Novendstern-Sandberg correlation (Ref. 4.4-64). This correlation consists of the following:

1. For isothermal conditions, this correlation uses the Moody (Ref. 4.4-26) 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 (Ref. 4.4-65) is used with a modification to account for a mass velocity and heat flux effect.

The friction factor for VIPRE-01 in the axial direction, parallel to the fuel rod axis, is evaluated using a correlation for a smooth tube (Ref. 4.4-89). The effect of two-phase flow on the friction loss is expressed in terms of the single-phase friction pressure drop and a two phase friction multiplier. The multiplier is calculated using the homogeneous equilibrium flow model.

For both the THINC-1V and VIPRE-01 codes, 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 (Ref. 4.4-25) is applicable. This correlation is of the from:

$$F_L = A\,Re_L^{-0.2} \qquad \text{(Eq. 4.4-17)}$$

where:

A is a function of the rod pitch and diameter as given in Reference 4.4-25.
Re_L is the lateral Reynolds number based on the rod diameter.

Extensive comparisons of THINC-IV predictions using the THINC-IV correlations to experimental data are given in Reference 4.4-49, and verify the applicability of these correlations in PWR design.

Extensive comparisons of VIPRE-01 predictions to THINC-1V predictions are given in Reference 4.4-89 and verify the applicability of the VIPRE-01 correlations in PWR design.

4.4.4.3 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 $F^N_{\Delta H}$ (defined in Section 4.3.2.2.1) as well as axial heat flux profiles are discussed in the following two sections.

4.4.4.3.1 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,

$$F^N_{\Delta H} = \frac{\text{hot rod power}}{\text{average rod power}} = \frac{\text{Max} \int_0^H q'(x_0, y_0, z)\, dz}{\frac{1}{N} \sum_{\text{all rods}} \int_0^H q'(x, y, z) dz} \qquad \text{(Eq. 4.4-18)}$$

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 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 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 design radial power distribution discussed in Reference 4.4-18 is used in the VIPRE-01 model.

For operation at a fraction P of full power, the design $F^N_{\Delta H}$ used for VANTAGE 5H, VANTAGE+ and RFA fuel is given by:

$$F^N_{\Delta H} = 1.62\ [1 + 0.3(1 - P)] \qquad \text{(Eq. 4.4-19)}$$

The permitted relaxation of $F^{N}_{\Delta H}$ is included in the DNB protection setpoints and allows radial power shape changes with rod insertion to the insertion limits (Ref. 4.4-66), thus allowing greater flexibility in the nuclear design. The cycle specific $F^{N}_{\Delta H}$ value is reported in the Core Operating Limits Report.

4.4.4.3.2 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 excore 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 ΔT 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 axial shape used in establishing core DNB limits (i.e., overtemperature ΔT protection system setpoints) is a chopped cosine shape with a peak-to-average value of 1.61. With respect to minimum DNBR, this axial shape bounds all of the shapes which could occur during power operation including overpower conditions as generated for the nuclear design (refer to Section 4.3.2.2.6). Accidents which are initiated from normal full power operation, including loss of flow with pump(s) coasting down freely, a single dropped control rod, and a statically misaligned control rod which are not protected by the overtemperature ΔT protection system setpoints, are analyzed with a skewed-to-the-top shape because this shape bounds all of the possible shapes which could occur at normal full power operating conditions.

To determine the penalty to be taken in protection setpoints for extreme values of flux difference, the reference shape (1.61 chopped cosine) is supplemented by other axial shapes skewed to the bottom and top of the core. The course of those accidents in which DNB is a concern is analyzed in Chapter 15 assuming that the protection setpoints have been set on the basis of these shapes. In many cases the axial power distribution in the hot channel changes throughout the course of the accident due to rod motion, coolant temperature, and power level changes.

The initial conditions for the accidents for which DNB protection is required are assumed to be those permissible within the constant axial offset control strategy for the load maneuvers described in Reference 4.4-67. In the case of the loss of flow accident, the hot channel heat flux profile is very similar to the power density profile in normal operation preceding the accident. It is therefore possible to illustrate the calculated minimum DNB ratio for conditions representative of the loss of flow accident as a function of the flux difference initially in the core. A plot of this type is provided on Figure 4.4-10 for first core initial conditions. As noted on this figure, all power shapes were evaluated with a full power radial peaking factor $\left(F^{N}_{\Delta H}\right)$ of 1.52. The radial contribution to the hot rod power shape is conservative both for the initial condition and for the condition at the time of minimum DNBR during loss of flow transient. Also shown is the minimum DNBR calculated for the Unit 1 first core reference normal full power shape (1.55 chopped cosine) at the same conditions.

4.4.4.4 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.

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 Chapter 7 are designed to meet these bases. The response of the core to Condition II transients is given in Chapter 15.

4.4.4.5 Analytical Techniques with THINC-IV.

4.4.4.5.1 Core Analysis: The objective of reactor core thermal design is to determine the maximum heat removal capability in all flow subchannels and to show that the core safety limits (as presented in the Technical Specifications) are not exceeded while compounding engineering and nuclear effects. The thermal design considers 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 (Refs 4.4-18 and 4.4-49). 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-IV 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.

4.4.4.5.2 Steady-State Analysis: The THINC-IV computer program determines 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 group of assemblies 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 the THINC-IV are similar to the Euler equations (Ref. 4.4-68), 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 cross-flow resistance model used in the lateral momentum equations was developed from experimental data for flow normal to tube banks (Refs. 4.4-25 and 4.4-69). The energy equation for each element also contains additional terms which represent the energy gain or loss due to the cross-flow between elements.

The unique feature in THINC-IV is that lateral momentum equations, which include both inertial and cross-flow 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-I (Ref. 4.4-7) and COBRA (Ref. 4.4-70) the solutions are obtained by stepwise integration throughout the array.

The resulting formulation of the conservation equations are 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.

In the standard THINC-IV modeling scheme, 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 computational element. The channel layout and inlet flow distribution used in the THINC analysis are consistent with those used in Reference 4.4-18. Appropriate pressure loss coefficients are used in modeling the various channels, and the output parameters specified below are then calculated as a function of distance along the channels.

The first computation is a core-wide, assembly-by-assembly analysis which uses an inlet velocity distribution modeled from experimental reactor models, (Refs. 4.4-23, 4.4-24, and 4.4-62) (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, and void fraction in each assembly. The system of equations is solved using the specific 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 is know 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 in order to determine the conditions in the hot channel. The engineering hot channel factors are described in Section 4.4.2.2.4.

The standard THINC-IV modeling scheme has been replaced by the improved THINC-IV modeling scheme (Ref. 4.4-3c). This modeling scheme improves the accuracy of the solution by minimizing the inaccuracies which result from the use of the perturbation technique in the solution of the governing equations. This is done by updating the unperturbed conditions for each computation, and adding one computation. In the core-wide analysis, the computational elements represent groups of assemblies. In the second computation the elements represent full fuel assemblies. In the third and fourth computations, the computational elements are the same as in the second and third computations of the standard modeling scheme.

The effect of crud on the flow and enthalpy distribution in the core is accounted for directly in the THINC-IV evaluations by assuming a crud thickness several times that which would be expected to occur. This results in slightly conservative evaluations of the minimum DNBR.

Estimates of uncertainties are discussed in Section 4.4.2.9.

4.4.4.5.3 Experimental Verification: An experimental verification (Ref. 4.4-49) 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 PWR core designs.

During the operation of a reactor, various incore monitoring systems obtain measured data indicating the core performance. Assembly power distributions and assembly mixed mean temperature are measured and can be conveyed 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 very low power (approximately 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 inter-assembly 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. Inter-assembly cross flow 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 cross flow 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 cross flow velocities than at normal reactor operating conditions.

The incore instrumentation was used to obtain the assembly-to-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 in this figure, and are predicted from the momentum equations and the cross flow resistance model used in THINC-IV. The larger cross flow 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 l0l 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 interassembly 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 nonuniformly heated rod bundle (Ref. 4.4-17). The inner nine heater rods were operated at approximately 20 percent more power than the outer rods to create a typical PWR inter-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 4.4-49.

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.

4.4.4.5.4 <u>Transient Analysis</u>: The THINC-IV thermal-hydraulic computer code does not have a transient capability. Since the third section of the THINC-I program (Ref. 4.4-7) 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 15.0.10.

4.4.4.5.5 Core Analysis Techniques with VIPRE: The objective of reactor core thermal design is to determine the maximum heat removal capability in all flow subchannels and to 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. VIPRE-01 (VIPRE) is a three-dimensional subchannel code that has been developed to account for hydraulic and nuclear effects on the enthalpy rise in the core and hot channels (Ref 4.4-88). VIPRE modeling, of a PWR core is based on one-pass modeling approach (Ref 4.4-89). In the one-pass modeling hot channels and their adjacent channels are molded in detail, while the rest of the core is modeled simultaneously on a relatively coarse mesh. The behavior of the hot assembly is determined by superimposing the power distribution upon inlet flow distribution while allowing for flow mixing and flow distribution between flow channels. Local variations in fuel rod power, fuel rod and pellet fabrication, and turbulent mixing are also considered in determining conditions in the hot channel. Conservation equations of mass, axial and lateral momentum, and energy are solved for the fluid enthalpy, axial flow rate, lateral flow and pressure drop.

4.4.4.5.6 Steady-State Analysis: The VIPRE core model as approved by the NRC, (Ref. 4.4-89) is used with the applicable DNB correlations to determine DNBR distributions along the hot channels of the reactor core under all expected operating conditions. The VIPRE code is described in detail in (Ref. 4.4-88), including discussions on code validation with experimental data. The VIPRE modeling method is described in (Ref. 4.4-89), including empirical models and correlations used. The effect of crud on the flow and enthalpy distribution in the core is not directly accounted for in the VIPRE evaluations. However, conservative treatment by the VIPRE modeling method has been demonstrated to bound this effect in DNBR calculations.

Estimates of uncertainties are discussed in Section 4.4.2.9.

4.4.4.5.7 Experimental Verification: Extensive additional experimental verification of VIPRE is presented in (Ref. 4.4-88).

The VIPRE 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 VIPRE analysis provides a realistic evaluation of the core performance and is used in the thermal hydraulic analysis as described above.

4.4.4.5.8 Transient Analysis: VIPRE is capable of transient DNB analysis. The conservation equations in the VIPRE code contain the necessary accumulation terms for transient calculations. The input description can include one or more of the following time dependent arrays:

1. Inlet flow variations,
2. Core heat flux variation,
3. Core pressure variation,
4. Inlet temperature or enthalpy variation.

At the beginning of the transient, the calculation procedure is carried out as in the steady state analysis. The time is incremented by an amount determined either by the user or by the time step control options in the code itself. At each new 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 time intervals selected by the user, a complete description of the coolant parameter distributions as well as DNBR is printed out. In this manner the variation of any parameter with time can be readily determined.

4.4.4.6 Hydrodynamic and Flow Power Coupled Instability. Boiling flows may be susceptible to thermohydrodynamic instabilities (Ref. 4.4-72). 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 thermohydraulic design criterion was developed which states that modes of operation under Conditions I and II events hall not lead to thermohydrodynamic 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 (Ref. 4.4-72) when the slope of the RCS pressure drop-flow rate curve $\frac{\partial \Delta P}{\partial G_{internal}}$ becomes algebraically smaller than the loop supply (pump head) pressure drop-flow rate curve $\frac{\partial \Delta P}{\partial G_{external}}$. The criterion for stability is thus $\frac{\partial \Delta P}{\partial G_{internal}} > \frac{\partial \Delta P}{\partial G_{external}}$. 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 Conditions I and II operational ranges. Thus, a Ledinegg instability will not occur.

The mechanism of density wave oscillations in a heated channel has been described by Lahey and Moody (Ref. 4.4-73). 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 (Ref. 4.4-74) 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 (Refs 4.4-75, 4.4-76, and 4.4-77), including Virgil C. Summer, under Conditions 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 (Ref. 4.4-74) 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 (Refs. 4.4-78) 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-to-channel cross flow and enthalpy perturbations would be greater than that which would exist in a PWR core which has a relatively low resistance to cross flow.

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 (Ref. 4.4-79) analyzed parallel closed channel stability experiments simulating a reactor core flow. These experiments were conducted at pressures up to 2,200 psia. The results showed that for flow and power levels typical of power reactor conditions, no flow oscillations could be induced above 1,200 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 inconsistent data which might be indicative of flow instabilities in the rod bundle.

In summary, it is concluded that thermohydrodynamic instabilities will not occur under Conditions I and II modes of operation for Westinghouse PWR reactor designs. A large power margin, greater than doubling-rated power, exists 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.

4.4.4.7 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 is 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 or VIPRE-01 program. Inspection of the DNB correlation (Section 4.4.2.2 and Ref. 4.4-10) shows that the predicted DNBR is dependent upon the local values of quality and mass velocity.

The THINC-IV or VIPRE-01 code is capable of predicting the effects of local flow blockages on DNBR within the fuel assembly on subchannel basis, regardless of where the flow blockage occurs. In Reference 4.4-49 for THINC-IV and Reference 4.4-88 for VIPRE, it is shown that for a fuel assembly similar to the design, THINC-IV or VIPRE-01 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 in. downstream of the blockage. With the reactor operating at the nominal full power conditions specified in Table 4.1-1, 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 a minimum DNBR less than the design limit DNBR.

From a review of the open literature it is concluded that flow blockage in "open lattice cores" similar to the cores cause flow perturbations which are local to the blockage. For instance, Ohtsubol, et al (Ref. 4.4-81) show that the mean bundle velocity is approached asymptotically about 4 in. downstream from a flow blockage in a single flow cell. Similar results were also found for two and three cells completely blocked. Basmer, et al (Ref. 4.4-82) 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 in. 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 plants were operating at full power and nominal steady state conditions as specified in Table 4.1-1, a reduction in local mass velocity greater than 58 percent would be required to reduce the DNBR to the design limit DNBR. 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 likely would not effect DNBR at all.

Coolant flow blockages induce local cross-flows as well as promote turbulence. Fuel rod behavior is changed under the influence of a sufficiently high cross-flow component. Fuel rod vibration could occur, caused by the cross-flow component through vortex shedding or turbulence mechanisms. If the cross-flow 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. The cross-flow velocity required to exceed fluid elastic stability limits is dependent on the axial location of the blockage and the characterization of the cross-flow (jet flow or not). These limits are greater than those for vibratory fuel rod wear. Cross-flow 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).

4.4.5 Testing and Verification

4.4.5.1 Tests Prior to Initial Criticality. A reactor coolant flow test was performed following fuel loading but prior to initial criticality. Pressure drop from coolant loop elbow taps was obtained in this test. This data allowed determination of the coolant flow rates at reactor operating conditions. This test verified that proper coolant flow rates have been used in the core thermal and hydraulic analysis.

4.4.5.2 Initial Power and Plant Operation. Core power distribution measurements are made at several core power levels (Chapter 14). 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 incore thermocouple measurements (Ref. 4.4-83). These measurements were performed on the Zion reactor. No further in-reactor testing is planned. VIPRE-01 has been confirmed to be as conservative as the THINC code in Reference 4.4-89.

4.4.5.3 Component and Fuel Inspections. Inspections performed on the manufactured fuel are delineated in Section 4.2.4. Fabrication measurements critical to thermal and hydraulic analysis are obtained to verify that the engineering hot channel factors in the design analyses (Section 4.4.2.2.4) are met.

4.4.6 Instrumentation Requirements

4.4.6.1 Incore Instrumentation. Instrumentation is located in the core so that by correlating movable neutron detector information with fixed thermocouple information radial, axial, and azimuthal core characteristics may be obtained for all core quadrants.

The incore instrumentation system is comprised of thermocouples, positioned to measure fuel assembly coolant outlet temperatures at preselected positions, and fission chamber detectors positioned in guide thimbles which run the length of selected fuel assemblies to measure the neutron flux distribution. Figure 4.4-20 shows the number and location of instrumented assemblies in the core.

The core-exit thermocouples provide a backup to the flux monitoring instrumentation for monitoring power distribution. The routine, systematic, collection of thermocouple readings by the operator provides a data base. From this data base, abnormally high or abnormally low readings, quadrant temperature tilts, or systematic departures from a prior reference map can be deduced.

The movable incore neutron detector system would be used for more detailed mapping if the thermocouple system were to indicate an abnormality. These two complementary systems are more useful when taken together than either system alone would be. The incore instrumentation system is described in more detail in Section 7.7.1.9.

The incore instrumentation is provided to obtain data from which fission power density distribution in the core, coolant enthalpy distribution in the core, and fuel burnup distribution may be determined.

4.4.6.2 Overtemperature and Overpower ΔT Instrumentation. The overtemperature ΔT trip protects the core against low DNBR. The overpower ΔT trip protects against excessive power (fuel rod rating protection).

As discussed in Section 7.2.1.1.2, factors included in establishing the overtemperature ΔT and overpower ΔT trip setpoints include the reactor coolant temperature in each loop and the axial distribution of the core power through the use of the two-section excore neutron detectors.

4.4.6.3 Instrumentation to Limit Maximum Power Output. The output of the three ranges (source, intermediate, and power) of detectors, with the electronics of the nuclear instruments, are used to limit the maximum power output of the reactor within their respective ranges.

There are six radial locations containing a total of eight neutron flux detectors installed around the reactor in the primary shield. Two proportional counters for the source range are installed on opposite “flat” portions of the core containing the primary startup sources at an elevation approximately one-quarter of the core height. Two compensated ionization chambers for the intermediate range, located in the same instrument wells and detector assemblies as the source range detectors, are positioned at an elevation corresponding to one-half of the core height. Four dual section uncompensated ionization chamber assemblies for the power range are installed vertically at the four corners of the core and located equidistant from the reactor vessel at all points and, to minimize neutron flux pattern distortions, within one foot of the reactor vessel. Each power range detector provides two signals corresponding to the neutron flux in the upper and in the lower sections of a core quadrant. The three ranges of detectors are used as inputs to monitor neutron flux from a completely shutdown condition to 120 percent of full power with the capability of recording overpower excursions up to 200 percent of full power.

The output of the power range channels is used for:

1. The rod speed control function

2. To alert the operator to an excessive power imbalance between the quadrants

3. Protect the core against rod ejection accidents

4. Protect the core against adverse power distributions resulting from dropped rods

Details of the neutron detectors and nuclear instrumentation design and the control and trip logic are given in Chapter 7. The limits on neutron flux operation and trip setpoints are given in the Technical Specifications.

4.4.6.4 Loose Parts Monitoring System. The Loose Parts Monitoring System (LPMS) consists of permanently installed sensors and electronic equipment that continuously monitor selected locations on the reactor vessel and steam generators for noise resulting from loose parts in the RCS. The system is designed to meet the guidance of Regulatory Guide (RG) 1.133, as noted in Section 3.12.

Sensors are located at each of the following locations:

a. Lower Pressure Vessel (Incore Instrumentation Guide Tubes)

b. Upper Pressure Vessel (Head Lifting Lugs)

c. Steam Generators (Steam Generator Housing)

The above are locations where any loose part released within the primary loop will migrate and impact. Signal cables are run separately between the sensors and a point normally accessible during full power operation. The accelerometers have been selected for the function of loose parts monitoring. All sensors and preamplifiers are designed to function in their normal service environment. High temperature sensors are provided for the upper pressure vessel and the steam generators. The system is designed to be capable of detecting a metallic part with an impact force $\geq$ .0.5 ft-lb striking the inside of the reactor coolant pressure boundary within 3 ft of a sensor.

The LPMS electronics are located in the control room cabinet assemblies. The panel includes all signal conditioning and processing equipment, loose parts detection, alarm analysis, audio monitoring and test capabilities.

Hybrid techniques are used for signal processing and indication. A spectrum analyzer provides a capability for on-line signature analysis. The accelerometer sensors are equipped with piezoelectric crystals that are used for both vibration and acoustic monitoring, with special-purpose preamplifiers for wide-band operation. A digital signal, which activates an annunciator alarm and initiates recording of appropriate channels, is generated when the vibration or loose parts signal level exceeds a preset alarm level for a specified period of time.

The system is designed and installed to survive an Operating Basis Earthquake (OBE) with either the audio or the visual alarm capability intact. CN-3328

Hydraulic, mechanical and electrical background noise affect the ability of the LPMS to distinguish between very low energy impact signals and background noise factors. For this reason, the 0.5 ft-lb. sensitivity may be reduced for those installations where normal background noise dominates the frequencies of concern. Alarm setpoints for these channels are determined based on background noise and individual channel gains.

Noise and vibration frequencies and amplitudes are dependent upon specific installations, therefore appropriate baseline data is taken during startup to allow determination of the appropriate alarm setting for each channel.

The LPMS is required to be functional in Modes 1 and 2. Functionality of the LPMS will be ensured by the performance of a channel check at least once per 24 hours, an analog channel functional test, except for verification of setpoints, at least once per 31 days, and a channel calibration at least once per 18 months. CN-3328

Operator training on the LPMS will be included as Part of the onsite training program for licensed operators. This training program is described in response to Question 492.02N.

Procedures for operation of the LPMS will be included as Part of the System Operating Procedures to be prepared initially as indicated in Section 13.5.2.1, item 1.

Plant administrative procedures will address retention of plant operational phase records including those of LPMS.

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HISTORICAL

TABLE 4.4-1

THERMAL AND HYDRAULIC COMPARISON TABLE

Design Parameters	W. B. McGuire Units 1 and 2	South Texas Units 1 and 2
Reactor Core Heat Output, MWt	3,411	3,853
Reactor Core Heat Output, 10^6 Btu/hr	11,641	12,969
Heat Generated in Fuel, %	97.4	97.4
System Pressure, Nominal, psia	2,250	2,250
System Pressure, Minimum Steady State, psia	2,220	2,220
Minimum DNBR at Nominal Conditions		
Typical Flow Channel	2.08	2.190
Thimble (Cold Wall) Flow Channel	1.74	2.108
Minimum DNBR for Design Transients	>1.30	Typical Flow Channel >1.26 Thimble Flow Channel >1.24
DNB Correlation	"R" (W-3 with Modified Spacer Factor)	WRB-1
Coolant Flow		
Total Thermal Flow Rate, 10^6 lbm/hr	140.3	145.0
Effective Flow Rate for Heat Transfer, 10^6 lbm/hr	134.0	132.7
Effective Flow Area for Heat Transfer, ft^2	51.1	51.3[(f)]
Average Velocity Along Fuel Rods, ft/sec	16.7	15.6[(f)]
Average Mass Velocity, 10^6 $lb_m/hr\text{-}ft^2$	2.62	2.59[(f)]

HISTORICAL

HISTORICAL

TABLE 4.4-1 (Continued)

THERMAL AND HYDRAULIC COMPARISON TABLE

Design Parameters	W. B. McGuire Units 1 and 2	South Texas Units 1 and 2
Coolant Temperature		
Nominal Inlet, °F	558.1	549.8 to 561.2
Average Rise in Vessel, °F	60.2	63.6 to 65.0
Average Rise in Core, °F	62.7	68.7 to 70.3
Average in Core, °F (Based on Avg. Enthalpy)	592.1	586.9 to 597.8
Average in Vessel, °F	588.2	582.3 to 593.8
Heat Transfer		
Active Heat Transfer, Surface Area, ft^2	59,700	69,700
Average Heat Flux, $Btu/hr\text{-}ft^2$	189,800	181,200
Maximum Heat Flux for Normal Operation, $Btu/hr\text{-}ft^2$	440,300($^{a}{}_{1}$)	489,200($^{a}{}_{2}$)
Average Linear Power, kW/ft	5.44	5.20
Peak Linear Power for Normal Operation, kW/ft	12.6($^{a}{}_{1}$)	14.0($^{a}{}_{2}$)
Peak Linear Power Resulting from Overpower Transients/Operators Errors (assuming a maximum overpower of 118%), kW/ft[(b)]	18.0	22.0
Peak Linear Power for Prevention of Centerline Melt, kW/ft[(c)]	>18.0	>22.45
Power Density, kW per liter of core[(d)]	104.5	98.8
Specific Power, kW per kg Uranium[(d)]	38.4	36.4 (Unit 1) 36.6 (Unit 2)

HISTORICAL

TABLE 4.4-1 (Continued)

THERMAL AND HYDRAULIC COMPARISON TABLE

Design Parameters	W. B. McGuire Units 1 and 2	South Texas Units 1 and 2
Fuel Central Temperature		
Peak at Peak Linear Power for Prevention of Centerline Melt, °F	4,700	4,700
Pressure Drop[(e)]		
Across Core, psi	27.9 ± 5.6	39.78 ± 4.0
Across Vessel, including nozzle, psi	48.2 ± 7.2	62.68 ± 8.92[(g)]

HISTORICAL

HISTORICAL

Notes:

a_1 This limit is associated with the value of $F_Q = 2.32$
a_2 This limit is associated with the value of $F_Q = 2.70$
b See Section 4.3.2.2.6
c See Section 4.4.2.11.6
d Based on cold dimension and 95% of theoretical density fuel
e Based on best estimate reactor flow rate as discussed in Section 5.1
f Based on a full core of RFA fuel
g RFA fuel with Thimble Plugs removed and T_{COLD} conversion

TABLE 4.4-4

COMPARISON OF THINC-IV AND THINC-I PREDICTIONS WITH DATA

FROM REPRESENTATIVE WESTINGHOUSE TWO AND THREE LOOP REACTORS

Reactor	Power (MWt)	% Full Power	Measured Inlet Temp (°F)	σ rms (°F) THINC-I	σ (°F) THINC-IV	Improvement (°F) for THINC-IV over THINC-I
Ginna	847	65.1	543.7	1.97	1.83	0.14
	854	65.7	544.9	1.56	1.46	0.10
	857	65.9	543.9	1.97	1.82	0.15
	947	72.9	543.8	1.92	1.74	0.18
	961	74.0	543.7	1.97	1.79	0.18
	1091	83.9	542.5	1.73	1.54	0.19
	1268	97.5	542.0	2.35	2.11	0.24
	1284	98.8	540.2	2.69	2.47	0.22
	1284	98.9	541.0	2.42	2.17	0.25
	1287	99.0	544.4	2.26	1.97	0.29
	1294	99.5	540.8	2.20	1.91	0.29
	1295	99.6	542.0	2.10	1.83	0.27
Robinson	1427.0	65.1	548.0	1.85	1.88	0.03
	1422.6	64.9	549.4	1.39	1.39	0.00
	1529.0	88.0	550.0	2.35	2.34	0.01
	2207.3	100.7	543.0	2.41	2.41	0.00
	2213.9	101.0	533.8	2.52	2.44	0.08

4.5 REACTOR MATERIALS

4.5.1 Control Rod System Structural Materials

4.5.1.1 Materials Specifications. All parts exposed to reactor coolant are made of metals which resist the corrosive action of the water. Three types of metals are used exclusively: stainless steels, nickel-chrome-iron, and cobalt based alloys. In the case of stainless steels, only austenitic and martensitic stainless steels are used; the martensitic stainless steels are not used in the heat treated conditions which cause susceptibility to stress corrosion cracking or accelerated corrosion in the Westinghouse pressurized water reactor (PWR) water chemistry.

1. Pressure Vessel

All pressure containing materials comply with Section III of the American Society for Mechanical Engineers (ASME) Boiler and Pressure Vessel (B&PV) Code, and are fabricated from austenitic (Type 304 and Type 316) stainless steel.

2. Coil Stack Assembly

The coil housings require a magnetic material. Both low-carbon cast steel and ductile iron have been successfully tested for this application. The choice, made on the basis of cost, indicates that ductile iron will be specified on the control rod drive mechanism (CRDM). The finished housing are zinc-plated or flame-sprayed to provide corrosion resistance.

Coils are wound on bobbins of molded Dow Corning 302 material, with double glass-insulated copper wire. Coils are then vacuum impregnated with silicon resin. A wrapping of mica sheet is secured to the coil outside diameter. The result is a well insulated coil capable of sustained operation at 200°C.

3. Latch Assembly

Magnetic pole pieces, latches, spacers and the key are fabricated from Type 410 stainless steel. All non-magnetic parts, except pins and springs, are fabricated from Type 304 stainless steel, with the exception of the threaded sleeve and retaining key, which are fabricated from Type 316 stainless steel. Haynes No. 25 cobalt alloy is used to fabricate the link pins. Springs are made from nickel-chrome-iron alloy (Inconel-X). Latch arm tips are clad with Stellite-6 to provide improved wearability. Hard chrome plate and Stellite-6 are used selectively for bearing and wear surfaces. CN-2994

4. Drive Rod Assembly

The drive rod assembly utilizes a Type 410 stainless steel drive rod. The coupling is machined from Type 403 stainless steel. Other parts are Type 304 stainless steel with the exception of the springs which are nickel-chrome-iron alloy and the locking button which is Haynes 25.

4.5.1.2 Fabrication and Processing of Austenitic Stainless Steel Components. The discussions provided in Section 5.2.3 concerning the processes, inspections, and tests on austenitic stainless steel components to assure freedom from increased susceptibility to intergranular corrosion caused by sensitization, and the discussions provided in Section 5.2.3 on the control of welding of austenitic stainless steels, especially control of delta ferrite, are applicable to the austenitic stainless steel pressure housing components of the CRDM.

The core hold down spring is the only stainless steel material in the reactor core support structure with a yield strength greater than 90,000 psi and is acceptable based upon ASME Code Case 1337.

4.5.1.3 Contamination Protection and Cleaning of Austenitic Stainless Steel. The CRDMs are cleaned prior to delivery in accordance with the guidance of American National Standards Institute (ANSI) 45.2.1. Process specifications in packaging and shipment are discussed in Section 5.2.3. Although the procedure at the construction site is not in the Westinghouse Nuclear Steam Supply System (NSSS) scope of supply, Westinghouse personnel do conduct surveillance of these operations to assure that manufacturers and installers adhere to appropriate requirements as discussed in Section 5.2.3.

4.5.2 Reactor Internals Materials

4.5.2.1 Materials Specification. All the major material for the reactor internals is Type 304 stainless steel. Parts not fabricated from Type 304 stainless steel include bolts and dowel pins, which are fabricated from Type 316 stainless steel, and radial support keybolts, which are fabricated on Inconel-750. These materials are listed in Table 5.2-5. There are no other materials used in the reactor internals or core support structures which are not otherwise included in ASME III, Appendix I.

4.5.2.2 Controls on Welding. The discussions provided in Section 5.2.3 are applicable to the welding of reactor internals and core support components.

4.5.2.3 Nondestructive Examination of Wrought Seamless Tubular Products and Fittings. The discussion provided in Section 5.2.3 verifies conformance of reactor internals and core support structures with Regulatory Guide (RG) 1.66 “Nondestructive Examination of Tubular Products”.

4.5.2.4 Fabrication and Processing of Austenitic Stainless Steel Components. The discussions provided in Section 5.2.3, verify conformance of reactor internals and core support structures with the intent of RG 1.44, “Control of the Use of Sensitized Stainless Steel”.

RG 136, “Nonmetallic Thermal Insulation for Austenitic Stainless Steel”, is not applicable to the reactor vessel internals since no insulation material of any kind is used on these structures.

The discussion provided in Section 5.2.3 verifies conformance of reactor internals and core support structures with RG 1.31, “Control of Stainless Steel Welding”. Where electroslag welding is used in fabricating nuclear plant components the Westinghouse procurement procedure required vendors to meet the guideline on RG 1.34.

Section 5.2.3 describes the degree of conformance of reactor internals and core support structures with RG 1.71, “Welder Qualification for Areas of Limited Accessibility”.

4.5.2.5 Contamination Protection and Cleaning of Austenitic Stainless Steel. The discussions provided in Section 5.2.3 are applicable to the reactor internals and core support structures and verify conformance with ANSI 45 specifications and RG 1.37.

4.6 FUNCTIONAL DESIGN OF REACTIVITY CONTROL SYSTEMS

4.6.1 Information for Control Rod Drive System

The Control Rod Drive System (CRDS) is described in Section 3.9.4.1. Figures 3.9-4 and 3.9-5 provide the details of the control rod drive mechanisms (CRDMs), and Figure 4.2-8 provides the layout of CRDS. No hydraulic system is associated with its functioning. The instrumentation and controls for the reactor trip system are described in Section 7.2 and the reactor control system is described in Section 7.7.

4.6.2 Evaluation of the Control Rod Drive System

The CRDS has been analyzed in detail in a failure mode and effects analysis (FMEA) (Ref. 4.6-1). This study, and the analyses presented in Chapter 15, demonstrates that the CRDS performs its intended safety function, a reactor trip, by putting the reactor in a subcritical condition when a safety system setting is approached, with any assumed credible failure of a single active component. The essential elements of the CRDS (those required to ensure reactor trip) are isolated from nonessential portions of the CRDS (the Rod Control System) as described in Section 7.2.

Despite the extremely low probability of a common mode failure impairing the ability of the reactor trip system to perform its safety function, analyses have been performed in accordance with the requirements of WASH 1270. These analyses, documented in References 4.6-2 and 4.6-3, have demonstrated that acceptable safety criterion would not be exceeded even if the control rod drive system were rendered incapable of functioning during a reactor transient for which their function would normally be expected.

The design of the CRDM is such that failure of the CRDM cooling system will, in the worst care, result in an individual control rod trip.

4.6.3 Testing and Verification of the Control Rod Drive System

The CRDS is extensively tested prior to its operation. These tests may be subdivided into five categories, 1) prototype tests of components, 2) prototype control rod drive system tests, 3) production tests of components following manufacture and prior to installation, 4) onsite proportional and initial startup tests, and 5) periodic inservice tests. These tests, which are described in Sections 3.9.4.4, 4.2, and 14.2, and in the Technical Specifications, are conducted to verify the operability of the CRDS when called upon to function.

4.6.4 Information for Combined Performance of Reactivity Systems

As is indicated in Chapter 15, the only postulated events which assume credit for reactivity control systems other than a reactor trip to render the plant subcritical, are the steam line break, feed line break, and loss-of-coolant accident (LOCA). The reactivity control systems for which credit is taken in these accidents are the reactor trip and the Safety Injection System (SIS). Additional information on the CRDS is presented in Section 3.9.4 and on the SIS in Section 6.3. Note that no credit is taken for the boration capabilities of the Chemical and Volume Control System (CVCS) as a system in the analysis of transients presented in Chapter 15. Information on the capabilities of the CVCS is provided in Section 9.3.4. The adverse boron dilution possibilities due to the operation of the CVCS are investigated in Section 15.4.6. Prior proper operaton of the CVCS has been presumed as an initial condition to evaluate transients and appropriate Technical Specifications prepared to ensure the correct operation or remedial action.

4.6.5 Evaluation of Combined Performance

The evaluation of the steam line break, feed line break, and the LOCA which presume the combined actuation of the reactor trip system to the CRDS and the SIS are presented in Sections 15.1.5, 15.2.8, and 15.6.5. Reactor trip signals and safety injection signals for these events are generated from functionally diverse sensors and actuate diverse means of reactivity control; i. e. control rod insertion and injection of soluble absorber.

Nondiverse but redundant types of equipment are only utilized in the processing of the incoming sensor signals into appropriate logic which initiates the protective action. This equipment is described in detail in Sections 7.2 and 7.3. In particular, note the protection from equipment failures is provided by redundant equipment and periodic testing. Effects of failures of this equipment have been extensively investigated as reported in Reference 4.6-4. This FMEA verifies that any single failure will not have a deleterious effect upon the Engineering Safeguards Features Actuation System. Adequacy of the ECCS and SIS performance under faulted conditions is verified in Section 6.3.

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