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CHAPTER 4.0

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CHAPTER 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 17 x 17 fuel assemblies that are similar in mechanical design and enrichments. The core may consist of any combination of Standard (STD), Optimized (OFA), VANTAGE 5, VANTAGE+, and PERFORMANCE+ fuel assemblies, as described in Subsection 4.2.2, and arranged in a checkered low-leakage pattern. Because the PERFORMANCE+ fuel has similar design parameters as VANTAGE 5 and VANTAGE+ fuel, references to VANTAGE 5 and VANTAGE+ also refer to PERFORMANCE+ unless noted otherwise.

The significant new mechanical design features of VANTAGE 5 design, as defined in Reference 2, relative to the previous OFA fuel design include the following:

a. Integral Fuel Burnable Absorber (IFBA),

b. Intermediate Flow Mixer (IFM) Grids,

c. Westinghouse Integral Nozzle (WIN) Top Nozzle,

d. Standardized Debris Filter Bottom Nozzle (SDFBN),

e. Extended Burnup Capability, and

f. Axial Blankets.

The VANTAGE+ fuel assembly design (Reference 3) includes the following features: ZIRLO™ clad fuel rods, ZIRLO™ thimble and instrumentation tubes, and variable pitch plenum spring. PERFORMANCE+ features were added as enhancements to the VANTAGE+ fuel design. These enhancements, as applied in the Callaway core, include: a protective grid; long, debris-mitigating fuel rod bottom end plug; external grip top end plug; ZIRLO™ mid-grids and IFM grids; low-enriched to fully-enriched annular axial blanket pellets; low cobalt Type 304 top and bottom nozzles and top grid assembly sleeve; and extended burnup bottom grid design.

A fuel assembly is composed of 264 fuel rods in a 17 x 17 square array, except that limited substitution of fuel rods by filler rods, consisting of Zircaloy-4, ZIRLO™ or stainless steel, may be made (if justified by a cycle specific reload analysis). The center position in the fuel assembly is reserved for incore instrumentation. The remaining 24 positions in the fuel assembly have guide thimbles. Depending on the position of the assembly in the core, the guide thimbles are used for rod cluster control assemblies (RCCAs), neutron source assemblies, or burnable absorber assemblies. If none of these is required, the guide thimbles can be fitted with plugging devices to limit bypass flow. The guide thimbles are joined to the bottom nozzles of the fuel assembly and also serve to support the fuel grids. The fuel grids consist of an "egg-crate" arrangement of interlocked straps that maintain lateral spacing between the rods. The straps have spring fingers and dimples which grip and support the fuel rods. The grids also have coolant-mixing vanes. The fuel rods consist of slightly enriched uranium, in the form of cylindrical pellets of uranium dioxide, contained in Zircaloy-4 or ZIRLO™ tubing. The tubing is plugged and seal-welded at the ends to encapsulate the fuel. All fuel rods are pressurized internally with helium during fabrication to reduce clad creepdown during operation and thereby to increase fatigue life.

The bottom nozzle is a box-like structure which serves as the lower structural element of the fuel assembly and directs the coolant flow distribution to the assembly. The top nozzle assembly serves as the upper structural element of the fuel assembly and provides a partial protective housing for the RCCA or other components.

The RCCAs consist of 24 absorber rods fastened at the top end to a common hub or spider assembly. Each absorber rod consists of all Ag-In-Cd clad in stainless steel and plated with chrome. The RCCAs are used to control relatively rapid changes in reactivity and to control the axial power distribution.

The reactor core is cooled and moderated by light water at a pressure of 2250 psia. Soluble boron in the moderator/coolant serves as a neutron absorber. The concentration of boron is varied to control reactivity changes that occur relatively slowly, including the effects of fuel burnup and transient xenon. Burnable absorber rods and/or integral fuel burnable absorbers (IFBAs) are employed to limit the amount of soluble boron required and thereby to maintain the desired negative reactivity coefficients (see Section 4.2.2.3.2).

The nuclear design analyses establish the core locations for control rods and burnable absorbers and define design parameters, such as fuel enrichments and boron concentration in the coolant. The nuclear design analyses establish that the reactor core and the reactor control system satisfy all design criteria, even if the highest reactivity worth RCCA is in the fully withdrawn position. The core has inherent stability against diametral and azimuthal power oscillations. Axial power oscillations which may be induced by load changes and resultant transient xenon may be suppressed by the use of the control rods (RCCAs).

The thermal-hydraulic design analyses establish 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.

The performance of the core is monitored by fixed neutron detectors outside of the core, movable neutron detectors within the core, and thermocouples at the outlet of selected fuel assemblies. The ex-core nuclear instrumentation provides input to automatic control functions.

Table 4.1-1 presents a comparison of the principal nuclear, thermal-hydraulic, and mechanical design parameters for the VANTAGE 5 and the VANTAGE+ fuel assembly used in the Callaway unit. A design description for the current PERFORMANCE+ assembly type is provided in Section 4.2.2.

The analysis techniques employed in the core design are tabulated in Table 4.1-2. The mechanical loading conditions considered 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 Section 4.2.1.5 and neutron absorber rods, burnable absorber rods, neutron source rods, and thimble plug assemblies in Section 4.2.1.6. The dynamic analyses, input forcing functions, and response loadings are presented in Section 3.9(N). Cycle Specific Safety evaluations are performed in accordance with Reference 1.

4.1.1 REFERENCES

1. Davidson, S. L., (Ed.), et.al., "Westinghouse Reload Safety Evaluation Methodology," WCAP-9272-P-A and WCAP-9273-A, July 1985.

2. Davidson, S. L., (Ed.), “VANTAGE 5 Fuel Assembly Reference Core Report," WCAP-10444-P-A, September 1985.

3. Davidson, S. L., and Nuhfer, D. L. (Eds.), "VANTAGE+ Fuel Assembly Reference Core Report," WCAP-12610-P-A, April 1995.

TABLE 4.1-1 REACTOR DESIGN PARAMETERS

	Thermal and Hydraulic Design Parameters		V5/V+
1.	Reactor core heat output, MWt		3,565
2.	Reactor core heat output, 10^6 Btu/hr		12,164
3.	Heat generated in fuel, %		97.4
4.	Reactor Coolant System (CORE) pressure, nominal, psia		2274
5.	Minimum departure from nucleate boiling ratio for design transients	(TYP cell)	1.22
		(THM cell)	1.21
6.	DNB correlation		WRB-2
Coolant Flow*			
7.	Vessel minimum measured flow rate (including bypass), 10^6 lb_m /hr		142.4
8.	Vessel thermal design flow rate (including bypass), 10^6 lb_m/hr		139.4
9.	Core flow rate (excluding bypass, based on TDF) 10^6 lb_m /hr		127.4(a)
9a.	Bypass flow (% of TDF)***		8.6(a)
10.	Fuel assembly flow area for heat transfer, ft^2		54.13
11.	Average velocity along fuel rods, ft/sec (based on TDF)		14.9
12.	Core inlet mass velocity, 10^6 $lb_m/hr\text{-}ft^2$ (based on TDF)		2.35(a)
Coolant Temperature (based on TDF, non-RTDP)			
13.	Nominal inlet,°F		556.8(b)
14.	Average rise in vessel,°F		63.2
15.	Average rise in core,°F		68.4(a)
16.	Average in core,°F		593.1(a)
17.	Average in vessel,°F		588.4(i)

* Based on vessel average temperature of 588.4°F.

** Deleted

*** This is the bypass flow fraction used in non-RTDP analyses; the value for RTDP analyses is 6.9%.(a)

TABLE 4.1-1 (Sheet 2)

Thermal and Hydraulic Design Parameters

Heat Transfer		V5/V+
18.	Active heat transfer surface area, ft^2	57,505
19.	Average heat flux, Btu/hr-ft^2	206,085
20.	Maximum heat flux for normal operation, Btu/hr-ft^2	515,200
21.	Average linear power, kW/ft	5.69 (c)
22.	Peak linear power for normal operation, kW/ft	14.23 (c)
23.	Peak linear power resulting from overpower transients/operator errors, assuming a maximum overpower of 120%, kW/ft and will not exceed centerline melt	22.46 (d)
24.	Heat flux hot channel factor, F_Q	2.50(e)
25.	Not used	
	Core Mechanical Design Parameters	
26.	Design	RCC canless, 17 x 17
27.	Number of fuel assemblies	193
28.	UO_2 rods per assembly	264 (f)
29.	Rod pitch, in.	0.496
30.	Overall dimensions, in. (f)	8.426 x 8.426
31.	Nominal Fuel weight, KgU per assembly	423.103+ 423.204++ 414.364+++
32.	Clad weight, lb	53,840

+ An assembly with no axial blanket pellets.

++ An assembly with solid axial blanket pellets.

+++ An assembly with annualar axial blanket pellets

TABLE 4.1-1 (Sheet 3)

	<u>Core Mechanical Design Parameters</u>	
33.	Number of grids per assembly	6 (Zircaloy/ZIRLO^{TM}-mix) 3 (Zircaloy/ZIRLO^{TM}-IFM) 2 (Inconel) 0/1(Inconel-protective)
34.	Loading technique	2 or 3 region nonuniform
Fuel Rods		
35.	Number	50,952
36.	Outside diameter, in.	0.360
37.	Diametral gap, in.	0.0062
38.	Clad thickness, in.	0.0225
39.	Clad material	Zircaloy-4/ ZIRLO^{TM}
Fuel Pellets		
40.	Material	UO_2 sintered
41.	Density, % of theoretical	95.5
42.	Diameter, in.	0.3088
43.	Length, in.	0.370 (enriched fuel) 0.462 or 0.500 (natural to fully enriched annular or solid blanket)
Rod Cluster Control Assemblies		
44.	Neutron absorber	
	Full length, Ag-In-Cd	Ag-In-Cd
45.	Cladding Material	Type 304 SS-cold worked with chrome plate
46.	Clad thickness, in.	0.0185
47.	Number of clusters, full length	53
48.	Number of absorber rods per cluster	24

TABLE 4.1-1 (Sheet 4)

Core Structure		
49.	Core barrel, I.D./O.D., in.	148.0/152.5
50.	Thermal shield	Neutron pad design
51.	Baffle thickness, in.	0.88
‘Structure Characteristics		
52.	Core diameter, equivalent, in.	132.7
53.	Core height, active fuel, in.	143.7
Reflector Thickness and Composition		
54.	Top, water plus steel, in.	~10
55.	Bottom, water plus steel, in.	~10
56.	Side, water plus steel, in.	~15
57.	H_2O/U molecular ratio core, lattice, cold	2.71

Notes:

(a) The value is for thimble plugs removed.

(b) T_{in} = 557.4°F for RTDP analyses.

(c) See Table 4.4-1.

(d) See Section 4.3.2.2.6 and Table 15.0-4.

(e) The value of F_Q for normal operation is 2.50 as presented in the Core Operating Limits Report (COLR).

(f) See Section 4.1.

(g) See Figures 4.2-1 and 4.2-2 for various grid dimensions.

(h) See Table 4.3-1A.

(i) Operation with RCS T_{avg} reduced as low as 570.7°F has been evaluated to meet all criteria for acceptable plant operation.

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	MULTIFLEX, FORCE 2, LATFORC, finite element, structural analysis code, and others	3.7(N).2.1 3.9(N).2 3.9(N).3
Fuel rod design			
Fuel performance characteristics (temperature, internal pressure, clad stress, etc.)	Semiempirical thermal model of fuel rod with consideration of fuel density changes, heat transfer, fission gas release, etc.	Westinghouse fuel rod design model	4.2.1.2 4.2.1.3 4.2.3.2 4.2.3.3 4.3.3.1 4.4.2.11
Nuclear design			
1. Cross sections and group constants	Microscopic data; macroscopic constants for homogenized core regions; group constants for control rods with self-shielding	ENDF/B-VI PHOENIX-P	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 and 3-D, 2-group diffusion theory	ANC	4.3.3.3

TABLE 4.1-2 (Sheet 2)

Analysis	Technique	Computer Code	Section Referenced
3. Axial power distributions, control rod worths, and axial xenon distribution	1-D, 2-group diffusion theory	APOLLO	4.3.3.3
4. Fuel rod power	Integral transport theory	LASER	4.3.3.1
Effective resonance temperature	Monte Carlo weighting function	REPAD	
5. Criticality of reactor and fuel assemblies	2-D, multigroup-transport theory	PHOENIX-P	4.3.2.6
6. Vessel irradiation	Multigroup 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 crossflow resistance terms, solution is based on a one-pass model which simulates the core.	VIPRE-01	4.4.4.5.2

TABLE 4.1-2 (Sheet 3)

Analysis	Technique	Computer Code	Section Referenced
2. Transient departure from nucleate boiling analysis	Subchannel analysis of local fluid conditions in rod bundles during transients by including accumulation terms in conservation equations; solution is based on a one-pass model which simulates the core. Including the hot assembly and hot subchannel.	VIPRE-01	4.4.4.5.2

TABLE 4.1-3 DESIGN LOADING CONDITIONS FOR REACTOR CORE COMPONENTS

1. Fuel assembly weight
2. Fuel assembly spring forces
3. Internals weight
4. Control rod trip (equivalent static load)
5. Differential pressure
6. Spring preloads
7. Coolant flow forces (static)
8. Temperature gradients
9. Differences in thermal expansion
    a. Due to temperature differences
    b. Due to expansion of different materials
10. Interference between components
11. Vibration (mechanically or hydraulically induced)
12. One or more loops out of service
13. All operational transients listed in Table 3.9(N)-1
14. Pump overspeed
15. Seismic loads (Operating Basis Earthquake and Safe Shutdown Earthquake)
16. Blowdown forces (due to cold and hot leg break)

4.2 FUEL SYSTEM DESIGN

The plant design conditions 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; and Condition IV - Limiting Faults. Chapter 15.0 describes bases and plant operation and events involving each condition.

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

a. 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) ensure that:

   1. 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 cleanup system and are consistent with plant design bases.**

   2. The reactor can be brought to a safe state following a Condition III event with only a small fraction of fuel rods damaged** although sufficient fuel damage might occur to preclude 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.

b. The fuel assemblies are designed to withstand loads induced during shipping, handling, and core loading without exceeding the criteria of Section 4.2.1.5. Fuel assemblies can withstand loads introduced by a postulated reactor vessel head drop as evaluated in Section 9.1.4.3 for Westinghouse fuel.

c. 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 (if in such core locations).

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

** In any case, the fraction of fuel rods damaged must be limited so as to meet the dose guideline of 10 CFR 100.

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

e. 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.

4.2.1 DESIGN BASES

For both the VANTAGE+ and the VANTAGE 5 fuel assemblies, the fuel rod and fuel assembly design bases are established to satisfy the general performance and safety criteria presented in this section.

Design values for the properties of the materials which comprise the fuel rod, fuel assembly and incore control components are given in Reference 2 for Zircaloy clad fuel and in Reference 24 for ZIRLO™ clad fuel. Other supplemental fuel design criteria/ limits are given in Reference 26.

An option has been analyzed to allow Callaway to operate at reduced RCS average and feedwater temperatures. The T_{avg} can vary from 588.4°F down to 570.7°F and still meet the criteria for acceptability that govern plant operation.

4.2.1.1 Cladding

a. Material and Mechanical Properties

Zircaloy-4 and ZIRLO™ combines neutron economy (low absorption cross-section); high corrosion resistance to coolant, fuel, and fission products; and high strength and ductility at operating temperatures. Reference 1 documents the operating experience with Zircaloy-4 and ZIRLO™ as a clad material. Information on the mechanical properties of the cladding is given in References 2 and 24 with due consideration of temperature and irradiation effects.

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 Zircaloy 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.

2. Clad tensile strain

The total tensile creep strain is less than 1% from the unirradiated condition. The elastic tensile strain during a transient is less than 1% 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 (Ref. 1).

2. Vibration

Potential fretting wear due to vibration is prevented, ensuring 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 creepdown combined with grid spring relaxation.

d. Chemical properties of the cladding are discussed in Reference 2 for Zircaloy and Reference 24 for Zirlo.

4.2.1.2 Fuel Material

a. Thermal-physical properties

The thermal-physical properties of UO_2 are described in Reference 2 with due consideration of temperature and irradiation effects.

Fuel pellet temperatures - The center temperature of the hottest pellet is below the melting temperature of the UO_2 [melting point of 5080°F (Ref. 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 ensure 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 95.5 percent of theoretical. Additional information on fuel properties is given in Reference 2.

b. Fuel densification and fission product swelling

The design bases and models used for fuel densification and swelling are provided in References 20 and 30.

c. Chemical properties

Reference 2 provides the justification 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 detailed fuel design establishes such parameters as pellet size and density, cladding-pellet diametral gap, gas plenum size and helium prepressurization level. The design also considers effects such as fuel density changes, fission gas release, cladding 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 to satisfy the conservative design bases in the following subsections 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.

The basic fuel rod models and the ability to predict operating characteristics are given in References 20 and 30. Refer also to 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 25 demonstrates that clad flattening will not occur in Westinghouse fuel designs.

The rod internal gas pressure remains below the value which causes the fuel/clad diametral gap to increase due to outward cladding creep during steady state operation. Rod pressure is also limited so that extensive departure from nucleate boiling (DNB) propagation does not occur during normal operation and any accident event (Reference 7).

4.2.1.4 Spacer Grids

a. Mechanical limits and materials properties

The grid component strength criteria are based on experimental tests. The limit is established at the lower 95% confidence on the true mean crush strength. This limit is sufficient to ensure that under worst-case combined seismic and blowdown loads from a Condition IV, loss-of-coolant accident, the core will maintain a geometry amenable to cooling. As an integral part of the fuel assembly structure, the grids satisfy the applicable fuel assembly design bases and limits defined in Section 4.2.1.5.

The grid material and chemical properties are given in Reference 2 for Zircaloy and Reference 24 for ZIRLO™.

b. Vibration and fatigue

The grids 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 ensured 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. (See Reference 23.)

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. The stress categories and strength theory presented in the ASME Boiler and Pressure Vessel 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 2/3 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 2/3 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 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 or ZIRLO™ structural components, which consist of guide thimble 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 theory is used to evaluate the guide thimble design. For conservative purposes, the Zircaloy and ZIRLO™ unirradiated properties are used to define the stress limits.

(c) Abnormal loads during Condition III or IV - worst cases represented by combined seismic and blowdown loads.

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

2. The fuel assembly structural component stresses under faulted conditions are evaluated using primarily the methods outlined in Appendix F of the ASME 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 and ZIRLO™ components, the stress intensity limit Sm is set as the small value of 2/3 of the material yield strength, Sy or 1/3 of the ultimate strength, Su, at reactor operating temperature. This results in Zircaloy and 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 and ZIRLO™ unirradiated properties are used to define the stress limits.

The material and chemical properties of the fuel assembly components are given in Reference 2 for Zircaloy-4 and Reference 24 for ZIRLO™.

3. Thermal-hydraulic design

This topic is discussed in Section 4.4.

4.2.1.6 Incore Control Components

The control components are subdivided into permanent and temporary devices.

The permanent type components are the rod cluster control assemblies, secondary neutron source assemblies, and thimble plug assemblies. The temporary components are the burnable absorber assemblies, which are normally used for one cycle of operation, and the primary neutron source assemblies, which were used only in the initial core.

Materials are selected for compatibility in a pressurized water reactor 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. Material properties are given in Reference 2.

The design bases for each of the mentioned components are given in the following subsections.

a. Control rods

Design conditions which are considered under Article NB-3000 of the ASME Code, Section III are as follows:

1. External pressure equal to the reactor coolant system operating pressure with appropriate allowance for overpressure transients

2. Wear allowance equivalent to 1,000 reactor trips

3. Bending of the rod due to a misalignment in the guide tube

4. Forces imposed on the rods during rod drop

5. Loads imposed by the accelerations of the control rod drive mechanism

6. Radiation exposure during maximum core life

   The control rod cladding is cold drawn Type 304 stainless steel tubing. The stress intensity limit, Sm, for this material is defined as 2/3 of the 0.2 percent offset yield stress.

   The absorber material temperature does not exceed its melting temperature (1454°F for Ag-In-Cd - Ref. 2).*

7. Temperature effects at operating conditions

b. Burnable absorber rods

The cladding for burnable poison rods is designed using Article NB-3000 of the ASME Code, Section III, 1973 as a guide 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 do not interfere with reactor shutdown or cooling of the fuel rods.

The burnable 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 wet annular burnable absorber (WABA), described in Reference 8 (and Section 4.2.2.3.2), is designed to ensure that the absorber material --

---

* The melting point basis is determined by the nominal material melting point minus uncertainty.

aluminum oxide-boron carbide (Al_2O_3 - B_4C) -- will not exceed a temperature of 1200°F during Conditions I and II operation.

c. Neutron source rods

The neutron source rods are designed to withstand the following:

1. The external pressure equal to the reactor coolant system operating pressure with appropriate allowance for overpressure transients, and

2. An internal pressure equal to the pressure generated by released gases over the source rod life and prepressurization to extend the secondary source rod lifetime.

d. Thimble plug assembly

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

When used the thimble plug assemblies satisfy the following criteria:

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

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

3. Limit the flow through each occupied thimble to an acceptable design value

4.2.1.7 Surveillance Program

Section 4.2.4.5 and Sections 8 and 23 of Reference 9 discuss the testing and fuel surveillance operational experience program that has been and is being conducted to verify the adequacy of the fuel performance and design bases. Fuel surveillance and testing results, as they become available, are used to improve fuel rod design and manufacturing processes and ensure that the design bases and safety criteria are satisfied.

4.2.2 DESIGN DESCRIPTION

The V5 and V+ fuel assemblies, fuel rod, and incore control component design data are given in Table 4.3-1A.

Each fuel assembly consists of 264 fuel rods, 24 guide thimble tubes, and one instrumentation thimble tube arranged within a supporting structure. 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 a rod cluster control assembly, a neutron source assembly, a burnable absorber assembly, or a thimble plug assembly, depending on 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, 4.2-2B and 4.2-2C show a fuel assembly full-length view. 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.

Fuel assemblies are installed vertically in the reactor vessel and stand upright on the lower core plate, which is fitted with alignment pins to locate and orient each assembly. After all fuel assemblies are set in place, the upper support structure is installed. Alignment pins, built into the upper core plate, engage and locate the upper ends of the fuel assemblies. The upper core plate then bears downward against the holddown springs on the top nozzle of each fuel assembly to hold the fuel assemblies in place.

The VANTAGE+ assembly skeleton is identical to that previously described for VANTAGE 5 except for those modifications necessary to accommodate the intended fuel operation to higher burnups. The modifications consist of the use of ZIRLO™ guide thimbles and small skeleton dimensional alterations to provide additional fuel assembly and rod growth space at the extended burnup levels (Reference 22). The VANTAGE+ fuel assembly is shorter than the VANTAGE 5 fuel assembly. The grid centerline elevations of the VANTAGE+ are identical to those of the VANTAGE 5 fuel assembly, except for the top grid. The VANTAGE+ top grid has been lowered. However, since the VANTAGE+ fuel is intended to replace the VANTAGE 5 fuel, the VANTAGE+ exterior assembly envelope is equivalent in design dimensions, and the functional interface with the reactor internals is also equivalent to those of previous Westinghouse fuel designs. Also the VANTAGE+ fuel assembly is designed to be mechanically and hydraulically compatible with the VANTAGE 5 fuel assembly. The same function requirements and design criteria is previously established for the Westinghouse VANTAGE 5 fuel assembly remains valid for the VANTAGE+ fuel assembly. The PERFORMANCE+ and VANTAGE+ fuel assembly design and a comparison to the VANTAGE 5 and VANTAGE+ designs are provided in Table 4.1-1, and in Figures 4.2-2, 4.2-2B, and 4.2-2C.

Several design enhancements, taken from the summary of PERFORMANCE+ features, have been made to the VANTAGE+ fuel design for Callaway. These enhancements include the following:

- A protective grid is included at the bottom of the assembly, to provide an additional debris barrier, thereby improving fuel reliability. The protective grid will also provide additional grid/rod fretting resistance by supporting the bottom of the fuel rod.

- A lengthened, debris-mitigating fuel rod bottom end plug, longer than prior design, is used which extends up through the protective grid, limiting the span for fretting to the solid end plug, and

- A lengthened external grip top end plug is also used for reconstitution capability.

- A smaller gap between the rods and the bottom nozzle exists as a result of the longer end plug while maintaining the fuel stack at the same elevation.

- The mid-grids and IFM grids are fabricated from the ZIRLO™ alloy for improved corrosion performance margin to high burnup.

- The Top and Bottom Nozzles and top grid assembly sleeve are fabricated of a low cobalt Type 304 Stainless Steel for dose reduction.

- Extended burnup bottom grid design (Reference 22).

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. Visual confirmation of proper orientation is also 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 Zircaloy-4 or ZIRLO™ tubing which is plugged and seal welded at the ends to encapsulate the fuel. 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 currently incorporate a small chamfer and are dished slightly to allow greater axial expansion at the center of the pellets. The fuel rod length is sized to provide a longer plenum and bottom end plug. The bottom end plug has an internal-grip feature to facilitate rod loading and an improved lead-in profile for the nozzle reconstitution feature. The bottom end plug has an increased radius in more recent regions. This increased radius in the transition between the chamfer and the end of the plug facilitates rod insertion during fuel reconstitution. There are no changes in the critical dimensions of the bottom end plug or the pressure drop. Therefore, fuel rod performance and core safety considerations are not adversely affected. The pre-welded design of the fuel rod top and bottom end plugs includes a groove around the circumference of the plug. The previous top and bottom end plug design had no groove and the diameter of the plug was the same as the fuel rod tube. The improved top and bottom end plug has a slightly increased diameter from the groove to the shoulder of the plug. The grooved design allows a greater depth of weld penetration due to the more favorable heat transfer characteristics of the geometry and also provides a visual indication of a proper weld. The net effect after welding is a fuel rod that looks and performs exactly like the previous design.

The VANTAGE+ fuel rod represents a modification to the VANTAGE 5 fuel rod with ZIRLO™ used for fuel cladding in place of Zircaloy-4. The ZIRLO™ alloy is a zirconium alloy similar to Zircaloy-4, which has been specifically developed to enhance corrosion resistance. The VANTAGE+ fuel rods will contain, as in VANTAGE 5, enriched uranium dioxide fuel pellets, and an Integral Fuel Burnable Absorber (IFBA) coating on some of the enriched fuel pellets. Schematics of the various fuel rod designs are shown in Figures 4.2-3, 4.2-3A and 4.2-3B.

The VANTAGE+ fuel rod has the same clad wall thickness as the VANTAGE 5 design. The VANTAGE+ fuel rod length is shorter to provide room for the required fuel rod growth. To offset the reduction in the plenum length, the VANTAGE+ fuel rod has a variable pitch plenum spring. The variable pitch plenum spring provides the same support as the regular VANTAGE+ plenum spring but with fewer spring turns which translates to less spring volume. The bottom end plug has an internal grip feature to facilitate fuel rod loading on both designs (VANTAGE+ and VANTAGE-5) and provides appropriate lead-in for the removable top nozzle reconstitution feature. The VANTAGE+ fuel rod also has an oxide coating at the bottom end of the fuel rod. The extra layer of oxide coating provides additional debris induced rod fretting wear protection. An enhancement to the VANTAGE+ design was applied to include a longer bottom end plug. This longer end plug in conjunction with the protective grid provides a significant improvement against debris induced fretting on the rod. The top end plug was also modified to add an external grip at the top.

All fuel assemblies are manufactured with the fuel pellet stack at the same elevation relative to the bottom surface of the legs of the bottom nozzle. As a consequence of this standardization of pellet stack elevation, gaps between fuel rods and bottom nozzles have been adjusted. The gap between the fuel and bottom nozzle is consistent with the design requirement of the extended burnup bottom grid design (Reference 22). For the enhancement to the VANTAGE+ design, fuel rods are positioned close to the bottom nozzle at beginning-of-life in order to accommodate the installation of the protective grid.

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 in order to minimize compressive clad stresses and prevent clad flattening under coolant operating pressures.

The fuel rods are prepressurized and designed so that: 1) the internal gas pressure mechanical design limit given in Section 4.2.1.3 is not exceeded, 2) the cladding stress-strain limits (see 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.

Integral Fuel Burnable Absorber (IFBA)

The IFBA coated fuel pellets are identical to the enriched uranium dioxide pellets except for the addition of a thin zirconium diboride (ZrB_2) coating on the pellet cylindrical surface. Coated pellets occupy the central portion of the fuel column. The number and pattern of IFBA rods within an assembly may vary depending on specific application. The ends of the enriched coated pellets and enriched uncoated pellets are dished to allow for greater axial expansion at the pellet centerline and to increase the void volume for fission gas release. Analysis of IFBA rods includes any geometry changes necessary to model the presence of burnable absorber, and conservatively models the gas release from the coating.

The natural boron IFBA rods with nominal IFBA loading, referred to as a 1.0X loading, contain 1.50 mg of B-10 per inch of IFBA rod. Natural boron contains 18.3 nominal weight percent of the neutron absorbing isotope B-10 whereas enriched IFBA rods contain boron with 55 weight percent B-10.

Variation of IFBA loading is allowed according to the needs presented for power distribution control in designing the core each cycle. Enriched IFBA, containing a greater presence of the neutron absorbing isotope B-10, can be specified in terms of multiples of the natural boron IFBA rod. IFBA loadings of 1.5X (2.25 mg, B-10/inch) or 2.0X (3.0 mg, B-10/inch) have been applied in various regions, for example. This is not to preclude other loadings (1.25X, 1.6X) according to need. Also, variations in the length of the span of the fuel stack that include IFBA pellets is at the discretion of the core designer, according to requirements imposed. The acceptability of a proposed IFBA configuration is dependent on meeting fuel rod design criteria. The principle criteria which come to bear are the fuel rod internal pressure criterion and the clad stress criterion.

The rod internal pressure design criterion is impacted by the IFBA loading because some additional amount of Helium is produced from the “burnout” of the ZrB_2 pellet coating over and above the inventory of fission product gases. Different IFBA loadings will produce varying increases of rod internal pressure. To offset this extra Helium within the rod, the initial Helium prepressurization of the IFBA rod (100 psig backfill pressure) is reduced as compared to the non-IFBA rod (275 psig backfill pressure). Also, for greater IFBA loadings, further pressure reduction measures are employed, such as providing additional void volume in the rod by inserting annular axial blankets. The annular axial blanket is formed by inserting pellets with an annulus (and void of approximately 25% of a solid pellet) for several inches in the top and bottom ends of the fuel stack (see Table 4.3-1A). Regardless of the region specific IFBA load, continued compliance to the rod internal pressure design criterion is assured by reload analysis and evaluation.

The reduction of fuel rod internal pressure in the IFBA-loaded rod has an impact on the LOCA analysis of record which is based on beginning of life conditions. The LOCA analysis assumes a nominal pressurization, that is, for a typical non-IFBA rod. Applicable PCT penalties for the IFBA rod have been assigned and reported per 10 CFR 50.46. Also, issues have been raised regarding fuel rod corrosion and associated feedback effect on rod internal pressure and the pressure stress limit. A potential for violation of the fuel rod design criteria and 10 CFR 50.46 acceptance criterion was identified and reported (Reference 26). Violation of the "no gap reopening" fuel rod criteria does not automatically result in a 10 CFR 50.46 design criteria violation. Using the methodology of Reference 28, plant-specific calculations are performed for each cycle to assure the gap reopening criterion is met for the entire cycle burnup and to show each cycle's reload design does not violate the 17% total localized corrosion criterion of the 10 CFR 50.46.

With respect to the clad stress design criterion, generic analyses and significant design experience have demonstrated that changes in boron loadings and pre-pressurization levels consistent with current Westinghouse non-IFBA and IFBA designs do not significantly impact the ability of the fuel to satisfy clad stress limits. Furthermore, for Callaway, assuming 2X IFBA design (with annular axial blankets), the significant design parameters which can impact clad stress, including overall IFBA pellet diameter and rod internal pressure (which impacts the rate of clad creepdown onto the pellet surface), have been shown to satisfy all design criteria.

Axial Blankets

The axial blankets are a section of fuel pellets at each end of the fuel rod pellet stack. Axial blankets reduce neutron leakage and improve fuel utilization. The axial blankets utilize chamfered pellets which are physically different (length) than the other fuel pellets to help prevent accidental mixing during manufacturing.

The primary role of axial blankets in the reactor core is to improve fuel utilization (thus reducing the fuel costs) by reducing axial neutron leakage. Neutrons born near the top or bottom of the assembly have a high probability of migrating into the surrounding water/ core support structure without causing fission. By implementing axial blankets, a larger proportion of enriched fuel is located in higher flux regions of the core, thus reducing the probability of neutron leakage. Although the use of the enriched portion of the assembly maintains the same reactivity, the overall enrichment, averaged over the entire assembly length, is lower in blanketed fuel compared to non-blanketed fuel.

The safety implications of loading fuel with axial blankets are well documented, and are of the same nature as those arising from the insertion of fresh non-blanketed fuel in a reload core. The structural and thermal-hydraulic characteristics of blanketed fuel are virtually the same as non-blanketed fuel, while the neutronic differences are well within the ranges encountered in normal reload cores. The safety-related neutronic impact of axial blankets is that of altering the axial burnup and isotopic distributions of the fuel assembly. However, all reload cores feature widely varying axial burnup and isotopic distributions, regardless of the use of axial blankets. In addition, since the blankets are relatively small and are located in the regions of low flux, their impact on safety is negligible.

Axial blankets may not be used in all feed assemblies; however, when used, they may be either unenriched (i.e., 0.74 w/o U-235) or enriched (e.g. 2.6 w/o U-235), and either solid or annular, depending on the cycle-specific reload design.

4.2.2.2 Fuel Assembly Structure

The fuel assembly structure consists of a bottom nozzle, thimble screws, top nozzle, guide thimbles, inserts, lock tubes, and grids, as shown in Figures 4.2-2 through 4.2-2C.

4.2.2.2.1 Bottom Nozzle

The bottom nozzle serves as the bottom structural element of the fuel assembly and distributes the coolant flow to the assembly. The square nozzle is fabricated from Type 304 stainless steel and consists of a perforated plate, skirt, and four angle legs with bearing plates, as shown in Figures 4.2-2 through 4.2-2C. The legs and skirt 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 thimble screws which penetrate through the nozzle and mate with a threaded plug in each guide tube.

Coolant flows 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 plate. Indexing and positioning of the fuel assembly are provided by alignment holes in two diagonally opposite bearing plates which mate with locating pins in the lower core plate. Lateral loads on the fuel assembly are tranmitted to the lower core plate through the locating pins.

The Bottom Nozzle design has a reconstitution feature which allows the nozzle to be easily removed. A locking cup is used to lock the guide thimble screw in place at assembly or during reconstitution and nozzle reattachment if required.

The Callaway fuel has the Standardized Debris Filter Bottom Nozzle (SDFBN), which is a direct replacement for previous bottom nozzle designs, e.g., Debris Filter Bottom Nozzle (DFBN) and Modified Bottom Nozzle (MBN).

The SDFBN is a revised version of the 17x17 nozzle design. The revised design includes an improved pattern of flow holes which:

1. Reduces the passage of debris into the fuel assembly,

2. Maintains the structural integrity of the nozzle design,

3. Maintains the hydraulic performance of the design.

The revised diameter flow hole pattern includes an increased number of smaller holes (190 mils vs. 360 mils) that reduces the possibility of debris entering the active region of the fuel assembly. In addition, the SDFBN has eliminated the side skirt communication flow holes as a means of improving the debris mitigation performance of the bottom nozzle. The side skirt is a reinforcing skirt around the perimeter of the bottom nozzle which enhances reliability during postulated adverse handling conditions during refueling. Based on extensive testing, the SDFBN can reduce the passage of debris into the fuel region by 90% or greater. This reduction of debris is expected to decrease the incidence of debris-related fuel rod failures, and ultimately reduce RCS coolant activity. Since the SDFBN and DFBN are designed to maintain the hydraulic properties of the previous nozzle, their use has no adverse impact on safety.

Also used for the Callaway fuel is a cast composite bottom nozzle. This change represents a manufacturing process change. The composite bottom nozzle is a two-piece design incorporating a highly machined stainless steel adapter plate welded to a low cobalt investment casting. This casting replaces the former eight-piece weldment comprised of four cast legs and four rolled skirt plates. Also a pitch change was introduced to provide for cold alignment of the thimble tube and thimble screwhole to improve part fit-up during nozzle removal/replacement. The composite bottom nozzle design is functionally interchangeable with the former design. All bottom nozzle design criteria continue to be met.

4.2.2.2.2 Top Nozzle

The top nozzle functions as the upper structural element of the fuel assembly and provides a partial protective housing for the rod cluster control assembly or other components that are installed in the guide thimble tubes.

The Westinghouse Integral Nozzle (WIN) top nozzle design is a direct replacement for the reconstitutible top nozzle (RTN) design. The WIN design incorporates design and manufacturing improvements to eliminate the Alloy 718 spring screw for attachment of the holddown springs. The springs are assembled into the nozzle pad and pinned in place, as shown in Figure 4.2-2D. The WIN design provides a wedged rather than a clampled (bolted) joint for transfer of the fuel assembly holddown forces into the top nozzle structure. The flow plate, thermal characteristics, and method of attachment of the nozzle are all unchanged from the RTN top nozzle design.

The RTN top nozzle consists of an adapter plate, enclosure, top plate, and pads. The nozzle assembly comprises holddown springs, screws, and clamps mounted on the top plate, as shown in Figures 4.2-2, 4.2-2B and 4.2-2C. The springs are made of Inconel-718 and spring screws are made of Inconel-600 or Inconel-718, whereas other components are made of Type 304 stainless steel.

The 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 nozzle inserts which are locked into internal grooves in the adapter plate at their upper ends using a locktube and mechanically attached to the thimble tubes at their 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. For the RTN nozzle, holddown springs are mounted on the top plate and are retained by spring screws and clamps located at two diagonally opposite corners. On the other two corners, integral pads are positioned which contain alignment holes for locating the upper end of the fuel assembly.

A groove is provided in each thimble thru-hole in the nozzle plate to facilitate removal; and the nozzle plate thickness is reduced to provide additional axial space for the fuel rod growth.

The top nozzle is designed to allow reconstitution of the assembly. A stainless steel nozzle insert is mechanically connected to the top nozzle adapter plate by means of a preformed circumferential bulge near the top of the insert. The insert engages a mating groove in the wall of the adapter plate thimble tube throughhole. The insert has four 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 adapter 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 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 Section 2.3.2 in Reference 21.

4.2.2.2.3 Guide Thimble and Instrument Tube

The guide thimbles are structural members which also provide channels for the neutron absorber rods, burnable absorber rods, neutron source, or thimble plug assemblies. Each thimble is fabricated from Zircaloy-4 or ZIRLO™ tubing having two different diameters.

The guide tube diameter at the top section provides the annular area necessary to permit rapid control rod insertion during a reactor trip. The lower portion of the guide thimble reduces to a smaller diameter to produce a dashpot action near the end of the control rod travel during normal trip operation. The dashpot is provided with a calibrated flow port to decelerate the rod at the end of the travel. The top end of the guide thimble is fastened to an insert by three expansion swages. The insert fits into and is locked in the top nozzle adapter plate using a locktube. The lower end of the guide thimble is fitted with an end plug which is then fastened to the bottom nozzle by a crimp locked thimble screw.

Fuel rod support grids are fastened to the guide thimble assemblies to create an integrated structure. Attachment of the Inconel and Zircaloy/ZIRLO™ grids to the Zircaloy/ZIRLO™ thimble is performed using the mechanical fastening technique depicted in Figures 4.2-4 and 4.2-5 except for the bottom grid which is retained by clamping between the thimble endplug and the bottom nozzle.

An expanding tool is inserted into the inner diameter of the Zircaloy/Zirlo thimble tube at the elevation of grid sleeves that have been previously attached into the grid assembly. The four-lobed tool forces the thimble and sleeve outward to a predetermined diameter, thus joining the two components.

The top inconel grid sleeve, top nozzle insert and thimble are joined together using three bulge joint mechanical attachments as shown in Figure 4.2-6.

The intermediate mixing vane and IFM zircaloy grids employ a single bulge connection to the sleeve and thimble as shown in Figure 4.2-5.

The bottom grid assembly and bottom nozzle are attached to the fuel assembly by a crimp lock thimble screw, as shown in Figure 4.2-7. The stainless steel insert is spot-welded to the bottom grid and captured between the guide thimble end plug and the bottom nozzle. The bottom Inconel grid used with the Robust Protective Grid (RPG) has all twenty-four of the inserts spot-welded to it. Eight inserts are spot-welded to the RPG, four at the outer-most inserts on the grid diagonal and four at the inner-most inserts on the grid centerlines. This is sufficient to hold the protective grid close to the bottom nozzle.

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 a counterbore in the bottom nozzle at its lower end and is expanded at the top and mid grids in the same manner as the previously discussed expansion of the guide thimbles to the grids. This tube has a constant diameter and guides the incore neutron detectors.

The guide thimble tube ID provides an adequate nominal diametral clearance of 61 mils for the control rods. The rod drop time to the dashpot for accident analyses is 2.7 seconds for the V5/V+ assemblies. This rod drop time has been used in all safety analysis, and results of the analyses show that all safety limits are satisfied. The thimble tube ID also provides sufficient diametral clearance for burnable absorber rods, source rods, and dually compatible thimble plugs. The thimble plugs used at Callaway are dually compatible plugs which can be inserted into the assembly guide thimbles.

4.2.2.2.4 Assemblies

The fuel rods, as shown in Figures 4.2-2 through 4.2-2C are supported at intervals along their length by grid assemblies which maintain the lateral spacing between the rods. Each fuel rod is supported within each grid by the combination of support dimples and springs. The grid assembly consists of individual slotted straps assembled and interlocked into an "egg-crate" arrangement with the straps permanently joined at their points of intersection.

The grid material is Inconel-718 or ZIRLO™. The magnitude of the grid-restraining force on the fuel rod is set high enough to minimize possible fretting without overstressing the cladding at the points of contact between the grids and fuel rods. The grid assemblies also allow axial thermal expansion of the fuel rods without imposing restraint sufficient to develop buckling or distortion of the fuel rods. The chemical composition of the IFM and mid-grids fabricated with ZIRLO™ alloy is similar to Zircaloy-4 except for a slight reduction in the content of tin (Sn) and iron (Fe) and the elimination of chromium (Cr). The ZIRLO™ alloy also contains a nominal amount of niobium (Nb). These changes, although small, are responsible for the improved corrosion resistance of ZIRLO™ compared to Zircaloy-4. The ZIRLO™ mid-grids and IFM grids have shorter sleeves.

Four types of grid assemblies are used in each fuel assembly. Six ZIRLO™ grids, with mixing vanes projecting from the edges of the straps into the coolant stream, are used in the high heat flux elevations of the fuel assemblies to promote mixing of the coolant. Two grids, one at each end of the assembly, do not contain mixing vanes on the internal straps. The top and bottom non-mixing vane grids are Inconel. Three ZIRLO™ Intermediate Flow Mixing vane (IFM) grids promote flow mixing and are non-structural components. At the bottom end of the fuel assembly, one protective grid is utilized which is a non-structural component but provides a debris mitigation function. 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 adjacent surfaces during handling or loading and unloading of the core. The Inconel and ZIRLO™ girds have been sized appropriately to assure sufficient structural strength for the assembly.

The Intermediate Flow Mixer (IFM) grids are located in the three uppermost spans between the ZIRLO™ mixing vane structural grids and incorporate a similar mixing vane array. Their prime function is mid-span flow mixing in the hottest fuel assembly spans. Each IFM grid cell contains four dimples which are designed to prevent mid-span channel closure in the spans containing IFMs and fuel rod contact with the mixing vanes. This simplified cell arrangement allows short grid cells so that the IFM grid can accomplish its flow mixing objective with minimal pressure drop.

The IFM grids are not intended to be structural members. The outer strap configuration was designed similar to current fuel designs to preclude grid hang-up and damage during fuel handling. Additionally, the grid outer dimensions are smaller which further minimizes the potential for damage and reduces calculated forces during seismic/LOCA events. A coolable geometry is, therefore, assured at the IFM grid elevation, as well as at the structural grid elevation.

A snag-resistant feature has been applied to the IFM grid assemblies. The snag-resistant IFM grid assembly contains outer grid straps which are modified to help prevent fuel assembly hangup due to grid strap interference during fuel assembly removal. This was accomplished by changing the grid strap corner goemetry and adding guide tabs on the outer grid strap.

To counter the effects of extended burnup, the bottom grid spring was modified to give a higher spring force. This increase assures that adequate EOL spring force is provided. The modified spring shape/profile results in a 45% increase in spring force at BOL (increased from 7.2 lbs. to 10.5 lbs.). This will accommodate a 60,000 MWD/MTU burnup (Reference 22). Evaluations were performed to determine the effects of modifying the bottom grid spring to provide a higher spring force. The modifications to the bottom grid spring have no adverse effect on the thermal-hydraulic performance. The structural support of the fuel rod end is enhanced with the increased bottom grid spring forces. Therefore, the resistance of the fuel rod to crossflow excitations is improved. The ability of the grid to withstand externally applied loads has not changed. All bottom grid design criteria continue to be met.

The protective grid is made of Inconel 718. The inner straps are laser welded at the intersects and to the perimeter straps similar to the ZIRLO™ grids. The top of the perimeter retains the anti-snag features used in the current top and bottom grids. The bottom portion of the perimeter strap is bent inward toward the top perimeter chamfer on the bottom nozzle to minimize hang-up potential. The inner strap contains compliant dimples coplanar in each grid cell. The dimples are coplanar to enhance debris-catching capability and to provide appropriate rod support. To accommodate the coplanar dimples, alternating cells have the dimples at alternating elevations. The lower sets of dimples are positioned to assure that the full diameter of the fuel rod bottom end plug is resting on the dimples. The protective grid is not intended to be a structural member.

Commencing with the fresh fuel installed in Cycle 21 and subsequent reloads, the Robust Protective Grid (RPG) will be utilized. The RPG was developed as a result of observed failures in the field, as noted in Post Irradiation Exams (PIE) performed at several different plants. It was determined that observed failures were the result of two primary issues: 1) fatigue failure within the protective grid itself at the top of the end strap and 2) stress corrosion cracking (SCC) primarily within the rod support dimples. The RPG implements design changes such as increasing the maximum nominal height of the grid, increasing the ligament length and the radii of the ligament cutouts, and the use of four additional inserts for a total of 8 inserts to help strengthen the grid. The nominal height of the grid was increased to allow “V-notch” window cutouts to be added to help minimize flow-induced vibration caused by vortex shedding at the training edge of the inner grid straps. These design changes incorporated into the RPG design help address the issues of fatigue failures and failures due to SCC. It was demonstrated that the above changes do not impact the thermal hydraulic performance of the RPG as there is no change to the pressure loss coefficient. In addition, the RPG retains the original protective grid function as a debris mitigation feature. The RPG satisfies the requirements of Regulatory Guide 1.82, which requires that no passageway in the fuel assembly be less than the size of the containment sump filter.

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:

a. Fuel depletion and fission product buildup

b. Cold to hot, zero power reactivity change

c. Reactivity change produced by intermediate-term fission products, such as xenon and samarium

d. Burnable absorber depletion

The chemical and volume control system is discussed in Chapter 9.0.

The rod cluster control assemblies provide reactivity control for:

a. Shutdown

b. Reactivity changes resulting from coolant temperature changes in the power range

c. Reactivity changes associated with the power coefficient of reactivity

d. Reactivity changes resulting from void formation

The rod cluster control assemblies and their control rod drive mechanisms are the only moving parts in the reactor. Figure 4.2-8 illustrates the rod cluster control and control rod drive mechanism 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 control rod drive mechanism assembly is described in Section 3.9(N).4.

The neutron source assemblies provide a means of monitoring the core during periods of low neutron level. The thimble plug assemblies can be used to limit bypass flow through those fuel assembly thimbles which do not contain control rods, burnable absorber rods, or neutron source rods.

4.2.2.3.1 Rod Cluster Control Assembly

The rod cluster control assemblies are divided into two categories: control and shutdown. The control groups compensate for reactivity changes associated with 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 groups. First, the total reactivity worth must be adequate to meet the nuclear requirements of the reactor. Second, in view of the fact that 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 banks provide adequate shutdown margin.

A rod cluster control assembly is composed of 24 neutron absorber rods fastened at the top end to a common spider assembly, as illustrated in Figure 4.2-9.

The absorber material used in the Callaway control rods is a solid Ag-In-Cd bar which is essentially black to thermal neutrons and has sufficient additional resonance absorption to significantly increase its worth. The absorber material is sealed in cold worked, high purity stainless steel tubes (see Figure 4.2-10). A thin chrome electroplate is applied to the tubing outer surface over a specified length which is in contact with the reactor internal guides. The cladding surface provides increased resistance to tube wear. Sufficient diametral and end clearances are provided to accommodate relative thermal expansion. In addition, the absorber diameter is slightly reduced at the lower extremity of the rodlets in order to accommodate absorber swelling and minimize cladding interaction.

The bottom plugs are 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 current Ag-In-Cd rod cluster control assemblies (RCCAs) are referred to as enhanced performance assemblies (EP-RCCAs).

The small increase in outer diameter of the rodlets due to the chrome plating (less than or equal to 1.5 mils) did not increase rod drop times significantly.

Further, due to the relatively high yield strength of the stainless cladding (minimum of 62,000 psi at 600 degrees°F), use of this material results in a design with practical wall thickness that meets ASME Section III type stress criteria for stresses induced by operating conditions. The high purity stainless steel has a significant reduction in cobalt content in the EP-RCCAs. The chrome plate further reduces the effluence of cobalt into the coolant, thereby benefiting the ALARA conditions. The high purity cladding is also very resistant to irradiation-assisted stress corrosion cracking which is a phenomena that has caused some cracking in the bottom tips of the rodlets in the past.

The absorber rod end plugs are 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 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 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. Two coil springs inside the spider body absorbs the impact energy at the end of a trip insertion. The radial vanes are joined to the hub by tack welding and brazing, and the fingers are joined to the vanes by brazing. A bolt which holds the springs and their retainer 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 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. 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 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 Burnable Absorber Assembly

Each burnable absorber assembly consists of burnable absorber rods attached to a hold-down assembly. Burnable absorber assemblies are shown in Figure 4.2-11.

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

The wet annular burnable absorber (WABA), rods described in Reference 8, consist of aluminum oxide-boron carbide pellets contained within two concentric Zircaloy tubes which form the inner and outer cladding. The Zircaloy tubes are plugged and seal welded at the ends to encapsulate the absorber pellets. A hold-down device is placed on top of the pellet stack to keep it in position and to accommodate pellet stack growth. An annular plenum is provided in the top end of the rods to accommodate the helium gas produced during boron depletion. The reactor coolant flows inside the inner tubing and outside the outer tubing of the annular rod. A typical burnable absorber rod (WABA) is shown in longitudinal and transverse cross sections in Figure 4.2-12a.

The rods are statically suspended and positioned in selected guide thimbles within specified fuel assemblies. 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 which fits within the fuel assembly top nozzle and rests on the adaptor plate. The retaining plate (and the absorber rods) is 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 assures that the absorber rods cannot be ejected from the core by flow forces. Each rod is permanently attached to the base plate by a nut which is lock welded into place.

The clad in the rod assemblies containing aluminum oxide-boron carbide pellets is Zircaloy. All other structural materials are Types 304 or 308 stainless steel except the springs which are Inconel-718. The absorber rods provide sufficient boron content to meet the criteria discussed in Section 4.3.1.

IFBAs are predominantly used, as discussed in Section 4.2.2.1; however, WABA rods are still retained as needed in feed assemblies.

4.2.2.3.3 Neutron Source Assembly

The purpose of the neutron source assembly is to provide base neutron level to ensure that the neutron detectors are operational and responding to core multiplication neutrons. For the first core, a neutron source was placed in the reactor to provide a positive neutron count of at least 2 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 permits detection of changes in the core multiplication factor during core loading and approach to criticality. This can be done since the multiplication factor is related to an inverse function of the detector count rate. Changes in the multiplication factor can be detected during addition of fuel assemblies while loading the core, changes in control rod positions, and changes in boron concentration.

The primary source rods, containing a radioactive material, spontaneously emitted neutrons during initial core loading, reactor startup, and initial operation of the first core.

After the primary source rods decayed beyond the desired neutron flux level, neutrons were then supplied by the secondary source rods. The secondary source rods contain a stable material, which is activated during reactor operation. The activation results in the subsequent release of neutrons.

Four source assemblies were installed in the initial reactor core: two primary source assemblies and two secondary source assemblies. After the initial reactor core, subsequent cores typically contain two secondary source assemblies, but may contain four secondary source assemblies. Four source assemblies may be installed if initial activation of two new source assemblies is needed prior to the existing source assemblies reaching the end of their design life. Following such transition cycles, the expired source assemblies are removed from service and the two newly activated source assemblies remain in use over a 12-year design life. The secondary source assemblies utilized have encapsulated source rods. The encapsulated sources replace the original secondary sources and alleviate concerns with leaking source rods reported at other plants. Each secondary source assembly contains either four or six secondary source rods and a number of thimble plugs. The source assemblies are shown in Figures 4.2-13, 4.2-14 and 4.2-14A.

Neutron source assemblies are positioned 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 Figures 4.2-13, 4.2-14, and 4.2-14A the source assemblies contain a holddown assembly identical to that of the burnable absorber assembly.

Secondary source rods have the same cladding material as the glass absorber rods. The secondary source rods contain Sb-Be pellets stacked to provide approximately 2000 grams of Sb-Be in each assembly. The rods in each assembly are permanently fastened at the top end to a holddown assembly.

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.

4.2.2.3.4 Thimble Plug Assembly

Thimble plug assemblies can be used to limit bypass flow through the rod cluster control guide thimbles in fuel assemblies which do not contain either control rods, source rods, or burnable absorber rods.

The thimble plug assemblies consist of a flat baseplate with short rods suspended from the bottom surface and a spring pack assembly, as shown in Figure 4.2-15. The 24 short rods, called thimble 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 or crimped to the threaded end of the plug. Similar short plugs are also used on the source assemblies and burnable poison assemblies to plug the ends of all vacant fuel assembly guide thimbles. When in the core, the thimble plug assemblies interface with both the upper core plate and with the fuel assembly top nozzles by resting on the adapter plate. The spring pack is compressed by the upper core plate when the upper internals assembly is lowered into place.

Thimble plugs can be removed or reinserted according to the need or desire for limitation on bypass flow. Analyses have been performed bounding either condition, having the core with thimble plugs in all available locations, or leaving all available thimble locations open (no thimble plugs in the core).

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

4.2.3 DESIGN EVALUATION

The fuel assemblies, fuel rods, and incore control components are designed to satisfy the performance and safety criteria of the introduction to 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 Conditions II, III, IV or anticipated transients without trip on fuel integrity are presented in Chapter 15.0 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 rods 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 highest 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 is limiting with respect to all design criteria.

After identifying the limiting rod(s), a worst-case performance analysis is performed which considers the effects of rod operating history, model uncertainties, and dimensional variations. To verify adherence to the design criteria, the evaluation considers the effects of postulated transient power changes during operation consistent with Conditions I and II. These transient power increases can affect both rod average and local power levels. 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 and fuel assembly models used for the performance evaluations are documented and maintained under an appropriate control system. Materials properties used in the design evaluations are given in Reference 2.

4.2.3.1 Cladding

a. 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 motions 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, based on out-of-pile flow tests, performance of similarly designed fuel in operating reactors, and design analyses.

Clad fretting and fuel vibration have been experimentally investigated, as shown in Reference 14.

b. Fuel rod internal pressure and cladding stresses

A burnup dependent fission gas release model (References 20 and 30) is used to determine 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 and extensive DNB propagation 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 10,000 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 during steady state operation due to stress relaxation. 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.

Tensile stresses can occur once the clad has come into contact with the pellet. These stresses are induced by the fuel pellet swelling during irradiation. Swelling of the fuel pellet 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). The 1-percent strain criterion is extremely conservative for fuel-swelling driven clad strain because the strain rate associated with solid fission products swelling is very slow. A detailed discussion on fuel rod performance is given in Section 4.2.3.3.

c. Materials and chemical evaluation

Zircaloy-4 and ZIRLO™ clad have a high corrosion resistance to the coolant, fuel, and fission products. As shown in Reference 1, there is considerable pressurized water reactor operating experience on the capability of Zircaloy and ZIRLO™ as a clad material. Controls on fuel fabrication specify maximum moisture levels to preclude clad hydriding.

Metallographic examination of irradiated commercial fuel rods has 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.

d. Fretting

Cladding fretting has been experimentally investigated as shown in Reference 7. No significant fretting of the cladding is expected during the life of the fuel assembly.

e. Stress Corrosion

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 cladding tensile stresses, large conentrations of selected fission products (such as iodine) can chemically attack the Zircaloy and ZIRLO™ tubing and can lead to eventual cladding cracking. Extensive postirradiation examination has produced no in-pile evidence that this mechanism is operative in commercial fuel.

f. Cycling and Fatigue

A comprehensive review of the available strain fatigue models was conducted by Westinghouse as early as 1968. This review include the Langer-O'Donnell model (Reference 15,) 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 bound the results of the Westinghouse testing program.

The Westinghouse testing program was subdivided into the following subprograms:

1. A rotating bend fatigue experiment on unirradiated Zircaloy-4 specimens at room temperature and at 725°F. Both hydrided and nonhydrided Zircaloy-4 cladding were tested.

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

3. A fatigue test program on irradiated cladding from the CVS and Yankee Core V conducted at Battelle Memorial Institute.

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

The design equations followed the concept for the fatigue design criterion according to the ASME Boiler and Pressure Vessel code, Section III.

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 cladding by low cycle strain fatigue. During their normal residence time in reactor, the fuel rods may be subjected to ~1000 cycles with typical changes in power level from 50% to 100% of their steady-state values.

The assessment of the fatigue life of the fuel rod cladding 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 cladding. This difficulty arises, for example, from such highly unpreditable 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 Zr-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.

g. Rod Bowing

For Zircaloy-4 grid fuel assemblies the largest contributors to significant rod bow are high end grid forces (Inconel grids) and low stiffness Zircaloy-4 grid springs. The VANTAGE 5 and VANTAGE+ fuel assembly designs have low spring forces on the top Inconel grids. This reduces the end loadings on the fuel rod brought about by fuel rod growth. The Zircaloy or ZIRLO™ mid-grid design has a very high spring stiffness. This design offers high resistance to fuel rod rotation within a grid and still has low spring force to allow the rods to slip freely thru the grids. This design reduces rod bow of the VANTAGE+ and VANTAGE 5 (or any Ziraloy grid design) to values as good or better than all Inconel gridded assemblies.

The current conservative NRC-approved methodology for comparing the magnitude of rod bow between two different fuel assembly designs is given in Reference 14. Based on this approved methodology, a comparison of L^2/I (where I = the fuel rod bending moment of inertia and L = span length) and the initial rod-to-rod gap for both the 17x17 VANTAGE 5 and VANTAGE+ fuel assembly designs, shows that the amount of rod bow at any given burnup is essentially the same for both 17x17 VANTAGE 5 and VANTAGE+ fuel assemblies.

The effects of rod bow on DNBR are described in Subsection 4.4.2.2.5.

h. Consequences of Power-Coolant Mismatch

This subject is discussed in Chapter 15.10.

i. Irradiation Stability of the Cladding

As shown in Reference 1, there is considerable PWR operating experience on the capability of Zircaloy as a cladding material. Extensive experience with irradiated Zircaloy-4 is summarized in Reference 2, and in Appendices A-E in Reference 24 for ZIRLO™.

j. Creep Collapse and Creepdown

This subject and the associated irradiation stability of cladding have been evaluated using the model described in Reference 25. It has been established that a clad collapse has been eliminated from the design basis.

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 protects 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. 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 pressurized water reactors (Ref. 9) 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 95.5-percent initial fuel density.

The evaluation of fuel densification effects and their consideration in fuel design are described in References 20 and 30 and the treatment of fuel swelling and fission gas release are described in References 20 and 30.

The effects of waterlogging on fuel behavior are discussed in Section 4.2.3.3.

4.2.3.3 Fuel Rod Performance

In the calculation of the steady state performance of a nuclear fuel rod, the following interacting factors must be considered.

a. Clad creep and elastic deflection

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

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

These effects are evaluated using a fuel rod design model (References 20 and 30). The model modifications for time dependent fuel densification are given in References 20 and 30. With the above 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 and to avoid the potential for flattened rod formation. It is limited, however, by the design criteria for the rod internal pressure (see Section 4.2.1.3).

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 or 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 (References 20 and 30). 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 with a variety of published and proprietary data on fission gas release, fuel temperatures, and clad deflections (References 20 and 30). These data include variations in power, time, fuel density, and geometry.

a. 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. The technology of using prepressurized fuel rods has been developed to further this objective.

The gap between the fuel and clad is initially 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 fuel/clad contact. Once fuel/clad contact occurs, changes in power level 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 contact occurs and hence significantly reduces the 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 fatique 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 developed 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 UO_2, and additional cracks in the fuel are created which limits the magnitude of the stress concentration in the clad.

As part of the 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 yield stress in order to assure that the stress/strain criteria are satisfied.

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. Such increases are a consequence of 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 full-length control rod movement. In the mechanical design model, lead rod burnup values are obtained using best estimate power histories, as determined by core physics calculations. During burnup, 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 the 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 effect of transients on the fuel rod cladding is discussed in Section 4.2.3.1.f.

b. Irradiation experience

Westinghouse fuel operational experience is presented in Reference 1. Additional test assembly and test rod experience are given in Sections 8 and 23 of Reference 9.

c. Fuel and cladding temperature

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

d. Waterlogging

Local cladding deformations typical for waterlogging bursts have never been observed in commercial Westinghouse fuel. Waterlogging 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.

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 waterlogging 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 or increase the effects of any postulated transients.

More information is provided in References 16 and 17.

e. 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 above.

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

Clad flattening, as shown in Reference 6, has been observed in some operating power reactors. Thermal expansion (axial) of the fuel rod stack against a flattened section of the clad could cause failure of the clad. This is no longer a concern because clad flattening is precluded during the fuel residence in the core (see Section 4.2.3.1).

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.

f. 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 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.

g. 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.

As shown in Reference 18, seismic and loss-of-coolant accident evaluations show that the grids will maintain a geometry that is capable of being cooled under the worst-case accident Condition III and IV events.

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 axial thermal expansion and Zircaloy/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/ZIRLO™ irradiation growth does not result in rod end interferences. Stresses in the fuel assembly caused by tripping of the rod cluster control assembly 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.

The fuel assembly design loads for shipping have been established at 4 g axial and 6 g lateral (See Reference 23). Accelerometers are permanently placed into the shipping cask to monitor and detect fuel assembly accelerations that would exceed the criteria. Past history and experience have 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. Seismic analysis of the fuel assembly is presented in Reference 18.

4.2.3.5.2 Dimensional Stability

No interference between control rods and thimble tubes will occur during insertion of the rods following a postulated loss-of-coolant accident transient due to fuel rod swelling, thermal expansion, or bowing. In the early phase of the transient following the coolant 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.

Reference 18 shows that the fuel assemblies will maintain a geometry amenable to cooling during a combined seismic and double-ended loss-of-coolant accident.

4.2.3.6 Reactivity Control Assembly and Burnable Absorber Rods

a. Internal pressure and cladding stresses during normal, transient and accident conditions

   The designs of the burnable absorber and source rods provide a sufficient cold void volume to accommodate the internal pressure increase during operation. This is not a concern for the absorber rod because no gas is released by the absorber material.

For the burnable absorber rods, the use of glass or aluminum oxide-boron carbide in tubular form provides a central void volume along the length of the rods (see Figures 4.2-12 and 4.2-12a). For the source rods, a void volume is provided within the rod in order to limit the internal pressure increase until end of life (see Figures 4.2-13 and 4.2-14).

The stress analysis of these rods assumes 100-percent gas release to the rod void volume, in addition to the initial pressure within the rod.

During normal transient and accident conditions the void volume limits the internal pressures to values which satisfy the criteria in Section 4.2.1.6. These limits are established not only to ensure that peak stresses do not reach unacceptable values, but also to limit the amplitude of the oscillatory stress component in consideration of the fatigue characteristics of the materials.

Rod, guide thimble, and dashpot flow analyses indicate that the flow is sufficient to prevent coolant boiling within the guide thimble. Therefore, clad temperatures at which the clad material has adequate strength to resist coolant operating pressures and rod internal pressures are maintained.

b. Thermal stability of the absorber material, including phase changes and thermal expansion

The radial and axial temperature profiles within the source and absorber rods have been determined by considering gap conductance, thermal expansion, neutron or gamma heating of the contained material as well as gamma heating of the clad.

The maximum temperature of the Ag-In-Cd control rod absorber material was calculated and found to be significantly less than the respective material melting point, and occurs axially at only the highest flux region. The thermal expansion properties of the absorber material and the phase changes are discussed in Reference 2.

The maximum temperature of the borosilicate glass absorber was calculated to be about 1300°F and takes place following the initial rise to power. As the operating cycle proceeds, the glass temperature decreases 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.

Sufficient diametral and end clearances have been provided in the neutron absorber, burnable absorber and source rods to accommodate the relative thermal expansions between the enclosed material and the surrounding clad and end plug.

c. Irradiation stability of the absorber material, taking into consideration gas release and swelling

The irradiation stability of the absorber material is discussed in Reference 2 for the Ag-In-Cd material. Irradiation produces no deleterious effects in the absorber material.

Gas release is not a concern for the control rod material because no gas is released by the absorber material. Sufficient diametral and end clearances are provided to accommodate swelling of the absorber material.

Based on experience with borosilicate glass and on nuclear and thermal calculations, gross swelling or cracking of the glass tubing was not expected during operation. Some minor creep of the glass at the hot spot, on the inner surface of the tube, could have occurred but would continue only until the glass came in contact with the inner liner. The wall thickness of the inner liner was 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 ends of the inner liner were open to allow helium, which diffuses out of the glass, to occupy the central void.

d. Potential for chemical interaction, including possible waterlogging rupture

The structural materials selected have good resistance to irradiation damage and are compatible with the reactor environment.

Corrosion of the materials exposed to the coolant is quite low, and proper control of chloride and oxygen in the coolant will prevent the occurrence of stress corrosion. The potential for the interference with rod cluster control movement due to possible corrosion phenomena is very low.

Waterlogging rupture is not a failure mechanism associated with Westinghouse-designed control rods. However, a breach of the cladding for any postulated reason does not result in serious consequences. The Ag-In-Cd absorber material is relatively inert and would still remain remote from high coolant velocity regions. Rapid loss of material resulting in significant loss of reactivity control material would not occur.

4.2.4 TESTING AND INSPECTION PLAN

4.2.4.1 Quality Assurance Program

The quality assurance program plan of the Westinghouse Nuclear Fuel Division is summarized in the Quality Management System program (latest revision); see Reference 19.

The program provides for control over all activities affecting product quality, commencing with design and development and continuing through procurement, materials handling, fabrication, testing and inspection, storage, and transportation. The program also provides for the indoctrination and training of personnel and for the auditing of activities affecting product quality through a formal auditing program.

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

4.2.4.2 Quality Control

Quality control 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.

a. Fuel system components and parts

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

   All material used in this core is accepted and released by Quality Control.

b. Pellets

   Inspection is performed for dimensional characteristics such as diameter, density and length. 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.

c. Rod inspection

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

1. Leak testing

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

2. Enclosure welds

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

3. Dimensional

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

4. Plenum dimensions

   All of the fuel rods are inspected by X-ray or other approved methods, as discussed in Section 4.2.4.4, to ensure proper plenum dimensions.

5. Pellet-to-pellet gaps

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

6. All fuel rods are gamma scanned. Non IFBA fuel rods are 'active' gamma scanned. This process is performed to verify enrichment control prior to acceptance for assembly loading.

7. Traceability

   Traceability of rods and associated rod components is established by Quality Control.

d. Assemblies

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

e. Other inspections

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

1. Tool and gage 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 of calibration and conditions of tools are kept.

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

3. Surveillance inspection, where appropriate, and audits of outside contractors are performed to ensure conformance with specified requirements.

f. Process control

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

The UO_2 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 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 Quality Control. 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 original 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; the bottom end plug is permanently identified to the contract and enrichment; and the end plugs are then inert welded to seal the tube. The fuel tube remains coded and traceability identified until just prior to installation in the fuel assembly. The traceability code and end plug identification character provide a cross reference of the fuel contained in the fuel rods.

At the time of installation into an assembly, the operator scans the unique traceability code to identify each rod in its position with a given assembly. This operation ensures all fuel rods in an assembly are of the correct character describing the fuel enrichment and density for the core region being fabricated. 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

Tests and inspections are performed on each reactivity control component to verify the mechanical characteristics. In the case of the rod cluster control assembly, prototype testing has been conducted, and both manufacturing tests/inspections and functional testing at the plant site are performed.

During the component manufacturing phase, the following requirements apply to the reactivity control components to ensure proper functioning during reactor operation:

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

b. All spider assemblies are proof tested by applying a load to the spider body so that an approximate weight can be applied to each vane. This proof load, combined with an increased factor, provides a bending moment at the spider body to simulate the acceleration imposed by the control rod drive mechanisms.

c. All rods are checked for integrity by the methods described in Section 4.2.4.2, item c.

d. To ensure 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 within a given tolerance is required on the entire inserted length of each rod assembly. Following core loading, but prior to initial criticality, the rod cluster control assemblies are tested to demonstrate reliable operation in accordance with Regulatory Guide 1.68, Appendix A, Section 2.b. This testing is further discussed in Section 14.2.12.3.27.

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 rod cluster control assembly, as required by the technical specifications. In addition, periodic drop tests of the rod cluster control assemblies are performed at each refueling shutdown to demonstrate continued ability to meet trip time requirements.

If a 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 rod cluster control assembly can be tolerated. More than one inoperable rod cluster control assembly could be tolerated, but would impose additional demands on the plant operator. Therefore, the number of inoperable rod cluster control assemblies 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 in Sections 4.2.4.1 through 4.2.4.3 and are performed to meet all Westinghouse requirements.

4.2.4.5 Inservice Surveillance

Westinghouse has conducted a program to examine detailed aspects of the 17 x 17 fuel assembly. This program is described in Section 23 of Reference 9. Reference 1 is periodically updated in order to provide recent results of operating experience with Westinghouse fuel and incore control components.

4.2.4.6 Onsite Inspection

Written procedures are used by the station staff for the post-shipment inspection of all new fuel and associated components, such as control rods, plugs, and inserts. Fuel handling procedures specify the sequence in which handling and inspection take place.

Loaded fuel containers, when received onsite, are externally inspected to ensure that labels and markings are intact and seals are unbroken. After the containers are opened, the shock indicators attached to the suspended internals are inspected to determine if movement during transit exceeded design limitations.

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

Control rod, source and burnable poison assemblies usually are shipped in fuel assemblies and are inspected after 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, and the exposed section is visually inspected for mechanical integrity, replaced in the fuel assembly and stored with the fuel assembly. Control rod source or burnable poison assemblies may be stored separately or within fuel assemblies in the new fuel storage area.

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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) presented in 10 CFR 50, Appendix A. Where applicable, supplemental criteria such as the "Final Acceptance Criteria for Emergency Core Cooling Systems" 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:

a. Condition I - Normal Operation
b. Condition II - Incidents of Moderate Frequency
c. Condition III - Infrequent Faults
d. 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 cladding) 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 CVCS and are consistent with the plant design basis.

Condition III incidents do 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 is not sufficient to interrupt or restrict public use of these areas beyond the exclusion radius. Furthermore, a Condition III incident does 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 do not cause a release of radioactive material that results in exceeding the limits of 10 CFR 100.

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 7.0, and the consequences of Condition II, III, and IV occurrences are given in Chapter 15.0.

4.3.1.1 Fuel Burnup

Basis

The fuel rod design basis is described in Section 4.2. A limitation on initial installed excess reactivity or average discharge burnup 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.

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.

4.3.1.2 Negative Reactivity Feedbacks (Reactivity Coefficient)

Basis

The fuel temperature coefficient will be negative, and the moderator temperature coefficient of reactivity at beginning of life will be no greater than + 5 pcm/°F between 0-70% rated thermal power, ramping down linearly to 0 pcm/°F at 100% rated thermal power. At full power this ensures 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 neutron spectrum and reactor composition change effects resulting from changing moderator density. These basic physics characteristics are often identified by reactivity coefficients. The use of slightly enriched uranium ensures that the Doppler coefficient of reactivity is negative. This coefficient provides the most rapid reactivity compensation.

4.3.1.3 Control of Power Distribution

Basis

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

a. The fuel will not be operated at greater than 14.5 kW/ft at 3565 MWt under normal operating conditions, including an allowance of 2 percent for calorimetric error and not including power spike factor due to densification.

b. Under abnormal conditions, including the maximum overpower condition, the fuel peak power will not cause melting, as defined in Section 4.4.1.2.

c. 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.

d. Fuel management will be such as to produce values of fuel rod power and burnup 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 calculated peak power and measurements, a nuclear uncertainty (see Section 4.3.2.2.1) 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 at power or by boron dilution is limited. During normal at power operation, the maximum controlled reactivity insertion rate is less than 35 pcm/sec*. A maximum reactivity change rate of 85 pcm/sec* for accidental withdrawal of control banks at hot zero power is set such that peak heat generation rate and DNBR do not exceed the maximum allowable at over-power conditions. This satisfies GDC-25.

The maximum reactivity worth of control rods and the maximum rates of reactivity insertion employing control rods are limited so as to preclude rupture of the coolant pressure boundary or disruption of the core internals to a degree which would impair core cooling capacity due to a rod withdrawal or ejection accident (see Chapter 15.0).

Following any Condition IV event (rod ejection, steam line break, etc.) the reactor can be brought to the shutdown condition, and the 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). For this reactor, the maximum control rod speed is 45 inches per minute, and the maximum rate of reactivity change considering two control banks moving is 85 pcm/sec* at hot zero power. During normal operation at power and with control rod overlap, the maximum reactivity change rate is less than 35 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 35 pcm/sec for normal operation and 85 pcm/sec for accidental withdrawal of two banks at hot zero power.

4.3.1.5 Shutdown Margins

Basis

Minimum shutdown margin as specified in the COLR 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 fullout position (stuck rod criterion). This satisfies GDC-26.

Discussion

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* 1 pcm = 10^{-5} $\Delta\rho$ (see footnote to Table 4.3-2A).

Two independent reactivity control systems are provided: 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 (very small number of rod failures), 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 control rods and soluble boron. Further, the fuel will be maintained sufficiently subcritical that removal of all rod cluster control assemblies will not result in criticality.

Discussion

ANSI Standard NI8.2 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 normally dry new fuel storage racks, assuming optimum moderation. No criterion is given 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 is specified in the Technical Specifications. Verification that this shutdown criteria is met, including uncertainties, is achieved using standard Westinghouse design methods such as the PHOENIX-P (Reference 36) and ANC (Reference 34) codes. The subcriticality of the core is continuously monitored, as 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 excitation 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. Movable 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 late in core life. The control bank 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 (OPDT) and Overtemperature ΔT (OTDT) trip functions which use the measured axial power imbalance as an input (although f_2 (ΔI) is always zero for OPDT). Detection and suppression of xenon oscillations are discussed in Section 4.3.2.7.

4.3.1.7 Anticipated Transients Without SCRAM

Refer to Sections 7.7.1.11 and 15.8. Callaway has installed the ATWS Mitigation System Actuation Circuitry (AMSAC).

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 UO_2 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 nominally composed of 264 fuel rods (see Section 4.1), 24 thimbles, and an incore instrumentation thimble. Figure 4.2-1 shows cross-sectional views of the current fuel assemblies and the related rod cluster control locations. Further details of the fuel assembly designs are given in Section 4.2.

For purposes of the Nuclear Design Description, specific information is presented, with figures and tables provided. Though these exhibits may be based on a particular cycle, they should be regarded as typical and are presented for illustration purposes only. They reflect the multi-enrichment cycle energy requirements, which have governed core design throughout the life of the Callaway Plant. In general, two or three regions of varying enrichments comprise the core. Design of the core and verification of expected operation within acceptance criteria is completed each cycle, documented per the requirements of Technical Specifications 5.6.5 and 5.6.6 and per plant reload procedures.

For initial core loading, 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. Figure 4.3-1 shows the fuel loading pattern as used in the first core. Two regions consisting of the two lower enrichments are interspersed so as to form a checker-board 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 core will typically operate 18 months between refueling, accumulating typically 18,000-22,000 MWD/MTU per cycle. The exact reloading pattern, initial and final positions of assemblies, and the 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 enrichment is determined by the amount of fissionable material required to provide the desired core life- time and energy requirements. The physics of the burnout process is such that operation of the reactor depletes the amount of fuel available due to the absorption of neutrons by the U-235 atoms and their subsequent fission. 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 in Figure 4.3-2 for a typical 17 x 17 fuel assembly, which occurs due to the nonfission absorption of neutrons in U-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 absorber poison rods.

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 poison depletion, and the cold-to-operating moderator temperature change.

Using its normal makeup path, the chemical and volume control system (CVCS) is capable of inserting negative reactivity at a rate of approximately 30 pcm/min when the reactor coolant boron concentration is 1,000 ppm and approximately 35 pcm/min when the reactor coolant boron concentration is 100 ppm. If the emergency boration path is used, the CVCS is capable of inserting negative reactivity at a rate of approximately 65 pcm/min when the reactor coolant concentration is 1,000 ppm and approximately 75 pcm/min when the reactor coolant boron concentration is 100 ppm. The peak burn-out rate for xenon is 25 pcm/min (Section 9.3.4 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 would result in a positive moderator coefficient at beginning-of-life for the first cycle. Therefore, burnable absorber rods are used in the first core to reduce the soluble boron concentration sufficiently to ensure that the moderator temperature coefficient is negative for power operating conditions. During operation, the poison 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 4.3-3A is a plot of typical core depletions with and without burnable absorber rods. Note that even at end-of-life conditions some residual poison remains in the absorber poison rods, resulting in a net decrease in the first cycle lifetime.

In addition to reactivity control, the burnable absorber rods are strategically located to provide a favorable radial power distribution. Figure 4.3-4A shows the burnable absorber distributions within a fuel assembly for the several burnable absorber patterns used in a 17 x 17 array. Initial core burnable absorber loading patterns are shown in Figure 4.3-5A.

Tables 4.3-1A through 4.3-3A contain a summary of the reactor core design parameters, including reactivity coefficients, delayed neutron fraction, and neutron lifetimes. 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 very similar to those expected. Details of this confirmation are given in Reference 2 and in Section 4.3.2.2.7.

4.3.2.2.1 Definitions

Power distributions are quantified in terms of hot channel factors. These factors are a measure of the peak pellet power within the reactor core and the total energy produced in a coolant channel, relative to the total reactor power output, 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 ($Btu\text{-}ft^{-2}\text{-}hr^{-1}$). 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 rod, 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 for the overpower (kW/ft) evaluations. For the DNB evaluations, the F_Q^E factors are 1.0 (ITDP) and 1.033 (non-ITDP).

$F_{\Delta H}^N$, 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 DNBR described in Section 4.4.

It is convenient for the purposes of discussion to define sub-factors of F_Q. However, design limits are set in terms of the total peaking factor.

$$F_Q = \text{Total peaking factor or heat flux hot channel factor} = \frac{\text{Maximum kW/ft}}{\text{Average kW/ft}}$$

$$F_Q = F_Q^N \times F_Q^E$$

$$F_Q = \max(F_{XY}^N(Z) \times P(Z)) \times F_U^N \times F_Q^E$$

where:

F_Q^N and F_Q^E are defined above

F_U^N = factor for conservatism, assumed to be 1.05

F_{XY}^N = ratio of peak power density to average power density in the horizontal plane at elevation Z

P(Z) = ratio of the power per unit core height in the horizontal plane at height Z to the average value of power per unit core height.

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 assembly and burnable absorber loading patterns, the control rod pattern, and the fuel burnup distribution. Thus, at any time in the cycle, a horizontal section of the core can be characterized as unrodded or 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 of F_{XY}^N (Radial Factor) are given in Table 4.3-2A. 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 4.3-6A through 4.3-11A show typical radial power distributions for 1/8 of the core for representative operating conditions. These conditions are: 1) hot full power (HFP) at beginning-of-life (BOL), unrodded, no xenon, 2) HFP at BOL, unrodded, equilibrium xenon, 3) HFP near BOL, bank D in, equilibrium xenon, 5) HFP near end-of-life (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 4.4

4.3.2.2.3 Assembly Power Distributions

For the purpose of illustration, typical assembly power distributions from the BOL and EOL conditions corresponding to Figures 4.3-7A and 4.3-10A, respectively, are given for the same assembly in 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 radial 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 $F^N_{\Delta H}$. 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 $F^N_{\Delta H}$.

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 through either the manual operation of the control rods or automatic insertion of control rods responding to manual operation of the CVCS. Nuclear effects which cause variations in the axial power shape include moderator density, Doppler effect on resonance absorption, spatial distribution of xenon, and burnup. Automatically controlled variations in total power output and full length rod motion are also important in determining the axial power shape at any time. Signals are available to the operator from the power range 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 axial 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:

$$\text{axial offset} = \frac{\Phi_t - \Phi_b}{\Phi_t + \Phi_b}$$

where Φ_t and Φ_b are the top and bottom detector readings.

Representative axial power shapes for BOL, MOL, and EOL conditions are shown in 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 4.3-8A and 4.3-11A involving the partial insertion of control rods represent a synthesis of power shapes from the rodded and unrodded planes. The applicability of the separability assumption upon which this procedure is based 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 Not Used

4.3.2.2.6 Limiting Power Distributions

According to the ANSI classification of plant conditions (see Chapter 15.0), 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 action. 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 Chapter 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 alerted 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 Conditions II, III, and IV events.

Improper procedural actions or errors by the operator are assumed in the design as occurrences of moderate frequency (Condition II). Some of the consequences which might result are discussed in Chapter 15.0. 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 flux difference limits given in the Core Operating Limits Report (COLR), 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 (automatic rod withdrawal is no longer available). 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 and the limits are specified in the COLR. A complete discussion of power distribution control in Westinghouse pressurized water reactors is included in Reference 6. Detailed background information on the following design constraints on local power density in a Westinghouse pressurized water reactor, the defined operating procedures, and on the measures taken to preclude exceeding design limits is presented in the Westinghouse topical report on power distribution control and load following procedures (Ref. 7). 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 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 4.3-6A and 4.3-7A) 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:

a. Core power level

b. Core height

c. Coolant temperature and flow

d. Coolant temperature program as a function of reactor power

e. Fuel cycle lifetimes

f. Rod bank worths

g. Rod bank overlaps

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

a. 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.

b. Control banks are sequenced with overlapping banks.

c. The full length control bank insertion limits are not violated.

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

The axial power distribution procedures referred to above are part of the required operating procedures which are followed in normal operation. During normal operation with relaxed axial offset control (RAOC), they require control of the axial flux difference at power levels above 50% RTP within a permissible operating space. The limits on axial flux difference (AFD) are given in the COLR. 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. Beginning, most reactive time, middle, and end-of-cycle conditions are included in the calculations. Different operation maneuvers are assumed prior to calculating the effect on the axial power distributions. 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 7, and they 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 line of Figure 4.3-21) 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 that 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 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 X-Y calculations. A conservative factor is applied on the unrodded radial peak that 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 7. 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 for the overpower (kW/ft) evaluations. F_Q^E factors used in the DNB evaluations are discussed in Section 4.3.2.2.1.

The envelope drawn over the calculated [max (F_Q • Power)] points in Figure 4.3-21 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, Figure 4.3-21 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.

This upper bound envelope is verified for operation within an allowed AFD operating space, per the RAOC methodology outlined in Reference 35. Axial offset control is detailed in the Technical Specifications, with AFD limits specified in the COLR, and followed by relying only upon excore surveillance supplemented by the normal monthly full core map requirement and by computer-based alarms on deviation from the allowed AFD operating space.

Allowing for fuel densification effects the average linear power is 5.69 kW/ft (See Table 4.4-1) at 3565 MWt. As presented in the Core Operating Limits Reports (COLR), the conservative upper bound value of normalized local power density, including uncertainty allowances, is 2.50 corresponding to a peak linear power of 14.23 kw/ft.

To determine reactor protection system 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) for full length banks (automatic rod withdrawal is no longer available). Also included are motions of the full-length 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 120 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. Representative results are given in 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 120 percent, is less than that required for center-line melt, including uncertainties and densification effects (See Table 15.0-4).

The second category, also appearing in Figure 4.3-22, assumes that the operator mispositions the full-length 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 representative results shown on Figure 4.3-23 are F_Q multiplied by 102% 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 that required for centerline melt. Since the peak kW/ft is below the above limit, 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 7.

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 4.3-2A and addressed in the Technical Specifications and the COLR.

The maximum allowable F_Q can be increased with decreasing power, as shown in the Technical Specifications. Increasing $F^N_{\Delta H}$ with decreasing power is permitted by the DNB protection set points 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 $F^N_{\Delta H}$ = 1.59 [1 + 0.3 (1-P)] for V5/V+ (for ITDP analyses). See Table 4.3-2A for non-ITDP analyses. This becomes a design basis criterion which is used for establishing acceptable control rod patterns and control bank sequencing. Like-wise, 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 in Figures 4.3-6A through 4.3-11A. The worst values generally occur when the rods are assumed to be at their insertion limits. Maintenance of relaxed axial offset control establishes rod positions which are at or above the allowed rod insertion limits. As discussed in Section 3.2 of Reference 8, it has been determined that the Technical Specification limits are met, provided the above conditions a and b are observed. 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 detail in Section 7.7.

4.3.2.2.7 Experimental Verification of Power Distribution Analysis

This subject is discussed in depth in Reference 2. 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 an incore instrumentation processing code described in Reference 9 or 36. The measured versus calculational comparison is normally performed periodically throughout the cycle lifetime of the reactor, as required by Technical Specifications.

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:

a. Reproducibility of the measured signal

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

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

The appropriate allowance for category a above has been quantified by repetitive measurements made with several inter-calibrated detectors by using the common thimble features of the incore detector system. The Callaway system allows more than one detector to access any thimble. Errors in category b above are quantified to the extent possible, by using the detector current 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 poisons, etc. These critical experiments provide quantification of errors of categories a and c above.

Reference 2 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 the Callaway 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 $.645_{\sigma}$ 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) with calculations for the same operating conditions, it is not possible to isolate out the detector reproducibility. Thus a comparison between measured and predicted power distributions has to include some measurement error. Such a comparison is given in Figure 4.3-24 for one of the maps used in Reference 2. Since the first publication of Reference 2, 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 for the uncertainty in $F^N_{\Delta H}$ (rod integral power) measurements results in an allowance of 3.65 percent at the equivalent of a 1.645_{σ} confidence level. For historical reasons, an 8 percent uncertainty factor is allowed in the nuclear design calculational basis; that is, the predicted rod integrals at full power must not exceed the design $F^N_{\Delta H}$ less 8 percent. In the determination of the DNB design limit DNBRs, 4 percent of this 8 percent is statistically convoluted.

A recent measurement in the second cycle of a 121 assembly, 12 foot, core is compared with a simplified one-dimensional core average axial calculation in Figure 4.3-25. This calculation does not give explicit representation to the fuel grids.

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

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

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

c. 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 8. 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

An extensive series of physics tests was performed on the first core. These tests and the criteria for satisfactory results are described in the initial Test Program (formerly FSAR Chapter 14). Since not all limiting situations can be created at BOL, the main purpose of the tests was to provide a check on the calculational methods used in the predictions for the conditions of the test. Tests performed at the beginning of each reload cycle are limited to verification of the selected safety-related parameters of the reload design.

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 2, 6, and 8. 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 on-line 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 pressure or void conditions, although the latter are relatively unimportant in the SNUPPS reactors. 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 15.0. The reactivity coefficients are calculated on a corewise 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, for example, in postulated rupture of the main steam line break and rupture of a rod cluster control assembly mechanism housing described in Sections 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, and 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 the Doppler broadening of U-238 and Pu-240 resonance absorption peaks. Doppler broadening of other isotopes is also considered but their contribution to the Doppler effect is 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, two or three dimension calculations, using the ANC Code (Ref. 34). 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.

A typical Doppler temperature coefficient is shown in 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. A typical Doppler-only contribution to the power coefficient, defined later, is shown in 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 in Figure 4.3-29 as a function of relative power. The Doppler coefficient becomes more negative as a function of life as the Pu-240 content increases, thus increasing the Pu-240 resonance absorption, but the overall value becomes less negative since the fuel temperature changes with burnup, as described in Section 4.3.3.1. The upper and lower limits of Doppler coefficient used in accident analyses are given in Figure 15.0-2.

4.3.2.3.2 Moderator Coefficients

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

<u>Moderator Density and Temperature Coefficients</u>

The moderator temperature (density) coefficient is defined as the change in reactivity per degree change in the moderator temperature. Generally, the effects of the changes in moderator density as well as the temperature are considered together.

The soluble boron used in the reactor as a means of reactivity control also has an effect on moderator density coefficient, since the soluble boron poison density as well as the water density is decreased when the coolant temperature rises. A decrease in the soluble poison density introduces a positive component in the moderator coefficient. If the concentration of soluble poison is large enough, the net value of the coefficient may be positive.The effect of control rods is to make the moderator coefficient more negative since the thermal neutron mean free path, and hence the volume affected by the control rods, increases with an increase in temperature.

With burnup, the moderator 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 coefficient is calculated for a range of plant conditions by performing two-group X-Y calculations, in which the moderator temperature (and density) is varied by about ±5°F about each of the mean temperatures. The moderator coefficient is shown as a function of core temperature and boron concentration for a typical unrodded and rodded core in 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 for a typical core plotted as a function of first cycle lifetime for the just critical boron concentration condition based on the design boron letdown condition.

The moderator coefficients presented here are calculated on a corewide 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.

<u>Moderator Pressure Coefficient</u>

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 on reactivity as a 1/2-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 operating conditions and becomes more positive during life (+0.3 pcm/psi, EOL).

<u>Moderator Void Coefficient</u>

The moderator void coefficient relates the change in neutron multiplication to the presence of voids in the moderator. In a pressurized water reactor, this coefficient is not very significant because of the low void content in the coolant. The core void content is less than 1/2 of 1 percent and is due to local or statistical boiling. The void coefficient varies from 50 pcm/percent void at BOL and at low temperatures to -250 pcm/ percent void at EOL and at operating temperatures. The 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. A typical power coefficient at BOL and EOL conditions is given in Figure 4.3-34.

It becomes more negative with burnup reflecting the combined effect of moderator and fuel temperature coefficients with burnup. The power defect (integral reactivity effect) at BOL and EOL is given in Figure 4.3-35.

4.3.2.3.4 Comparison of Calculated and Experimental Reactivity Coefficients

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:

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

b. ±2 pcm/F for the moderator coefficient

Experimental evaluation of the reactivity coefficients was performed during the physics startup tests described in the initial test program.

4.3.2.3.5 Reactivity Coefficients Used in Transient Analysis

Table 4.3-2A 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 the BOL or EOL, whether most negative or the most positive 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 15.0.

The reactivity coefficients shown in Figures 4.3-27 through 4.3-35 are typical best estimate values calculated for a first cycle. The limiting values shown in Table 4.3-2A 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 form the design basis range used in the transient analysis. A direct comparison of the best estimate and design limit values shown in Table 4.3-2A 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 concentration 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 15.0 with due allowance for the calculational uncertainties given in Section 4.3.3.3. Control rod requirements are given in Table 4.3-3A for a typical core design. Data provided in Table 4.3-3A is typical and is not updated with each reload cycle.

4.3.2.4 <u>Control Requirements</u>

To ensure the shutdown margin stated in the COLR 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 4.3-2A. For all core conditions including refueling, the boron concentration is well below the solubility limit. The rod cluster control assemblies are employed to bring the reactor to the hot shutdown condition. The minimum required shutdown margin is given in the COLR.

The ability to accomplish the shutdown for hot conditions is demonstrated in Table 4.3-3A by comparing the difference between the rod cluster control 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 (see Section 4.3.2.4.9). An uncertainty of 7% in rod worth calculations has been justified generically by Westinghouse for cores with silver-indium-cadmium control rods in Reference 39. Lower values of rod worth uncertainty may be further justified on a cycle specific basis, based on comparisons of startup physics testing rod worth measurements for current and past cycles. The largest reactivity control requirement 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, 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 U-238 and Pu-240 resonance cross-sections with an increase in effective pellet temperature. This effect is most noticeable over the range 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 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 is conservatively increased by 4°F to account for the control dead band and measurement errors. (See Section 15.0.3.2 for accident analysis uncertainties).

The moderator 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 toward the bottom of the core. On the other hand, at hot zero power 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 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 product buildup throughout the cycle. This reactivity is controlled by the addition of soluble boron to the coolant and by burnable absorber. The soluble boron concentration for several core configurations, the unit boron worth, and burnable absorber worth are given in Tables 4.3-1A and 4.3-2A. Since the excess reactivity for burnup is controlled by soluble boron and/or burnable absorber, it is not included in control rod requirements.

4.3.2.4.7 Xenon and Samarium Poisoning

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 can be controlled by changing the soluble boron concentration (also see Section 4.3.2.4.16).

4.3.2.4.8 pH Effects

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 11.

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-foot 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-foot 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 ability of the methods described in Section 4.3.3.

4.3.2.4.10 Control

Core reactivity is controlled by means of a chemical poison dissolved in the coolant, rod cluster control assemblies, and burnable absorber rods, as described below.

4.3.2.4.11 Chemical Poison

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

a. The moderator temperature defect in going from cold shutdown at ambient temperature to the hot operating temperature at zero power

b. The transient xenon and samarium poisoning, such as that following power changes or changes in rod cluster control position

c. The reactivity effects of fissile inventory depletion and buildup of long-life fission products

d. The burnable absorber depletion

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

4.3.2.4.12 Rod Cluster Control Assemblies

The number of rod cluster assemblies is shown in Table 4.3-1A. The rod cluster control assemblies are used for shutdown and control purposes to offset fast reactivity changes associated with:

a. The required shutdown margin in the hot zero power, stuck rods condition

b. The reactivity compensation as a result of an increase in power above hot zero power (power defect, including Doppler, and moderator reactivity changes)

c. Unprogrammed fluctuations in boron concentration, coolant temperature, or xenon concentration (with rods not exceeding the allowable rod insertion limits)

d. 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 the insertion limit uses conservative xenon distributions and axial power shapes. In addition, the rod cluster control assembly withdrawal pattern determined from these analyses is used in determining power distribution factors and in determining the maximum worth of an inserted rod cluster control assembly ejection accident. For further discussion, refer to the COLR 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 rod cluster control assemblies shown in Figure 4.3-36. All shutdown rod cluster control assemblies 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 COLR.

4.3.2.4.13 Reactor Coolant Temperature

Reactor coolant (or moderator) temperature control has added flexibility in reactivity control of the Westinghouse pressurized water reactor. This feature takes advantage of the moderator temperature coefficient in a pressurized water reactor to provide ±5 percent power load regulation capabilities without requiring control rod compensation.

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. In doing so, these rods make the moderator temperature coefficient less positive at normal operating conditions. They perform this function by reducing the requirement for soluble poison in the moderator at the beginning of the fuel cycle, as described previously. For purposes of illustration, a typical burnable absorber rod pattern in the core together with the number of rods per assembly are shown in Figure 4.3-5A, while the arrangements within an assembly are displayed in Figure 4.3-4A. The reactivity worth of these rods is shown in Table 4.3-1A.

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 on full-length rods, as provided in the COLR and discussed in Sections 4.3.2.4.12 and 4.3.2.4.13. The power distribution is maintained within acceptable limits through location of the full-length 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, when a negative MTC will exist.

Rapid power increases (5 percent/min) from part power during end of cycle 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 is returned 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 absorber. The boron concentration must be limited during operating conditions to ensure that the moderator temperature coefficient is not overly positive. Sufficient burnable absorber is installed at the beginning of a cycle to give the desired cycle lifetime, without exceeding the moderator temperature coefficient (MTC) upper limit. The boron concentration will be the minimum boron concentration needed to meet Technical Specification requirements for shutdown margin.

4.3.2.5 Control Rod Patterns and Reactivity Worth

The rod cluster control assemblies 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 in Figure 4.3-36, which is not expected to change during the life of the plant. The control banks are labeled A, B, C, and D and the shutdown banks are labeled SA, SB, SC, SD, and SE. With the exception of shutdown banks SC, SD, and SE, each bank, although operated and controlled as a unit, is composed of two groups. The axial position of the rod cluster control assemblies may be controlled manually or inserted automatically. The rod cluster control assemblies 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 reactivity defect and rod insertion requirements specified in Table 4.3-3A. (The data provided in Table 4.3-3A is typical and is not updated for each new reload cycle.) Second, in view of the fact that 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 3 to 4 percent $\Delta\rho$. Therefore, four banks (described as A, B, C, and D in Figure 4.3-36), each worth approximately 1 percent $\Delta\rho$, have been selected. Typical control bank worths are shown in Table 4.3-2A.

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 (see the Technical Specifications). Early in the cycle, there may also be a withdrawal limit at low power to maintain the moderator temperature coefficient within Technical Specification limits.

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 in 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 rod position versus time of travel after rod release assumed is given in Figure 4.3-38. 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 of 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. A typical result of these calculations is shown on Figure 4.3-39.

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 rod cluster control assemblies 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 (near full power).

The values given in Table 4.3-3A for a typical core design show that the available reactivity in withdrawn rod cluster control assemblies 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. The values in Table 4.3-3A are calculated from a nominal design model. These values may vary on a cycle to cycle basis due to loading pattern changes and may also change during the cycle based upon actual plant operation and/or reanalysis to remove conservatisms.

4.3.2.6 Criticality of the Reactor During Refueling and Criticality of Fuel Assemblies

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 Sections 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. Sections 9.1.1 and 9.1.2 identify those criteria important to criticality safety analyses.

4.3.2.7 Stability

4.3.2.7.1 Introduction

The stability of the pressurized water reactor cores against xenon-induced spatial oscillations and the control of such transients are discussed extensively in References 6, 14, 15, and 16. A summary of these reports is given in the following discussion, and the design bases are given 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, pressurized water reactor 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 current pressurized water reactors and the stability of the core against xenon- induced oscillations can be determined in terms of the eigen- values of the first flux overtones. Writing the eigenvalue ξ of the first flux harmonic as:

$$\xi = b + ic \qquad [4.3\text{-}1]$$

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

$$\delta\phi(t) = A\ e^{\xi t} = ae^{bt}\cos ct \qquad [4.3\text{-}2]$$

where A and a are constants. The stability index can also be obtained approximately by:

$$b = \frac{1}{T}\ln\frac{A_n + 1}{A_n} \qquad [4.3\text{-}3]$$

where A_n and 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 stability of the core described herein (i.e., with 17 x 17 fuel assemblies) against xenon-induced spatial oscillations is expected to be equal to or better than that of earlier designs for cores of similar size. The prediction is based on a comparison of the parameters which are significant in determining the stability of the core against the xenon-induced oscillations, namely: 1) the overall core size is unchanged and spatial power distributions will be similar, 2) the moderator temperature coefficient is expected to be similar and 3) the Doppler coefficient of reactivity is expected to be equal to or slightly more negative at full power.

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

a. Axial measurements

Two axial xenon transient tests conducted in a pressurized water reactor with a core height of 12 feet and 121 fuel assemblies is reported in Reference 17 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. 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 in 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 form 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 tests are:

1. 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 for the first test (Curve 1 of Figure 4.3-40) and -0.014/hr for the second test (Curve 2 of Figure 4.3-40). The corresponding oscillation periods are 32.4 and 27.2 hours, respectively.

2. The reactor core becomes less stable as fuel burnup progresses and the axial stability index was essentially zero at 12,000 MWD/MTU. However, the movable control rod system can control axial oscillations, as described in Section 4.3.2.7.

b. Measurements in the X-Y plane

Two X-Y xenon oscillation tests were performed at a pressurized water reactor plant with a core height of 12 feet and 157 fuel assemblies. The first test was conducted at a core average burnup of 1540 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 pressurized water reactors 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 movable 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 form of Equation [4.3-2] through a least-square method. A stability index of -0.076/hr with a period of 29.6 hours was obtained from the thermocouple data shown in Figure 4.3-41.

It was observed in the second X-Y xenon test that the pressurized water reactor 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, which is a predecessor of the current APOLLO Code (Ref. 18). The analysis of the X-Y xenon transient tests was performed in an X-Y geometry, using a modifed TURTLE Code (Ref. 10). Both the PANDA and TURTLE 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. 19) and CINDER (Ref. 20). 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 4.3-5. The calculations show conservative results for both of the axial tests with a margin of approximately -0.01/hr in the stability index.

An analytical simulation of the first X-Y xenon oscillation test shows a calculated stability index of -0.081/hr, in good agreement with the measured value of -0.076/hr. 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 14, 15 and 16. A more detailed description of the experimental results and analysis of the axial and X-Y xenon transient tests is presented in Reference 17 and Section 1 of Reference 21.

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 form part of the protection system.

a. Axial power distribution

For maintenance of proper axial power distributions, the operator is instructed to maintain an axial flux difference within an allowed operating space as a function of power, based on the power range excore detector readings. Should the axial flux difference be permitted to move far enough outside this operating space, the DNB protection limit will be reached, and the power will be automatically reduced.

Twelve-foot pressurized water reactor 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 full-length control rod banks are sufficient to dampen and control any axial xenon oscillations present. Should the axial flux difference be inadvertently permitted to move far enough outside the allowed operating space due to an axial xenon oscillation, or any other reason, the protection limit on axial flux difference will be reached and the power will be automatically reduced.

b. 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 pressurized water reactors has been further verified as part of the startup physics test program for pressurized water reactor 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, backup 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 will increase the stability of the core in the X-Y plane.

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

A more detailed discussion of the power distribution control in pressurized water reactor cores is presented in References 6 and 7.

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 on 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 4.3-6. The values listed are based on time-averaged equilibrium cycle reactor core parameters and power distributions; and, thus, are suitable for long-term nvt 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:

a. Determination of effective fuel temperatures

b. Generation of macroscopic few-group parameters

c. Space-dependent, few-group diffusion 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 form 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. 22) calculations.

The effective U-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. 23) Monte Carlo 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 Pu-240 temperature for resonance absorption is determined by a convolution of the radial distribution of Pu-240 densities from LASER burnup calculations and the radial weighting function. The resulting temperature is burnup dependent, but the difference between U-238 and Pu-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 4.3-7, and Doppler coefficient data, as shown in Figure 4.3-42. Stability index measurements also provide a sensitive measure of the Doppler coefficient near full power (see Section 4.3.2.7). It can be seen that Doppler defect data is typically within 0.2 percent $\Delta\rho$ of prediction.

4.3.3.2 Macroscopic Group Constants

Macroscopic few-group constants and consistent microscopic cross-sections (needed for feedback and microscopic depletion calculations) are generated for fuel cells by a recent version of the PHOENIX-P code (Ref. 36).

The PHOENIX-P computer code is a two-dimensional, multi-group, transport lattice code and capable of providing all necessary data for PWR analysis. Being a dimensional lattice code, PHOENIX-P does not rely on pre-determined spatial/spectral interaction assumptions for a heterogeneous fuel lattice, hence, will provide a more accurate multi-group flux solution than versions of historical codes. The PHOENIX-P computer code is approved by the USNRC as the lattice code for generating macroscopic and microscopic few group cross-sections for PWR analysis.

The solution for the detailed spatial flux and energy distribution is divided into two major steps in PHOENIX-P. In the first step, a two-dimensional fine energy group nodal solution is obtained which couples individual sub-cell regions (pellet, cladding, and moderator) as well as surrounding pins. PHOENIX-P uses a method based on the Carlvik's collision probability approach and heterogeneous response fluxes which preserves the heterogeneity of the pin cells and their surroundings. The nodal solution provides accurate and detailed local flux distributions, which is then used to spatially homogenize the pin cells to fewer groups.

The second step in the solution process solves for the angular flux distribution using a standard S4 discrete ordinates calculation. This step is based on the group-collapsed and homogenized cross-sections obtained from the first step of the solution. The S4 fluxes are then used to normalize the detailed spatial and energy nodal fluxes. The normalized nodal fluxes are used to compute reaction rates, power distribution and to deplete the fuel and burnable absorbers. A standard B1 calculation is employed to evaluate the fundamental mode critical spectrum and to provide an improved fast diffusion coefficient for the core spatial codes.

The PHOENIX-P code employs a 70 energy group library (Ref. 37) which as been derived mainly from ENDF/B-VI (base) files. The PHOENIX-P cross sections library was designed to properly capture integral properties of the multi-group data during group collapse, and enabling proper modeling of important resonance parameters. The library contains all neutronic data necessary for modeling fuel, fission products, cladding and structural, coolant, and control/burnable absorber materials present in Light Water Reactor cores.

Group constants for burnable absorber cells, guide thimbles, instruments thimbles, control rod cells, and other non-fuel cells are also generated by PHOENIX-P.

4.3.3.3 Spatial Few-Group Diffusion Calculations

Spatial few-group diffusion calculations historically consisted of 2-group X-Y calculations using an updated version of the TURTLE code (Ref. 10), and 2-group axial calculations using APOLLO (Ref. 18), an updated version of the PANDA code. However, with the advent of VANTAGE 5 fuel, and hence, axial features such as axial blankets and part length burnable absorbers, there has been a greater reliance on three-dimensional nodal codes such as 3D ANC (Advanced Nodal Code) (Ref. 34). The three-dimensional nature of the nodal codes provides both the radial and axial power distributions.

Nodal three-dimensional calculations are carried out to determine the critical boron concentrations and power distributions. The moderator coefficient is evaluated by varying the inlet temperature in the same calculations used for power distribution and reactivity predictions.

Validation of ANC 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 hot zero power conditions as shown in Table 4.3.11.

ANC is used in two-dimensional and three-dimensional calculations. ANC can be used for safety analyses and to calculate critical boron concentrations, control rod worths, reactivity coefficients, etc.

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 are obtained from ANC three-dimensional calculations 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% $\Delta\rho$ for Doppler defect
± 2 x 10-5 $\Delta\rho$/°F for moderator coefficient
± 50 ppm for critical boron concentration with depletion
± 3% for power distributions
± 0.2% $\Delta\rho$ for rod bank worth
± 4 pcm/step for differential rod worth
± 0.5 pcm/ppm for boron worth
± 0.1% $\Delta\rho$ for moderator defect

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TABLE 4.3-1A REACTOR CORE DESCRIPTION

	V5/V+
Active Core	
Equivalent diameter, in.	132.7
Active fuel height, in.	143.7
Height-to-diameter ratio	1.08
Total cross section area, ft^2	96.06
H_2O/U molecular ratio, lattice, cold	2.71
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[(a)]
Rod pitch, in.	0.496
Overall transverse dimensions, [(b)] in.,	8.426 x 8.426
Nominal Fuel weight, KgU per assembly	423.103 424.305* 416.080**
Zircaloy weight, lb	53,840
Number of grids per assembly	11*** 12****
Composition of grids	Inconel-718 (or Zirlo™ bottom) Zircaloy-4 or ZIRLO™
Number of guide thimbles per assembly	24
Composition of guide thimbles	Zircaloy-4 or ZIRLO™
Diameter of guide thimbles, upper part above dashpot, in.	0.442 I.D. x 0.474 0.D.
Diameter of guide thimbles, lower part, in.	0.397 I.D. x 0.430 0.D.
Diameter of instrument guide thimbles, in.	0.442 I.D. x 0.474 0.D.
Fuel Rods	
Number	50,952
Outside diameter, in.	0.360
Diameter gap, in.	0.0062
Clad thickness, in.	0.0225
Clad material	Zircaloy-4/ZIRLO™
Fuel Pellets Material	UO_2 sintered
Diameter, in.	0.3088
Length, in.	0.462/0.500 (natural and slightly enriched blanket)/ 0.370 (enriched)

* Includes solid axial blanket pellets.
** Includes annular axial blanket pellets.
*** Does not include protective grid.
**** Includes protective grid

TABLE 4.3-1A (Sheet 2)

Current Cycle Fuel Assemblies (Reload and Feed Assemblies)

Region	Number of Assemblies	Enrichment (w/o U235)	Density %	Fuel Stack Height (inches, cold)
21A1 Reload	16	4.202	95.55	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
21B Reload	12	4.599	95.81	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
21C1 Reload	2	4.699	95.62	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
21G Reload	1	4.699	95.62	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
22A Reload	16	4.205	95.75	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
23A Reload	52	4.201	95.57	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
23C Reload	2	4.702	95.67	144” [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
24A Feed	60	4.198	95.66	144 [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]
24B Feed	32	4.689	95.58	144 [mid-enriched annular axial blankets (2.6 w/o, top and bottom 6 inches) and 1.5X IFBA, 124-inch coating length; IFBA centerline is at the core midplane (no offset)]

Rod Cluster Control Assemblies	<u>V5/V+</u>
Neutron Absorber	Ag-In-Cd
Absorber Diameter, in.	0.341
Density, lb/in.^{3}	0.367
Cladding material[(c)]	Type 304, cold worked stainless steel with 0.0075 in. thick chrome plating.
Clad thickness, in.	0.0185
Number of clusters, full length	53
Number of absorber rods per cluster	24
Burnable Absorber Rods (First Core)	
Number	1518
Material	Borosilicate glass
Outside diameter, in.	0.381
Inner tube, O.D., in.	0.1815

TABLE 4.3-1A (Sheet 3)

Clad material	Stainless steel
Inner tube material	Stainless steel
Boron loading (w/o B_2O_3 in glass rod)	12.5
Weight of boron-10 per foot of rod, lb/ft	0.000419
Initial reactivity worth, %Δp	~7.6 (hot); ~5.5 (cold)
Wet Annular Burnable Absorber Rods*	
Number	0
Material	B_4C Dispersion in Al_2O_3
Outside diameter, in.	0.381
Inner tube O.D. in.	0.267
Clad material	Zircaloy
	V5/V+
Inner tube material	Zircaloy
Boron loading (w/o B_4C in rod)	13.5
Weight of boron-10 per foot of rod, g/cm	0.006
Excess Reactivity (typical values)	
Maximum fuel assembly k∞ (cold, clean unborated core water)	1.39
Maximum core reactivity (cold, zero power, beginning of cycle, zero soluble boron)	1.222

* Limit of 1600 per WCAP-10021 to limit bypass flow.

(a) See Section 4.1.

(b) See Figures 4.2-1 and 4.2-2.

(c) See Figure 4.2-10.

TABLE 4.3-2A NUCLEAR DESIGN PARAMETERS

Core average linear power, including densification effects, kW/ft		5.69*
Total heat flux hot channel factor, F_Q		2.50**
Nuclear enthalpy rise hot channel factor, $F^N_{\Delta H}$		1.65***
Reactivity Coefficients[a]	Design Limits	Best Estimate
Doppler-only power coefficients, see Figure 15.0-2		
(pcm/%power)[b]		
Lower curve (most negative)	-19.4 to -12.6	-15 to -11
Upper curve (least negative)	-9.55 to -6.0	-13 to -9
Doppler temperature coefficient (pcm/F)[b]	-2.9 to -0.91	-2.4 to -1.7
Boron coefficient (pcm/ppm)[b]	-16 to -7	-12.8 to -7.5
Moderator temperature coefficient (pcm/F)[b]	-47.9 to +5.0 (less than 70% RTP); +5.0 ramping linearly to 0.0 from 70% to 100% RTP	-37 to +5.0 (less than 70% RTP); +5.0 ramping linearly to 0.0 from 70% to 100% RTP
Rodded moderator density coefficient (pcm/gm/cc)[b]	$\leq 0.43 \times 10^5$	$\leq 0.35 \times 10^5$
Delayed Neutron Fraction and Lifetime		
β_{eff} BOL, (EOL)	0.0070[h] (0.0044)[d]	
Prompt neutron lifetime, μ sec	NA	
Control Rods[g]		
Rod requirements	See Table 4.3-3A	
Maximum bank worth, pcm	<2000	
Maximum ejected rod worth	See Chapter 15.0	
Bank worth, HZP no overlap (pcm)[b]	BOL, Xe free	EOL Eq. Xe
Bank D	650	750
Bank C	1250	1450
Bank B	1200	1400
Bank A	500	450

TABLE 4.3-2A (Sheet 2)

Radial Factor (BOL to EOL)(g)	
Unrodded	1.37 to 1.28
D bank	1.50 to 1.45
D + C banks	1.60 to 1.45
D + C + B banks	1.80 to 1.55
Boron Concentrations(g)	
Zero power, k_{eff} = 0.99, cold(e) rod cluster control assemblies out	1435
Zero power, k_{eff} = 0.99, hot(f) rod cluster control assemblies out	1408
Design basis refueling water storage tank boron concentration	2350-2500
Zero power, $k_{eff} \leq 0.95$, cold rod cluster control assemblies in	1327
Zero power, k_{eff} = 1.00, hot(f) rod cluster control assemblies out	1307
Full power, no xenon, k_{eff} = 1.0, hot rod cluster control assemblies out	1178
Full power, equilibrium xenon, k_{eff} = 1.0, hot rod cluster control assemblies out	882
Reduction with fuel burnup	
First cycle (ppm/GWD/MTU)(c)	See Figure 4.3-3A
Reload cycle (ppm/GWD/MTU)	~ 100

* See Table 4.4-1.

** The total heat flux hot channel factor, FQ, is 2.50 as presented in the Core Operating Limits Report (COLR).

*** This is the non-ITDP analysis value (no uncertainties) that is specified in the COLR. The value used in the ITDP analyses is 1.59 (1.65 - 3.87% uncertainty).

NOTES:

(a) Uncertainties are given in Section 4.3.3.3.

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

(c) Gigawatt day (GWD) = 1000 megawatt day (1,000 MWD). During the first cycle, fixed burnable poison rods are present which significantly reduce the boron letdown rate compared to reload cycles.

(d) Bounding lower value used for safety analysis.

TABLE 4.3-2A (Sheet 3)

(e) Cold means 68°F, 1 atm.

(f) Hot means 557°F, 2250 psia.

(g) These are typical first cycle values. New values are confirmed for each cycle via the RSE/RSAC process.

(h) 0.0050 used for BOL in RCCA ejection accident (Section 15.4.8).

TABLE 4.3-3A REACTIVITY REQUIREMENTS FOR ROD CLUSTER CONTROL ASSEMBLIES[C]

Reactivity Effects (percent)	Beginning-of-Life (Cycle 15)	End-of-Life (Cycle 15)
1. Control rod requirements (%$\Delta\rho$)		
a. Reactivity Defects (Doppler, T_{avg}, Void, Redistribution)	1.63	2.90
b. Rod insertion allowance (%$\Delta\rho$)	0.44	0.31
2. Total control rod requirements (%$\Delta\rho$)	2.07	3.21
3. Estimated rod cluster control assembly worth (53 rods)		
All full length assemblies inserted less most reactive stuck rod (%$\Delta\rho$)	4.68	6.28
4. Estimated rod cluster control assembly credit with 10 percent adjustment to accommodate uncertainties [Item #3 minus 10 percent (%$\Delta\rho$)[a]]	4.21	5.65
5. Shutdown margin available [Item #4 minus Item #2 (%$\Delta\rho$)[b]]	2.14	2.44

NOTES:

(a) See Section 4.3.2.4 regarding changes to rod worth uncertainty values.

(b) The COLR minimum shutdown margin in Modes 1-4 is 1.3 percent $\Delta\rho$. A post-trip shutdown margin of 2.52% $\Delta\rho$ was used in the steamline break mass/energy release analysis. The conservatism of this assumption is verified on a cycle-specific basis.

(c) The values in Table 4.3.3A are typical (taken from the Cycle 15 design report) and are calculated from a nominal design model. The data is not updated for each reload cycle.

TABLE 4.3-3B DELETED

Table 4.3-3B (Sheets 1 Through 2) is Deleted.

TABLE 4.3-4 DELETED.

Table 4.3-4 is Deleted

TABLE 4.3-5 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
1550	1.34	1065	-0.041	-0.032
7700	1.27	700	-0.014	-0.006
5090*			-0.0325	-0.0255
RADIAL STABILITY INDEX				
2250**			-0.068	-0.07

* 4-loop plant, 12-foot core in Cycle 1, axial stability test.

** 4-loop plant, 12-foot core in Cycle 1, radial (X-Y) stability test.

TABLE 4.3-6 TYPICAL NEUTRON FLUX LEVELS (N/CM^2-SEC) AT FULL POWER

	E > 1.0 MeV	0.111 Mev < E <1.0 MeV	0.3 eV ≤ E <0.111 MeV	<E 0.3 eV
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}	4.35×10^{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 azimuthal peak, core mid-height	2.08×10^{10}	2.83×10^{10}	6.18×10^{10}	1.20×10^{11}

TABLE 4.3-7 COMPARISON OF MEASURED AND CALCULATED DOPPLER DEFECTS

Plant	Fuel Type	Core Burnup (MWD/MTU)	Measured (pcm)*	Calculated (pcm)
1	Air-filled	1800	1700	1710
2	Air-filled	7700	1300	1440
3	Air and helium-filled	8460	1200	1210

* pcm = $10^5 \times \ln (k_1/k_2)$

TABLE 4.3-8 DELETED.

Table 4.3-8 is Deleted

TABLE 4.3-9 DELETED.

Table 4.3-9 is Deleted

TABLE 4.3-10 DELETED.

Table 4.3-10 is Deleted

TABLE 4.3-11 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
4-loop, 193 assemblies, 12-foot core		
ARO	-0.52	-1.2
D in	-4.35	-5.7
D and C in	-8.59	-10.0
D, C, and B in	-10.14	-10.55
D, C, B, and A in	-14.63	-14.45

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

$$\alpha_{iso} = 10^5 \ln\frac{k_2}{k_1} / \Delta T^\circ F$$

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 or the emergency core cooling system (when applicable) assures that the following performances and safety criteria requirements are met:

a. 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.

b. 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 resumption of operation without considerable outage time.

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.

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

Note: For purposes of description of the thermal and hydraulic design of the reactor core, specific information is presented in this section, with figures and tables provided. Though these exhibits may be based on a particular cycle, they should be regarded as typical and presented for illustration purposes only. Thermal and hydraulic design of the reactor core and verification of expected operation within acceptance criteria is completed each cycle per plant procedures and documented per the requirement of Technical Specification 5.6.5 and 5.6.6.

4.4.1.1 Departure from Nucleate Boiling Design Basis

Basis

There will be at least a 95 percent probability that 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.

Discussion

In this application the WRB-1 (OFA) and WRB-2 (V5/V+) correlations (Ref. 1, 2, 86) were employed. OFA fuel is no longer used in Callaway reactor cores.

For those accidents which are outside the range of applicability of the WRB-2 correlation, the DNBR correlation limits are 1.13 for ABB-NV (RCCA Bank Withdrawal from Subcritical) and 1.18 for WLOP (Steam System Piping Failure at less than 1000 psia; see Reference 83). The RCCA Bank Withdrawal from Subcritical analysis in Section 15.4.1 uses the WRB-2 correlation, except for below the first mixing vane grid location (grid 2 on Figures 4.2-2, 4.2-2B and 4.2-2C) where the ABB-NV correlation is used.

The design method employed to meet the DNB design basis is the revised thermal design procedure (RTDP), Reference 3. With RTDP methodology, uncertainties in the plant operating parameters, nuclear and thermal parameters, fuel fabrication parameters, computer codes and DNB correlation predictions are combined statistically to obtain the overall DNB uncertainty factor which is used to define the design limit DNBR that satisfies the DNB design criterion. Since the parameter uncertainties are considered in determining the RTDP design limit DNBR values, the plant safety analyses are performed using input parameters at their nominal values without uncertainties. This procedure is illustrated in Figure 2-1 of Reference 3.

The RTDP design limit DNBR values are 1.22 and 1.21 for the typical and thimble cells, respectively. To maintain DNBR margin to offset DNB penalties such as those due to fuel rod bow (paragraph 4.4.2.2.5) and the lower plenum flow anomaly (paragraph 4.4.2.2.6), the RTDP safety analyses were performed to DNBR limits higher than the design limit DNBR values. The difference between the design limit DNBRs and the safety analysis limit DNBRs results in available DNBR margin. The net DNBR margin, after consideration of all penalties, is available for operating and design flexibility.

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

The design DNBRs are used as the bases for Technical Specifications, and for consideration in evaluations completed in accordance with 10 CFR 50.59.

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

4.4.1.2 Fuel Temperature Design Basis

Basis

During modes of operation associated with Condition I and Condition II events, there is at least a 95-percent probability that the peak kW/ft fuel rods will not exceed the UO_2 melting temperature at the 95-percent confidence level. The melting temperature of UO_2 is taken as 5,080°F (Ref. 4), unirradiated and decreasing 58°F per 10,000 MWD/MTU. By precluding UO_2 melting, the fuel geometry is preserved, and possible adverse effects of molten UO_2 on the cladding are eliminated. To preclude fuel melting, the peak local power experienced during Condition I and II events can be limited to a maximum value which is sufficient to ensure that the fuel centerline temperatures remain below the melting temperature at all burnups. Design evaluations for Condition I and II events have shown that fuel melting will not occur for achievable local burnups up to 75,000 MWD/MTU, Ref. 90. Note that NRC approved design evaluations up to 60,000 MWD/MTU in Reference 90 and up to 62,000 MWD/MTU in Reference 91. This provides sufficient margin for uncertainties in the thermal evaluations, 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 15.0.

4.4.1.3 Core Flow Design Basis

Basis

A minimum of 91.4 percent (thimble plugs removed) of the thermal design flow rate will pass through the fuel rod region of the core and be effective for V5/V+ fuel rod cooling. Coolant flow through the thimble tubes, as well as the leakage from flow paths outside the core including the core barrel-baffle region into the core, are not considered effective for heat removal. See Table 4.1-1 Item 9a.

Discussion

Core cooling evaluations are based on the thermal design flow rate (minimum flow) entering the reactor vessel. A maximum of 8.6 percent of this value (thimble plugs removed) is allotted as bypass flow (for V5/V+ cores). 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 so no additional limits are required.

Fuel rod diametral gap characteristics, moderator-coolant flow velocity and distribution, and moderator void are not inherently limiting. Each of these parameters is incorporated into the thermal and hydraulic models used to ensure the above-mentioned design criteria are met. For instance, the fuel rod diametral gap characteristics change with time (see Section 4.2.3.3), and the fuel rod integrity is evaluated on that basis. The effect of the moderator flow velocity and distribution (see Section 4.4.2.2) and moderator void distribution (see Section 4.4.2.4) are included in the core thermal evaluations 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 (see Section 15.6.5), control rod ejection accident, and locked rotor accident.

4.4.2 DESCRIPTION

4.4.2.1 Summary Comparison

Table 4.4-1 provides a summary of the design parameters for the core described herein for the V5/V+ 17x17 design.

Fuel densification has been considered in the DNB and fuel temperature evaluations, utilizing the methods and models described in detail in References 6 and 92.

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

The minimum DNBRs for the rated power, design overpower, and anticipated transient conditions are given in Table 4.4-1. The minimum DNBR in the limiting flow channel will be downstream of the peak heat flux location (hot spot) due to the increased downstream enthalpy rise.

DNBRs are calculated by using the correlation and definitions described in Sections 4.4.2.2.1 and 4.4.2.2.2. The VIPRE-01 computer code (discussed in Section 4.4.4.5.1) is used to determine the flow distribution in the core and the local conditions in the hot channel for use in the DNB correlation. The use of hot channel factors is discussed in Section 4.4.4.3.1 (nuclear hot channel factors) and in Section 4.4.2.2.4 (engineering hot channel factors).

4.4.2.2.1 Departure from Nucleate Boiling Technology

The W-3 correlation, and several modifications of it, have been used in Westinghouse CHF calculations. The W-3 correlation was originally developed from single tube data (Ref. 7), but was subsequently modified to apply to the 0.422 in. O.D. rod "R" grid (Ref. 8) and "L" grid (Ref. 9) as well as the 0.374 in. O.D. (Ref. 10 and 11) rod bundle data. These modifications to the W-3 correlation have been demonstrated to be adequate for reactor rod bundle design. W-3 alternative correlations are discussed following the WRB-2 correlation information.

The WRB-1 (Ref. 1) correlation was developed based exclusively on the large bank of mixing vane grid rod bundle CHF data (over 1100 points) that Westinghouse has collected. The WRB-1 and WRB-2 correlations, based on local fluid conditions, represent the rod bundle data with better accuracy over a wider range of variables than the previous correlations. These correlations account 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 variables (WRB-1 correlation) is:

Pressure	$1440 \le P \le 2490$ psia
Local mass velocity	$0.9 \le G_{loc}/ 10^6 \le 3.7$ lb/ft^2-hr
Local quality	$-0.2 \le X_{loc} \le 0.3$
Heated length, inlet to CHF location	$L_h \le 14$ feet
Grid spacing	$13 \le g_{sp} \le 32$ inches
Equivalent hydraulic diameter	$0.37 \le d_e \le 0.60$ inches
Equivalent heated hydraulic diameter	$0.46 \le d_h \le 0.59$ inches

Figure 4.4-2A shows measured critical heat flux plotted against predicted critical heat flux using the WRB-1 correlation.

Critical heat flux tests which model the 17 x 17 OFA have been performed with the results described in detail in Reference 5. It was concluded that the CHF characteristics of the 17 x 17 OFA/V5/V+ designs are not significantly different from those of the 17 x 17 STD design, and can be adequately described by the "R" grid form of the WRB-1 correlation. Furthermore, the new data can be incorporated into the "R" grid database. The WRB-2 correlation (Ref. 86) was developed to take credit for the V5 Intermediate Flow Mixing (IFM) grid design. Figure 4.4-2b shows measured critical heat flux plotted against critical heat flux using the WRB-2 correlation.

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

Pressure	$1440 \leq P \leq 2490$ psia
Local Mass Velocity	$0.9 \leq G_{loc}/ 10^6 \leq 3.7$ lb/ft^2-h
Local Quality	$-0.1 \leq X_{loc} \leq 0.3$
Heated Length, inlet to CHF location	$L_h \leq 14$ ft
Grid Spacing	$10 \leq g_{sp} \leq 26$ in.
Equivalent Hydraulic Diameter	$0.37 \leq d_e \leq 0.51$ inches
Equivalent Heated Hydraulic Diameter	$0.46 \leq d_h \leq 0.59$ inches

The ABB-NV and WLOP, W-3 Alternative correlations, are based exclusively on DNB data from rod bundle tests. They have a wider applicable range and are more accurate than the W-3 correlation for the prediction of margin to DNB. They are used for DNBR calculations as an alternative to the W-3 correlation and supplement the primary WRB-2 DNB correlation.

The ABB-NV correlation was originally developed for fuel designs of Combustion Engineering designed Pressurized Water Reactors (PWRs) based on a linear relationship between the Critical Heat Flux (CHF) and local quality. The correlation includes the following parameters: pressure, local mass velocity, local equilibrium quality, distance from grid to CHF location, heated length from inlet to CHF location, and heated hydraulic diameter of the subchannel. Supplemental rod bundle data evaluation confirms that ABB-NV, with a 95/95 correlation limit of 1.13, is applicable to the fuel region below the first mixing vane grid for fuel designs that are compatible with Westinghouse designed PWRs (Reference 83). Figure 4.4-3a shows measured critical heat flux plotted against predicted heat flux using the ABB-NV correlation.

The applicable range of the ABB-NV correlation is:

Pressure (psia)	:	1750 to 2415
Local Mass Velocity (Mlbm/hr-ft^2)	:	0.8 to 3.16
Local Quality (fraction)	:	<0.22
Heated Length, inlet to CHF location (in.)	:	48 (minimum) to 150
Heated Hydraulic Diameter Ratio	:	0.679 to 1.08
Grid Distance (in.)	:	7.3 to 24

The WLOP correlation is a modified ABB-NV correlation specifically developed for low pressure conditions and extended flow range to cover low pressure/low flow conditions. Modifications to ABB-NV were made based on test data from rod bundles containing non-mixing vane grids. The WLOP correlation with a 95/95 DNBR limit has also been validated with test data from rod bundles containing mixing vane grids (Reference 83). The WLOP correlation with a 95/95 DNBR limit of 1.18 has also been validated with test data from rod bundles containing mixing vane grids (Reference 83). Figure 4.4-3b shows measured critical heat flux plotted against predicted heat flux using the WLOP correlation.

The applicable range of the WLOP correlation is:

Pressure (psia)	:	185 to 1800
Local Mass Velocity (Mlbm/hr-ft^2)	:	1.23 to 3.07
Local Quality (fraction)	:	<0.75
Heated Length, inlet to CHF location (in.)	:	48 (minimum) to 168
Heated Hydraulic Diameter Ratio	:	0.679 to 1.00
Grid Spacing Term (Reference 93)	:	27 to 115

4.4.2.2.2 Definition of Departure from Nucleate Boiling Ratio

The DNB heat flux ratio (DNBR) as applied to this design when all flow cell walls are heated is:

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

where:

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

and $q''_{\mathrm{DNB,\,EU}}$ is the uniform critical heat flux as predicted by the WRB-1 DNB correlation (Ref. 1) or the WRB-2 correlation (Ref. 86).

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, the local fluid density, and flow velocity. The proportionality is expressed by the dimensionless TDC which is defined as:

$$\mathrm{TDC} = \frac{w'}{\rho V a}$$

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 on heat exchange rate is presented in Reference 12. The application of the TDC in the VIPRE-01 analysis is presented in Reference 89.

Westinghouse has sponsored and directed mixing tests at Columbia University (Ref. 13). 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 pressurized water reactor core under the following single and two phase (subcooled boiling) flow conditions:

Pressure	1,500 to 2,400 psia
Inlet temperature	332 to 642°F
Mass velocity	1.0 to 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-inch axial grid spacing are presented in 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 investigated by Cadek (Ref. 13), Rowe and Angle (Ref. 14 and 15), and Gonzalez-Santalo and Griffith (Ref. 16). In the subcooled boiling region, the values of TDC were indistinguishable from the single phase values. In the quality region, Rowe and Angle show that in the case with rod spacing similar to that in pressurized water reactor 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 (TDC) 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. 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.

The Zircaloy and ZIRLO™ grids employed in the 17 x 17 V5/V+ designs were designed to have the same mixing characteristics as the 17 x 17 R-grid design. This is verified by the fact that the DNB performance of the new grid design is similar to that of the R-grid design, as discussed in Section 4.4.2.2.1. Thus, the current conservative design value of TDC is applicable to the 17 x 17 OFA/V5 designs.

Since the actual reactor grid spacing is approximately 20 inches for mixing vane grid spans and approximately 10 inches for IFM spans, additional margin is available for this design, as the value of TDC increases as grid spacing decreases (Ref. 13).

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 factors is composed of a nuclear hot channel factor (see Section 4.4.4.3) describing the fission 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 density, enrichment, and burnable absorber B-10 coating; inlet flow distribution; flow redistribution; and flow mixing.

<u>Heat Flux Engineering Hot Channel Factor, F_Q^E</u>

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 density, enrichment, and burnable absorber B-10 coating and has a value of 1.03 with 95% probability at a 95% confidence level. F_Q^E factors used in the DNB evaluation are discussed in Section 4.3.2.2.1. Measured manufacturing data on Westinghouse 17 x 17 fuel were used to verify that this value was not exceeded for 95% of the limiting fuel rods at a 95% confidence level. Thus, it is expected that a statistical sampling of the fuel assemblies of the manufacturing plant will yield a value no larger than 1.03 (see Section 4.3.2.2.1). As shown in Reference 87, no DNB penalty needs to be taken for the heat flux spikes caused by variations in the above parameters, as well as fuel pellet eccentricity and fuel rod diameter variation.

<u>Enthalpy Rise Engineering Hot Channel Factor, $F_{\Delta H}^E$</u>

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

a. Pellet density, enrichment and burnable absorber B-10 coating:

   Variations in pellet density, enrichment, and burnable absorber B-10 coating are considered statistically in establishing the limit DNBRs (see Section 4.4.1.1) for the revised thermal design procedure (Ref. 3) employed in this application. Uncertainties in these variables are determined from sampling of manufacturing data.

b. 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 VIPRE-01 analysis.

c. 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 on flow of the nonuniform power distribution is inherently considered in the VIPRE-01 analysis for every operating condition which is evaluated.

d. Flow mixing

The subchannel mixing model incorporated in the VIPRE-01 Code and used in reactor design is based on experimental data (Ref. 18) discussed in Sections 4.4.2.2.3 and 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 Effects of Rod Bow on DNBR

The phenomenon of fuel rod bowing, as described in Reference 19, must be accounted for in the DNBR safety analysis of Condition I and Condition II events for each plant application. Applicable credits for margin resulting from retained conservatism in the evaluation of DNBR are used to offset the effect of rod bow.

The safety analysis for Callaway maintains sufficient margin between the safety analysis limit DNBRs and the design limit DNBRs (1.21 and 1.22 V5/V+ for thimble and typical cells, respectively) to accommodate full flow and low flow DNBR penalties based on the methodology in Reference 20. The rod bow DNBR penalties applicable to 17 x 17 fuel assembly analysis utilizing the WRB-2 DNB correlation were determined using the methodology in Reference 20.

The maximum rod bow penalties accounted for in the design safety analysis are based on an assembly average burnup of 24000 MWD/MTU. At burnups greater than 24000 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. In the V5/V+IFM spans, the grid-to-grid spacing is approximately 10 inches compared to approximately 20 inches in the other spans. Using the NRC-approved scaling factor results in predicted channel closure in the limiting 10-

inch spans of less than 50% closure. Therefore, no rod bow penalty is required in the V5/V+IFM spans, as indicated in Reference 86. Specific values for the rod bow penalties, as well as the other DNBR penalties which may be revised from cycle to cycle, are provided for each reload in an attachment to the Reload Safety Evaluation.

4.4.2.2.6 Effects of Flow Anomaly on DNBR

In addition to the rod bow penalty discussed in Section 4.4.2.2.5 a DNBR penalty of 3.3% is identified for V5/V+ fuel to accommodate the lower plenum flow anomaly penalty.

4.4.2.2.7 Effects of Temperature Bias on DNBR

A 1.3°F temperature bias is implemented on OTΔT events as a DNBR penalty (1.4%). For non-OTΔT events, the bias is added to the nominal Tin and statepoint Tin values used in the analysis. This bias is added as part of the Margin Recovery Project. (See Reference 30.)

4.4.2.2.8 Effects of Flow Bias on DNBR

A flow bias of 0.2% is added to the total DNBR penalty assessment for both the non-OTΔT events and the OTΔT events. This bias is also added as part of the Margin Recovery Project. (See Reference 30.)

4.4.2.2.9 Effects of Pressurizer Pressure Bias on DNBR

A Pressuizer Pressure bias is added which resulted in a DNBR penalty assessment of 2.4% for both the non-OTΔT events and the OTΔT events. This bias is added to allow the Minimum DNBR value used in the COLR Section 2.11 to be lowered from ≥ 2224 psi to the current value of ≥ 2195 psi. (See Reference 32.)

4.4.2.3 Linear Heat Generation Rate

The core average and maximum linear heat generation rates are given in Table 4.4-1. The method of determining the maximum linear heat generation rate is given in Section 4.3.2.2.

4.4.2.4 Void Fraction Distribution

The calculated core average and the hot subchannel maximum and average void fractions are presented in Table 4.4-3 for operation at full power with design hot channel factors. The void fraction distribution in the core at various radial and axial locations is presented in Reference 21, based on THINC-IV predictions. The void models used in the VIPRE-01 Code are described in Section 4.4.2.7.3.

Since void formation due to subcooled boiling is an important promoter of interassembly flow redistribution, a sensitivity study (Ref. 21) was performed with THINC-IV using the void model referenced above.

The results of this study showed that because of the realistic crossflow model used in THINC-IV, the minimum DNBR in the hot channel is relatively insensitive to variations in this model. The range of variations considered in this sensitivity study covered the maximum uncertainty range of the data used to develop each part of the void fraction correlation. The conclusions of the sensitivity study remain applicable to the VIPRE-01 code.

4.4.2.5 Core Coolant Flow Distribution

Assembly average coolant mass velocity and enthalpy at various radial and axial core locations for a near the beginning of core life power distribution for the cycle 1 core are given in Figures 4.4-5 through 4.4-7. Typical coolant enthalpy rise and flow distributions for the 4-foot elevation (1/3 of core height) are shown in Figure 4.4-5, for the 8-foot elevation (2/3 of core height) in Figure 4.4-6, and at the core exit in Figure 4.4-7. These distributions are for the full power conditions as given in Table 4.4-1 and for the radial power density distribution shown in Figure 4.3-7A. 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 4.4-1 are described in Section 4.4.2.7. The core pressure drop includes the fuel assembly (including the effect of inserted core components, such as rod cluster controls), lower core plate, and upper core plate pressure drops. The full power operation pressure drop values shown in Table 4.4-1 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 4.4-1 are based on this best estimate flow rather than the thermal design flow.

Uncertainties associated with the core pressure drop values are discussed in Section 4.4.2.9.2.

4.4.2.6.2 Hydraulic Loads

The fuel assembly holddown springs, Figure 4.2-2, are designed to keep the fuel assemblies in contact with the lower core plate under all Condition I and II events, with the exception of the turbine overspeed transient associated with a loss of external load. The holddown springs are designed to tolerate the possible overdeflection 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 best estimate flow and accounting for the best estimate core bypass flow . Core hydraulic loads at cold plant startup conditions are based on the cold best estimate flow, but are adjusted to account for the coolant density difference. 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. Applicable uncertainties are applied to these results.

Core hydraulic loads were measured during the prototype assembly tests described in Section 1.5. Reference 22 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. 23), with the properties evaluated at bulk fluid conditions:

$$\frac{hD_e}{K} = 0.023\left(\frac{D_eG}{\mu}\right)^{0.8}\left(\frac{C_p\mu}{K}\right)^{0.4}$$

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, (lb$_m$/hr-ft^2)
μ	=	dynamic viscosity, (lb$_m$/ft-hr)
C_p	=	heat capacity, (Btu/lb$_m$-F)

This correlation has been shown to be conservative (Ref. 24) for rod bundle geometries with pitch to diameter ratios in the range used by pressurized water reactors.

The onset of nucleate boiling occurs when the clad wall temperature reaches the amount of superheat predicted by Thom's (Ref. 25) correlation. After this occurrence, the outer clad wall temperature is determined by:

$$\Delta T_{sat} = [0.072 \exp(-P/1260)] (q'')^{0.5}$$

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 (see Table 4.4-3).

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

$$\Delta P_L = \left(K + F\frac{L}{D_e}\right)\frac{\rho V^2}{2 g_C (144)}$$

where:

ΔP_L = unrecoverable pressure drop, (lb$_f$/in.^{2})

ρ = fluid density, (lb$_m$/ft^3)

L = length, (ft)

D_e = equivalent diameter, (ft)

V = fluid velocity, (ft/sec)

g_C = 32.174, (lb_m-ft/lb_f-sec^2)

K = form loss coefficient, dimensionless

F = friction loss coefficient, dimensionless

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

Values are quoted in Table 4.4-1 for unrecoverable pressure loss across the reactor vessel, including the inlet and outlet nozzles, and across the core. The results of full-scale tests of core components and fuel assemblies were utilized in developing the core pressure loss characteristic. The pressure drop for the vessel was obtained by combining the core loss with correlation of 1/7th scale model hydraulic test data on a number of vessels (Ref. 26 and 27) and form loss relationships (Ref. 28). Moody (Ref. 29) curves were used to obtain the single phase friction factors.

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

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 quality, a bulk void model is applied to compute the vapor volume fraction (void fraction).

VIPRE-01 uses a profile fit model (Reference 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 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

DNB core safety limits are generated as a function of coolant temperature, pressure, core power, and axial and radial power distributions. Operation within these DNB safety limits ensures that the DNB design basis is met for both steady-state operation and for 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 (Chapter 15.0), specific protection functions are provided as described in Section 7.2 and the use of these protection functions is described in Chapter 15.0. The thermal response of the fuel is discussed in Section 4.2.3.

4.4.2.9 Uncertainties in Estimates

4.4.2.9.1 Uncertainties in Fuel and Clad Temperatures

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 inpile measurements (Ref. 33 through 39), by out-of-pile measurements of the fuel and clad properties (Ref. 40 through 51), and by measurements of the fuel and clad dimensions during fabrication. The resulting uncertainties are then used in all evaluations involving the fuel temperature. The effect of densification on fuel temperature uncertainties is presented in Reference 6.

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.

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

Uncertainty in determining the cladding temperature results from uncertainties in the crud and oxide thicknesses. Because of the excellent heat transfer between the surface of the rod and the coolant, the film temperature drop does not appreciably contribute to the uncertainty.

4.4.2.9.2 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 4.4-1. The uncertainties quoted are based on the uncertainties in both the test results and the analytical extension of these values to the reactor application.

A major use of the core and vessel pressure drops is to determine the primary system coolant flow rates, as discussed in Section 5.1. In addition, as discussed in Section 4.4.5.1, tests on the primary system prior to initial criticality will be made to verify that a conservative primary system coolant flow rate has been used in the design and analyses of the plant.

4.4.2.9.3 Uncertainties Due to Inlet Flow Maldistribution

The effects of uncertainties in the inlet flow maldistribution criteria used in the core thermal analyses are discussed in Section 4.4.4.2.2.

4.4.2.9.4 Uncertainty in DNB Correlation

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

4.4.2.9.5 Uncertainties in DNBR Calculations

The uncertainties in the DNBRs calculated by VIPRE-01 analysis (see Section 4.4.4.5.1) due to uncertainties in the nuclear peaking factors are accounted for by either applying conservatively high values of the nuclear peaking factors (non-RTDP) or including measurement error allowances in the statistical evaluation of the limit DNBR (see Section 4.4.1.1) using the RTDP (Ref. 3). 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. 21) 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 VIPRE-01 code to accurately predict flow and enthalpy distributions in rod bundles is discussed in Section 4.4.4.5.1 and in Reference 89. Studies have been performed (Ref. 88) to determine the sensitivity of the minimum DNBR in the hot channel to the void fraction correlation (also see Section 4.4.2.7.3) and the inlet flow distributions. The results of these studies show that the minimum DNBR in the hot channel is relatively insensitive to variations in these parameters.

4.4.2.9.6 Uncertainties in Flow Rates

The uncertainties associated with loop flow rates are discussed in Section 5.1. For core thermal performance evaluations, a thermal design loop flow is used which is less than the best estimate loop flow. In addition, another 8.6 percent of the thermal design flow (thimble plugs removed) 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, applicable uncertainties are applied to the hydraulic loads on the fuel assembly calculated using best estimate flow rates.

4.4.2.9.8 Uncertainty in Mixing Coefficient

The results of the mixing tests done on 17 x 17 geometry, as discussed in Section 4.4.2.2.3, gave a mean value of TDC of 0.059 and standard deviation of 0.007. The value of TDC used in VIPRE-01 analyses for this application is 0.038, three standard deviations below the mean value.

4.4.2.10 Flux Tilt Considerations

Significant quadrant power tilts are not anticipated during normal operation since this phenomenon is caused by some asymmetric perturbation. A dropped or misaligned rod cluster control assembly could cause changes in hot channel factors. However, these events are analyzed separately in Chapter 15.0. This discussion will be confined to flux tilts caused by x-y xenon transients, inlet temperature mismatches, enrichment variations within tolerances, and so forth.

The design value of the enthalpy rise hot channel factor $F^N_{\Delta H}$ is assumed to be sufficiently conservative that flux tilts up to and including the alarm point (see the Technical Specifications) will not result in values of $F^N_{\Delta H}$ greater than that assumed in the limiting analysis. 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 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.

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 UO_2 (see Section 4.4.1.2). To preclude center melting and as a basis for overpower protection system setpoints, a calculated center-line fuel temperature of

4,700°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 primarily a function of the local power density and the UO_2 thermal conductivity. However, the computation of radial fuel rod temperature distributions combines crud, oxide, clad gap, and pellet conductances. The factors which influence these conductances, such as gap size (or contact pressure), internal gas pressure, gas composition, pellet density, and radial power distribution within the pellet, etc., have been combined into a semiempirical thermal model (see Section 4.2.3.3) with the model modifications for time dependent fuel densification given in Reference 6 (See also References 5 and 20 of Section 4.2). 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 (Ref. 33 through 39) and melt radius data (Ref. 53 and 54) with good results.

As described in Reference 6, fuel rod thermal evaluations (fuel centerline, average and surface temperatures) are determined throughout the fuel rod lifetime 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 UO_2 Thermal Conductivity

The thermal conductivity of uranium dioxide was evaluated from data reported by Howard, et al. (Ref. 40); Lucks et al. (Ref. 41); Daniel, et al. (Ref. 42); Feith (Ref. 43); Vogt, et al. (Ref. 44); Nishijima, et al. (Ref. 45); Wheeler, et al. (Ref. 46); Godfrey, et al. (Ref. 47); Stora, et al. (Ref. 48); Bush (Ref. 49); Asamoto, et al. (Ref. 50); Kruger (Ref. 51); and Gyllander (Ref. 55).

At higher temperatures, thermal conductivity is best obtained by utilizing the integral conductivity to melt, which can be determined with more certainty. From an examination of the data, it has been concluded that the best estimate for the value of $\int_0^{2800\ C} K dt$ is 93 watts/cm. This conclusion is based on the integral values reported by Gyllander (Ref. 55), Lyons, et al. (Ref. 56), Coplin, et al. (Ref. 57), Duncan (Ref. 53), Bain (Ref. 58), and Stora (Ref. 59).

The design curve for the thermal conductivity is shown in Figure 4.4-9. The section of the curve at temperatures between 0 and 1,300°C is in excellent agreement with the recommendation of the IAEA panel (Ref. 60). The section of the curve above 1,300°C is derived for an integral value of 93 watts/cm (Ref. 53, 55 and 59).

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

$$K = \frac{1}{11.8 + 0.0238T} + 8.775 \times 10^{-13}\ T^3$$

where:

K = watts/cm-C

T = C

4.4.2.11.2 Radial Power Distribution in UO_2 Fuel Rods

An accurate description of the fuel rod 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. Radial power distributions in UO_2 fuel rods are 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 (Ref. 61 and 62). 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:

$$\int_{T_s}^{T_C} K(T) \qquad dT = \frac{q'f}{4\pi}$$

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 UO_2 thermal conductivity model, the calculated fuel centerline temperatures predict the inpile temperature measurements.

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

$$h = \frac{K_{gas}}{\frac{\delta}{2} + \delta_r}$$

where:

h	=	contact conductance, (Btu/hr-ft^2-F)
K_{gas}	=	thermal conductivity of the gas mixture including a correction factor (Ref. 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 equations is used to calculate the temperature drop across the gap for noncontact 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 (Ref. 63 and 64). This information together with the pellet and clad inner surface roughness for Westinghouse fuel leads to the following correlation:

$$h = 0.6P + \frac{K_{gas}}{\delta_r}$$

δ_r	=	effective gap spacing due to surface roughness, (ft)
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. 63) 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 Temperatures

The outer surface of the fuel rod at the hot spot operates at a temperature of approximately 660°F for steady state operation at rated power throughout core life due to the presence 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 as the ratio of the maximum to core average heat flux. As discussed in Section 4.3.2.2.6 and presented in the Core Operating Limits Report, the maximum design value of F_Q for normal operation is 2.50. This results in a peak linear power of 14.23 kW/ft at full-power conditions.

The fuel temperature design basis is discussed in Section 4.4.1.2. For Condition I and Condition II events, there is at least a 95-percent probability at the 95-percent confidence level that the fuel centerline temperature remains below the UO2 melt temperature over the lifetime of the rod, including allowances for uncertainties. The peak linear power for prevention of fuel centerline melting is 22.46 kW/ft. As described in Section 4.3.2.2.6, power distributions resulting from overpower transients/operator errors are generated for power levels up the maximum overpower value presented in Table 15.0-4. From these power distributions, the peak linear power (including allowances for nuclear uncertainty, engineering factor, power-spike factor vs. elevation, stack-height penalty, and calorimetric error) does not exceed the specified fuel centerline melting limit. Therefore, the fuel centerline temperature at the peak linear power resulting from overpower transients/operator errors (assuming the maximum overpower presented in Table 15.0-4) is less than the fuel melting temperature.

4.4.3 DESCRIPTION OF THE THERMAL AND HYDRAULIC DESIGN OF THE REACTOR COOLANT SYSTEM

4.4.3.1 <u>Plant Configuration Data</u>

Plant configuration data for the thermal hydraulic and fluid systems external to the core are provided as appropriate in Chapters 5.0, 6.0, and 9.0. Implementation of the emergency core cooling system (ECCS) is discussed in Chapter 15.0. Some specific areas of interest are the following:

a. Total coolant flow rates for the reactor coolant system (RCS) and each loop are provided in Table 5.1-1. Flow rates employed in the evaluation of the core are presented throughout Section 4.4.

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

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

d. 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.

e. Components of the ECCS are to be located so as to meet the criteria for net positive suction head described in Section 6.3.

f. Line lengths and sizes for the safety injection system are determined so as to guarantee a total system resistance which will provide, as a minimum, the fluid delivery rates assumed in the safety analyses described in Chapter 15.0.

g. The parameters for components of the RCS are presented in Section 5.4.

h. 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 net positive suction head and minimum seal injection flow rate must be established before operating the reactor coolant pumps. With the minimum 6-gpm labyrinth seal injection flow rate established before each pump operation, the operator will have to verify that the system pressure satisfies net positive suction head requirements.

4.4.3.3 Power-Flow Operating Map (BWR)

Not applicable to SNUPPS.

4.4.3.4 Temperature-Power Operating Map

The relationship between RCS temperature and power is shown in Figure 4.4-10.

The effects of reduced core flow due to inoperative pumps are discussed in Sections 5.4.1, 15.2.5, and 15.3.4.

4.4.3.5 Load Following Characteristics

Load follow using control rod motion and dilution or boration by the boron system is discussed in Section 4.3.2.4.16.

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 Tables 4.3-1A and 4.4-1.

4.4.4 EVALUATION

4.4.4.1 Critical Heat Flux

The critical heat flux correlation utilized in the core thermal analysis is explained in detail 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 for core bypass flow are considered:

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

b. Flow entering into the rod cluster control guide thimbles to cool the control rods

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

d. Flow introduced between the baffle and the barrel for the purpose of cooling these components and which is not considered available for core cooling

e. 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 for V5 fuel is equal to 8.6 percent of the thermal design flow (thimble plugs removed). See Table 4.1-1 item 9.a.

Of the total allowance, 4.2 percent is associated with the internals (items a, c, d, and e above) and 4.4 percent for the core (See above). 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 <8.6 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 from several 1/7 scale hydraulic reactor model tests (Ref. 26, 27, and 65) have been utilized in arriving at the core inlet flow maldistribution criteria to be used in the VIPRE-01 analyses (see Section 4.4.4.5.1). THINC-I analyses made using this data have indicated that a conservative design basis is to consider a 5-percent reduction in the flow to the hot assembly (Ref. 66). The same design basis of 5-percent reduction to the hot assembly inlet is used in VIPRE-01 analyses.

The experimental error estimated in the inlet velocity distribution has been considered as outlined in Reference 21 where the sensitivity of changes in inlet velocity distributions to hot channel thermal performance is shown to be small. Studies (Ref. 21) 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 26 and 27.

The effect of the total flow rate on the inlet velocity distribution was studied in the experiments of Reference 26. As was expected, on the basis of the theoretical analysis, no significant variation could be found in inlet velocity distribution with reduced flow rate.

4.4.4.2.3 Empirical Friction Factor Correlations

Two empirical friction factor correlations are used in the VIPRE-01 Code (described in Section 4.4.4.5.1).

The friction factor in the axial direction, parallel to the fuel rod axis, is evaluated using a correlation for the smooth tube (Ref. 89). The effect of two-phase flow on the friction loss is expressed in terms of a single-phase friction pressure drop and a two-phase friction multiplier. The multiplier is calculated directly using the homogeneous equilibrium flow model.

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. 28) is applicable. This correlation is of the form:

$F_L = A\ Re_L^{-0.2}$

where:

A is a function of the rod pitch and diameter as given in Reference 28.

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

Extensive comparisons of VIPRE-01 predictions, using these correlations to THINC-IV predictions are given in Reference 89, and verify the applicability of these correlations in pressurized water reactor 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 through-out 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 linear power density q' (kW/ft) at a point x, y, z in a core with N fuel rods and height H,

$$F^N_{\Delta H} = \frac{\text{hot rod power}}{\text{average rod power}} = \frac{\text{Max}\int_o^H q'(x_o, y_o, z_o)dz}{\frac{1}{N}\sum_{\text{all rods}}\int_o^H q'(x, y, z)dz}$$

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 21 is used in the VIPRE-01 model.

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

$$F^N_{\Delta H} = 1.59[1 + 0.3(1 - P)]$$

The permitted relaxation of $F^N_{\Delta H}$ with power level is included in the DNB protection setpoints and allows radial power shape changes with rod insertion to the insertion limits (Ref. 69), thus allowing greater flexibility in the nuclear design.

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 or power change or due to a spatial xenon transient 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 a large axial offset corresponds to an axial shape which could lead to a DNBR which is less than that calculated for the reference DNB design axial shape.

The reference DNB design axial shape used is a chopped cosine shape with a peak to average value, F_Z of 1.75.

To determine the penalty to be taken in protection setpoints for extreme values of axial flux difference, this reference shape 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 15.0, 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 relaxed axial offset control for normal operation, as described in Reference 84. 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 DNBR for conditions representative of the loss-of-flow accident as a function of the axial flux difference initially in the core. A plot of this type is provided in Figure 4.4-11 for first core initial conditions (first cycle based on constant axial offset control per Reference 70). As noted on this figure, all power shapes were evaluated with a full power radial peaking factor ($F^N_{\Delta H}$) of 1.55 for cycle 1. 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 the loss of flow transient. Also shown is the minimum DNBR calculated for a 1.55 cosine reference power shape 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 7.0 are designed to meet these bases. The response of the core to Condition II transients is given in Chapter 15.0.

4.4.4.5 Analytical Techniques

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 show that the core safety limits, are not exceeded, using the most conservative power distribution. The thermal design takes into account local variations in dimensions, power generation, flow redistribution, and mixing. VIPRE-01 (VIPRE) is a three-dimensional sub-channel code that has been developed to account for hydraulic and nuclear effects on the enthalpy rise in the core and hot channels (Ref. 88). VIPRE modeling of a PWR core is based on one-pass modeling approach (Ref. 89). In the one-pass modeling, hot channels and their adjacent channels are modeled 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 channels. Conservation equations of mass, axial and lateral momentum, and energy are solved for the fluid enthalpy, axial flow rate, lateral flow and pressure drop.

4.4.4.5.2 Steady State Analysis

The VIPRE core model as approved by the NRC, Reference 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 Reference 88, including discussions on code validation with experimental data. The VIPRE modeling method is described in Reference 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 (Ref. 89).

Estimates of uncertainties are discussed in Section 4.4.2.9.

Experimental Verification

Extensive additional experimental verification of VIPRE is presented in Reference 88.

The VIPRE analysis is based on a knowledge and understanding of heat transfer and the 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 analysis as described above.

Transient Analysis

VIPRE is capable of transient DNB analysis. The conservation equations in the VIPRE code contain the necessary accumulation of terms for transient calculations. The input description can include one of the following time dependent arrays:

1. inlet flow variation,

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 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 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.

The methods for evaluating fuel rod thermal response are described in Section 15.0.11.

4.4.4.6 Hydrodynamic and Flow Power Coupled Instability

Boiling flows may be susceptible to thermohydrodynamic instabilities (Ref. 71). These instabilities are undesirable in reactors, since they may cause a change in thermohydraulic 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 mode of operation under Condition I and II events shall 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. 71) when the slope of the reactor coolant system pressure drop-flow rate curve ($\partial\Delta P/\partial G|_{\text{internal}}$) becomes algebraically smaller than the loop supply (pump head) pressure drop-flow rate curve ($\partial\Delta P/\partial G|_{\text{external}}$). The criterion for stability is thus $\partial\Delta P/\partial G|_{\text{internal}} > \partial\Delta P/\partial G|$ external. The Westinghouse pump head curve has a negative slope ($\partial\Delta P/\partial G|_{\text{external}} < 0$), whereas the reactor coolant system pressure drop-flow curve has a positive slope ($\partial\Delta P/\partial G|_{\text{internal}} > 0$) over the Condition I and Condition II operational ranges. Thus, the Ledinegg instability will not occur.

The mechanism of density wave oscillations in a heated channel has been described in Lahey and Moody (Ref. 72). Briefly, an inlet flow fluctuation produces an enthalpy perturbation. This perturbs the length and the pressure drop of 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. 73) 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 the typical Westinghouse reactor designs similar to Callaway (Ref. 74, 75, and 76), under Condition I and II operation. The results indicate that a large margin to density wave instability exists, e.g., increases on the order of 150 to 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. 73) 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 (Ref. 77) 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 pressure relative to the Westinghouse PWR operating pressures. Kao, Morgan, and Parker (Ref. 78) analyzed parallel closed channel stability experiments simulating a reactor core flow. These experiments were conducted at pressures up to 2200 psia. The results showed that for flow and power levels typical of power reactor conditions no flow oscillations could be induced above 1200 psia.

Additional evidence that flow instabilities do not adversly affect thermal margin is provided by the data from the rod bundle DNB tests. Many Westinghouse rod bundles have been tested over wide ranges of operating conditions with no evidence of premature DNB or of inconsistent data which might be indicative of flow instabilities in the rod bundle.

In summary, it is concluded that thermohydrodynamic instabilities will not occur under Condition I and II modes of operation for Westinghouse PWR 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 to 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 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, 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 (see Section 4.4.2.2 and Ref. 79) shows that the predicted DNBR is dependent upon the local values of quality and mass velocity.

The VIPRE-01 Code is capable of predicting the effects of local flow blockages on DNBR within the fuel assembly on a subchannel basis, regardless of where the flow blockage occurs. In Reference 88, it is shown that for a fuel assembly similar to the Westinghouse design, 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 inches downstream of the blockage. With the reactor operating at the nominal full power conditions specified in Table 4.4-1, the effects of an increase in enthalpy and decrease in mass velocity in the lower portion of the fuel assembly would not result in the reactor reaching the design DNBR specified in Section 4.4.1.1.

From a review of the open literature, it is concluded that flow perturbations caused by flow blockage in "open lattice cores" similar to the Westinghouse cores are confined to the vicinity of the blockage. For a flow blockage in a single flow cell, Ohtsubo, et al. (Ref. 80) show that the mean bundle velocity is approached asymptotically about 4 inches downstream from the blockage. Similar results were also found for two and three cells completely blocked. Basmer, et al. (Ref. 81) tested an open lattice fuel assembly in which 41 percent of the subchannels were completely blocked in the center of the test bundle between spacer grids. Their results show the stagnant zone behind the flow blockage essentially disappears after 1.65 L/De or about 5 inches for their test bundle. They also found that leakage flow through the blockage tended to shorten the stagnant zone or, in essence, the complete recovery length. Thus, local flow blockages within a fuel assembly have little effect on subchannel enthalpy rise. The reduction in local mass velocity is then the main parameter which affects the DNBR. If the plants were operating at full power and nominal steady state conditions, as specified in Table 4.4-1, a reduction in local mass velocity greater than 56 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 would likely not effect DNBR at all.

Coolant flow blockages induce local crossflows as well as promote turbulence. Fuel rod behavior is changed under the influence of a sufficiently high crossflow component. Fuel rod vibration could occur, caused by this crossflow component, through vortex shedding or turbulent mechanisms. If the crossflow velocity exceeds the limit established for fluid elastic stability, large amplitude whirling results. The limits for a controlled vibration mechanism are established from studies of vortex shedding and turbulent pressure fluctuations. The crossflow velocity required to exceed fluid elastic stability limits is dependent on the axial location of the blockage and the characterization of the crossflow (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 (see Section 4.2.3.1).

4.4.5 TESTING AND VERIFICATION

4.4.5.1 Tests Prior to Initial Criticality

A reactor coolant flow test is performed following fuel loading but prior to initial criticality. Coolant loop pressure drop data is obtained in this test. These data, in conjunction with coolant pump performance information, allow determination of the coolant flow rates at reactor operating conditions. This test verifies that proper coolant flow rates have been used in the core thermal and hydraulic analysis. Chapter 14.0 describes the initial test programs.

4.4.5.2 Initial Power and Plant Operation

Core power distribution measurements are made at several core power levels (see Chapter 14.0). 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. 82). 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 89

4.4.5.3 Component and Fuel Inspections

Inspections performed on the manufactured fuel are described 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 (see 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 4.4-21 shows the number and location of instrumented assemblies in the core. The core-exit thermo-couples can provide a backup to the flux monitoring instrumentation for monitoring power distribution.

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 set-points includes the reactor coolant temperature in each loop and the axial distribution of 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 installed on opposite "flat" portions of the core containing the primary startup sources at an elevation approximately 1/4 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 1/2 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 are located equidistant from the reactor vessel at all points and, to minimize neutron flux pattern distortions, within 1 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.

The output of the power range channels is used for:

a. The rod speed control function

b. Alerting the operator to an excessive power unbalance between the quadrants

c. Protecting the core against rod ejection accidents, and

d. Protecting 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 7.0. 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) monitors the reactor coolant system (RCS) for the presence of metallic loose parts. It consists of 12 active instrumentation channels, each comprising a piezoelectric accelerometer (sensor), signal conditioning, and diagnostic equipment.

Two redundant sensors are fastened mechanically to the RCS at each of the following potential loose parts collection regions:

Reactor pressure vessel - upper head region

Reactor pressure vessel - lower head region

Each steam generator - reactor coolant inlet region

The output signal from each accelerometer is amplified by a preamplifier and amplifier. The amplified signal is processed through a discriminator to eliminate noises and signals not indicative of loose parts, and the processed signal is compared to a preset alarm setpoint. Loose parts detection is accomplished at a frequency range of 1 kHz to 20 kHz. Spurious alarming from control rod stepping is prevented by a module that detects control rod drive mechanism (CRDM) motion commands and automatically inhibits alarms during control rod stepping.

If a measured signal exceeds the preset alarm level, audible and visible alarms at the LPMS console in the control room are activated. Digital signal processors record the times that the first and subsequent impact signals reach various sensors. This timing information provides a basis for locating the loose part. The LPMS also has a provision for audio monitoring of any channel. The audio signal can be compared with a previously recorded audio signal, if there is an ambiguity, to confirm the presence of a loose part.

The on-line sensitivity of the LPMS is such that the system will detect a loose part that weighs from 0.25 to 30 pounds and impacts with a kinetic energy of 0.5 feet pounds on the inside surface of the RCS pressure boundary within 3 feet of a sensor.

The components of the LPMS are designed for the environmental conditions specified in Table 4.4-5. All of the equipment inside the containment is designed to remain functional through an OBE and radiation exposures anticipated during a 40-year operating lifetime. Physical separation of the two instrument channels, associated with the redundant sensors at each RCS location, exists from the sensor to the output of the signal conditioning devices.

The LPMS alarm setpoints were verified prior to initial plant startup. Channel audio outputs were recorded during hot functional testing to obtain a signature record for subsequent comparison with real or suspected loose parts signals. Periodic testing currently performed is addressed in Section 16.3.3.5.

The loose parts monitoring system complies with NRC Regulatory Guide 1.133, except as noted in FSAR Chapter 3, Appendix 3A “Conformance to NRC Regulatory Guides”.

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TABLE 4.4-1 THERMAL AND HYDRAULIC DESIGN PARAMETERS

Design Parameters	
Reactor core heat output, MWt	3,565
Reactor core heat output, 10^6 Btu/hr	12,164
Heat generated in fuel, %	97.4
Core pressure, nominal, psia	2274
Minimum DNBR at nominal design conditions	
Typical flow channel	2.45
Thimble (cold wall) flow channel	2.35
Minimum DNBR for design transients	
Typical flow channel	1.22
Thimble (cold wall) flow channel	1.21
DNB correlation	WRB-2
HFP Nominal Coolant Conditions*	
Vessel minimum measured flow	
Rate (including bypass), 10^6 lbm/hr	142.4
GPM	382,630
Vessel thermal design flow	
Rate (including bypass), 10^6 lbm/hr	139.4
GPM	374,400
Core flow rate (excluding bypass of 8.6%), based on TDF (b)	
10^6 lbm/hr	127.4 (b)
GPM	342,200 (b)
Fuel assembly flow area for heat transfer, ft^2	54.13

TABLE 4.4-1 (Sheet 2)

Core inlet mass velocity, (b) 10^6 lbm/hr-ft^2 (based on TDF)	2.35	

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* Based on vessel average temperature of 588.4°F

HFP Thermal and Hydraulic
<u>Design Parameters</u>
(Based on Thermal Design Flow)

Nominal vessel/core inlet temperature,°F		556.8
Vessel average temperature,°F		588.4
Core average temperature,°F		593.1 (b)
Vessel outlet temperature,°F		620.0
Average temperature rise in vessel,°F		63.2
Average temperature rise in core,°F		68.4 (b)
<u>Heat Transfer</u>		
Active heat transfer surface area, ft^2	57,505	
Average heat flux, Btu/hr-ft^2	206,085	
Average linear power, kW/ft	5.69 (c)	
Peak linear power for normal operation, kW/ft	14.23 (c)	
<u>Pressure Drop</u>		
Across core, psia	30.1 #	

Based on best estimate reactor flow rate

TABLE 4.4-1 (Sheet 3)

(a) Not used.

(b) The value is for thimble plugs removed.

(c) Based on average active fuel stack height of 143.7 inches; 5.68 kW/ft and 14.26 kw/ft based on 144 inches.

TABLE 4.4-2 DELETED

Table 4.4-2 Is Deleted.

TABLE 4.4-3 VOID FRACTIONS AT NOMINAL REACTOR CONDITIONS WITH DESIGN HOT CHANNEL FACTORS (FIRST CYCLE)

	Average	Maximum
Core	0.46	-
Hot Subchannel	6.0	20.6

TABLE 4.4-4 DELETED

Table 4.4-4 Is Deleted.

TABLE 4.4-5 LOOSE PARTS MONITORING SYSTEM ENVIRONMENTAL CONDITIONS

A.	Accelerometers	
	Temperature	40-650°F
	Humidity	0-100%
	Radiation	10^{18}nvt and 10^{8} rad
	Pressure	69 psig
	Vibration	OBE
	Atmosphere	Air
B	Preamplifiers and Cables (inside containment)	
	Temperature-electronics	40-150°F
	Hardline Cable	40-650°F
	Cable inside containment	40-150°F
	Humidity	0-100%
	Radiation	10^{12}nvt and $6x10^{6}$ rad
	Pressure	69 psig
	Shock and Vibration	OBE
	Atmosphere	Air
C.	Signal Conditioning Amplifier, Signal Processor, and Associated Equipment (outside of containment, excluding recording equipment)	
	Temperature	40-120°F
	Radiation	10^{3} rad
	Pressure	Atmospheric
	Humidity	0-95%
	Shock and Vibration	OBE
	Atmosphere	Air
D.	Recording Equipment (outside of containment)	
	Temperature	40-120°F
	Radiation	Not Applicable
	Pressure	Atmospheric
	Humidity	0-95%
	Shock and Vibration	In accordance with good engineering practice
	Atmosphere	Air

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-chromium-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 water chemistry.

a. Pressure vessel

All pressure containing materials comply with Section III of the ASME Boiler and Pressure Vessel Code, and are fabricated from austenitic (Type 304) stainless steel.

b. 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 housings are zinc plated or flame sprayed to provide corrosion resistance.

Coils are wound with double glass insulated copper wire on bobbins made of a compound of mica paper sheet impregnated with resin silicone. Coils are then vacuum impregnated with silicon varnish. 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.

c. Latch assembly

Magnetic pole pieces are fabricated from Type 410 stainless steel. All nonmagnetic parts, except pins and springs, are fabricated from Type 304 stainless steel. Haynes 25 is used to fabricate link pins. Springs are made from nickel-chromium-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.

d. 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-chromium-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 ensure 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.

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 ANSI N45.2.1. Process specifications in packaging and shipment are discussed in Section 5.2.3.

4.5.2 REACTOR INTERNALS MATERIALS

4.5.2.1 Materials Specifications

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 key bolts, which are fabricated of Inconel-750. These materials are listed in Table 5.2-4. There are no other materials used in the reactor internals or core support structures which are not otherwise included in ASME Code, Section 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 nondestructive examination of wrought seamless tubular products and fittings is in accordance with Section III of the ASME Code at a minimum and as amended by Equipment Specification 952634, Rev. 5.

4.5.2.4 Fabrication and Processing of Austenitic Stainless Steel Components

The discussions provided in Section 5.2.3 and Appendix 3A verify conformance of reactor internals and core support structures with Regulatory Guide 1.44.

The discussions provided in Section 5.2.3 and Appendix 3A verify conformance of reactor internals and core support structures with Regulatory Guide 1.31.

The discussion provided in Appendix 3A verifies conformance of reactor internals with Regulatory Guide 1.34.

The discussion provided in Appendix 3A verifies conformance of reactor internals and core support structures with Regulatory Guide 1.71.

4.5.2.5 Contamination Protection and Cleaning of Austenitic Stainless Steel

The discussions provided in Section 5.2.3 and Appendix 3A are applicable to the reactor internals and core support structures and verify conformance with ANSI N45 specifications and Regulatory Guide 1.37.

4.6 FUNCTIONAL DESIGN OF REACTIVITY CONTROL SYSTEMS

4.6.1 INFORMATION FOR CONTROL ROD DRIVE SYSTEM (CRDS)

The CRDS is described in Section 3.9(N).4.1. Figures 3.9(N)-5 and 3.9(N)-6 provide the details of the control rod drive mechanisms, and Figure 4.2-8 provides the layout of the 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 CRDS

The CRDS has been analyzed in detail in a failure mode and effects analysis (Ref. 1). This study, and the analyses presented in Chapter 15.0, demonstrates that the CRDS performs its intended safety function, 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 2 and 3, have demonstrated that acceptable safety criteria would not be exceeded even if the CRDS were rendered incapable of functioning during a reactor transient for which their function would normally be expected.

The design of the control rod drive mechanism is such that failure of the control rod drive mechanism cooling system will, in the worst case, result in an individual control rod trip or a full reactor trip (see Section 7.2).

4.6.3 TESTING AND VERIFICATION OF THE CRDS

The CRDS is extensively tested prior to its operation. These tests may be subdivided into five categories: 1) prototype tests of components, 2) prototype CRDS tests, 3) production tests of components following manufacture and prior to installation, 4) onsite preoperational and initial startup tests, and 5) periodic inservice tests. These tests, which are described in Sections 3.9(N).4.4, 4.2, 14.2, and 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 15.0, 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, feedwater line break, and loss-of-coolant accident. The reactivity control systems for which credit is taken in these accidents are the reactor trip system and the safety injection system (SIS). Additional information on the CRDS is presented in Section 3.9(N).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 15.0. 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 operation of the CVCS has been presumed as an initial condition to evaluate transients, and appropriate Technical Specifications have been prepared to ensure the correct operation or remedial action.

4.6.5 EVALUATION OF COMBINED PERFORMANCE

The evaluations of the steam line break, feedwater line break, and the loss-of-coolant accident, 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 poison.

Nondiverse but redundant types of equipment are utilized only 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 that 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. The failure mode and effects analysis described in this reference verifies that any single failure will not have a deleterious effect on the engineered safety features actuation system. Adequacy of the emergency core cooling system and SIS performance under faulted conditions is verified in Section 6.3.

4.6.6 REFERENCES

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CHAPTER 4.0

REACTOR

4.1 SUMMARY DESCRIPTION

"Core A" has been chosen as the first core for the Callaway Plant, Unit 1. Therefore, the enrichments for Cycle 1 at Callaway are 2.10 (Region 1), 2.60 (Region 2) and 3.10 (Region 3) weight percent. Beginning with Cycle 2, reload fuel incorporates the Westinghouse Optimized Fuel Assembly (OFA) design. The enrichments for Cycle 2 reload fuel at Callaway are 3.40 and 3.80 weight percent.