Table of Contents

List of Tables

List of Figures

Figure 4-95. Typical Reload Core Fuel Loading Arrangement for IFBA Fuel

4.0 Reactor

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4.1 Summary Description

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

The reactor core is comprised of an array of fuel assemblies, which are similar in mechanical design.

The core is cooled and moderated by light water at a pressure of 2250 psia in the Reactor Coolant System. The moderator coolant contains boron as a neutron poison. The concentration of boron in the coolant is varied as required to control relatively slow reactivity changes, including the effects of fuel burnup. Additional boron, in the form of Wet Annular Burnable Absorbers (WABAs), or burnable poison rods, is employed to establish the desired initial reactivity, and shape the radial power distribution. An Integral Fuel Burnable Absorber (IFBA) coating may be used on some of the fuel to establish the desired initial reactivity.

Beginning with McGuire Unit 2 Cycle 14, the reload fuel will be Westinghouse's Robust fuel assembly (RFA) design described in Reference 5. Westinghouse 17x17 Optimized fuel previously used at McGuire and now stored in the spent fuel pools is described in Reference 4. Areva Mark-BW fuel previously used at McGuire and now stored in the spent fuel pools is described in Reference 3.

Two hundred and sixty-four fuel rods are mechanically joined in a 17 x 17 square array to form a fuel assembly. The fuel rods are supported in intervals along their length by grid assemblies which maintain the lateral spacing between the rods throughout the design life of the assembly. The grid assembly consists of an "egg-crate" arrangement of interlocked straps. The straps contain spring fingers and dimples for fuel rod support as well as coolant mixing vanes. The fuel rods consist of natural or slightly enriched uranium dioxide ceramic cylindrical pellets contained in slightly cold worked Zircaloy-4 tubing which is plugged and seal welded at the ends to encapsulate the fuel. All fuel rods are pressurized with helium during fabrication to reduce stresses and strains and to increase fatigue life.

The RFA design used at McGuire consists of the VANTAGE+ fuel assembly base design (Reference 6) with several additional design features as described in Reference 5. The major design features are summarized below:

<u>VANTAGE+</u>
0.374 inch fuel Rod OD
ZIRLO clad fuel rods
ZIRLO guide thimbles, instrumentation tubes, mid structural grids, and intermediate flow mixer grids
Zirconium Diboride Integral Fuel Burnable Absorbers (IFBA)
Mid-enriched annular axial blanket pellets
High burnup fuel skeleton
Debris Filter Bottom Nozzle (DFBN)

<u>Additional Duke/RFA Design Features</u>
ZIRLO/Optimized ZIRLO™ clad fuel rods
Increased guide thimble and instrumentation tube thickness
Pre-oxide coating on the bottom of the fuel rods
Modified Low pressure drop structural mid-grids

Modified intermediate flow mixer grids
Protective bottom end grid
Quick disconnect top nozzle
Fuel rods positioned on the bottom nozzle

For the RFA design, the top and bottom grids and protective bottom grid made of Inconel and the intermediate grids are made of ZIRLO. The grid assemblies consist of an “egg-crate” arrangement of interlocked straps. The straps contain spring fingers and dimples for fuel rod support as well as coolant mixing vanes. The fuel rods contain natural or slightly enriched uranium dioxide ceramic cylindrical pellets and may also include axial blanket pellets (natural or low enriched annular or solid uranium dioxide pellets) and/or Integral Fuel Burnable Absorber (IFBA) coating on some of the enriched fuel pellets. The pellets are contained in ZIRLO tubing, which is plugged and seal welded at the ends to encapsulate the fuel. Beginning in Region 27 (Cycle 25) of McGuire Unit 2, Optimized ZIRLO High Performance Fuel Cladding material will be utilized to contain the fuel pellets. The Optimized ZIRLO cladding material is further described in Reference 8. All fuel rods are pressurized with helium during fabrication to reduce stresses and strains to increase fatigue life.

The center position in the assembly is reserved for the in-core instrumentation, while the remaining 24 positions in the array are equipped with guide thimbles joined to the grids and the top and bottom nozzles. Depending upon the position of the assembly in the core, the guide thimbles are used as core locations for rod cluster control assemblies, wet annular burnable absorbers, or burnable poison rods. Otherwise, the guide thimbles are fitted with plugging devices to limit bypass flow.

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

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

The rod cluster control assemblies each consist of a group of individual absorber rods fastened at the top end to a common hub or spider assembly. The rods in these assemblies contain absorber material to control the reactivity of the core under operating conditions, to control axial power distribution, and to provide adequate shutdown margin.

The control rod drive mechanisms for the full length rod cluster control assemblies are of the magnetic latch type. The latches are controlled by three magnetic coils. They are so designed that upon a loss of power to the coils, the rod cluster control assembly is released and falls by gravity to shutdown the reactor.

The components of the reactor internals are divided into three parts consisting of the lower core support structure (including the entire core barrel and neutron shield pad assembly), the upper core support structure and the in-core instrumentation support structure. The reactor internals support the core, maintain fuel alignment, limit fuel assembly movement, maintain alignment between fuel assemblies and control rod drive mechanisms, direct coolant flow past the fuel elements and to the pressure vessel head, provide gamma and neutron shielding, and provide guides for the in-core instrumentation.

The nuclear design analyses and evaluation establish physical locations for control rods and burnable poison rods and/or rods containing IFBA coated fuel and physical parameters such as fuel enrichments and boron concentration in the coolant. The design analyses are performed to ensure that the reactor core has inherent characteristics which together with corrective actions of the reactor control, protective and emergency cooling systems provide adequate reactivity control even if the highest reactivity worth rod cluster control assembly is stuck in the fully withdrawn position.

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

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

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

The reactor core design together with corrective actions of the reactor control protection and emergency cooling systems can meet the reactor performance and safety criteria specified in Section 4.2.

Table 4-1 presents the principal nuclear, thermal-hydraulic and mechanical design parameters for McGuire Units 1 and 2 operating with Westinghouse 17x17 Robust Fuel Assemblies.

Currently both McGuire units are operating with Westinghouse RFA Assemblies. Areva Mark-BW fuel assemblies and Westinghouse Standard Fuel Assemblies (STD) and Optimized Fuel Assemblies (OFA) are located in the spent fuel pools (Chapter 9).

The analysis techniques employed in the core design are tabulated in Table 4-2.

4.1.1 References

1. Deleted Per 1999 Update.
2. Deleted Per 2000 Update.
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4. Davidson, S. L., Iorii, J. A., "Reference Core Report, 17 x 17 Optimized Fuel Assembly", WCAP-9500-A, May 1982.
5. DPC-NE-2009P-A, Rev. 2, Duke Power Company Westinghouse Fuel Transition Report, SER dated Dec. 18, 2002.
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7. Deleted Per 2006 Update.
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4.2 Mechanical Design

Descriptions of the RFA design and method of evaluation are included in Reference 38. Figure 4-93 shows a full length view of the Westinghouse Robust fuel assembly (Reference 38).

The design bases, design description and design evaluation of the 17 x 17 Westinghouse RFA fuel is documented in Reference 38.

The unit conditions for design are divided into four categories in accordance with their anticipated frequency of occurrence and risk to the public: Condition I - Normal Operation; Condition II - Incidents of Moderate Frequency; Condition III - Infrequent Incidents; Condition IV - Limiting Faults.

Design Criteria

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

1. The mechanical design of the reactor core components and their physical arrangement, together with corrective actions of the reactor control, protection, and emergency cooling systems (when applicable) assure that:
   a. Fuel damage[1] 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 unit cleanup system and are consistent with the unit 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[1] 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.
2. The fuel assemblies are designed to accommodate expected conditions for design, for handling during assembly inspection, and refueling operations and shipping loads.
3. The fuel assemblies are designed to accept control rod insertions in order to provide the required reactivity control for power operations and reactivity shutdown conditions.
4. All fuel assemblies have provisions for the insertion of in-core instrumentation necessary for unit operation.
5. The reactor internals in conjunction with the fuel assemblies direct reactor coolant through the core to achieve acceptable flow distribution and to restrict bypass flow so that the heat transfer performance requirements can be met for all modes of operation. In addition, the internals provide core support and distribute coolant flow to the pressure vessel head so that the temperature differences between the vessel flange and head do not result in leakage from the flange during the Condition I and II modes of operation. Required in-service inspection can be carried out as the internals are removable and provide access to the inside of the pressure vessel.

[1] Fuel Damage as used here is defined as penetration of the fission product barrier (i.e., the fuel rod clad).

4.2.1 Fuel

4.2.1.1 Design Bases

The fuel rod and fuel assembly design bases are established to satisfy the general performance and safety criteria presented in this section.

4.2.1.1.1 Fuel Rods

4.2.1.1.1.1 Cladding

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RFA

1. Material and Mechanical Properties

ZIRLO combines low absorption cross section; high corrosion resistance to coolant, fuel and fission products; high strength and ductility at operating temperature; and high reliability. Reference 3 documents the operating experience with ZIRLO as a clad material, and Reference 44 provides its mechanical properties with due consideration of temperature and irradiation effects.

Optimized ZIRLO enhances corrosion resistance of the ZIRLO cladding material. Reference 56 provides the mechanical properties with due consideration of temperature and irradiation effects.

2. Stress-Strain Limits

Cladding Stress - The cladding stress design basis is the fuel system will not be damaged due to excessive fuel cladding stress (Reference 54). Cladding stress intensities, excluding pellet cladding interaction induced stress, are evaluated using ASME Pressure Vessel Code (Reference 55) guidelines. Stresses are combined to calculate maximum stress intensities which are compared to the criteria, based on the ASME code, given in Reference 54. An alternate methodology for evaluating cladding stress is to calculate the volume average effective stress with the Von Mises equation and show that it is less than the 0.2% offset cladding yield stress (Reference 44 and 57). The volume average effective stress is calculated considering interference due to uniform cylindrical pellet cladding contact caused by thermal expansion, pellet swelling and uniform cladding creep, and pressure differences, with due consideration of temperature and irradiation effects under Condition I and II events.

Cladding 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 pre-transient value. This limit is consistent with proven practice.

3. Vibration and Fatigue

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

Vibration - Potential for fretting wear of the clad surface exists due to flow induce vibrations. This condition is taken into account in the design of the fuel rod support system. The clad wear depth is limited to acceptable values by the grid support dimple and spring design.

4.2.1.1.1.2 Fuel Material

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RFA

1. Thermal-Physical Properties

The thermal-physical properties of UO_2 are described in Reference 18 with due consideration of temperature and irradiation effects.

Fuel Pellet Temperatures - The center temperature of the hottest pellet is below the melting temperature of UO_2. While a limited amount of center melting can be tolerated, the design conservatively precludes center melting. A calculated fuel center temperature of 4700 ºF has been selected as an overpower limit to ensure no fuel melting. This provides sufficient margin for uncertainties as described in Section 4.4.2.9.1

Fuel Pellet Density - The nominal design density of the fuel is 95.5% of theoretical.

2. Fuel Densification and Fission Product Swelling

The design bases and models used for fuel densification and swelling are provided in References 39 and 40.

3. Chemical Properties

Reference 18 provides the basis for justifying that no adverse chemical interactions occur between the fuel and its adjacent material.

4.2.1.1.1.3 Fuel Rod Performance

The detailed fuel rod design establishes such parameters as pellet size and density, cladding-pellet diametral gap, gas plenum size, and helium pre-pressurization 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 sections 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.

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RFA

1. Fuel Rod Models

   The basic fuel rod models and the ability to predict operating characteristics are given in Reference 40 and Section 4.2.1.3.

2. Mechanical Design Limits

Cladding collapse shall be precluded during the fuel rod design life-time. The models described in Reference 14 are used for this evaluation.

The rod internal pressure shall remain below a 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 41).

4.2.1.1.2 Fuel Assembly Structure

1. Structural Design

   a. The structural integrity of the fuel assemblies is assured by setting design limits on stresses and deformations due to various non-operational, 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.

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RFA

The design bases for evaluating the structural integrity of the Robust fuel assemblies are:

a. Nonoperational – 4g loading with dimensional stability.

b. Normal and abnormal loads for Condition I and II – the fuel assembly component structural design criteria are established for the two primary material categories, namely austenitic steels and ZIRLO. The stress categories and strength theory presented in the ASME Boiler and Pressure Vessel Code, Section III, are used as a general guide.

   For austenitic steel structural components, Tresca criterion is used to determine the stress intensities. The design stress intensity value, S_m, is given by the lowest of the following:

- One-third of the specified minimum tensile strength or 2/3 of the specified minimum yield strength at room temperature
- One-third of the tensile strength or 90% of the yield strength at operating temperature, but not to exceed 2/3 of the specified minimum yield strength at room temperature.

The stress intensity limits are given below. All stress nomenclature is per the ASME Boiler and Pressure Vessel Code, Section III.

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

The ZIRLO structural components, which consist of guide thimbles, intermediate spacer grids, and fuel tubes are in turn subdivided into two categories because of material differences and functional requirements. The fuel tube and grid design criteria are covered separately in Section 4.2.1.1.1.1 and 4.2.1.1.2, respectively. The Zircaloy-4 and ZIRLO structural component stresses will be consistent with ASME Code Section III requirements after accounting for thinning due to corrosion (Reference 51). For the guide thimble design, the stress intensities, the design stress intensities, and the stress intensity limits are calculated using the same methods as for the austenitic steel structural components. For conservative purposes, the unirradiated properties of ZIRLO are used.

c. Abnormal loads suring Condition III or IV events – worst cases represented by seismic loads, or blowdown loads during a LOCA event.

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

The fuel assembly structural component stresses under faulted conditions are evaluated using primarily the methods outlined in Appendix F of the ASME Boiler and Pressure Vessel Code, Section III.

For the austenitic steel fuel assembly components, the stress intensity and the design stress intensity value, S_m are defined in accordance with the rules described in the previous section for normal operating conditions. Since the current analytical methods utilize elastic analysis, the stress intensity limits are defined as the smaller values of $2.4S_m$ or $0.70S_u$ for primary membrane and $3.6S_m$ or $1.05S_u$ for primary membrane plus primary bending.

For the ZIRLO components the stress intensities are defined in accordance with the rules described in the previous section for normal operating conditions, and the design stress intensity values, S_m, are set at two-thirds of the material yield strength, S_y, at reactor operating temperature. This results in ZIRLO stress intensity limits being the smaller of $1.6S_y$ or $0.70S_u$ for primary membrane and $2.4S_y$ or $1.05S_u$ for primary membrane plus bending. For conservative purposes, the ZIRLO unirradiated properties are used to define the stress limits.

2. Spacer Grids

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RFA

a) Material Properties and Mechanical Design Limits

Spacer grids made of two different materials, ZIRLO and Inconel-718, are used in the RFA design. The top and bottom grids and protective bottom grid are made of Inconel-718.

Lateral loads resulting from a seismic or LOCA event will not cause unacceptably high plastic grid deformation. Each fuel assembly's geometry will be maintained such that the fuel rods remain in an array amenable to cooling. The behavior of the grids under loading has been studied experimentally.

b) Vibration and Fatigue

The grids provide sufficient fuel rod support to limit fuel rod vibration and maintain cladding fretting wear to within acceptable limits.

3. Thermal-hydraulic Design

This topic is covered in Section 4.4 for the RFA design.

4.2.1.2 Design Description

Each fuel assembly consists of 264 fuel rods, 24 guide thimble tubes and 1 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 poison assembly or a thimble plug assembly, depending on the position of the particular fuel assembly in the core. Figure 4-90 shows a cross-section of the RFA fuel assembly array. The fuel rods are loaded into the fuel assembly structure so that there is clearance between the fuel rod ends and the top nozzles. The fuel assembly fuel rod, and core component design data are given in Table 4-4 for Robust fuel.

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

Visual confirmation of the orientation of the fuel assemblies within the core is provided by an engraved identification number on a corner clamp on the top nozzle, and an indexing hole in the opposite corner clamp.

4.2.1.2.1 Fuel Rods

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RFA

The fuel rods contain enriched uranium dioxide ceramic pellets and may also include axial blanket pellets (natural or low enriched annular or solid uranium dioxide pellets) and/or Integral Fuel Burnable Absorber (IFBA) coating on some of the enriched fuel pellets. The pellets are contained in ZIRLO tubing which is plugged and seal welded at the ends to encapsulate the fuel. Beginning in Region 27 (Cycle 25) of McGuire Unit 2, Optimized ZIRLO High Performance Fuel Cladding material will be utilizied to contain the fuel pellets. The Optimized ZIRLO cladding material is further described in Reference 56. A schematic of the fuel rod is shown in Figure 4-94.

The fuel pellets are right circular cylinders consisting of uranium dioxide powder which is compacted by cold pressing and then sintered to the required density. The ends of each pellet are dished slightly to allow for greater axial expansion at the center of the pellets.

Void volume and clearances are provided within the rods to accommodate fission gases released from the fuel, differential thermal expansion between the cladding and the fuel, and fuel density changes during irradiation, thus avoiding overstressing the cladding or seal welds. Shifting of the fuel within the cladding during handling or shipping prior to core loading is prevented by a stainless steel helical spring, which bears on top of the fuel. Prior to fuel rod loading, the bottom end plug is pressed and welded to the fuel tube.The pellets are then loaded in the fuel tube to the required stack height, the spring is inserted into the top end of the fuel tube, and the top end plug is pressed into the top end of the tube and welded. All of the fuel rods are internally pressurized with helium during the welding process to minimize compressive cladding stresses and prevent cladding flattening due to coolant operating pressures.

4.2.1.2.2 Fuel Assembly Structure

The fuel assembly structure consists of a bottom nozzle, top nozzle, guide and instrument thimbles, and grids, as shown in Figure 4-93 (RFA).

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RFA

Bottom Nozzle

The bottom nozzle serves as a bottom structural element of the fuel assembly and directs the coolant flow distribution to the assembly. The square nozzle is fabricated from Type 304 stainless steel and consists of a perforated plate and four angle legs with bearing plates as shown in Figure 4-93. The legs form a plenum for the inlet coolant flow to the assembly. 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 fuel rods. The plate also prevents accidental downward ejection of the fuel rods from the fuel assembly. The bottom nozzle is reconstitutable and is fastened to the fuel assembly guide thimbles by locked screws which penetrate through the nozzle and mate with a threaded plug in each guide thimble.

The RFA design includes the use of the Debris Filter Bottom Nozzle (DFBN) to reduce the possibility of fuel rod damage due to debris induced fretting. Flow holes are sized to minimize passage of debris large enough to cause damage while providing sufficient flow area, acceptable pressure drop and maintaining structural integrity of the nozzle. The DFBN includes a reinforcing skirt to enhance reliability during postulated adverse handling conditions during refueling. Given a larger open flow area than the perforations in the ECCS sump strainer, the DFBN has no impact on the ability to meet long term core cooling requirements (see Chapter 6). The topics of debris downstream effects, in-vessel effects and fuel blockage criterion are addressed in Section 6.5.2.

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 is controlled by alignment holes in two diagonally opposite bearing plates which mate with locating pins in the lower core plate. Any lateral loads on the fuel assembly are transmitted to the lower core plate through the locating pins.

Top Nozzle

The RFA fuel assembly incorporates a reconstitutable top nozzle (RTN). The RTN attaches to the 24 guide tubes by way of lock tubes. The lock tube captures an insert on the upper end of each guide tube within a circumferential groove located on the inner diameter of the through-hole in the RTN top nozzle which receives the guide tube. In addition to allowing reconstitution of fuel rods and functioning as the upper structural element of the fuel assembly, the RTN also provides a partial protective housing for control components that are positioned within the fuel assembly's guide tubes. The RTN top nozzle consists of an adapter plate, enclosure, top plate, and pads. Holddown springs are mounted on the top plate as shown in Figure 4-93, and are fastened in place by bolts and clamps located at two diagonally opposite corners. On the other two corners integral pads are positioned which contain alignment holes for locating the upper end of the fuel assembly. The top nozzle structure is made of Type 304 stainless steel and the holddown springs are made of Inconel-718. The holddown spring clamp screws are made of Inconel 600 or 718.

The McGuire 2 Cycle 15 is the first full RFA fuel batch to implement an improved top nozzle design referred to as the "Quick Release Top Nozzle" (QRTN) design. The QRTN design was licensed by Westinghouse using the Fuel Criteria Evaluation Process (FCEP) per Reference 38. As required by the Westinghouse FCEP SER (Reference 50), the NRC was notified in Reference 52 of the QRTN design. The QRTN design will be used in the RFA fuel in all future cycles.

The QRTN feature is utilized on the sixteen – (16) outer thimble tubes and is housed within the upper nozzle adapter plate. It consists of a locking ring that is rotated an eighth of a turn to lock and unlock the upper nozzle from the guide tubes of the fuel assembly skeleton. The top nozzle is attached to the guide tubes through axial interference between the lugs on the upper portion of the guide tubes and the locking ring when the locking ring has been rotated to the locked position in the top nozzle. The locking ring is prevented from accidentally unlocking by a spring that seats the locking ring such that rotational interference is encountered. To unlock the quick-disconnect feature, the spring must be compressed before rotating the locking ring.

The square adapter plate (common to both RTN and QRTN) is provided with round penetrations and semi-circular ended slots to permit the flow of coolant upward through the top nozzle. 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 that 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.

If fuel assemblies are damaged or develop leaking fuel rods, the fuel assemblies can be reconstituted in order to replace damaged rods. The typical replacement is a fuel rod that contains pellets of naturally enriched uranium dioxide (UO_2). Aside from enrichment, this rod is similar in design and behavior to a standard fuel rod and is analyzed using standard approved methods. If grid damage exists, solid filler rods made of stainless steel, Zircaloy, or ZIRLO could be used as a replacement. A maximum of 10 such filler rods can be substituted into a single fuel assembly. Fuel assemblies with severe structural damage or with failed fuel pins that cannot be completely removed can be recaged or discharged. A recage operation entails transferring all of the sound fuel rods from the damaged cage to a new fuel assembly cage. This new fuel assembly will function the same as the assembly that it replaces.

As discussed in Reference 38, Duke's NRC-approved reconstitution topical report (Reference 37) will be used to support reconstitution of Robust fuel assemblies with filler rods. The methodology discussed in Reference 37 ensures acceptable nuclear, mechanical, and thermal-hydraulic performance of reconstitutable assemblies.

Guide and Instrument Thimbles

The guide thimbles are structural members that also provide channels for the neutron absorber rods, burnable poison rods, neutron source, or thimble plug assemblies. Each thimble is fabricated from ZIRLO tubing having two different diameters. The 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 is swaged to a smaller diameter to reduce the diametral clearance and produce a dashpot action near the end of the control rod travel during normal trip operation. Holes are provided on the thimble tube above the dashpot to reduce the rod drop time. The dashpot is closed at the bottom by means of an end plug with a small flow port to avoid fluid stagnation in the dashpot during normal operation. A ZIRLO instrument sheath occupies the center lattice position and provides guidance and protection for the incore instrumentation assemblies.

Grid Assemblies

The fuel rods, as shown in Figure 4-93, are supported at intervals along their length by grid assemblies that maintain the lateral spacing between the rods. The grid assembly consists of individual slotted straps interlocked in an "egg-crate" arrangement. Each fuel rod is supported within each grid by the combination of support dimples and springs. The magnitude of the grid restraining force on the fuel rods 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 buckle or distort the fuel rods.

As shown in Figure 4-93, there are twelve grid assemblies in the RFA design. There are six intermediate mixing vane grids made of ZIRLO. This material is chosen for its low neutron absorption and resistance to corrosion. The internal straps include mixing vanes that project into the coolant stream and promote mixing of the coolant.

The RFA design also includes ZIRLO modified intermediate flow mixer grids. As shown in Figure 4-93, modified intermediate flow mixer grids are located in the three uppermost spans between the structural intermediate grids. The modified intermediate flow mixer grids promote mixing, but are not intended to be structural members. Each modified intermediate flow mixer grid cell provides four point fuel rod support. The simplified cell arrangement allows the modified intermediate flow mixer grid to accomplish its flow mixing objective with minimal pressure drop.

The top and bottom grids, made of Inconel-718, do not include mixing vanes. Inconel-718 is used because of its corrosion resistance and high strength. The intersections of the individual straps are joined by brazing.

The RFA design also includes a protective bottom grid (PBG), which is similar to the modified intermediate flow mixer grid, but fabricated of Inconel without mixing vanes. The PBG is positioned directly above the bottom nozzle. The PBG provides added protection against debris induced fretting by trapping debris below the grid where it can wear against the solid end plug. The PBG also provides improved resistance to grid-to-rod fretting by means of additional support at the bottom of the fuel rod.

The spacer grid assemblies are fastened to the guide thimble assemblies to create an integrated structure. Attachment of the Inconel and ZIRLO spacer grids to the thimble tubes is performed using a mechanical 4-lobe bulge process, with the exception of the bottom end grid and bottom protective grid. These two grids are spot-welded to a stainless screw insert that is captured between the guide thimble end plug and the bottom nozzle by means of a stainless steel thimble screw. The mechanical bulge fastening process has been used successfully by Westinghouse since the introduction of zircaloy guide thimbles in 1969.

4.2.1.3 Design Evaluation

The fuel assemblies, fuel rods, and incore control components are designed to satisfy the performance and safety criteria of Section 4.2, the mechanical design bases of Sections 4.2.1.1 and 4.2.3.1, and other interfacing nuclear and thermal-hydraulic design bases specified in Sections 4.3 and 4.4. Effects of Accident Conditions II, III, IV or Anticipated Transients Without Trip on fuel integrity are presented in Chapter 15 or supporting topical reports.

4.2.1.3.1 Fuel Rods

4.2.1.3.1.1 Cladding

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RFA

1. Vibration and Wear

Fuel rod vibrations are flow induced. The effect of the vibration on the fuel assembly and individual fuel rods is minimal. The cyclic stress range associated with deflections of such small magnitude is insignificant and has no effect on the structural integrity of the fuel rod. No significant wear of the cladding or grid supports is expected during the life of the fuel assembly. Fuel vibration has been experimentally investigated.

2. Fuel Rod Internal Pressure and Cladding Stresses

The burnup dependent fission gas release model (Reference 40) is used in determining the internal gas pressures as a function of irradiation time. 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 an increase in the fuel cladding diametral gap or extensive DNB propagation during normal operation.

The cladding 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 cladding inside diameter and positive at the cladding outside diameter and their contribution to the cladding volume average stress is small. Furthermore, the thermal stress decreases with time during steady-state operation due to stress relaxation. The stress due to a 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 the steep temperature gradient.

The internal gas pressure at beginning-of-life is approximately 1400 psia at operating temperature for a typical lead burnup fuel rod. The total tangential stress at the cladding inside diameter at beginning-of-life is approximately 14,400 psi compressive (~13,000 psi due to ΔP and ~1,400 psi due to ΔT) for a low power rod, operating at 5 kw/ft and approximately 12,000 psi compressive (~8,500 psi due to ΔP and ~3,500 psi due to ΔT) for a high power rod operating at 15 kw/ft. However, the volume average effective stress at beginning-of-life is between 8,000 psi (high power rod) and approximately 10,000 psi (low power rod). These stresses are substantially below even the unirradiated cladding strength (~55,000 psi) at a typical cladding mean operating temperature of 700°F.

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

Pellet thermal expansion due to power increases is considered the only mechanism by which significant stresses and strains can be imposed on the cladding. Power increases in commercial reactors can result from fuel shuffling, reactor power escalation following extended reduced power operation, and control rod movement. Cladding stress intensities, excluding pellet cladding interaction induced stress, are evaluated using ASME Pressure Vessel Code (Reference 55) guidelines. Stresses are combined to calculate maximum stress intensities which are compared to the criteria, based on the ASME code, given in Reference 54. An alternate methodology for evaluating cladding stress is to calculate the volume average effective stress with the Von Mises equation and show that it is less than the 0.2% offset cladding yield stress (Reference 44 and 57). The volume average effective stress is calculated considering interference due to uniform cylindrical pellet cladding contact caused by thermal expansion, pellet swelling and uniform cladding creep, and pressure differences, with due consideration of temperature and irradiation effects under Condition I and II events.

Power increases can result in large cladding strains without exceeding the cladding yield stress because of cladding creep and stress relaxation. Based on high strain rate burst and tensile test data on irradiated tubing, 1% strain was determined to be a conservative lower limit for irradiated cladding ductility and thus was adopted as a design criterion (see Section 4.2.1.1.1.1). The intent of this criterion is to minimize the potential for clad failure due to excessive clad straining. This criterion addresses slow strain rate mechanisms where the effective clad stress never reaches the yield strength due to stress relaxation. A spectrum of pin power histories is analyzed to determine allowable changes in local linear heat rate (Δ kw/ft). At various times during the steady-state depletion, the power is increased locally on the rod until 1% clad strain is calculated. For each reload design, the allowable changes in local linear heat rate (Δ kw/ft) as a function of burnup are compared to predicted peaking changes that result from either Condition I or II events.

3. Materials and Chemical Evaluation

ZIRLO cladding has a high corrosion resistance to the coolant, fuel, and fission products. As shown in Reference 3, there is considerable PWR operating experience on the capability of ZIRLO as a cladding material. Optimized ZIRLO enhances corrosion resistance of the ZIRLO cladding material Reference 56 provides the mechanical properties with due consideration of temperature and irradiation effects.

Controls on fuel fabrication specify maximum moisture levels to preclude cladding hydriding.

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

4. Fretting

Cladding fretting has been experimentally investigated. No significant fretting of the cladding is expected during the life of the fuel assembly.

5. 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 concentrations of selected fission products (such as iodine) can chemically attack the ZIRLO tubing and can lead to eventual cladding cracking. Extensive post-irradiation examination has produced no in-pile evidence that this mechanism is operative in commercial fuel.

6. Cycling and Fatigue

A comprehensive review of the available strain-fatigue models was conducted by Westinghouse as a early as 1968. This review included the Langer-O'Donnell model (Reference 9), the Yao-Munse model, and the Manson-Halford model. Upon completion of this review and using the results of the Westinghouse experimental programs discussed below, it was concluded that the approach defined by Langer-O'Donnell would be retained and the empirical factors of their correlation modified in order to conservatively bound the results of the Westinghouse testing program.

The Westinghouse testing program was subdivided into the following subprograms:

a. A rotating bend fatigue experiment on unirradiated Zircaloy-4 specimens at room temperature and at 725°F. Both hydrided and non-hydrided Zircaloy-4 cladding was tested.

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

c. 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 effect of irradiation, of hydrogen level, 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 cladding is subject to 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 unpredictable phenomena as pellet cracking, fragmentation, and relocation. Nevertheless, since 1968, this particular phenomenon has been investigated analytically and experimentally. Strain fatigue tests on irradiated and nonirradiated hydrided ZIRLO 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 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.

Strain fatigue test results for Optimized ZIRLO cladding material demonstrates that the fatigue behavior is within the Westinghouse fatigue design limit.

7. Rod Bowing

Reference 42 presents the NRC-approved model used for evaluation of fuel rod bowing. The effects of row bow on DNBR are described in Section 4.4.2.2.5.

8. Consequences of Power-Coolant Mismatch

This subject is discussed in Chapter 15.0.

9. Irradiation Stability of the Cladding

As shown in Reference 3, there is considerable PWR operating experience on the capability of ZIRLO as a cladding material. Extensive experience with irradiated ZIRLO is summarized in Reference 44.

Reference 56 provides a summary as well as the conditions and limitations associated with the irradiation effects on Optimized ZIRLO cladding material.

10. Creep Collapse and Creepdown

This subject and the associated irradiation stability of cladding have been evaluated using the models described in Reference 14. It has been established that the design basis of no clad collapse during planned core life can be satisfied by limiting fuel densification, and by having a sufficiently high initial rod pressure.

11. Linear Heat Rate to Melt

The fuel cannot exceed the temperature which could cause it to melt. Linear Heat Rate to Melt (LHRTM) limits are used to determine core protection limits which ensure that fuel melting will not occur. A generic LHRTM analysis is performed using the methodology described in Reference 38. The melt temperature of UO_2 used in the PAD fuel performance code is given in

Reference 40. A fuel centerline temperature limit of 4700°F is conservatively used to cover the reduction on melt temperature with burnup and manufacturing and modeling uncertainties.

4.2.1.3.1.2 Fuel Materials Considerations

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1. Dimensional Stability of the Fuel

The mechanical design of the fuel rods accounts for the differential thermal expansion of the fuel and the cladding, and for the densification of the fuel pellets.

2. Potential for Chemical Interaction

Sintered, high density uranium dioxide fuel reacts only slightly with the cladding at core operating temperatures and pressures. In the event of cladding defects, the high resistance of uranium dioxide to attack by water, protects against fuel deterioration, although limited fuel erosion can occur. The effects of water-logging on fuel behavior are discussed in Section 4.2.1.3.1.3, item 4.

3. Thermal Stability

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. Observations from several Westinghouse PWR's (Reference 47) 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 high initial fuel density.

The evaluation of fuel densification effects and their considerations in fuel design are described in References 39 and 40.

4. Irradiation Stability

The treatment of fuel swelling and fission gas release are described in Reference 40.

4.2.1.3.1.3 Fuel Rod Performance

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In calculating the steady-state performance of a nuclear fuel rod, the following factors must be considered:

1. Cladding creep and elastic deflection;
2. Pellet density changes, thermal expansion, gas release, and thermal properties as a function of temperature and burnup; and
3. Internal pressure as a function of fission gas release, rod geometry, and temperature distribution.

These effects are evaluated using a fuel rod design model (Reference 40) which includes appropriate models for time dependent fuel densification. With these interacting factors considered, the model determines the fuel rod performance characteristics for a given rod geometry, power history, and axial power shape. In particular, internal gas pressure, fuel and cladding temperatures, and cladding deflections are calculated. The fuel rod is divided into several axial sections and radially into a number of annualar 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 given in Section 4.2.1.1.1.3.

The gap conductance between the pellet surface and the cladding inner diameter is calculated as a function of the composition, temperature, and pressure of the gas mixture, the gap size, and contact pressure between the cladding and the 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 (Reference 40).The total amount of gas released is based on the generation rate which is in turn a function of burnup. Finally, the gas release is summed over all the zones and the pressure is calculated.

1. Fuel-Cladding Mechanical Interaction

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

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

2. Irradiation Experience

Westinghouse fuel operational experience is presented in Reference 3.

3. Fuel and Cladding Temperature

The methods used for evaluation of fuel rod temperatures are presented in Reference 38

4. Water-logging

Local cladding deformations typical of water-logging[2] bursts have never been observed in commercial Westinghouse fuel. Experience has shown that the small number of rods which have acquired cladding 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; such secondary failures do not result in flow blockage. Hence, the presence of such fuel, the quantity of which must be maintained below technical specification limits, does not in any way exacerbate the effects of any postulated transients.

Zircaloy clad fuel rods which have failed due to water-logging (Reference 31) indicate that very rapid power transients are required for fuel failure. Normal operational transients are limited to about 40 cal/gm-min. (peak rod), while the Spert tests (Reference 32) indicate that 120 cal/gm to 150 cal/gm are required to rupture the cladding even with very short transients (5.5 milli sec period).

5. Potentially Damaging Temperature Effects During Transients

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

The cladding 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 cladding is in contact with the pellet. Clad-pellet interaction is discussed in Section 4.2.1.3.1.3.

Clad flattening, as shown in Reference 14, has been observed in some operating power reactors. Thermal expansion (axial) of the fuel rod stack against a flattened section of cladding could cause failure of the cladding. This is no longer a concern because clad flattening is precluded during the fuel residence in the core (see Section 4.2.1.3.1.1).

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 and fuel assembly grid restraint system so that axial loads imposed on the fuel rods, during a thermal transient, will not result in excessively bowed fuel rods.

6. Fuel Element Burnout and Potential Energy Release

As discussed in Section 4.4.2.2, the core is protected from DNB over the full range of possible operating conditions. In the extremely unlikely event that DNB should occur, the cladding temperature will rise due to the steam blanketing at the rod surface and the consequent

[2]Water-logging damage of a previously defective fuel rod has occasionally been postulated asa 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 cladding defect of appropriate size. Rupture is postulated upon power increase if the rod internal pressure increae is excessive due to insuficient venting of water to the reactor coolant.

degradation in heat transfer. During this time there is a potential for chemical reaction between the cladding and the coolant. However, because of the relatively good film boiling heat transfer following DNB, the energy release resulting from this reaction is insignificant compared to the power produced by the fuel.

7. Coolant Flow Blockage Effects on Fuel Rods

This evaluation is presented in Section 4.4.4.7.

4.2.1.3.2 Fuel Assembly Structure

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RFA

1. 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 a spring/dimple support system. 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. Intermediate Flow Mixers (IFMs) are located in the three uppermost spans between structural grids and provide mid-span flow missing in the hottest fuel assembly spans. Grid testing as described in Reference 46 has been performed for the RFA design to confirm design acceptability.

The fuel assembly component stress levels are limited by the grid design. For example, stresses in the fuel rod due to thermal expansion and ZIRLO irradiation growth are limited by the relative motion of the rod as it slips over the grid spring and dimple surfaces.

2. Loads Applied by Core Restraint System

The upper core plate bears downward against the fuel assembly top nozzle springs. The springs are designed to accommodate the differential thermal expansion and irradiation growth between the fuel assembly and the core internals. Lateral position is maintained through the engagement of the core pins on the top and bottom core plate with “S” holes in the top and bottom nozzles.

3. Analysis of Accident Loads

Grid crush analyses using combined seismic and LOCA loadings show that the fuel assembly will maintain a geometry that is capable of being cooled under the worst-case accident Condition IV event. Minimum grid crush strength has been confirmed through testing.

Load carrying capability of the fuel assembly for both normal service and faulted conditions has been confirmed through analysis and testing.

No interference with control rod insertion into thimble tubes will occur during a Safe Shutdown Earthquake (SSE).

Stresses in the fuel assembly caused by a trip 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 as described in Reference 47 to verify that the structural design criteria are met.

4. Loads Applied in Fuel Handling

The fuel assembly design loads for shipping have been established at 4 g’s. Accelerometers are permanently placed into the shipping cask to monitor and detect fuel assembly accelerations that would exceed the criteria. Past history and experience has indicated that loads which exceed the allowable limits rarely occur. Exceeding the limits requires reinspection of the fuel assembly for damage. Tests on various fuel assembly components such as the grid assembly, sleeves, and structure joints have been performed to assure that the shipping design limits do not result in impairment of fuel assembly function.

4.2.1.3.3 Operational Experience

Westinghouse has had considerable experience with Zircaloy-clad fuel as shown in Reference 3, therefore a significant amount of electricity has been generated with Westinghouse fuel. Burnup levels achieved with Westinghouse supplied fuel is also provided in Reference 3. These burnups were obtained with minimal effect on reactor coolant activity levels. Nuclear reactors now operating with Westinghouse Zircaloy-clad fuel have not experienced availability limitations due to fuel defects. This excellent record is supported by numerous onsite examinations designed to confirm the adequacy of current fuel designs and to develop design improvements. This fuel performance record provides considerable evidence for the continued reliability of Westinghouse fuel assemblies.

4.2.1.4 Tests and Inspections

4.2.1.4.1 Quality Assurance Program

The Quality Assurance Program Manual of the Westinghouse Electric Company (Reference 45), as summarized in Chapter 17, has been developed to serve in planning and monitoring its activities for the design and manufacture of nuclear fuel assemblies and associated components.

The programs provide for control of all activities affecting product quality, commencing with design and development and continuing through procurement, materials handling, fabrication, testing and inspection, and storage. 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 inspection to be performed.

4.2.1.4.2 Quality Control

Quality control philosophy is generally based on the following inspections being performed to a 95% confidence level that at least 95% of the product meets specification, unless otherwise noted.

1. Fuel System Components and Parts

The characteristics which are inspected depends upon the component parts, and generally includes dimensional/visual appearance checks, audits of test reports/material certification, and nondestructive examinations such as X-ray and/or ultrasonic.

All materials used in this fuel are accepted and released by personnel at the vendor facility.

2. 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. Chemical analyses are taken on a specified sample basis throughout pellet production.

3. Rod Inspection

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

a. Leak Testing

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

b. Enclosure Welds

Rod welds are inspected by ultrasonic test and/or X-ray techniques, in accordance with qualified standards, equipment, and personnel.

c. Dimensional

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

d. Plenum Dimensions

All fuel rods are inspected to ensure proper plenum dimensions using methods such as gamma scanning or real-time x-rays.

e. Pellet-to-Pellet Gaps

All fuel rods are inspected to ensure that no significant gaps exist within the pellet stack using methods such as gamma scanning or real-time x-ray.

f. Enrichment Control

All fuel rods are inspected to verify proper enrichment prior to assembly loading using methods such as gamma scanning.

g. Traceability

Fabrication records establish and maintain traceability of fuel rod locations within the bundle to fuel rod IDs and fuel rod IDs to their associated components.

4. Assemblies

Each fuel, rod cluster control (or control rod), wet annular burnable absorber (or burnable absorber), secondary source rod, and thimble tube plugging assembly is inspected for compliance with drawing and specification requirements.

5. Other Inspections

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

a. Gage inspection and control including standardization to primary and/or secondary working standards.

Complete records are kept of calibration and conditions of tools.

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

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

6. Process Control

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

The UO_2 powder is kept in sealed containers with the contents identified by descriptive tagging. An identification tag completely describing the contents is affixed to the containers before transfer to powder storage. Isotopic content is confirmed by analysis

Only authorized personnel can transfer powder from storage to a specific pelleting production line. All pellet production lines are physically separated from each other and pellets of only a single nominal enrichment and density are produced at any given time.

Finished pellets are stored on trays, with enrichments clearly identified. 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 the storage area.

Loading of pellets into the cladding is performed in an isolated production line and only one pellet stack configuration is loaded on a line at a time.

A serialized traceability number is placed on each fuel tube that remains with that tube throughout the fabrication process. This identification number is recorded versus bundle location as fully fabricated fuel rods are loaded into the bundle.

Verification of proper rod loading is performed to ensure rods have been loaded into the correct locations within the bundle. This verification is performed manually by a QA inspector, or automatically by a computerized system as each rod ID is recorded versus its location within the bundle. The top nozzle is inscribed with a permanent identification number providing traceability to the fuel contained in the assembly.

Similar traceability is provided for all insert assemblies (RCCAs, source rod, burnable poison rods, etc.).

4.2.1.4.3 On-Site Inspection

Surveillance of fuel and reactor performance is routinely conducted on Westinghouse reactors. Power distribution is monitored using the excore fixed and incore movable detectors. Coolant activity and chemistry is followed which permits early detection of any fuel clad defects.

Fuel inspections are conducted based upon the results of operational monitoring and previous visual inspections.

Receipt inspections are performed to confirm the mechanical integrity of new fuel assemblies and components after shipment from the supplier facility.

4.2.2 Reactor Vessel Internals

4.2.2.1 Design Bases

The design bases for the mechanical design of the reactor vessel internals components are as follows:

1. The reactor internals in conjunction with the fuel assemblies shall direct reactor coolant through the core to achieve acceptable flow distribution and to restrict bypass flow so that the heat transfer performance requirements are met for all modes of operation. In addition, required cooling for the pressure vessel head shall be provided so that the temperature

differences between the vessel flange and head do not result in leakage from the flange during reactor operation.

2. In addition to neutron shielding provided by the reactor coolant, a separate neutron shield is provided to limit the exposure of the pressure vessel in order to maintain the required ductility of the material for all modes of operation.
3. Provisions shall be made for installing in-core instrumentation useful for the plant operation and vessel material test specimens required for a pressure vessel irradiation surveillance program.
4. The core internals are designed to withstand mechanical loads arising from the SSE and 1/2 SSE and pipe ruptures and meet the requirement of Item 5 below.
5. The reactor shall have mechanical provisions which are sufficient to adequately support the core and internals and to assure that the core is intact with acceptable heat transfer geometry following transients arising from abnormal operating conditions.
6. Following the design basis accident, the plant shall be capable of being shutdown and cooled in an orderly fashion so that fuel cladding temperature is kept within specified limits. This implies that the deformation of certain critical reactor internals must be kept sufficiently small to allow core cooling.
7. The UHI upper internals assembly orginally provided passage for the UHI accumulator water from the vessel head plenum directly to the top of the fuel assemblies during a LOCA. The UHI accumulator has been removed from service by capping the injection piping at the top of the vessel head. The UHI internals were not modified.

Section 3.9 discusses the analyses and testing performed to show that the reactor vessel internals meet the design bases.

4.2.2.2 Description and Drawings

The reactor vessel internals are described as follows:

The components of the reactor internals consist of the lower core support structure (including the entire core barrel and neutron pad assembly), the upper core support and the in-core instrumentation support structure. The reactor internals support the core, maintain fuel alignment, limit fuel assembly movement, maintain alignment between fuel assemblies and control rod drive mechanisms, direct coolant flow past the fuel elements, direct coolant flow to the pressure vessel head, provide gamma and neutron shielding, and guides for the in-core instrumentation. The coolant flows from the vessel inlet nozzles down the annulus between the core barrel and the vessel wall and then into a plenum at the bottom of the vessel. It then reverses and flows up through the core support and through the lower core plate. The lower core plate is sized to provide the desired inlet flow distribution to the core. After passing through the core, the coolant enters the region of the upper support structure and then flows radially to the core barrel outlet nozzles and directly through the vessel outlet nozzles. A small portion of the coolant from the lower plenum flows between the baffle plates and the core barrel to provide additional cooling of the barrel. Similarly, a small amount of the entering flow is directed into the vessel head plenum and exits through the vessel outlet nozzles.

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 the flexure section of the RVCH CRDM compressible thermal sleeve which is fabricated from Nitronic 50 stainless steel. The radial support clevis insert and bolts are fabricated of inconel. The only stainless steel materials used in the reactor core support structures which have yield strengths greater than 90,000 pounds are the 403 series used for holddown springs. The use of these materials is compatible with the reactor coolant and is acceptable based on the 1971 ASME Boiler and Pressure Vessel Code, Case Number 1337.

All reactor internals are removable from the vessel for the purpose of their inspection as well as the inspection of the vessel internal surfaces.

Lower Core Support Structure

The major containment and support member of the reactor internals is the lower core support structure, shown in Figure 4-10. This support structure assembly consists of the core barrel, the core baffle, and the lower core plate and support columns, the neutron shield pads, and the core support which is welded to the core barrel. All major material for this structure is Type 304 stainless steel. The lower core support structure is supported at its upper flange from a ledge in the reactor vessel and its lower end is restrained from transverse motion by a radial support system attached to the vessel wall. Within the core barrel are an axial baffle and a lower core plate, both of which are attached to the core barrel wall and form the enclosure periphery of the core. The lower core support structure and core barrel serve to provide passageways and direct the coolant flow. The lower core plate is positioned at the bottom level of the core below the baffle plates and provides support and orientation for the fuel assemblies. The lower core plate is a member through which the necessary flow distribution holes for each fuel assembly are machined. Fuel assembly locating pins (two for each assembly) are also inserted into this plate. Columns are place between the lower core plate and the core support of the core barrel to provide stiffness and to transmit the core load to the core support. Adequate coolant distribution is obtained through the use of the lower core plate and core support.

The neutron shield pad assembly consists of four pads that are bolted and pinned to the outside of the core barrel. These pads are constructed of Type stainless steel and are approximately 49 inches wide by 148 inches long by 2.8 inches thick. The pads are located azimthally to provide the required degree of vessel protection. Rectangular tubing in which material surveillance samples can be inserted and irradiated during reactor operation are attached to the pads. The samples are held in the rectangular tubing by a preloaded spring device at the top and bottom to prevent sample movement.

Vertically downward loads from weight, fuel assembly preload, control rod dynamic loading, hydraulic loads and earthquake acceleration are carried by the lower core plate into the lower core plate support flange on the core barrel shell and through the lower support columns to the core support and thence through the core barrel shell to the core barrel flange supported by the vessel flange. Transverse loads from earthquake acceleration, coolant cross flow, and vibration are carried by the core barrel shell and distributed between the lower radial support to the vessel wall, and to the vessel flange. Transverse loads of the fuel assemblies are transmitted to the core barrel shell by direct connection of the lower core plate to the barrel wall and by upper core plate alignment pins which are welded into the core barrel.

The radial support system of the core barrel is accomplished by “key” and “keyway” joints to the reactor vessel wall. At six equally spaced points around the circumference, an Inconel clevis block is welded to the vessel inner diameter. Another Inconel block is bolted to each of these blocks, and has a “keyway” geometry. Opposite each of these is a “key” which is welded to the lower core support. At assembly, as the internals are lowered into the vessel, the keys engage the keyways in the axial direction. With this design, the internals are provided with a support at the furthest extremity, and may be viewed as a beam fixed at the top and simply supported at the bottom.

Radial and axial expansions of the core barrel are accommodated, but transverse movement of the core barrel is restricted by this design. With this system, cyclic stresses in the internal structures are within the ASME Section III limits. In the event of an abnormal downward vertical displacement of the internals following a hypothetical failure, energy absorbing devices limit the displacement of the core after contracting the vessel bottom head. The load is then transferred through the energy absorbing devices of the lower internals to the vessel.

The energy absorbers are mounted on a base plate which is contoured on its bottom surface to the reactor vessel bottom internal geometry. Their number and design are determined so as to limit the stresses imposed on all components except the energy absorber to less than yield (ASME Code Section III values). Assuming a downward vertical displacement, the potential energy of the system is absorbed mostly by the strain energy of the energy absorbing devices.

Upper Core Support Assembly

The upper core support structure, shown in Figure 4-11, Figure 4-12, and Figure 4-13 consists of the upper support assembly and the upper core plate between which are contained UHI support columns and guide tube assemblies. The UHI support columns establish the spacing between the top support plate assembly and the upper core plate and are fastened at top and bottom to these plates. The UHI support columns serve to transmit the fuel assembly holddown loads from the upper core plate to the upper support and thence to the vessel flange. They position the upper core plate and upper support which act as the boundaries for the upper plenum at the outlet of the core. Additionally each UHI column has a central axial flow passage providing a flow path between the upper head and the upper plenum for upper head cooling. A support column is provided at each fuel assembly position that does not contain accommodation for a control rod with the exception of the peripheral low power fuel assembly locations. The fuel assemblies which do not have a support column above them are located in front of the inlet and outlet nozzles of the vessel. The UHI support columns also contain thermocouple supports. Figure 4-12 illustrates a typical UHI support column.

The guide tube assemblies, (See Figure 4-11 and Figure 4-14) shield and guide the control rod drive rods and control rods. They are fastened to the upper support and are guided by pins in the upper core plate for proper orientation and support. Additional guidance for the control rod drive rods is provided by the upper guide tube extension which is attached to the upper support. The guide tubes also provide a flow path between the upper head and the upper plenum for upper head cooling.

The upper core support assembly, which is removed as a unit during refueling operation, is positioned in its proper orientation with respect to the lower support structure by slots in the upper core plate which engage flat-sided upper core plate alignment pins which are welded into the core barrel. At an elevation in the core barrel where the upper core plate is positioned, the flat-sided pins are located at angular positions of 90° from each other. As the upper support structure is lowered into the lower internals, the slots in the plate engage the flat-sided pins axial direction. Lateral displacement of the plate and of the upper support assembly is restricted by this design.

Fuel assembly locating pins protrude from the bottom of the upper core plate and engage the fuel assemblies as the upper assembly is lowered into place. Proper alignment of the lower core support structure, the upper core support assembly, the fuel assemblies and control rods are thereby assured by this system of locating pins and guidance arrangement. The upper core support assembly is restrained from any axial movements by a large circumferential spring which rests between the upper barrel flange and the upper core support assembly. The spring is compressed when the reactor vessel head is installed on the pressure vessel.

Vertical loads from weight, earthquake acceleration, hydraulic loads and fuel assembly preload are transmitted through the upper core plate via the support columns to the upper support assembly and then into the reactor vessel head. Transverse loads from coolant cross flow, earthquake acceleration, and possible vibrations are distributed by the support columns to the upper support and upper core plate. The upper support plate is particularly stiff to minimize deflection.

In-Core Instrumentation Support Structures

The in-core instrumentation support structures consist of an upper system to convey and support thermocouples penetrating the vessel through the head and a lower system to convey and support flux thimbles penetrating the vessel through the bottom (Figure 7-27 shows the basic flux mapping system).

The upper system utilizes the reactor vessel head penetrations. Instrumentation port columns are slip-connected to in-line columns that are in turn fastened to the upper support plate. These port columns protrude through the head penetrations. The thermocouples are carried through these port columns and the upper support plate at positions above their readout locations. The thermocouple conduits are supported from the columns of the upper core support system. The thermocouple conduits are 304 stainless steel tubes.

In addition to the upper in-core instrumentation, there are reactor vessel bottom port columns which carry the retractable, cold worked stainless steel flux thimbles that are pushed upward into the reactor core. Conduits extend from the bottom of the reactor vessel down through the concrete about 144 inches and the trailing ends of the thimbles (at the seal line) are extracted approximately 15 feet during refueling of the reactor in order to avoid interference within the core. The thimbles are closed at the leading ends and serve as the pressure barrier between the reactor pressurized water and the Containment atmosphere.

Mechanical seals between the retractable thimbles and conduits are provided at the seal line. During normal operation, the retractable thimbles are stationary and move only during refueling or for maintenance, at which time a space of approximately 15 feet above the seal line is cleared for the retraction operation.

The in-core instrumentation support structure is designed for adequate support of instrumentation during reactor operation and is rugged enough to resist damage of distortion under the conditions imposed by handling during the refueling sequence. These are the only conditions which affect the in-core instrumentation support structure. Reactor vessel surveillance specimen capsules are covered in Section 5.4.3.7. This section covers all the necessary details with regard to irradiation surveillance and includes a cross-section of the reactor showing the capsule identity and location.

4.2.2.3 Design Loading Conditions

The design loading conditions that provide the basis for the design of the reactor internals are:

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 5-3
14. Pump overspeed
15. Seismic loads (operation basis earthquake and design basis earthquake)
16. Blowdown forces injection transients for the cold and hot leg break

Combined seismic and blowdown forces are included in the stress analysis as a design loading condition by assuming the maximum amplitude of each force to act concurrently.

The main objectives of the design analysis are to satisfy allowable stress limits, to assure an adequate design margin, and to establish deformation limits which are concerned primarily with the functioning of the components. The stress limits are established not only to assure that peak stresses will not reach unacceptable values, but also limit the amplitude of the oscillatory stress component in consideration of fatigue characteristics of the materials. Both low and high cycle fatigue stresses are considered when the allowable amplitude of oscillation of established. Dynamic analysis on the reactor internals are provided in Section 3.9.

As part of the evaluation of design loading conditions, extensive testing and inspections are performed from the initial selection of raw materials up to and including component installation and plant operation. Among these tests and inspections are those performed during component fabrication, plant construction, startup and checkout, and during plant operation.

4.2.2.4 Design Loading Categories

The combination of design loadings fit into either the normal, upset or faulted conditions as defined in the ASME Section III Code.

Loads and deflections imposed on components due to shock and vibration are determined analytically and experimentally in both scaled models and operating reactors. The cyclic stresses due to these dynamic loads and deflections are combined with the stresses imposed by loads from component weights, hydraulic forces and thermal gradients for the determination of the total stresses of the internals.

The reactor internals are designed to withstand stresses originating from various operating conditions are summarized in Table 5-3. The scope of the stress analysis problem is very large requiring many different techniques and methods, both static and dynamic. The analysis performed depends on the mode of operation under consideration.

The allowable stress limits during the design basis accident used for the core support structures are based on the January 1971 draft of the ASME Code for Core Support Structures, Section NG, and the Criteria for Faulted Conditions.

4.2.3 Reactivity Control System

4.2.3.1 Design Bases

Bases for temperature, stress on structural members, and material compatibility are imposed on the design of the reactivity control components.

4.2.3.1.1 Design Stresses

The reactivity control system is designed to withstand stresses originating from various operating conditions as summarized in Table 5-3.

Allowable Stresses: For normal operating conditions Section III of the ASME Boiler and Pressure Vessel Code is used. All components are analyzed as Class I components under Article NB-3000.

Dynamic Analysis: The cyclic stress due to dynamic loads and deflections are combined with the stresses imposed by loads from component weights, hydraulic forces and thermal gradients for the determination of the total stresses of the reactivity control system.

4.2.3.1.2 Material Compatibility

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.

4.2.3.1.3 Reactivity Control Components

The reactivity control components are subdivided into two categories:

1. Permanent devices used to control or monitor the core and,
2. Temporary devices used to control or monitor the core.

The permanent type components are the full-length rod cluster control assemblies, neutron source assemblies, and thimble plug assemblies. Although the thimble plug assembly does not directly contribute to the reactivity control of the reactor, it is presented as a reactivity control system component in this document because it is needed to restrict bypass flow through those thimbles not occupied by absorber, source or burnable poison rods.

The temporary component is the Wet Annular Burnable Absorber (WABA) burnable poison assembly, which is used in reload cores to shape the radial power distribution and to maintain a negative moderator temperature coefficient at operating conditions. The design bases for each of the mentioned components are in the following paragraphs.

<u>Absorber Rods Design Bases</u>

The following are considered design conditions under Article NB-3000 of the ASME Boiler and Pressure Vessel Code Section III. The control rod clad, which is cold worked 304 stainless, is the only non-code material used in the control rod assembly. The stress intensity limit S_m for this material is defined at 2/3 of the 0.2% offset yield stress.

1. The external pressure equal to the Reactor Coolant System operating pressure.
2. The 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 caused by accelerations imposed by the control rod drive mechanism.
6. Radiation exposure for maximum core life.

The absorber material temperature shall not exceed its melting temperature (1454°F for Ag-In-Cd and 4400°F for B_4C). (References 13, 17 and 18)

Burnable Poison Rods Design Bases

The burnable poison rod clad is designed as a class I component under Article NB-3000 of the ASME Boiler and Pressure Vessel Code, Section III, 1978 for Conditions I and II. For abnormal loads during Conditions III and IV code stresses are not considered limiting. Failures of the burnable poison rods during these conditions must not interfere with reactor shutdown or emergency cooling of the fuel rods.

The WABA poison material is $A1_2O_3$-B_4C and is non-structural. The structural elements of the burnable poison rod are designed to maintain the absorber geometry even if the poison material is fractured. The rods are designed so that the poison material remains below 1200°F per Reference 43. This is well below any of the WABA component melting temperatures.

The burnable poison rod assemblies satisfy the following:

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. Provide a flow path from the bottom of the UHI to the fuel assemblies during a postulated LOCA.

Neutron Source Rods Design Bases

The neutron source rods are designed to withstand the following:

1. The external pressure equal to the Reactor Coolant System operating pressure and
2. An internal pressure equal to the pressure generated by released gases over the source rod life.

The neutron source rods satisfy the following:

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. Provide a flow path from the bottom of the UHI to the fuel assemblies during a postulated LOCA.

Thimble Plug Assembly Design Bases

The thimble plug assemblies satisfy the following:

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. Be inserted into or withdrawn from the fuel assembly by a force not exceeding 100 pounds.

4. Provide a flow path from the bottom of the UHI to the fuel assemblies during a postulated LOCA.

4.2.3.1.4 Control Rod Drive Mechanisms

Applicable CRDS Design Specification

The mechanisms are Class I components to meet the stress requirements for normal operating conditions of Section III of the ASME Boiler and Pressure Vessel Code. Both static and alternating stress intensities are considered. The stresses originating from the required design transients are included in the analysis. See Section 5.2.1.5 for transient details.

A dynamic seismic analysis is required on the full length control rod drive mechanism when a seismic disturbance has been postulated to confirm the ability of the mechanism to meet ASME Code, Section III allowable stresses and to confirm its ability to trip when subjected to the seismic disturbance.

The control rod drive mechanism (CRDM) design used for the 17 x 17 fuel assembly control rod is identical to the 15 x 15 control rod drive mechanism. The seismic analysis and response of the 17 x 17 control rod drive mechanism will be identical to those of the 15 x 15 mechanism.

Full Length Control Rod Drive Mechanism Operational Requirements

The basis operational requirements for the full length control rod drive mechanisms are:

1. 5/8 inch step,
2. 150 inch travel,
3. 360 pound maximum load,
4. Step in or out at 45 inches/min (72 steps/min),
5. Power interruption shall initiate release of drive rod assembly,
6. Trip delay of less than 150 ms - Free fall drive rod assembly shall begin less than 150 ms after power interruption no matter what holding or stepping action is being executed with any load and coolant temperatures of 100°F to 550°F.
7. 40 year design life with normal refurbishment,
8. 28,000 complete travel excursions which is 13 x 10^6 steps with normal refurbishment.

4.2.3.2 Design Description

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

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

Chemical and Volume control is covered in Section 9.3.4.

The rod cluster control assemblies provide reactivity control for:

1. Shutdown.
2. Reactivity changes due to coolant temperature changes in the power range.
3. Reactivity changes associated with the power coefficient of reactivity.
4. Reactivity changes due to void formation.

Reload cores are designed to contain an adequate amount of excess reactivity at beginning-of-life conditions to achieve a desired fuel cycle length. If soluble boron were the sole means of control, the moderator temperature coefficient would be positive at HFP conditions. It is desirable to have a negative moderator temperature coefficient at HFP conditions throughout the entire cycle in order to reduce possible deleterious effects caused by a positive coefficient during loss of coolant or loss of flow accidents. This is accomplished by installation of burnable poison assemblies.

The neutron source assemblies provide a means of monitoring the core during periods of low neutron activity.

The most effective reactivity control components are the full length rod cluster control assemblies and their corresponding drive rod assemblies which are the only kinetic parts in the reactor. Figure 4-14 identifies the full length rod cluster control and drive rod assembly, in addition to the arrangement of these components in the reactor relative to the interfacing fuel assembly, guide tubes, and control rod drive mechanism. In the following paragraphs, each reactivity control component is described in detail. The guidance system for the full-length control rod cluster is provided by the guide tube as shown in Figure 4-14. The guide tube provides two regimes of guidance. 1) In the lower section a continuous guidance system provides support immediately above the core. This system protects the rod against excessive deformation and wear due to hydraulic loading. 2) The region above the continuous section provides support and guidance at uniformly spaced intervals.

The envelope of support is determined by the pattern of the control rod cluster as shown in Figure 4-15. The guide tube assures alignment and support of the control rods, spider body, and drive rod while maintaining trip times at or below required limits.

4.2.3.2.1 Reactivity Control Components

Full Length Rod Cluster Control Assembly

The full length rod cluster control assemblies are divided into two categories: control and shutdown. The control groups compensates for reactivity changes due to variations in operating conditions of the reactor, i.e., power and temperature variations. Two 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 some of 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 groups provide adequate shutdown margin which is defined as the amount by which the core would be subcritical at hot shutdown if all rod cluster control assemblies are tripped assuming that the highest worth assembly remains fully withdrawn and assuming no changes in xenon or boron concentration. However, with all rod cluster control assemblies verified fully inserted by two independent means, it is not necessary to account for a stuck rod cluster control assembly in the shutdown margin calculation.

A rod cluster control assembly comprises a group of individual neutron absorber rods fastened at the top end to a common spider assembly, as illustrated in Figure 4-15.

The absorber material used for the Unit 1 control rods is a silver-indium-cadmium alloy which is essentially “black” to thermal neutrons and has sufficient additional resonance absorption to significantly increase the worth of the rods. The absorber material is in the form of extruded rods which are sealed in stainless steel tubes to prevent the rods from coming in direct contact with the coolant (Figure 4-16). The absorber materials are sealed in cold worked stainless steel tubes (Figure 4-17) to prevent contact with the coolant. The stainless steel tubing is then sealed at the bottom and the top by welded end plugs. Sufficient diametral and end clearance is provided to accommodate relative thermal expansions and material swelling.

Ag-In-Cd RCCAs (Unit 1)

For Unit 1, the Westinghouse RCCA, Westinghouse Enhanced Performance (EP) RCCA, and AREVA AIC HARMONI RCCAs have all been approved for use.

Ag-In-Cd Westinghouse RCCAs (Unit 1)

The Westinghouse EP RCCA rodlets are fabricated with a flash chrome coating which makes the surface less susceptible to fretting wear. The Ag-In-Cd to clad gap at lower 12 inches of absorber has been increased slightly compared to the nominal gap to offset the effect of swelling. 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 upper plug is threaded for assembly to the spider and has a reduced end section to make the joint more flexible.

The Westinghouse 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. A coil spring pack inside the spider body absorbs the impact energy at the end of a trip insertion. The radial vanes are joined to the hub and the fingers are joined to the vanes by brazing. A centerpost which holds the spring and its 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 type 304 and 308 stainless steel except for the retainer which is of 17-4 PH material and the springs which are Inconel 718 alloy or oil tempered carbon steel where the springs do not contact the coolant.

The spider assembly configuration used on the Westinghouse EP-RCCA is the same as the standard Westinghouse 17x17 RCCA design. There is sufficient clearance between the EP-RCCA and the Westinghouse upper end fitting grillage surface. The rod contains an upper end plug flex joint to accommodate small operating or assembly misalignments.

The Westinghouse absorber rods are fastened securely to the spider to assure trouble free service. The end plug below the pin position is designed with a reduced section to permit flexing of the rods to correct for small operating or assembly misalignments.

Ag-In-Cd AREVA HARMONI RCCAs (Unit 1)

The AREVA ion-nitrided AIC RCCA consists of a group of 24 individual control rods fastened to a common spider assembly. The AREVA AIC RCCA has been designed to increase the life of the RCCA. The absorber materials are encapsulated in cold worked stainless steel tubes. The absorber materials are seal welded at the bottom and top with end plugs to prevent contact with the reactor coolant. The diameter in the lower 12 inches of absorber has been decreased slightly.

The stainless steel cladding is nitrogen treated to harden the surface and make it less susceptible to freeting wear. Unlike the previous Westinghouse EP RCCA which did not have a protective coating for 1 inch of the ends of the cladding, the AREVA RCCA protective coating extends over the lower end plug to just below the very top of the cladding near the upper weld (0.339 inches max). Sufficient diametral and end clearance is provided to accommodate relative thermal expansions and material swelling.

The AREVA RCCA spider assembly is in the form of a cast spider. Since, the spider assembly is a one-piece cast design, the control rods and fingers are not required to be brazed to the spider hub. The RCCA spider is fabricated of 316L stainless steel. A spring is located in the lower part of the hub. The spider springs are fabricated from Inconel X-750. The spring is preloaded and maintained within the hub by the action of a spring retainer and tension bolt. The spring pack is designed to absorb the kinetic energy of the RCCA during an RCCA scram.

The absorber rods are fastened to the spider by lock pins and nuts. The top end of the rodlets are fastened to the spider with a flex joint. The threaded end of the upper end plug is inserted into the bottom of the spider boss hole. The upper end is machined with a reduced diameter shank to provide flexibility to the joint (flex joint). The flex joint provides the ability to accommodate small operating or assembly misalignments.

The absorber end plug is lengthened by 0.8 inches over previous designs to provide solid material in an area that is susceptible to wear in Westinghouse-type 17x17 reactors due to flow induced vibration against the inner diameter of the fuel assembly guide tubes. This provides a sacrificial surface for wear and reduces the neutron fluence exposure of the absorber, thereby improving the life of the RCCA.

BWFC Ionitrided Hybrid RCCAs (Unit 2)

The BWFC B_4C ionitrided hybrid RCCA design control rods are used in Unit 2. The BWFC ionitrided hybrid RCCA consists of a group of 24 individual control rods fastened to a common spider assembly. The absorber materials for the hybrid design are B_4C pellets stacked on top of extruded Ag-In-Cd alloy tip. The absorber materials are encapsulated in cold worked stainless steel tubes (Figure 4-17). The absorber materials are seal welded at the bottom and top with end plugs to prevent contact with the reactor coolant. The diameter in the lower 12 inches of absorber has been decreased slightly. The stainless steel cladding is nitrogen treated to harden the surface and make it less susceptible to fretting wear. Sufficient diametral and end clearance is provided to accommodate relative thermal expansions and material swelling.

The BWFC ionitrided hybrid RCCA spider assembly is in the form of a cast spider. Since, the spider assembly is a one-piece cast design, the control rods and fingers are not required to be brazed to the spider hub. The RCCA spider is fabricated of 316L stainless steel. A spring is located in the lower part of the hub. The spider springs are fabricated from Inconel 718. The spring is preloaded and maintained within the hub by the action of a spring retainer and tension bolt. The spring pack consists of the spider spring, spring retainer, and spring tension bolt. The spring pack is designed to absorb the kinetic energy of the RCCA during an RCCA scram.

The absorber rods are fastened to the spider by lock pins and nuts. The top end of the rodlets are fastened to the spider with a flex joint. The threaded end of the upper end plug is inserted into the bottom of the spider boss hole. The upper end is machined with a reduced diameter shank to provide flexibility to the joint (flex joint). The flex joint provides the ability to accommodate small operating or assembly misalignments.

Burnable Poison Assembly

General Discussion

Unless otherwise noted, the “Burnable Poison Assembly” discussion in Section 4.2.3.3.1 is applicable to the WABA design. Each burnable poison assembly consists of burnable poison rods attached to a hold down assembly.

The WABA holddown assembly provides an attachment for the burnable poison rods and is compressed between the reactor internals upper core plate and fuel assembly top nozzle. The BPRA provides a flow path to the upper head injection (UHI) support column. The holddown assembly is comprised of a holddown spring pack, baseplate assembly, holddown bar, two retainer pins, and a flow cup. All components are fabricated from 304L stainless steel except for the spring pack, which is wound from Inconel 718 wire. The BPRA holddown assembly is compressed by overcoming the spring preload, and forcing the hold down bar weldment down over the sleeve. The loading arms on the hold down bar directly contact the bottom surface of the upper core plate. The BPRA flow cup protrudes through the upper core plate flow hole and interfaces closely with the lower end nozzle of the UHI support column (reactor internals). The flow cup is recessed slightly in the hold down bar for lateral support and for alignment prior to welding. The holddown assembly provides the required holddown force to prevent the WABA from lifting out of the fuel assembly due to hydraulic loads, yet allows for fuel assembly growth due to irradiation. For WABAs, each rod is permanently attached to the base place by a crimp nut.

Wet Annular Burnable Absorbers (WABAs)

The WABA, shown in Figure 4-91, consists of a cluster of burnable poison rods with Zircaloy-4 cladding. All other structural materials in the assembly are Types 304 or 308 stainless steel except for the springs which are Inconel-718. The pellets in the WABA rods are made with 6 mg B10/cm. The WABA BPRA rod design is comprised of annular pellets of aluminum oxide – boron carbide ($A1_2O_3$-B_4C) burnable poison material contained between two concentric Zircaloy tubings. The Zircaloy tubings, which form the inner and outer clad for the BPRA rod are plugged and seal welded at the ends to encapsulate the annular stack of absorber material. Reactor coolant flows inside the inner tubing and outside the outer tubing of the annular rod. A WABA rod is shown in longitudinal and transverse cross-sections in Figure 4-92.

Neutron Source Assembly

The purpose of the neutron source assembly is to provide a base neutron level to insure that the detectors are operational and responding to core multiplication neutrons. Since there is very little neutron activity during loading, refueling, shutdown, and approach to criticality, a neutron source is placed in the reactor to provide a positive neutron count. The source range detectors, provided by either the SR/IR Neutron Flux Monitoring System (NFMS) or Wide Range Neutron Flux Monitoring System (Gamma-metrics), are used primarily when the core is subcritical and during critical testing modes of operations.

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

Both primary and secondary neutron source rods were used in the initial core. The primary source rod, contained a radioactive material spontaneously emits neutrons during initial core loading and reactor startup. The two primary source assemblies were discharged after the first cycle. Both the primary and secondary sources are stored in the spent fuel pit, and remain available for future use if needed.

The secondary source rod, contains a stable material which must be activated by neutron bombardment during reactor operation. The activation results in the buildup of isotopes that later decay by the subsequent release of neutrons. This becomes a source of neutrons during periods of low neutron flux, such as during refueling and the subsequent startups.

Based on core design and core monitoring considerations, a reload core may or may not contain any secondary sources. If secondary source assemblies are used, typically two secondary source assemblies will be loaded into the core, in proximity to the SR/IR detectors. Some cores may contain only one secondary source assembly (for example, if a source assembly is damaged during handling). As many as four secondary source assemblies may be used when activating new secondary source assemblies. Each secondary source assembly contains a grouping of six secondary source rods and eighteen thimble plugs. Core locations not filled with either a source or burnable poison rod contain a thimble plug. Secondary source assemblies are shown in Figure 4-89.

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

The secondary source rods both utilize Type 304 stainless steel. The rods in each assembly are permanently fastened at the top end of a holddown subassembly, which is identical to that of the burnable poison assemblies. This subassembly has a central bore hole and side slots which provide for UHI flow distributed uniformly to the fuel assemblies.

A double encapsulated secondary source rod assembly has replaced the original secondary source rod assembly in McGuire Units 1 and 2. The double encapsulated secondary source design provides additional margin against source leakage. This assembly has essentially the same exterior dimensions as the original secondary source assembly. The antimony-berrilium pellets (stack height 88 inches) are encapsulated in a pressurized 304 stainless steel tube and then inserted in an outer pressurized stainless steel tube as indicated on Figure 4-89.

The other structural members are constructed of type 304 stainless steel except for the springs. The springs exposed to the reactor coolant are wound from an age hardened nickel base alloy for corrosion resistance and high strength. The springs, when contained within the rods where corrosion resistance is not necessary, are oil tempered carbon steel.

Thimble Plug Assembly

Thimble plug assemblies are used to limit bypass flow through the fuel assemblies guide thimbles in fuel assemblies which do not contain either control rods, source rods, or burnable poison rods.

The thimble plug assemblies as shown in Figure 4-23 consist of a flat base plate with short rods suspended from the bottom surface and a spring pack assembly wound around a central spring guide. The twenty-four short rods, called thimble plugs, project into the upper ends of the guide thimbles to reduce the bypass flow area. Similar short rods are also used on the source assemblies and burnable poison assemblies to plug the ends of all vacant fuel assembly guide thimbles. While in service 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 adaptor plate. The spring pack is compressed by the upper core plate when the upper internals assembly is lowered into place. Each thimble plug is permanently attached to base plate by a nut which is locked to the threaded end of the plug by a small lock-bar welded to the nut.

The central spring pack guide has a central bore hole and side slots which provide for UHI flow distributed uniformly to the fuel assemblies.

All components in the thimble plug assembly, except for the springs, are constructed from type 304 stainless steel. The springs are wound from an age hardened nickel base alloy for corrosion resistance and high strength.

4.2.3.2.2 Control Rod Drive Mechanism

All parts of the control rod drive mechanism 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, Inconel-X and cobalt based alloys. Wherever magnetic flux is carried by parts exposed to the main coolant, 400 series stainless steel is used. Cobalt based alloys are used for the pins and latch tips. Inconel-X is used for the springs of both latch assemblies and 304 stainless steel is used for all pressure containing parts. Hard chrome plating provides wear surfaces on the sliding parts and prevents galling between mating parts.

A position indicator assembly slides over the full length control rod drive mechanism rod travel housing. It detects the drive rod assembly position by means of 42 discrete coils that magnetically sense the entry and presence of the rod drive line over the normal length of the drive rod travel.

Full Length Control Rod Drive Mechanism

Control rod drive mechanisms are located on the dome of the reactor vessel. They are coupled to rod control clusters which have absorber material over the entire length of the control rods and derive their name from this feature. The full length control rod drive mechanism is shown in Figure 4-24 and schematically in Figure 4-25.

The primary function of the full length control rod drive mechanism is to insert or withdraw rod control clusters within the core to control average core temperature and to shut down the reactor.

The full length control rod drive mechanism is a magnetically operated jack. A magnetic jack is an arrangement of three electro-magnets which are energized in a controlled sequence by a power cycler to insert or withdraw rod control clusters in the reactor core in discrete steps.

The control rod drive mechanism consists of four separate subassemblies. They are the pressure vessel, coil stack assembly, the latch assembly, and the drive rod assembly.

1. The pressure vessel includes a latch housing, a rod travel housing which are connected by a threaded, seal welded, maintenance joint which facilitates replacement of the latch assembly. The closure at the top of the rod travel housing is a threaded plug with a canopy seal weld for pressure integrity.

The latch housing is the lower portion of the vessel and contains the latch assembly. The rod travel housing is the upper portion of the vessel and provides space for the drive rod during its upward movement as the control rods are withdrawn from the core.

2. The coil stack assembly includes the coil housing, an electrical conduit and connector, and three operating coils; 1) the stationary gripper coil, 2) the moveable gripper coil, and 3) the lift coil.

The coil stack assembly is a separate unit which is installed on the drive mechanism by sliding it over the outside of the latch housing. It rests on the base of the latch housing without mechanical attachment.

Energizing of the operation coils causes movement of the pole pieces and latches in the latch assembly.

3. The latch assembly includes the guide tube, stationary pole pieces, moveable pole pieces, and two sets of latches; 1) the moveable gripper latch, and 2) the stationary gripper latch.

The latches engage grooves in the drive rod assembly. The moveable gripper latches are moved up or down in 5/8 inch steps by the lift pole to raise or lower the drive rod. The stationary gripper latches hold the drive rod assembly while the moveable gripper latches are repositioned for the next 5/8 inch step.

4. The drive rod assembly includes a flexible coupling, a drive rod, a disconnect button, a disconnect rod, and a locking button.

The drive rod has 5/8 inch grooves which receive the latches during holding or moving of the drive rod. The flexible coupling is attached to the drive rod and produces the means for coupling to the rod control cluster assembly.

The disconnect button, disconnect rod, and locking button provide positive locking of the coupling to the rod control cluster assembly and permits remote disconnection of the drive rod.

The control rod drive mechanism is a trip design. Tripping can occur during any part of the power cycler sequencing if power to the coils is interrupted.

The control rod drive mechanism is threaded and seal welded on an adaptor on top of the reactor vessel and is coupled to the rod control cluster assembly directly below.

The mechanism is capable of handling a 360 pound load, including the drive rod weight, at a rate of 45 inches/minute. Withdrawal of the rod control cluster is accomplished by magnetic forces while insertion is by gravity.

The mechanism internals are designed to operate in 650°F reactor coolant. The pressure vessel is designed to contain reactor coolant at 650°F and 2500 psia. The three operating coils are designed to operate at 392°F with forced air cooling required to maintain that temperature.

The full length control rod drive mechanism shown schematically in Figure 4-25 withdraws and inserts its control rod as electrical pulses are received by the operator coils. An ON or OFF sequence, repeated by silicon controlled rectifiers in the power programmer, causes either withdrawal or insertion of the control rod. Position of the control rod is measured by 42 discrete coils mounted on the position indicator assembly surrounding the rod travel housing. Each coiled magnetically senses the entry and presence of the top of the ferro-magnetic drive rod assembly as it moves through the coil center line.

During unit operation the stationary gripper coil of the drive mechanism holds the control rod withdrawn from the core in a static position until the movable gripper coil is energized.

Rod Cluster Control Assembly Withdrawal

The control rod is withdrawn by repetition of the following sequence of events:

1. Movable Gripper Coil (B) - ON

The latch locking plunger raises and swings the movable gripper latches into the drive rod assembly groove. A 1/16 inch axial clearance exists between the latch teeth and the drive rod.

2. Stationary Gripper Coil (A) - OFF

The force of gravity, acting upon the drive rod assembly and attached control rod, causes the stationary gripper latches and plunger to move downward 1/16 inch until the load of the drive rod assembly and attached control rod is transferred to the movable gripper latches. The plunger continues to move downward and swings the stationary gripper latches out of the drive rod assembly groove.

3. Lift Coil (C) - ON

The 5/8 inch gap between the movable gripper pole and the lift pole closes and the drive rod assembly raises one step length (5/8 inch).

4. Stationary Gripper Coil (A) - ON

The plunger raises and closes the gap below the stationary gripper pole. The three links, pinned to the plunger, swing and the stationary gripper latches into a drive rod assembly groove. The latches contact the drive rod assembly and lift it (and the attached control rod) 1/16 inch. The 1/16 inch vertical drive rod assembly movement transfers the drive rod assembly load from the movable gripper latches to the stationary gripper latches.

5. Movable Gripper Coil (B) - OFF

The latch locking plunger separates from the movable gripper pole under the force of a spring and gravity. Three links, pinned to the plunger, swing the three movable gripper latches out of the drive rod assembly groove.

6. Lift Coil (C) - OFF

The gap between the movable gripper pole and lift pole opens. The movable gripper latches drop 5/8 inch to a position adjacent to a drive rod assembly groove.

7. Repeat Step 1

The sequence described above (1 thru 6) is termed as one step or one cycle. The control rod moves 5/8 inch for each step or cycle. The sequence is repeated at a rate of up to 72 steps per minute and the drive rod assembly (which has a 5/8 inch groove pitch) is raised 72 grooves per minute. The control rod is thus withdrawn at a rate up to 45 inches per minute.

Rod Cluster Control Assembly Insertion

The sequence for control rod insertion is similar to that for control rod withdrawal, except the timing of lift coil (C) ON and OFF is changed to permit lowering the control rod.

1. Lift Coil (C) - ON

The 5/8 inch gap between the movable gripper and lift pole closes. The movable gripper latches are raised to a position adjacent to a drive rod assembly groove.

2. Movable Gripper Coil (B) - ON

The latch locking plunger raises and swings the movable gripper latches into a drive rod assembly groove. A 1/16 inch axial clearance exists between the latch teeth and the drive rod assembly.

3. Stationary Gripper Coil (A) - OFF

The force of gravity, acting upon the drive rod assembly and attached control rod, causes the stationary gripper latches and plunger to move downward 1/16 inch until the load of the drive rod assembly and attached control rod is transferred to the movable gripper latches. The plunger continues to move downward and swings the stationary gripper latches out of the drive rod assembly groove.

4. Lift Coil (C) - OFF

The force of gravity separates the movable gripper pole from the lift pole and the drive rod assembly and attached control rod drop down 5/8 inch.

5. Stationary Gripper (A) - ON

The plunger raises and closes the gap below the stationary gripper pole. The three links, pinned to the plunger, swing the three stationary gripper latches into a drive rod assembly groove. The latches contact the drive rod assembly and lift it (and the attached control rod) 1/16 inch. The

1/16 inch vertical drive rod assembly movement transfers the drive rod assembly load from the movable gripper latches to the stationary gripper latches.

6. Movable Gripper Coil (B) - OFF

The latch locking plunger separates from the movable gripper pole under the force of a spring and gravity. Three links, pinned to the plunger, swing the three movable gripper latches out of the drive rod assembly groove.

7. Repeat Step 1

The sequence is repeated, as for control rod withdrawal, up to 72 times per minute which gives a control rod insertion rate of 45 inches per minute.

Holding and Tripping of the Control Rods

During most of the unit operating time, the control rod drive mechanisms hold the control rods withdrawn from the core in a static position. In the holding mode, only one coil, the stationary gripper coil (A), is energized on each mechanism. The drive rod assembly and attached control rod hang suspended from the three latches.

If power to the stationary gripper coil is cut off, the combined weight of the drive rod assembly and the rod cluster control assembly is sufficient to move latches out of the drive rod assembly groove. The control rod falls by gravity into the core. The trip occurs as the magnetic field, holding the stationary gripper plunger half against the stationary gripper pole, collapses and the stationary gripper plunger half is forced down by the weight acting upon the latches. After the drive rod assembly is released by the mechanism, it falls freely until the control rods enter the buffer section of their thimble tubes.

4.2.3.3 Design Evaluation

4.2.3.3.1 Reactivity Control Components

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

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

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

1. Seismic Loads (Safe shutdown earthquake and ½ safe shutdown earthquake).
2. Differential Pressure
3. Differences in thermal expansion
    a. Due to temperature differences
    b. Due to expansion of different materials
4. Control Rod Scram (equivalent static load)
5. All operational transients listed in Table 5-3
6. Interference between components

Other loads that are considered are the spring preloads, coolant flow forces (static), temperature gradients, vibration (mechanically or hydraulically induced), and pump overspeed.

Discussion of Loads

1. Seismic Loads:

The main objective of the analysis is to satisfy allowable stress limits, to assure an adequate design margin, and to establish deformation limits which are concerned primarily with the functioning of the components. The stress limits are established not only to assure that peak stresses will not reach unacceptable values, but also limit the amplitude of the oscillatory stress component in consideration of fatigue characteristics of the materials. Standard methods of strength of materials are used to establish the stresses and deflections of these components for the control rod drive mechanism. These stress limits are established by their manufacturer and translated into allowable bending moments which may result from a seismic disturbance. Verification is then provided that earthquake induced bending moments are below the maximum tolerable umbrella described above. The analytical procedure used for this verification is a normal mode/seismic response spectrum linear analysis of a lumped parameter finite element model of the CRDM system. A typical comparison for the system is given in Figure 4-28. The dynamic behavior of the reactivity control components has been studied using experience from operating reactors.

2. Differential Pressure:

The design of reactivity component rods (discussed below) provides a sufficient cold void volume within the burnable poison, source rods, and the B_4C hybrid absorber rods (Unit 2) to limit the internal pressures to a value which satisfies the criteria in Section 4.2.3.1.

- WABAs: The void volume for the helium in the WABAs ($A1_2O_3$-B_4C) is obtained through the use of an annular plenum within the rod.
- Unit 1 RCCA Absorber (Ag-In-Cd): Helium gas is not released by the Ag-In-Cd neutron absorber rod material thus the absorber rod only sustains an external pressure during operating conditions.
- Unit 2 RCCA B_4C Hybrid Absorber: A gas plenum at the top of the B_4C hybrid absorber provides void volume for the gas accumulation. The internal pressure of B_4C hybrid absorber rods continues to increase from ambient until end of life at which time the internal pressure never exceeds that allowed by the criteria in Section 4.2.3.1.
- Source Rod: A gas plenum at the top of the source rod provides void volume for the gas accumulation. The internal pressure of source rods continues to increase from ambient until end of life at which time the internal pressure never exceeds that allowed by the criteria in Section 4.2.3.1.

The stress analysis of the burnable poison and source rods either conservatively calculated gas release or assumed 100% gas release to the rod void volume, considers the initial pressure within the rod, and assumes the pressure external to the component rod is zero. For the B_4C hybrid absorber rod, either a conservative 30% He gas release is assumed or specific calculations are used for satisfying cladding design bases during ANS Conditions I and II (See Reference 17).

3. Differential Thermal Expansion:

Sufficient diametral and end clearances have been provided in the neutron absorber, burnable poison, and source rods to accommodate the relative thermal expansions between the enclosed material and the surrounding clad and end plugs. There is no bending or warping induced in the rods although the clearance offered by the guide thimble would permit a postulated warpage to occur without restraint on the rods. Bending, therefore, is not considered in the analysis of the rods. The radial and axial temperature profiles have been determined by considering gap conductance, thermal expansion, and neutron and/or gamma heating of the contained material as well as gamma heating of the clad. The maximum neutron absorber material temperature was found to be less then 850°F which occurs axially at only the highest flux region. Rod, guide thimble, and dashpot flow analysis performed indicates that the flow is sufficient to prevent coolant boiling and maintain clad temperatures at which the clad material has adequate strength to resist coolant operating pressure and rod internal pressures.

4. Control Rod Scrams:

Analysis on the full length rod cluster control spider indicates the spider is structurally adequate to withstand the various operating loads including the higher loads which occur during the drive mechanism stepping action and rod drop. The spider capability has been studied using experience from operating reactors.

a. Structural Materials

With regard to the material of construction exhibiting satisfactory resistance to adverse property changes in a radioactive environment, it should be noted that work on breeder reactors in current design utilize similar materials. At high fluences the austenitic materials increase in strength with a corresponding decreased ductility (as measured by tensile tests) but energy absorption (as measured by impact tests) remain quite high. Corrosion of the materials exposed to the coolant is quite low and proper control of Cl and O_2 in the coolant will prevent the occurrence of stress corrosion. All of the austenitic stainless steel base materials used are processed and fabricated to preclude sensitization. Although the control rod spiders are fabricated by furnace brazing, the procedure used requires that the pieces be rapidly cooled so that the time-at-temperature is minimized. The time that is spent by the control rod spiders in the sensitization range, 800°F - 1500°F, is controlled typically not exceeding 20 minutes during fabrication to preclude sensitization. The 17-4 PH parts are all aged at the highest standard aging temperature of 1100°F to avoid stress corrosion problems exhibited by aging at lower temperatures.

The BWFC hybrid ionitrided RCCAs (for Unit 2) spider assembly is in the form of a cast spider. Therefore, the control rods and fingers do not require brazing (See Section 4.2.3.2.1).

5. Operational Transients:

Ejection of the thimble plug assembly is conceivable based on the postulation that the hold down bar fails and the base plate and the burnable poison rods are severely deformed. In the case of the thimble plug assembly, the thimble plugs will partially remain in the fuel assembly guide thimbles thus maintaining a majority of the desired flow impedance. Further displacement or complete ejection would necessitate the square base plate and burnable poison rods be forced, thus plastically deformed, to fit up through a small diameter hole. It is expected that this condition requires a substantially higher force or pressure drop than that of the hold down bar failure.

Experience with control rods, burnable poison rods, and source rods is discussed in Reference 3.

The mechanical design of the reactivity control components provides for the protection of the active elements to prevent the loss of control capability and functional failure of critical components. The components have been reviewed for potential failure and consequences of a functional failure of critical parts. The following failure modes were identified:

<u>Full Length Rod Cluster Control Assembly</u>

1. The basic absorbing material is sealed from contact with the primary coolant and the fuel assembly and guidance surfaces by a high quality stainless steel clad. Potential loss of

absorber mass or reduction in reactivity control material due to mechanical or chemical erosion or wear is therefore reliably prevented.

2. If a cladding breach occurred, for Unit 1, the absorber material silver-indium-cadium 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. For Unit 2, it is conservatively assumed that B_4C pellet disintegration could occur within one year after a postulated cladding breach. Postulated B_4C washout through cladding breaches of a few isolated hybrid absorber rods would not cause a violation of their design bases (see Section 3 of Reference 17).

3. The individually clad absorber rods are doubly secured to the retaining spider vane by a threaded joint and a welded lock pin. No failure of this joint has ever been experienced in functional testing or in years of actual service in operating plants such as San Onofre, Connecticut Yankee, Zorita, Beznau No. 1, Robert Emmett Ginna, etc.

It should also be noted that in several instances of control rod jamming caused by foreign particles, the individual rods at the site of the jam have borne the full capacity of the control rod drive mechanism and higher impact loads to dislodge the jam without failure. The guide tube card/thimble arrangement is such that large loads are required to buckle individual control rods. The conclusion to be drawn from this experience is that this joint is extremely insensitive to potential mechanical damage. A failure of the joint would result in the insertion of the individual rod into the core. This results in reduced reactivity which is a fail safe condition.

4. The spider finger braze joint (for Westinghouse RCCAs) by which the individual rods are fastened to the vanes has also experienced the service described above and been subjected to the same jam freeing procedures also without failure. A failure of this joint would also result in insertion of the individual rod into the core.

5. The radial vanes are attached to the spider body, again by a brazed joint in the Westinghouse RCCA design (for Unit 1). The joints are designed to a theoretical strength in excess of that of the components joined.

The BWFC hybrid ionitrided RCCAs (for Unit 2) spider assembly is in the form of a cast spider. Therefore, the control rods and fingers do not require brazing (See Section 4.2.3.2.1).

It is a feature of the design that the guidance of the rod cluster control is accomplished by the inner fingers of these vanes. They are therefore the most susceptible to mechanical damage. Since these vanes carry two rods, failure of the vane-to-hub joint such as the isolated incidents at Connecticut-Yankee does not prevent the free insertion of the rod pair (Reference 3). Neither does such a failure interfere with the continuous free operation of the drive line, also as experienced at Connecticut-Yankee (Reference 3).

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

This possibility is considered extremely remote because the single rod vanes are subjected to only vertical loads and very light lateral reactions from the rods. The lateral loads are light even during a seismic event because the guide tube/guide thimble arrangement allows very limited lateral motion. The consequences of such a failure are not considered critical since only one drive line of the reactivity control system would be involved. This condition is readily observed and can be cleared at shutdown. Furthermore, this has never occurred.

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

7. The rod cluster control rods are provided a clear channel for insertion by the guide thimbles of the fuel assemblies. All fuel rod failures are protected by the guide thimbles providing a physical barrier between the fuel rod and the intended insertion channel. Distortion of the fuel rods by bending cannot apply sufficient force to damage or significantly distort the guide thimble. Fuel rod distortion by swelling, though precluded by design, would be terminated by fracture before contact with the guide thimble occurs. If such were not the case, it would be expected that a force reaction at the point of contact would cause a slight deflection of the guide thimble. The radius of curvature of the deflected shape of the guide thimbles would be sufficiently large to have a negligible influence on rod cluster insertion.

8. Analysis of the full length rod cluster control assemblies show that if the drive mechanism housing ruptures the rod cluster control assembly will be ejected from the core by the pressure differential of the operating pressure and ambient pressure across the drive rod assembly. The ejection is also predicted on the failure of the drive mechanism to retain the drive rod/rod cluster control assembly position. It should be pointed out that a drive mechanism housing rupture will cause the ejection of only one rod cluster control assembly with the other assemblies remaining in the core. Analysis also showed that a pressure drop in excess of 4000 psi must occur across a two-fingered vane to break the vane/spider body joint causing ejection of two neutron absorber rods from the core. Since the greatest pressure of the primary system coolant is only 2250 psi, a pressure drop in excess of 4000 psi could not be expected to occur. Thus, the ejection of the neutron absorber rods is not possible.

Burnable Poison Assemblies

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

The individual rods are shouldered against the underside of the retainer plate and securely fastened at the top by a crimp nut for WABAs. The square dimension of the retainer plate is larger than the diameter of the flow holes through the core plate. Failure of the hold down bar or spring pack therefore does not result in ejection of the burnable poison rods from the core.

The only incident that could potentially result in ejection of the burnable poison rods is a multiple fracture of the retainer plate. This is not considered credible because of the light loads borne by this component. During normal operation the loads borne by the plate are approximately 5 lb/rod or a total of 100 lb. distributed at the points of attachment. Even a multiple fracture of the retainer plate would result in jamming of the plate segments against the upper core plate, again preventing ejection. Excessive reactivity increase due to burnable poison ejection is therefore prevented.

Zircaloy is used to maintain the integrity of the aluminum oxide-boron carbide burnable poison rods. In this application there is even less susceptibility to mechanical damage since these are static assemblies. The guide thimbles of the fuel assembly afford the same protection from damage due to fuel rod failures as that described for the rod cluster control rods.

The consequences of clad breach are not small for alumina boron carbide burnable poison rods. Should the zircaloy clad of a Al_2O_3-B_4C burnable poison rod fail the B_4C could be leached-out by the coolant water. If this occurred early in the cycle, in-core instruments could detect large peaking factors, and corrective action would be undertaken if warranted. A postulated clad breach after substantial irradiation would have no significant effect on peaking factors since most of the B10 will have been burned-out per Reference 43.

Drive Rod Assemblies

All postulated failures of the drive rod assemblies either by fracture or uncoupling lead to the fail safe condition. If the drive rod assembly fractures at any elevation, that portion remaining coupled falls with, and is guided by the rod cluster control assembly. This always results in reactivity decrease for full length control rods.

6. Interference Between Components

Irradiation Stability & Chemical Compatibility

The irradiation stability of the absorber/poison material is discussed in References 17 and 18. Irradiaiton produces no deleterious effects in the absorber/poison material. Helium gas is not released by Ag-In-Cd neutron absorber rod material, thus the absorber rod only sustains an external pressure during operating conditions. Thus, gas release is not a concern for Ag-In-Cd absorber rod design. Sufficient diametral and end clearances are provided to accommodate expected gas release from the Hybrid B_4C (Unit 2) and swelling of the absorber material.

The materials selected are considered to be the best available from the standpoint of resistance to irradiation damage and compatibility to the reactor environment. The materials selected partially dictate the reactor environment (e.g., chlorine control in the coolant). The current design type reactivity controls have experienced no apparent degradation of construction materials.

4.2.3.3.2 Control Rod Drive Mechanism

Material Selection

All pressure-containing materials comply with Section III of the ASME pressure vessel code, and with the exception of the needle vent valve, will be fabricated from austenitic (304) stainless steel of CF-8 stainless steel. The vent valve is a modified austenitic stainless steel cap screw.

1. Latch Assembly

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

At the start of the development program, a survey was made to determine whether a material better than 410 stainless steel was available for the magnetic pole pieces. Ideal material requirements are as follows:

1. High magnetic saturation value
2. High permeability

3. Low coercive force
4. High resistivity
5. High curie temperature
6. Corrosion resistant
7. High impact strength
8. Non-oriented
9. High machinability
10. Resistance to radiation damage.

After a comprehensive material trade-off study was made it was decided that the 410 stainless steel was satisfactory for this application.

2. Coil Stack Assembly

The cast 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. The finished housings are zinc plate to provide corrosion resistance.

Coils are wound on bobbins of molded Dow Corning 302 material, with double glass-insulated copper wire. Coils are then vacuum impregnated with silicon varnish. A wrapping of mica sheet is secured to the coil outer surface. The result is a well-insulated coil capable of sustained operation at 200 degrees centigrade.

3. Drive Shaft Assembly

The drive shaft assembly utilizes a 410 stainless steel drive rod. The coupling is machined from 403 stainless steel. Other parts are 304 stainless steel with the exception of the springs which are Inconel-X and the locking button which is Haynes 25.

<u>Radiation Damage</u>

As required by the equipment specification, the control rod drive mechanisms are designed to meet a radiation requirement of 10 RADS/HR. Materials have been selected to meet this requirement. The above radiation level which amounts to 1.753×10^6 RADS in twenty years will not limit control rod drive mechanism life. Control rod drive mechanisms at Yankee Rowe which have been in operation since 1960 have not experienced problems due to radiation.

<u>Positioning Requirements</u>

The mechanism has a step length of 5/8 inches which determines the positioning capabilities of the control rod drive mechanism. (Note: Positioning requirements are determined by reactor physics and control rod wear.)

<u>Evaluation of Materials Adequacy</u>

The ability of the pressure housing components to perform throughout the design lifetime as defined in the equipment specification is confirmed by the stress analysis report required by the ASME Boiler and Pressure Vessel Code, Section III. Internal components subjected to wear will withstand a minimum of 3,000,000 steps without refurbishment as confirmed by life tests.

<u>Results of Dimensional and Tolerance Analysis</u>

With respect to the control rod drive mechanism systems as a whole, critical clearances are present in the following areas:

1. Latch assembly (Diametral clearances)
2. Latch arm-drive rod clearances
3. Coil stack assembly-thermal clearances
4. Coil fit in coil housing.

The following write-up defines clearances that are designed to provide reliable operation in the control rod drive mechanism in these four critical areas. These clearances have been proven by life tests and actual field performance at operating plants.

1. Latch Assembly - Thermal Clearances

The magnetic jack has several clearances where parts made of 410 stainless steel fit over parts made from 304 stainless steel. Differential thermal expansion is therefore important. Minimum clearances of these parts at 68°F is 0.011 inches. At the maximum design temperature of 650°F minimum clearance is 0.0045 inches and at the maximum expected operation temperatures of 550°F is 0.0057 inches.

2. Latch Arm - Drive Rod Clearances

The control rod drive mechanism incorporates a load transfer action. The movable or stationary gripper latch is not under load during engagement, as previously explained, due to load transfer action. Figure 4-26 shows latch clearance variation with the drive rod as a result or minimum and maximum temperatures. Figure 4-27 shows clearance variations over the design temperature range.

3. Coil Stack Assembly - Thermal Clearances

The assembly clearance of the coil stack assembly over the latch housing was selected so that the assembly could be removed under all anticipated conditions of thermal expansion.

At 70°F the inside diameter of the coil stack is 7.308/7.298 inches. The outside diameter of the latch housing is 7.260/7.270 inches. Thermal expansion of the mechanism due to operating temperature of the control rod drive mechanism results in minimum inside diameter of the coil stack being 7.310 inches at 222°F and the maximum latch housing diameter being 7.302 inches at 532°F.

Under extreme tolerance conditions listed above it is necessary to allow time for a 70°F coil housing to heat during a replacement operation.

Four coil stack assemblies were removed from four hot control rod drive mechanisms mounted on 11.035 inch centers on a 550°F test loop, allowed to cool, and then replaced without incident as a test to prove the preceding.

4. Coil Fit in Coil Housing

Control rod drive mechanism and coil housing clearances are selected so that coil heat up results in a close to tight fit. This is done to facilitate thermal transfer and coil cooling in a hot control rod drive mechanism.

4.2.3.4 Tests, Verification and Inspections

4.2.3.4.1 Reactivity Control Components

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

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

1. All materials are procured to specifications to attain the desired standard of quality.
2. All clad/end plug welds are checked for integrity by visual inspection, X-ray, and are helium leak checked. All the seal welds in the neutron absorber rods, burnable poison rods and source rods are checked in this manner.

To assure proper fitup with the fuel assembly, the rod cluster control, burnable poison and source assemblies are installed in the fuel assembly to demonstrate there is no excessive restriction or binding. Also, other applicable dimensional requirements described in Section 4.2.1.4.2 help ensure proper and unrestricted control rod and component operation.

Finally, the rod cluster control assemblies are functionally tested per Improved Technical Specifications following core loading, but prior to criticality, to demonstrate reliable operation of the assemblies. Additionally, following refueling, but prior to power operation, control rod worth measurements are performed on the control and shutdown banks. Thus, any anomalies would be detected by this surveillance.

If a rod cluster control assembly 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.

Historical Information shown in Italics below:

The full length rod cluster control assemblies are functionally tested, following core loading but prior to criticality to demonstrate reliable operation of the assemblies. Each assembly is operated (and tripped) one time at no flow/cold conditions and one time at full flow/hot conditions. In addition, selected assemblies, amounting to about 15 to 20 percent of the total assemblies are operated at no-flow/operating temperature conditions and full flow/ambient conditions. Also the slowest rod and the fastest rod are tripped 10 times at no-flow/ambient conditions and at full flow/operating temperature conditions. Thus each assembly is tested a minimum of 2 times or up to 14 times maximum to insure that the assemblies are properly functioning.

4.2.3.4.2 Control Rod Drive Mechanisms

Quality assurance procedures during production of control rod drive mechanisms include material selection, process control, mechanism component test during production and hydrotests.

After all manufacturing procedures had been developed, several prototype control rod drive mechanisms and drive rod assemblies were life tested with the entire drive line under environmental conditions of temperature, pressure and flow. All acceptance tests were of duration equal to or greater than service required for the plant operation. All drive rod assemblies tested in this manner have shown minimal wear damage.

These tests include verification that the trip time achieved by the full length control rod drive mechanism meet the design requirement of 2.2 seconds from start of rod cluster control assembly motion to dashpot entry. This trip time requirement will be confirmed for each control rod drive mechanism prior to initial reactor operation and at periodic intervals after initial reactor operation. In addition, a Technical Specification has been set to ensure that the trip time requirement is met.

It is expected that all control rod drive mechanisms will meet specified operating requirements for the duration of unit life with normal refurbishment. However, a Technical Specification pertaining to an inoperable rod cluster control assembly has been set.

If a rod cluster control assembly 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.

In order to demonstrate continuous free movement of the full length rod cluster control assemblies and to ensure acceptable core power distributions during operation, partial movement checks are performed on every full length rod cluster control assembly periodically during reactor critical operation. In addition, periodic drop tests of the full length rod cluster control assemblies are performed at each refueling shutdown to demonstrate continued ability to meet trip time requirements, to ensure core subcriticality after reactor trip, and to limit potential reactivity insertions from a hypothetical rod cluster control assembly ejection. During these tests the acceptable drop time of each assembly is not greater than 2.2 seconds, at full flow and operating temperature, from the beginning of motion to dashpot entry.

To confirm the mechanical adequacy of the fuel assembly and full length rod cluster control assembly, functional test programs have been conducted on a full scale control rod. The prototype assembly was tested under simulated conditions of reactor temperature, pressure, and flow for approximately 1000 hours. The prototype mechanism accumulated about 3,000,000 steps and 600 trips. At the end of the test the control rod drive mechanism was still operating satisfactorily.

Actual experience on the Ginna, Mihama No. 1, Point Beach No. 1 and H. B. Robinson plants indicates excellent performance of control rod drive mechanisms.

All units are production tested prior to shipment to confirm the ability of the control rod drive mechanisms to meet design specification-operational requirements. Periodic tests are also conducted to confirm brake operation.

During refueling, tests are also conducted to confirm condition of CRDM coils.

4.2.3.5 Instrumentation Applications

Instrumentation for determining reactor coolant average temperature (T_{avg}) is provided to create demand signals for moving groups of full length rod cluster control assemblies to provide load follow (determined as a function of turbine inlet pressure) during normal operation and to counteract operational transients. The hot and cold leg resistance temperature detectors (RTD's) in the reactor coolant bypass loops are described in Section 7.2. The location of the RTD's in each loop is shown on the flow diagrams in Chapter 5. The Reactor Control System which controls the reactor coolant average temperature by regulation of control rod bank position is described in Section 7.3.

Rod position indication instrumentation is provided to sense the actual position of each control rod so that the actual position of the individual rod may be displayed to the operator. Signals are also supplied by this system as input to the rod deviation comparator. The rod position indication system is described in Chapter 7.

The reactor makeup control system whose functions are to permit adjustment of the reactor coolant boron concentration for reactivity control (as well as to maintain the desired operating fluid inventory in the volume control tank), consists of a group of instruments arranged to provide a manually preselected makeup composition that is borated or diluted as required to the charging pump suction header or the volume control tank. This system, as well as other systems including boron sampling provisions that are part of the Chemical and Volume Control System, are described in Section 9.3.

When the reactor is critical, the normal indication of reactivity status in the core is the position of the control bank in relation to reactor power (as indicated by the Reactor Coolant system loop T) and coolant average temperature. These parameters are used to calculate insertion limits for the control banks to giving warning to the operator of excessive rod insertion. Monitoring of the neutron flux for various phases of reactor power operation as well as of core loading, shutdown, startup, and refueling is by means of the Nuclear instrumentation System. The monitoring functions and readout and indication characteristics for the following means of monitoring reactivity are included in the discussion on safety related display instrumentation in Chapter 7 “Instrumentation and Controls”:

1. Nuclear Instrumentation System
2. Temperature Indicators
    a. T average (Measured)
    b. ΔT (Measured)
    c. Auctioneered T average
    d. T reference
3. Demand Position of Rod Cluster Control Assembly Group
4. Actual Rod Position Indicator.

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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) in 10CFR50, Appendix A. Where appropriate, 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:

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

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

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

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

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

4.3.1.1 Fuel Burnup

Basis

The fuel rod design basis is described in Section 4.2. The nuclear design basis is to install sufficient reactivity in the fuel to attain a region discharge burnup of approximately 50,000 MWD/MTU. The above along with the design basis in Section 4.3.1.3, satisfies GDC-10.

Discussion

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

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

Initial excess reactivity installed in the fuel, although not a design basis, must be sufficient to maintain core criticality at full power operating conditions throughout cycle life with equilibrium xenon, samarium, and other fission products present. The end of design cycle life is defined to occur when the chemical shim concentration is essentially zero with control rods present to the degree necessary for operational requirements (e.g., the controlling bank at the "bite" position). In terms of chemical shim boron concentration this represents approximately 5 ppm with no control rod insertion. The design cycle life can be extended past the minimum boron conditions by a Tave, combination Tave/power or power level coastdown. The moderator temperature reduction and/or power reduction provides the positive reactivity necessary to extend power operation capability.

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

4.3.1.2 Negative Reactivity Feedbacks (Reactivity Coefficient)

Basis

The fuel temperature coefficient will be negative and the moderator temperature coefficient of reactivity will be non-positive for full power operating conditions, thereby providing negative reactivity feedback characteristics. The design basis meets GDC-11.

Discussion

When compensation for a rapid increase in reactivity is considered, there are two major effects. These are the resonance absorption effects (Doppler) associated with changing fuel temperature and the spectrum effect resulting from changing moderator density. These basic physics characteristics are often identified by reactivity coefficients. The use of slightly enriched uranium ensures that the Doppler coefficient of reactivity is negative. This coefficient provides the most rapid reactivity compensation. The core is also designed to have an overall negative moderator temperature coefficient so that average coolant temperature or void content provides another, slower compensatory effect. Nominal power operation is permitted only in a range of overall negative moderator temperature coefficient. The negative moderator temperature coefficient can be achieved through use of fixed burnable poison and/or control rods by limiting the reactivity held down by soluble boron.

The maximum burnable poison content (quantity and distribution) is not stated as a design basis other than as it relates to accomplishment of a non-positive moderator temperature coefficient at power operating conditions discussed above.

4.3.1.3 Control of Power Distribution

Basis

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

1. The fuel will not be operated at greater than the LOCA linear heat rate criteria during normal operation by establishing the appropriate core power distribution (Fq) limit in the Core Operational Limits Report (COLR).
2. Under abnormal conditions including the maximum overpower condition, the fuel peak power will not cause melting as defined in Section 4.4.1.2.
3. The fuel will not operate with a power distribution that violates the departure from nucleate boiling (DNB) design basis (i.e., the DNBR shall not be less than the limiting value as discussed in Section 4.4.1) under Condition I and II events including the maximum overpower condition.
4. Fuel management will be such as to produce rod powers and burnups consistent with the assumptions in the fuel rod mechanical integrity analysis of Section 4.2.

The above basis meets GDC-10.

Discussion

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

Even though there is good agreement between peak power calculations and measurements, a nuclear uncertainty margin (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 power operation, the maximum controlled reactivity rate change is less than 45 pcm/sec[1]. A maximum reactivity change rate of 65 pcm/sec for accidental withdrawal of control banks from subcritical is set such that the peak heat generation does not exceed the maximum (and DNBR is not below the minimum) allowable at overpower conditions. This satisfies GDC-25.

The maximum reactivity worth of control rods and the maximum rates of reactivity insertion employing control rods are limited 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).

Following any Condition IV event (rod ejection, steamline 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

[1] 1 pcm = 1^{-5} $\Delta\lambda$ (see footnote Table 4-5)

Reactivity addition associated with an accidental withdrawal of a control bank (or banks) is limited by the maximum rod speed (or travel rate) and by the worth of the bank(s). The maximum control rod speed is 45 inches per minute and the maximum rate of reactivity change considering two control banks moving in 100% overlap is less than 65 pcm/sec. During normal operation at power and with normal control rod overlap, the maximum reactivity change rate is less than 45 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 45 pcm/sec for normal operation and 65 pcm/sec for accidental withdrawal of two banks.

4.3.1.5 Shutdown Margins

Basis

Minimum shutdown margin as specified in the Core Operating Limits Report (COLR) is required at any power operating condition, in the startup, hot standby, hot shutdown and cold shutdown conditions.

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

Discussion

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

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

Basis

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

ANS/ANSI N210-1976 specifies a k_{eff} not to exceed 0.95 in spent fuel storage racks and transfer equipment flooded with pure water. No criterion is given for the refueling operation; however, a five 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 Core Operating Limits Report. Verification that this shutdown criteria is met, including uncertainties, is achieved using standard the Duke Energy design methods as described in References 4, 52, and 53. The subcriticality of the core is continuously monitored as described in the Technical Specifications.

Core subcriticality during refueling operations is maintained at an appropriate level by limiting new fuel assembly clusters in the core region to appropriate combinations based on the core refueling boron concentration (References 69, 70, and 71 - responses to NRC IEB89-03, "Potential Loss of Required Shutdown Margin During Refueling Operations"). The plant fuel handling guidelines (operations procedures and training for fuel handling) are used to ensure that adequate subcriticality margin is maintained.

4.3.1.6 Stability

Basis

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

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 with little or no operator action 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. Moveable incore detectors can be activated to provide more detailed information. In all 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 as the result of rod motion and power maneuverers during the majority of 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 and Overtemperature ΔT trip functions which use the measured axial power imbalance as an input.

4.3.1.7 Anticipated Transients Without Trip

The effects of anticipated transients with failure to trip are not considered in the design bases of the plant. Analysis has shown that the likelihood of such a hypothetical event is negligibly small. Furthermore, analysis of the consequences of a hypothetical failure to trip following anticipated transients has shown that no significant core damage would result, system peak pressures would be limited to acceptable values and no failure of the Reactor Coolant System would result (Reference 1). However, in order to comply with the ATWS rule (10CFR50.62), an ATWS Mitigation System has been installed. This system is described in Section 7.7.1.16.

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 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 composed of 264 fuel rods, 24 guide thimbles and an incore instrumentation thimble. In special instances, non-fuel filler rods may be used in fuel locations as the result of fuel assembly reconstitution. Figure 4-90 shows cross sectional view of a typical 17 x 17 fuel assembly and the related guide thimble locations. Further details of these fuel assembly designs are given in Section 4.2.

The fuel rods within a given assembly are of the same enrichment design radially. Axially, the rods/assembly may be of a constant enrichment, or they may be blanketed, meaning that the top and bottom end portions of the fuel may be at a lower enrichment. Figure 4-30 shows the fuel loading pattern to be used in the first core. Two regions consisting of the two lower enrichments are interspersed so as to form a checkerboard pattern in the central portion of the core. The third region is arranged around the periphery of the core and contains the highest enrichment. The enrichments for the first core are shown in Table 4-4.

Typical reload core loading patterns are shown in figures 4-31 and 4-95. Figure 4-31 shows fresh and burned fuel assemblies interspersed in a checker board pattern. The checker board pattern may be extended either inboard or out to the periphery depending on the needs of the reload design. Figure 4-95 shows a “ring of fire” design with feed assemblies ringing the core one row in from the periphery. This is a typical reload scheme used when integrated fuel burnable absorber (IFBA) is used in the feed assemblies. The placement of the assemblies is dependent on the previous cycles and the energy requirements of the cycle being designed. The fuel cycle times are appropriate for the refueling interval and for the performance criteria utilized.

The core average enrichment or feed region enrichment is determined by the amount of fissionable material required to provide the desired core lifetime and energy requirements, namely a design region average discharge burnup. 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. The rate of U-235 depletion is directly proportional to the power level at which the reactor is operated. In addition, the fission process results in the formation of fission products, some of which readily absorb neutrons. These effects, depletion and the buildup of fission products, are partially offset by the buildup of plutonium (which occurs due to the non-fission absorption of neutrons in U-238) illustrated in Figure 4-32 for the 17x17 fuel assembly. 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 removal of neutron absorbing material in the form of boron dissolved in the primary coolant and burnable poison.

The concentration of boric acid in the primary coolant is varied to provide control and to compensate for long-term reactivity requirements. The concentration of the soluble neutron absorber is varied to compensate for reactivity changes due to fuel burnup, fission product poisoning including xenon and samarium, burnable 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 1000 ppm and approximately 35 pcm/min when the reactor coolant boron 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 1000 ppm and approximately 75 pcm/min when the reactor coolant boron concentration is 100 ppm. The peak burnout rate for xenon is 25 pcm/min (Section 9.3.4.3 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 poison alone would result in a positive moderator coefficient at beginning-of-life for the first cycle and most reload cycles. Therefore, burnable poison is used to reduce the soluble boron concentration sufficiently to ensure that the moderator temperature coefficient is non-positive at full power operating conditions. During operation the burnable poison 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 poison is not critical since chemical shim is always available and flexible enough to cover any possible deviations in the expected burnable poison depletion rate. Figure 4-33 is a graph of a typical core depletion with and without burnable poison. Note that even at end-of-life conditions some residual poison remains resulting in a net decrease in cycle lifetime.

In addition to reactivity control, the burnable poison is strategically located to provide favorable radial and axial power distributions. Figure 4-34 shows the burnable poison distribution within a fuel assembly for the several burnable poison patterns used in a 17 x 17 array. Typical burnable poison loading patterns are shown in Figure 4-35 and Figure 4-36.

Table 4-4 through Table 4-6 contain a summary of the reactor core design parameters for the initial fuel cycle and typical reload fuel cycles, 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 comparisons of predicted versus measured power distributions as described in References 52, 53, and 64, and through ongoing comparisons between predicted and measured powers performed for each reload core design.

4.3.2.2.1 Definitions

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

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

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

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

Local heat flux is the heat flux at the surface of the cladding (BTU/hr-ft^2). 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:

Fq, 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.

Fq^{SCUF}, Nuclear Heat Flux Hot Channel Factor or Statistically Combined 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.

Fq^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.

$F\Delta H^N$, 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 DNB ratio described in Section 4.4.

Fz, Axial Peaking Factor, is defined as the ratio of the nuclear heat flux hot channel factor, Fq, divided by the nuclear enthalpy rise hot channel factor, FΔH. The axial peaking factor, Fz, can be defined on a core average or assembly average basis.

Relationships between Fq, FΔH and Fz are defined below.

$Fq = F\Delta H \times Fz$

$F\Delta H = Fq/Fz$

$Fz = Fq/F\Delta H$

4.3.2.2.2 Radial Power Distributions

The power shape in horizontal sections of the core at full power is a function of the fuel and burnable poison loading patterns and the presence or absence of a single bank of full length control rods. 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. The effects on radial power shape due to power level, xenon, samarium, and moderator density are considered also, but these are small. The effect of non-uniform 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. Figure 4-37 through Figure 4-41 show typical radial power distributions for one-quarter 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 - 28% inserted-equilibrium xenon, 4) HFP near

Middle-of- Life (MOL) - unrodded - equilibrium xenon, and 5) HFP at End-of-Life (EOL) - unrodded - 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 of the fuel rods within an assembly and their variations with burnup is utilized in thermal calculations and fuel rod design calculations as discussed in Section 4.2.

4.3.2.2.3 Assembly Power Distributions

For the purpose of illustration, assembly power distributions from similar BOL and EOL conditions corresponding to Figure 4-38 and Figure 4-41 respectively, are given for the same assembly in Figure 4-42 and Figure 4-43 respectively.

Since the detailed power distribution surrounding the hot channel varies from time to time, a conservatively flat assembly power distribution is assumed in the DNB analysis, described in Section 4.4. $F\Delta H^N$ is a variable limit which depends upon the location and magnitude of the axial peak, Fz. Care is taken in the nuclear design of all fuel cycles and all operating conditions to ensure that a flatter assembly power distribution does not occur with limiting values of $F\Delta H^N$. (See references 50 and 64).

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

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

and 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 Figure 4-44 through Figure 4-47. These figures cover a wide range of axial offset including values not permitted at full power. Figure 4-47 compares the axial power distribution for several assemblies at different distances from inserted control rods with the core average distribution.

4.3.2.2.5 Local Power Peaking

Fuel densification, which has been observed to occur under irradiation in several operating reactors, causes the fuel pellets to shrink both axially and radially. The pellet shrinkage combined with random hang-up of fuel pellets results in gaps in the fuel column when the pellets below the hung-up pellet settle in the fuel rod. The gaps vary in length and location in the fuel rod. Because of decreased neutron absorption in the vicinity of the gap, power peaking occurs in the adjacent fuel rods resulting in an increased power peaking factor. However, current fuel designs employed in the design of reactor cores at the McGuire Nuclear Station make use of fuel pellet densities which are greater than or equal to 95% of theoretical density. Results of hot cell and gamma scan measurements of fuel manufactured at this density have not shown significant gap formation. Therefore, no power peaking penalty due to fuel densification effects is taken in the nuclear analysis.

4.3.2.2.6 Limiting Power Distributions

According to the ANS/ANSI classification of plant conditions (See Chapter 15), Condition I occurrences are those which are expected frequently or regularly in the course of power operation, maintenance, or maneuvering of the plant. As such, Condition I occurrences are accommodated with margin between any plant parameter and the value of that parameter which would require either automatic or manual protective 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 (such as one coolant loop out of service) and operational transients is given in Chapter 15. 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 ANS/ANSI Conditions II, III and IV events.

Improper procedural actions or errors by the operator are assumed in the design as occurrences of moderate frequency (ANS/ANSI Condition II). Some of the consequences which might result are discussed in Chapter 15. Therefore, the limiting power distributions which result from such Condition II events, are those power distributions which deviate from the normal operating condition at the recommended axial offset band, e.g., due to lack of proper action by the operator during a xenon transient following a change in power level brought about by control rod motion. Power distributions 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.

Power distribution control within the reactor core is maintained by defining allowable limits for the nuclear heat flux hot channel factor, nuclear enthalpy rise hot channel factor, axial flux difference (AFD) and control rod insertion. The purpose of these limits is two fold. First, they provide assurance that the ECCS acceptance criteria is not exceeded during a loss of coolant accident (LOCA), and second, they provide assurance that DNBR limits are not exceeded during condition I and II transients. Limits for the above parameters are specified in either Technical Specifications, or the Core Operating Limits report (COLR).

The Topical report titled, "Nuclear Design Methodology for Core Operating Limits of Westinghouse Reactors", Reference 7, describes the methodology used to assure that the power peaking limits are not exceeded during condition I and II transients. This report focuses on the analysis performed to determine the power dependent axial flux difference and rod insertion limits (RILs), and the f(ΔI) penalty function for the over-power delta-T (OPDT) and over-temperature delta-T (OTDT) trip functions. A qualitative description of the analysis performed to establish these limits follows. Refer to Reference 7 for a detailed description of the analysis performed to set these limits.

An analysis is performed for each reload core design to confirm the adequacy of the current power dependent axial flux difference limits, rod insertion limits and the f(ΔI) penalty function. If the results from the analysis indicate that the current limits are no longer valid, new limits are derived based on this analysis. Once the new limits are developed, the appropriate changes to the Technical Specifications and the COLR are performed. However, if the new limits are found to overly restrict core operation, a new core design may be pursued in an attempt to reduce the magnitude core peaking during condition I and II transients, since a decrease in peaking will translate into a less restrictive f(ΔI) penalty and less limiting axial flux difference and rod insertion limits.

The analysis performed to develop axial flux difference limits, rod insertion limits and the f(ΔI) penalty function involves the generation and evaluation of several thousand three dimensional power distributions. Power distributions used in this evaluation are developed as a function of the following variables.

1. Burnup
2. Reactor power
3. Coolant temperature
4. Rod position
5. Xenon

The generation of conservative limits is assured through the generation of power distributions which are more severe than the power distributions expected to occur during normal or transient operation. Conservatisms are introduced into the analysis by assuming instantaneous changes in reactor power, soluble boron and rod positions. The selection of severe xenon distributions for the peaking analysis also adds another degree of conservatism to the analysis. Using these assumptions in concert provides a high level of confidence that the AFD limits, the rod insertion limits and the f(ΔI) penalty functions developed will be conservative. The analysis performed assumes the application of appropriate uncertainty factors to the power distribution used in the analysis as described in Reference 7. The nuclear uncertainty factors used are based on a 95 percent probability and 95 percent confidence level.

The methodology of Reference 7 assumes that the plant is in compliance with the following conditions. These conditions are assumed to exist during normal plant operation.

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

2. Control banks are sequenced with overlapping banks;

3. The control bank insertion limits are not violated;

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

The power dependent operational AFD and rod insertion limits are developed to ensure that the design basis local power peaking limits for the loss of coolant accident (LOCA) and the DNBR peaking limits for the loss of flow accident (LOFA) are not exceeded. The power dependent AFD limits are developed assuming only condition I type transients. Typical LOCA local peaking limits (Fq limits) are shown in Figure 4-51. LOCA peaking limits for each cycle are defined in the Core Operating Limits Report (COLR). LOFA DNB limits are defined in terms of maximum allowable total peaks (MATPs), which are representative of a constant DNBR, and are a function of both the magnitude and location of the axial peak. The generation of MATP limits are described in detail in Reference 50. If the power dependent AFD limits that are developed are overly restrictive such that the plant cannot operate about the ARO full power target AFD, a new set of AFD and rod insertion limits can be developed to allow power operation between 80% and 100% rated thermal power (RTP). These limits are known as Base Load AFD limits and are established by defining a narrow AFD band about the target AFD. By limiting the imbalance band about the target AFD, additional peaking margin can be gained through the reduction of transient xenon induced peaking effects.

Reactor protection system (RPS) AFD limits are developed through the analysis of power distributions produced from both condition I and II transients. If the results of the analysis performed confirm that the current set of RPS AFD limits are valid, the analysis performed to set the f(ΔI) penalty is also confirmed. However, if the current set of RPS AFD limits are exceeded, new RPS AFD limits are developed and the f(ΔI) penalty function recalculated. The f(ΔI) penalty trip reset function portion of the OPDT and OTDT trip functions is used to ensure that both DNB and center-line fuel melt (CFM) margin exists during postulated condition I and II transients.

Several condition II transients are evaluated in order to develop RPS AFD limits. These transients are:

1. Boron dilution with control rods in automatic or manual
2. Reduction in feedwater temperature
3. Increase in feedwater flow
4. Increase in steam flow
5. Inadvertent opening of a steam line valve
6. Uncontrolled bank withdrawal accident
7. Control rod mis-operation.

The initial conditions for the above transients are assumed to be within the conditions of normal operation outlined previously. Control rod motion during the transient is not constrained to the RILs since these limits do not mitigate the consequences of the accident, or produce a reactor trip. The analysis performed considers a range of reactor powers, with the upper limit in reactor power assumed to be less than or equal to 118% RTP. A reactor trip is assumed to occur at this power level.

RPS DNB evaluations are performed for each of the transients described above to confirm if current RPS AFD limits are bounding, or to generate new RPS AFD limits. DNB evaluations are performed as a function of both reactor power and inlet temperature. The f(ΔI) penalty functions for the OPDT and OTDT trip functions are developed using the RPS AFD space defined in this analysis. The peak power densities produced from the condition I and II transients are also verified to be less than that required to produce center-line fuel melt.

Both Fq and FΔH peaking limits are monitored as required by Technical Specifications to assure that initial conditions for the loss of coolant and loss of flow accidents are not exceeded. Fq limits are power dependent and increase with decreasing power as shown in Technical Specifications. FΔH limits are also power dependent and increase with decreasing reactor power and can be essentially defined by the following equation.

$$F\Delta H = MARP[1 + 0.3(1 - P)]$$

Maximum allowable radial peaks (MARPs) are developed based on the thermal conditions produced from the loss of flow accident and are a function of both the magnitude and location of the axial peak. MARPs are algebraically derived from MATP limits, which are discussed in depth in Reference 50.

Core designs are developed based on allowable Fq, FΔH and rod insertion limits. The limiting power distributions for a core design typically occur with control banks at or near their rod insertion limit. Therefore, power operation about the HFP ARO equilibrium xenon target AFD is recommended since operation about this target AFD will result in an increase in margin to both the Fq and FΔH limits.

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

4.3.2.2.7 Experimental Verification of Power Distribution Analysis

Verification of predicted versus measured power distributions has been performed as part of the licensing of Duke's reload design methodology as described in References 52, 53, and 64. An in depth discussion of these comparisons can be found in these references. The conversion of measured reaction rates into three-dimensional power distributions was performed using the computer code DETECTOR as described in Reference 54. The DETECTOR algorithm has been integrated into a new code named COMET as described in Reference 56. 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:

1. Reproducibility of the measured signal
2. Errors in the calculated relationship between detector current and local flux
3. Errors in the calculated relationship between detector flux and peak rod power some distance from the instrumentation thimble.

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

Statistical analyses have been performed in References 52, 53, and 64 to develop observed nuclear reliability factors (ONRFs) for the nuclear heat flux hot channel factor, Fq, the nuclear enthalpy rise hot channel factor, FΔH, and the axial peak uncertainty factor, Fz. Predicted versus measured power distributions were compared for several cycles at different burnup points during each cycle in order to generate a statistical data base. The analysis performed in References 53, and 64 assumed that the difference between the measured and predicted powers was normally distributed. This assumption was subsequently confirmed by performing a D-prime test to establish the normality of the distribution. The one-sided upper tolerance limit methodology was used to develop the ONRF based on a 95 percent probability and a 95 percent confidence level. For an in depth discussion on how these ONRFs were developed, refer to Reference 53. References 53, and 64 refer to the statistical analysis performed for CASMO-3/SIMULATE-3 Nuclear Design power distribution analysis methods. For the CASMO-4/SIMULATE-3 MOX methods, an additional statistical analysis is performed when the D-prime test for normality shows that the difference between the measured and predicted powers is non normally distributed. The non-parametric statistical evaluation is described in Reference 41 and was used in Reference 52 to develop the ONRFs based on a 95/95 one-sided tolerance confidence level. The 95/95 uncertainty of a distribution is the m^{th} worst comparison where m is a function of the number of comparisons. For an in depth discussion on how these ONRFs were developed, refer to Reference 52.

The ability of the advanced nodal code SIMULATE-3 to predict local pin powers has been assessed, as described in References 52 and 53. The predictive capability of SIMULATE-3 was assessed by comparing predicted pin powers against measured pin powers for several B&W critical experiments. The results of these comparisons and the resulting statistical analysis showed that SIMULATE-3 could predict peak pin power to within approximately 1.0%. However, a value of 2% was selected for CASMO-3/SIMULATE-3P methods (Reference 53). For CASMO-4/SIMULATE-3 MOX methods, the calculated pin uncertainty is 1.69% for low enriched Uranium (LEU) fuel and 2.15% for mixed oxide (MOX) fuel (Reference 52.).

In References 52, 53, and 64 , statistically combined uncertainty factors for Fq and FΔH are produced by statistically combining the local peaking factor and the engineering hot channel factor with the observed nuclear reliability factors for Fq and FΔH. The uncertainty factor for Fz was also developed in References 52, 53, and 64 . The statistically combined uncertainty factors for Fq, FΔH, and Fz are shown in Table 4-22. Note that CASMO-4/SIMULATE-3 uncertainties in this table bound uncertainty values developed in Reference 52.

The above uncertainties are used in the LOCA, center-line fuel melt, transient strain, and DNBR evaluations of design basis transients.

4.3.2.2.8 Testing

An extensive series of physics tests is performed on each core. These tests and the criteria for satisfactory results are described in Chapter 14. Since not all limiting situations can be created at beginning-of-life, one purpose of the tests is to provide a check on the calculational methods used in the predictions for the conditions of the test. Tests are performed at the beginning of each reload cycle to verify that the reactor core is operating as designed.

4.3.2.2.9 Monitoring Instrumentation (HISTORICAL INFORMATION NOT REQUIRED TO BE REVISED)

The adequacy of instrument numbers, spatial deployment, required correlations between readings and peaking factors, calibration and errors are described in References 2, 6, and 10. The relevant conclusions are summarized here in Sections 4.3.2.2.7 and 4.4.6.

Provided the limitations given in Section 4.3.2.2.6 on rod insertion and flux difference are observed, the excore detector system provides adequate online monitoring of power distributions. Further details of specific limits on the observed rod positions and flux difference are given in the Technical Specifications or Core Operating Limits (COLR) report. Limits for alarms, reactor trip, etc., are given in the Technical Specifications. Descriptions of the systems provided are given in Section 7.7.

4.3.2.3 Reactivity Coefficients

The kinetic characteristics of the reactor core determine the response of the core to changing plant conditions or to operator adjustments made during normal operation, as well as the core response during abnormal or accidental transients. These kinetic characteristics are quantified in reactivity coefficients. The reactivity coefficients reflect the changes in the neutron multiplication due to varying plant conditions such as power, moderator or fuel temperatures, or less significantly due to a change in pressure or void conditions. Since reactivity coefficients change during the life of the core, ranges of coefficients are employed in transient analysis to determine the response of the plant throughout life. The results of such simulations and the reactivity coefficients used are presented in Chapter 15. The reactivity coefficients are calculated on a corewide basis using transport theory and nodal analysis 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 and are included; for example, in the postulated rupture of the main steamline break and the rupture of an RCCA mechanism housing described in Sections 15.1.5 and 15.4.8 respectively.

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 and benchmarking as described herein; results of these tests are discussed in Section 4.3.3.

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

4.3.2.3.1 Fuel Temperature (Doppler) Coefficient

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

The Doppler temperature coefficient is calculated using a three-dimensional simulator. The coefficient is calculated by performing a set of cases which vary the effective fuel temperature about a mean fuel temperature. The resulting reactivity difference between the two fuel temperatures divided by the change in fuel temperature defines the Doppler temperature coefficient. The moderator temperature is held constant for the calculation. Spatial variations in the fuel temperature are taken into account by functionalizing the fuel temperature against local power density.

Doppler temperature coefficients are shown as a function of effective fuel temperature in Figure 4-57 for both BOC and EOC conditions. The Doppler-only contribution to the power coefficient (Section 4.3.2.3.3) is shown in Figure 4-58 as a function of relative core power. The integral of the differential curve on Figure 4-58 is the Doppler contribution to the power defect and is shown in Figure 4-59 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. The minimum and maximum limits of the Doppler coefficient used in accident analyses are given in Chapter 15.

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.

4.3.2.3.2.1 Moderator Density and Temperature Coefficients

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

The soluble boron used in the reactor as a means of reactivity control also has an effect on moderator temperature coefficient since the soluble boron poison density as well as the water density is decreased when the coolant temperature rises. A decrease in the soluble poison concentration introduces a positive component in the moderator temperature coefficient. Thus, if the concentration of soluble poison is large enough, the net value of the coefficient may be positive. With the burnable poison present, however, the initial hot boron concentration is sufficiently low such that the moderator temperature coefficient is negative at full power operating temperatures. The effect of control rods is to make the moderator temperature coefficient more negative by reducing the required soluble boron concentration and by increasing the leakage of the core.

Positive moderator temperature coefficients are allowed at reactor powers less than 100% rated thermal power. Specifically, the moderator temperature coefficient shall be less than or equal to 7 pcm/°F from 0% rated thermal power through 70% rated thermal power, and less than or equal to 0 pcm/°F at 100% rated thermal power. The moderator temperature coefficient decreases linearly from 7 pcm/°F at 70% rated thermal power to 0 pcm/°F at 100% rated thermal power. Refer to Figure 4-72 for a graphical representation of this coefficient.

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

The moderator coefficient is calculated for the various plant conditions by varying the moderator temperature (and density) about a mean temperature. This calculation is typically performed using a three dimensional reactor simulator. The moderator temperature coefficient is shown as a function of moderator temperature and boron concentration for the unrodded and rodded core in Figure 4-60 through Figure 4-62. The temperature range covered is from cold (68°F) to about 600°F. The contribution due to Doppler coefficient (because of a change in moderator temperature) has been subtracted from these results. Figure 4-63 shows the hot, full power moderator temperature coefficient 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.

4.3.2.3.2.2 Moderator Pressure Coefficient

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

4.3.2.3.2.3 Moderator Void Coefficient

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

4.3.2.3.3 Power Coefficient

The combined effect of moderator temperature and fuel temperature change as the core power level changes is called the total power coefficient and is expressed in terms of reactivity change per percent power change. The power coefficient at BOL and EOL conditions is given in Figure 4-64.

The power coefficient becomes more negative with burnup reflecting the combined effect of moderator and fuel temperature coefficient changes with burnup. The power defect (integral reactivity effect) at BOL and EOL is given in Figure 4-65.

4.3.2.3.4 Comparison of Calculated and Experimental Reactivity Coefficients

References 52 and 53 describe the comparison of calculated and experimental reactivity coefficients in detail. Based on the data presented there, it is estimated that the accuracy of the current analytical model is:

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

±2 pcm/°F for the moderator temperature coefficient.

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

4.3.2.3.5 Reactivity Coefficient Used in Transient Analysis

Reactivity coefficients are calculated as part of the safety analysis for each reload core using NRC-approved methodology to systematically confirm that the reactivity coefficients used in the licensing Chapter 15 accident analyses are bounding. The models used to perform these calculations are based on the available operating history of the previous cycle to assure best estimate calculations. Determination of whether a nuclear-related physics parameter is within the bounding value assumed in the Reference safety analysis is made by performing explicit calculations of the parameter, or by comparison to values generated in previous reload core designs. Comparison to previously calculated physics parameters are only performed if the reload core being analyzed is similar to previously analyzed reload cores.

Table 4-5 gives the limiting values as well as typical 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 the most negative or the most positive (least negative) coefficients are appropriate, and whether spatial non-uniformity 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. A description of the combination of nuclear parameters used in Chapter 15 can also be found in References 48 and 51.

Typical best estimate reactivity coefficients are shown in Figure 4-57 through Figure 4-65. 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. Table 4-6 presents (information only) control rod requirements for the first cycle, at BOC and EOC, and a hypothetical equilibrium cycle at EOC.

4.3.2.4 Control Requirements

To ensure the shutdown margin stated in the Core Operating Limits Report (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 4-5. 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 4-6 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 based on the available rod worth for analytic uncertainties. 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, variable average moderator temperature, flux redistribution, and reduction in void content as discussed below.

4.3.2.4.1 Doppler

The Doppler effect arises from the broadening of U-238 and Pu-240 resonance peaks 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.

Since the moderator coefficient is negative, there is a reactivity addition with power reduction. 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 reactivity control requirement at end-of-life.

4.3.2.4.3 Redistribution

During full power operation, the coolant density decreases with core height, and this, together with partial insertion of control rods, results in less fuel depletion near the top of the core. Under steady state conditions, the relative power distribution will be slightly asymmetric towards the bottom of the core. On the other hand, at 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 periodic changes in boron concentration, changes in temperature, and very small changes in the xenon concentration not compensated for by a change in boron concentration. When the control bank reaches either limit of this band, a change in boron concentration is required to compensate for additional reactivity changes. Since the insertion limit is set by a rod travel limit, a conservatively high calculation of the inserted worth is made which accounts for possible transient xenon effects.

4.3.2.4.6 Burnup

Excess reactivity of 12 - 15 percent $\Delta\rho$ (hot) is installed at the beginning of each cycle to provide sufficient reactivity to compensate for fuel depletion and fission products throughout the cycle. This reactivity is controlled by the addition of soluble boron to the coolant and by burnable poison. The burnable poison worth and soluble boron concentration for several core configurations are given in Table 4-4 and Table 4-5. Since the excess reactivity for burnup is controlled by soluble boron and/or burnable poison, 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.

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 12.

4.3.2.4.9 Experimental Confirmation

The core reactivity change from normal operating conditions to shutdown conditions with all rods inserted (ARI) and the highest worth rod stuck out of the core is not typically measured. However, several reactivity components of this reactivity change are measured for each reload core. Comparisons between predicted and measured isothermal temperature coefficients (ITCs), control rod worths and critical boron concentrations are performed at BOC HZP conditions for each reload core. Through these comparisons the ability of the nuclear codes to accurately predict the behavior of the reload core and safety related parameters is confirmed. Additional comparisons that are performed throughout the cycle which confirm the reload models accuracy are summarized below.

1. Comparisons between predicted and measured full power or near full power boron concentrations and power distributions.
2. Comparisons of predicted versus measured critical conditions (e.g. rod position, boron concentration and xenon worth) during the reactor startup following a reactor trip.
3. Comparisons of the predicted versus measured EOC ITC as required.

The results of these comparisons provide a high level of confidence in the ability of the core model to predict control requirements, or shutdown margin. As an additional degree of conservatism in addition to the highest worth stuck rod, shutdown margin calculations include a 10% allowance for uncertainty in the calculated rod worth, a rod insertion allowance and also an allowance to account for transient xenon effects. The accuracy of the analytical model to predict control requirements, based on comparisons between measured and predicted startup data, is believed to be within 0.3% $\Delta\rho$.

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 poison 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:

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

Example boron concentrations for various core conditions are presented in Table 4-5.

4.3.2.4.12 Rod Cluster Control Assemblies

Fifty three full length Rod Cluster Control Assemblies are employed. The full length rod cluster control assemblies are used for shut-down and control purposes to offset fast reactivity changes associated with:

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

The allowed control bank reactivity insertion is limited at full power to maintain shutdown capability. Because of the reduction in the magnitude of the power defect with decreasing power, 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 Technical Specifications on Rod Insertion limits.

Power distribution, rod ejection, and rod misalignment analyses are based on the arrangement of the shutdown and control groups of the rod cluster control assemblies shown in Figure 4-66. In preparing for power operation, all shutdown banks are withdrawn before withdrawal of the control banks is initiated. After which, Control Banks A, B, C, D are withdrawn sequentially in 50% overlap. The limits of rod positions and further discussion on the basis for rod insertion limits are provided in the Technical Specifications.

As a minimum, the following accident analyses shall be re-evaluated as required by Technical Specifications for continued power operation with a full length rod not within alignment limits: Rod Cluster Control Assembly Insertion Characteristics, Rod Cluster Control Assembly Misoperation, Loss of Reactor Coolant from Small Ruptured Pipes or from Cracks in Large Pipes Which Actuates the Emergency Core Cooling System, Major Secondary System Pipe Rupture, and Rupture of a Control Rod Drive Mechanism Housing (Rod Cluster Control Assembly Ejection).

4.3.2.4.13 Reactor Coolant Temperature

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

1. Maximize return to power capabilities.
2. Provide ±5 percent power load regulation capabilities without requiring control rod compensation.

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

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

4.3.2.4.14 Burnable Poison

Burnable poison may be in the form of discrete rods or as a coating on the fuel pellets (or Integral Fuel Burnable Absorber, IFBA).

The burnable poison rods provide partial control of the excess reactivity available at the beginning of a cycle. In doing so, these rods prevent the moderator temperature coefficient from being positive at normal operating conditions. They perform this function by reducing the requirement for soluble poison in the moderator at the beginning of the cycle as described previously. For purposes of illustration, typical and initial core burnable poison rod patterns in the core together with the number of rods per assembly are shown in Figure 4-35 , while the arrangements within an assembly are displayed in Figure 4-34. The range of typical reactivity worths of these rods is shown in Table 4-4. The boron in the rods is depleted with burnup but at a sufficiently slow rate so that the resulting critical concentration of soluble boron is such that the moderator temperature coefficient remains negative at all times for full power operating conditions.

The burnable poison rods also provide partial control of the axial power distribution by shifting power to the upper or lower region of the core through selective positioning of the absorber region.

The burnable poison that is used as a coating applied directly to the fuel pellet, such as IFBA (Integral Fuel Burnable Absorber), behaves similarly to the discrete rods. However, the negative reactivity effect of the IFBA in the core lasts for a shorter duration than that of the burnable poison rods. The residual negative reactivity at the end of the cycle is also generally less for IFBA than discrete burnable poison rods, which tends to reduce fuel enrichment costs. Varying the number of fuel rods within a given assembly (or core location) that contain IFBA pellets can allow some freedom of control on how much reactivity hold down is introduced to that core location. Typical radial arrangements of IFBA rods within an assembly can be found in Figure 4-21. Axially, the placement and number of the IFBA pellets as well as the thickness of the poison coating can vary to allow some control of reactivity hold down, axial offset, and internal rod pressure. Table 4-4 lists typical mechanical design characteristics of the assemblies with IFBA.

The arrangement of assemblies with IFBA within the core may include locations with or without control rods. In some cases, burnable poison rods and IFBA might be used. In all cases, assemblies with discrete burnable poison rods are limited to core locations without control rods.

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 Core Operating Limits Report (COLR) and discussed in Sections 4.3.2.4.12. The power distribution is maintained within acceptable limits through the location of the full length rod bank (within the allowable control rod insertion limits). Reactivity changes due to the changing xenon concentration can be controlled by rod motion and/or changes in the soluble boron concentration.

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

4.3.2.4.17 Burnup

Control of the excess reactivity for burnup is accomplished using soluble boron and/or burnable poison. The boron concentration must be limited during operating conditions to ensure the moderator temperature coefficient is negative. Sufficient burnable poison is installed at the beginning of a cycle to give the desired cycle lifetime without exceeding the boron concentration limit. The typical minimum boron concentration is 5 ppm.

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 control assembly pattern is displayed in Figure 4-66 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, etc., as applicable. Each bank, although operated and controlled as a unit, is comprised of two subgroups. The axial position of the rod cluster control assemblies may be controlled manually or 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 requirements specified in Table 4-6. 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 three to four percent $\Delta\rho$; therefore, four banks (described as A, B, C, and D in Figure 4-66) each worth approximately one percent $\Delta\rho$ have been selected.

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 margin 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. The usual practice is to adjust boron to assure that the rod position lies within the maneuvering band, such that an escalation from zero power to full power does not require further adjustment of the boron concentration.

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 illustrated in Figure 4-67.

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 normalized to “Distance to top of Dashpot” and “Drop time to Dashpot” is discussed in Section 15.0. 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. The result of these calculations is discussed in Section 15.

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 rod cluster control assembly remains fully withdrawn and no changes in xenon or boron concentration take place. However, with all rod cluster control assemblies verified fully inserted by two independent means, it is not necessary to account for a stuck rod cluster control assembly in the shutdown margin calculation. The loss of control rod worth due to material irradiation is negligible, since during operation all the rod cluster control assemblies are positioned out of or slightly in the core.

The values given in Table 4-6 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 and the effects of transient xenon are made before determination of the shutdown margin.

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

Criticality of fuel assemblies outside the reactor is precluded by adequate design of fuel transfer, shipping and storage facilities and by administrative control procedures. The two principal methods of preventing criticality are limiting the fuel assembly array size and limiting assembly interaction by fixing the minimum separation between assemblies and/or inserting neutron poisons between assemblies.

The design basis for preventing criticality outside the reactor is that, considering possible variations, there is a 95 percent probability at a 95 percent confidence level that the effective multiplication factor (k_{eff}) of fuel assembly array will be less than or equal to 0.95 as recommended in ANSI/ANS-57.2-1983. The conditions that are assumed in meeting this design basis are outlined in Section 9.1.2.3.1.2.

The analysis method used to ensure the criticality safety of fuel assemblies is dependent on the conditions being analyzed.

These methods conform with ANSI N18.2-1973, "Nuclear Safety Criteria for the Design of Stationary Pressurized Water Reactor Plants," Section 5.7, ANSI/ANS-57.2-1983, "Design Requirements for LWR Spent Fuel Storage Facilities at Nuclear Power Stations," Section 6.4.2.2, ANSI N16.9-1975, "NRC Standard Review Plan," Section 9.1.2, and the NRC guidance, "Guidance on the Regulatory Requirements for Criticality Analysis of Fuel Storage at

Light Water Reactor Power Plants" contained in an NRC memo from Laurence Kopp dated August 19, 1998.

4.3.2.6.1 New Fuel Vault Criticality Analysis Methodology

The new fuel vaults are designed exclusively for temporary storage of fresh unirradiated fuel. ANSI/ANS-57.3-1983, "Design Requirements for New LWR Fuel Storage Facilities", simply requires that k_{eff} be maintained at less than or equal to 0.98 under optimum moderation conditions. Analysis used to determine k_{eff} in these storage racks must therefore assume maximum allowable fuel enrichments. Criticality control relies strictly on the wide spacing between individual storage locations and a specified upper limit for as-built fuel assembly enrichment. The absence of other factors such as soluble boron, fixed poisons, burnup effects, and fission products makes for a relatively straightforward analysis. The normally dry condition of the fuel vaults introduces the possibility of water intrusion. Consequently, full density water flooding was conservatively modeled as a normal condition in this analysis. Other less likely events which could create low density moderator conditions (i.e. foaming, misting, etc.) dictated analysis of optimum moderator conditions as an accident condition. Vault criticality analysis is therefore performed as a function of both enrichment and moderator density.

The SCALE Version 4.4 system of computer codes (Reference 66) was employed for the new fuel vault criticality analyses. These calculations used the CSAS25 sequence of codes in the Criticality Safety Analysis Sequences (CSAS) control module in SCALE 4.4. The CSAS25 control sequence first runs two processing codes (BONAMI for resonance self-shielding, and NITAWL-II to produce a working transport cross-section library), before performing the criticality computations on the new fuel vault model with KENO V.a (a 3-D Monte Carlo criticality code) to determine a system k_{eff}. The new fuel vault criticality calculations used the SCALE-4.4 238-group neutron library based on ENDF-B Version 5 data.

To demonstrate the validity of SCALE-4.4 for performing criticality calculations, a set of critical experiments was evaluated to establish an appropriate method bias and uncertainty. These critical experiments are summarized in Table 4-7. They include a diverse group of water-moderated, uranium oxide fuel rod arrays, separated by various materials. The experiments are representative of LWR storage conditions, including the McGuire new and spent fuel storage racks.

A total of 41 of the 58 critical experiments in Table 4-7 – those at the highest enrichment of 4.31 wt % U-235 – were selected for determination of a method bias and uncertainty to be applied to the maximum 95/95 k_{eff} calculations for the McGuire new fuel storage racks. SCALE-4.4 calculations for these critical experiments yielded a benchmark bias of +0.0061 Δk (average under-prediction of k_{eff}) and uncertainty of ± 0.007 Δk.

4.3.2.6.2 Spent Fuel Rack Criticality Analysis Methodology

The McGuire SFPs are designed to store fresh and irradiated fuel assemblies in a wet, borated envorinment. The SFPs are divided into two regions: Region 1 and Region 2. McGuire Region 1 is normally used for storage of fresh fuel and irradiated fuel that will be reloaded into the reactor core. Region 2 is designed to store fuel asssmblies that have been permanently discharged from the reactor.

SCALE-4.4 (described in Section 4.3.2.6.1) was used to evaluate the Region 1 racks, which do not have burnup requirements for stored fuel assemblies. All of the 58 critical experiments in Table 4-7 were selected for determination of a method bias and uncertainty to be applied to the maximum 95/95k_{eff} calculations for the McGuire Region 1 storage racks. SCALE-4.4

calculations for these critical experiments yielded a benchmark bias of +0.0064 Δk and an uncertainty of ±0.0066 Δk.

For the Region 2 storage racks, the CASMO-3/TABLES-3/SIMULATE-3 (References 34, 35, and 36) suite of codes were employed to determine the minimum fuel assembly burnup requirements. CASMO-3 is an integral transport theory code, SIMULATE-3 is a nodal diffusion theory program, and TABLES-3 is the linkage code that functionalizes CASMO-3 cross-sectional data into a tabular form for SIMULATE 3-D nodal computations. The CASMO-3/SIMULATE-3 methodology allows reactor depletion data to be employed in the ex-core Region 2 storage rack analysis. In order to compute the most accurate set of isotopic data for the fuel assemblies being considered, nominal reactor depletion parameters including moderator temperature, fuel temperature, soluble boron concentration, and burnable poison presence were incorporated into the CASMO-3 models.

Benchmarking of CASMO-3/SIMULATE-3, using the fine-energy-group (70-group) neutron cross-section library available with CASMO-3, was accomplished by evaluating 10 B&W close-pooximity critical experiments performed at the CX-10 facility. These B&W critical experiments, summarized in Table 4-7, were specifically designed for reactivity benchmarking purposes. Results from these B&W critical benchmark cases yielded a calculational bias of -.0015 Δk (average over-predicition of k_{eff}) and an uncertainty of ±0.0121 Δk. Even though SIMULATE-3 tends to over-predict k_{eff}, its negative bias was conservatively ignored. The uncertainty, however, was still used in computing the overall 95/95 k_{eff}S for the McGuire Region 2 irradiated-fuel storage configurations.

The spent fuel pool Region 1 and Region 2 criticality analyses are performed with partial credit for soluble boron, in accordance with 10 CFR 50.68 (b)(4) and the NRC memo from Laurence Kopp – "Guidance on the Regulatory Requirements for Criticality Analysis of Fuel Storage at Light-Water Reactor Power Plants" – dated August 19, 1998. The maximum 95/95 k_{eff}, including all pertinent biases and uncertainties, is less than 1.0 in unborated water, and less than 0.95 with 800 ppm soluble boron.

Credit is taken for the fixed Boral poison material within the Region 1 racks. However, no credit is taken for any remaining Boraflex in the Region 2 storage racks.

No credit is taken for any short-lived Xe-135 poisions in the irradiated fuel stored in the McGuire spent fuel pools.

There are two types of uncertainty – computational and measured – associated with the average (2-D) burnup of the fuel assemblies considered for the Region 2 evaluations. The computational burnup uncertainty quantifies, in a global sense, the ability of the CASMO-3/SIMULATE-3 codes to accurately determine the isotopic content, and hence k_{eff}, of a collection of irradiated assemblies in the McGuire reactors, assuming the actual average burnup of the fuel in the reactor core is the same as the average burnup of the SIMULATE-3 model for that reactor core. This uncertainty is determined by examining several cycles of McGuire reactor operational data to evaluate the differences between measured and SIMULATE-3-predicted core reactivity at various times during the operating cycle. The analysis of these data yields a reactivity bias and burnup-dependent reactivity uncertainty to be applied to the overall system 95/95 k_{eff} calculation for each particular Region 2 storage configuration evaluated.

The measured burnup uncertainty represents the reactivity penalty associated with difference between the measured burnup and the code-predicted burnup. Measured burnups, which are used for Technical Specification verification, have many sources of instrumentation error that can result in the measurement value being different from the "true" burnup of a specific fuel assembly. Differences between measured and core follow predicted burnups are evaluated to produce the distribution of burnup measurement errors for a database of McGuire discharge fuel assemblies, and quantify an appropriate measurement bias and uncertainty.

There is one other burnup-related uncertainty to be considered for the Region 2 criticality analysis, related to the 3-D axial variations of burnup and other reactor depletion parameters. This axial profile uncertainty represents a bounding reactivity penalty associated with differences between the k_{eff} calculated using an average "estimated" axial burnup and depletion parameter profiles for a particular fuel assembly, and the k_{eff} calculated using the actual axial burnup and history profiles for that fuel assembly. The large database of McGuire core follow profiles available allows for comparison between actual and average "estimated" profiles with a good sample size of fuel assemblies. In this manner axial profile k_{eff} errors for each of the fuel assemblies in the end-of-cycle McGuire core follow cases are determined, and the resulting distribution of errors is statistically analyzed to yield a conservative axial profile reactivity uncertainty.

Other applicable uncertainties and biases are discussed in Section 9.1.2.3.1.2.

With all of the relevant biases and uncertainties determined for application in the total 95/95 k_{eff} calculations, 3-D SIMULATE-3 computations are made for the Region 2 storage racks, for each of the seven different fuel assembly designs described in Section 9.1.2.3.1.2. Three types of storage configurations are analyzed: Unrestricted, 2/4 Restricted/Filler, and 3/4 Checkerboard/Empty. For each of the fuel assembly types and storage configuration categories, a series of minimum burnup curves is generated as a function of initial maximum enrichment and post-irradiation cooling time. All of the minimum burnup curves meet the overall 95/95 k_{eff} criteria described earlier in this section. Additional details of the methods used for these analyses can be found in Reference 67, which was approved per Reference 68.

4.3.2.6.2.1 Spent Fuel Storage Rack Sub-Region Interface Criticality Analysis Methodology

Because Region 2 has three defined fuel storage configurations, it is also important to examine the reactivity effects of storing one storage configuration next to another. To limit the potential reactivity increases associated with storing one type of SFP configuration next to, within, or around another, the following Region 2 storage configuration boundary restrictions have been imposed:

- Unrestricted storage – No boundary restrictions.
- 2/4 Restricted/Filler storage – No boundary restrictions.
- 3/4 Checkerboard/Empty storage – Any row or column of fuel in a 3/4 Checkerboard/Empty storage configuration that borders any other storage configuration must have alternating Checkerboard fuel and empty cells. That is, it cannot be a row or column of solid Checkerboard fuel.

In addition, a minimum storage array size of 2x2 cells is required for all SFP Region 2 storage configurations except for Unrestricted storage regions, which can be any size.

Using the boundary restrictions defined above, several scenarios were considered in which one of these storage configurations is adjacent to or surrounded by another. These cases were evaluated with random variations of fuel type/enrichment/cooling time within the Unrestricted, 2/4 Restricted/Filler, and 3/4 Checkerboard/Empty storage arrays. The storage configuration boundary scenarios were analyzed in unborated water. The highest system k_{eff} among all these boundary cases became the maximum nominal value that was used in computing the maximum 95/95 k_{eff} in Region 2.

4.3.2.7 Stability

4.3.2.7.1 Introduction

The stability of the PWR cores against xenon-induced spatial oscillations and the control of such transients are discussed extensively in References 6, 15, 16, and 17. The design bases for core stability 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 in 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 negative power coefficient of reactivity, PWR cores are inherently stable to oscillations in total power. Protection against total power instabilities is provided by the Control and Protection System as described in Section 7.7. Hence, the discussion on the core stability will be limited here to xenon-induced spatial oscillations.

4.3.2.7.2 Stability Index

Power distributions, either in the axial direction or in the X-Y plane, can undergo oscillations due to perturbations introduced in the equilibrium distributions without changing the total core power. The xenon-induced oscillations are essentially limited to the first flux overtones in the current PWRs, and the stability of the core against xenon-induced oscillations can be determined in terms of the eigenvalues of the first flux overtones. Writing, either in the axial direction or in the X-Y plane, the eigenvalue u of the first flux harmonic as:

$$u = b + ic, \qquad \text{Equation 4.3-1}$$

then b is defined as the stability index and $T = \dfrac{2\pi}{c}$ 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:

$$\delta\phi(t) = Ae^{ut} = ae^{bt}\cos ct \qquad \text{Equation 4.3-2}$$

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

$$b = \frac{1}{T}\ln\frac{A_{n+1}}{A_n} \qquad \text{Equation 4.3-3}$$

where An, An+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

Analysis of both the axial and X-Y xenon transient tests and actual axial xenon transients which are discussed in Section 4.3.2.7.5, shows that the calculational models are adequate for the prediction of core stability.

4.3.2.7.4 Stability Measurements (HISTORICAL INFORMATION NOT REQUIRED TO BE REVISED)

1. *Axial Measurements*

   Two axial xenon transient tests conducted in a PWR with a core height of 12 feet and 121 fuel assemblies are reported in Reference 18, 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 4-70.

   The total core power was held 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:

   a. *The core was stable against induced axial xenon transients both at the core average burnups of 1550 MWD/MTU and 7700 MWD/MTU. The measured stability indices WERE -0.041 hr $^{-1}$ for the first test (Curve 1 of Figure 4-70) and -0.014 hr $^{-1}$ for the second test (Curve 2 of Figure 4-70). The corresponding oscillation periods were 32.4 hrs. and 27.2 hrs., respectively.*

   b. *The reactor core became less stable as fuel burnup progressed and the axial stability index was essentially zero at 12,000 MWD/MTU.*

2. *Measurements in the X-Y Plane*

   Two X-Y xenon oscillation tests were performed at a PWR 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 12900 MWD/MTU. Both of the X-Y xenon tests showed that the core was stable in the X-Y plane at both burnups. The second test showed that the core became more stable as the fuel burnup increased and all Westinghouse PWRs with 121 and 157 assemblies are expected to be stable throughout their burnup cycles.

   In each of the two X-Y tests, a perturbation was introduced to the equilibrium power distribution through an impulse motion of one rod cluster control unit located along the diagonal axis. Following the perturbation, the uncontrolled oscillation was monitored using the moveable detector and thermocouple system and the excore power range detectors. The quadrant tilt difference (QTD) is the quantity that properly represents the diametral oscillation in the X-Y plane of the reactor core in that the differences of the quadrant average powers over two symmetrically opposite quandrants 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 $^{-1}$ with a period of 29.6 hours was obtained from the thermocouple data shown in Figure 4-71.

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

4.3.2.7.5 Comparison of Calculations with Measurements (HISTORICAL INFOROMATION NOT REQUIRED TO BE REVISED)

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 (Reference 19). The analysis of the X-Y xenon transient tests was performed in an X-Y geometry using a modified TURTLE (Reference 11) code. 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 (Reference 20) and CINDER (Reference 21). 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 4-12. The calculations show conservative results for both of the axial tests with a margin of approximately -0.01 hr $^{-1}$ *in the stability index.*

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

Previous studies of the physics of xenon oscillations are reported in the series of topical reports, References 15, 16, and 17. A more detailed description of the experimental results and analysis of the axial and X-Y xenon transient tests is presented in Reference 18 and Section 1 of Reference 22.

4.3.2.7.6 Stability Control and Protection (HISTORICAL INFORMATION NOT REQUIRED TO BE REVISED)

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.

1. Axial Power Distribution

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

Twelve foot PWR cores become less stable to axial xenon oscillations as fuel burnup increases. Instabilities may occur near BOC of a reload cycle, assuming that no operator action is performed to mitigate the consequences of the xenon oscillation. However, free xenon oscillations are not permitted 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 offset be inadvertently permitted to move far enough outside the control band due to an axial xenon oscillation, or any other reason, the protection limit on axial offset will be reached and the power will be automatically reduced.

An example of the axial offset behavior of a xenon transient used in the determination of xenon stability in reload cores is shown in Figure 4-87. This transient was induced by a 10% power reduction from full power, and was performed at cycle average burnups corresponding to BOC, MOC and EOC. The xenon transients shown in Figure 4-87 assume that control rods were held at their initial position for the duration of the transient.

2. Radial Power Distribution

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

The X-Y stability of large PWRs has been further verified as part of the startup physics test program for PWR cores with 193 fuel assemblies. The measured X-Y stability of the PWR core with 157 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, actions can be taken 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 asymmetric perturbations in the X-Y power distribution that could result from equipment malfunctions are made in the protection system design. These include control rod drop, rod misalignment and asymmetric loss of coolant flow.

A more detailed discussion of the power distribution control in PWR cores is presented in 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 5.4.3.7.

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 or advanced nodal 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. Outside the active core, methods such as those which use multigroup space dependent slowing down codes described in Section 5.4.3.7 are used. Regionwise power sharing information from the core calculations is often used as reference source data for the multigroup codes.

The neutron flux distribution and spectrum in the various structural components varies significantly from the core to the pressure vessel.

As discussed in Section 5.4.3.7, the irradiation surveillance program utilizes actual test samples to verify the accuracy of the calculated fluxes at the vessel.

4.3.3 Analytical Methods

The CASMO-3/TABLES-3/SIMULATE-3P methodology and CASMO-4/CMS-LINK/SIMULATE-3 methodology have been approved for use in the nuclear design of a reactor core. These codes were used to generate few group constants and reactor models which can accurately predict the behavior of the reactor core in either two or three dimensions. A description of the methodologies and computer codes used in the evaluation of the reactor core designs are described in the topical reports titled "Nuclear Physics Methodology for Reload Design" (Reference 4), "Nuclear Design Methodology Using CASMO-3/SIMULATE-3P" (Reference 53), "Nuclear Design Methodology using CASMO-4/SIMULATE-3 MOX" (Reference 52), and also in References 48 and 64. These methodologies are NRC approved to perform nuclear design analyses and either CASMO-3P/SIMULATE-3P (Reference 53) or CASMO-4/SIMULATE-3 MOX (Reference 52) methods are applicable to low enriched uranium (LEU) fuel analyses. The transition to CASMO-4/SIMULATE-3 MOX method is required to model cores containing mixed oxide fuel (MOX) within the limitations specified in the safety evaluation contained in Reference 52 or to model cores with fuel containing gadolinia. The CASMO-4/SIMULATE-3 methodology is also applicable to cores containing only LEU fuel, and is required to utilize the conditional exemption of the EOC MTC measurement method described in DPC-NE-1007-PA Reference 57). Use of the CASMO-4/SIMULATE-3 metodology is required for the conditional EOC MTC exemption methodology because results from CASMO-4/SIMULATE-3 calculations form the basis of the measured-to-predicted EOC MTC uncertainty used by this method.

Since many generic analyses were performed with CASMO-3/SIMULATE-3P methods, this methodology will be retained in the UFSAR even after transition. An overview of the nuclear design analyses performed as part of the licensing basis of each reload core design follows. Details pertaining to the analyses performed can be found in the referenced topical reports.

The design of a reload core initially requires the development of a preliminary loading pattern which satisfies desired energy, feed batch size and enrichment requirements. Following this initial step, analyses are performed to ensure that applicable safety, fuel mechanical and thermal limits are also satisfied. Calculation of these limits are performed using NRC approved thermal hydraulic, system thermal hydraulic (e.g. RETRAN) and space-time kinetics transient analysis codes. A conservative set of safety, mechanical or thermal limits are determined and assured through the selection of conservative initial conditions, boundary conditions, code options, key physics parameters and core thermal hydraulic models. Key physics parameters, which are identified for each analysis, are calculated for each reload core and verified to be bounded by the values used in the licensing analysis. The confirmation of cycle-specific values of the key physics parameters relative to the values used in the licensing analysis ensures that the analyses performed to establish safety, mechanical and thermal limits bound the reload core. The method employed to select the key physics parameters important to each Chapter 15 event are described in References 48 and 51.

4.3.3.1 Computer Codes for ARMP Methodology

The ARMP Methodology, consisting of computer codes EPRI ARMP PDQ and EPRI-NODE-P (NODE), are associated with a legacy methodology that is obsolete and no longer used in Duke reload design analysis. Therefore the description of the computer codes for ARMP methodology has been deleted.

4.3.3.2 Computer Codes for CASMO-3/SIMULATE-3P Methodology

The methodology used to perform reload design nuclear calculations is based on CASMO-3 and SIMULATE-3P. The computer codes used are described as follows.

CASMO-3 is a multigroup two-dimensional transport theory code for fuel assembly burnup calculations. It uses a library of 40 or 70 energy group cross sections based primarily on the ENDF/B-IV data base. Certain data used in the CASMO-3 library, such as the Xe-135 yields and fission spectra data for U-235 and Pu-239, are taken from ENDF/B-V. This code produces two-group cross sections, assembly discontinuity factors, fission product data, detector reaction rates, and pin power data. The data from CASMO-3 is reformatted into two- or three-dimensional tables using a data processing program, TABLES-3 for input to the three-dimensional code SIMULATE-3P. SIMULATE-3P interpolates the data from TABLES-3 for the independent variables for certain core conditions that SIMULATE-3P models.

SIMULATE-3P is a two-group three-dimensional coarse mesh diffusion theory code based on the QPANDA neutronics model. SIMULATE-3P accounts for the effects of fuel and moderator temperature feedback using its nodal thermal-hydraulics model. The program explicitly models the baffle and reflector region. The program uses data from CASMO-3 for each pin in the fuel assembly and uses inter-assembly and intra-assembly data obtained from the coarse mesh solution to reconstruct the power distribution for each pin.

The primary uses of this program include the calculation of critical boron concentrations, control rod worths, reactivity coefficients, boron worths, kinetics data and the time dependent behavior of the xenon distribution following a change in reactor power, or perturbation in the three-dimensional power distribution. Shutdown margin, and ejected and stuck rod worth calculations are also performed with this code.

The capability of the SIMULATE-3P code to predict measured power distributions has been demonstrated by comparisons between measured and predicted power distributions as described in References 53 and 64. The capability of SIMULATE-3P to predict pin power distributions has been demonstrated through comparison of measured and predicted pin powers for the B&W critical experiments. These comparisons are also described and discussed in Reference 53.

Predicted versus measured reactivity comparisons are contained in Reference 53 and are also performed as part of the startup and physics testing program at the beginning of each cycle. The predictive capability of SIMULATE-3P is also assessed through core follow power distribution and critical boron concentration comparisons and the evaluation of startup conditions following a reactor trip.

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

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

±2 pcm/°F for moderator temperature coefficient

±50 ppm for critical boron concentration with depletion

±3 percent for power distributions

±0.2 percent Δρ for rod bank worth

±4 pcm/step for the differential rod worth

±0.5 pcm/ppm for boron worth

±0.1 percent Δρ for the moderator defect

4.3.3.3 Computer Codes for CASMO-4/SIMULATE-3 MOX Methodology

Another methodology used to perform reload design nuclear calculations is based on CASMO-4 and SIMULATE-3 MOX. This methodology is similar to CASMO-3/SIMULATE-3P methodology described in Section 4.3.3.2 with additional capabilities included to model mixed oxide (MOX) fuel. The CASMO-4/SIMULATE-3 MOX methodology is also used to model reactor cores with fuel containing gadolinia. The computer codes used are described as follows.

CASMO-4 is a multigroup two-dimensional transport theory code for fuel assembly burnup calculations. It uses a library of 70 energy group cross sections based primarily on the ENDF/B-IV data base. Certain data used in the CASMO-4 library, such as the Xe-135 yields, and fission spectra data for U-235 and Pu-239, as well as data for Ag, Gd, Er, and Tm are taken from ENDF/B-V. Data for Pu-241 was taken from JENDL-2. This code produces two-group cross sections, assembly discontinuity factors, fission product data, detector reaction rates, and pin power data. The data from CASMO-4 is reformatted into two- or three-dimensional tables using a data processing program, CMS-LINK, for input to the three-dimensional code SIMULATE-3 MOX. SIMULATE-3 MOX interpolates the data from CMS-LINK for the independent variables for certain core conditions that SIMULATE-3 MOX models.

SIMULATE-3 MOX is a two-group three-dimensional coarse mesh diffusion theory code based on the QPANDA neutronics model. SIMULATE-3 MOX includes enhancements to model the steep thermal flux gradient between MOX and LEU fuel and is applicable for analysis of cores containing LEU fuel with and without gadolinia or cores containing LEU and MOX LTA fuel. SIMULATE-3 MOX accounts for the effects of fuel and moderator temperature feedback using its nodal thermal-hydraulics model. The program explicitly models the baffle and reflector region. The program uses data from CASMO-4 for each pin in the fuel assembly and uses inter-assembly and intra-assembly flux data obtained from the coarse mesh solution to reconstruct the power distribution for each pin.

The primary uses of this program include the calculation of critical boron concentrations, control rod worths, reactivity coefficients, boron worths, kinetics data and the time dependent behavior of the xenon distribution following a change in reactor power, or perturbation in the three-dimensional power distribution. Shutdown margin, and ejected and stuck rod worth calculations are also performed with this code.

The capability of the SIMULATE-3 MOX code to predict measured power distributions in LEU, gadolinia, and MOX core designs has been demonstrated by comparisons between measured and predicted power distributions as described in Reference 52. The capability of SIMULATE-3 MOX to predict pin power distributions has been demonstrated through comparison of measured and predicted pin powers for the B&W critical experiments for LEU fuel, LEU fuel containing gadolinia, and three MOX critical experiments for MOX fuel. These comparisons are described in Reference 52.

Predicted versus measured reactivity comparisons are contained in Reference 52 and are also performed as part of the startup and physics testing program at the beginning of each cycle. The predictive capability of SIMULATE-3 MOX is also assessed through core follow power distribution and critical boron concentration comparisons and the evaluation of startup conditions following a reactor trip.

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

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

±2 pcm/°F for moderator temperature coefficient

±50 ppm for critical boron concentration with depletion

±3 percent for power distributions

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

±4 pcm/step for the differential rod worth

±0.5 pcm/ppm for boron worth

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

4.3.4 Fuel Temperature (Doppler) Calculations UFSAR Section was removed per 2003 Update. See Section 4.3.5 Changes for details.

4.3.5 Changes

[Include a discussion of changes, as required by Reg Guide 1.70, Standard Format.]

Section 4.3.4 Fuel Temperature (Doppler) Calculations was removed for the following reasons:

1. It is not a major area of nuclear design, such as Section 4.3.1 – Design Bases, Section 4.3.2 – Description, and Section 4.3.3 – Analytical Methods. It is not required as part of Reg Guide 1.70 standard format.
2. The utilization of fuel temperature data in a reactor physics code should appropriately characterize core reactivity and feedback effects. The primary function of a fuel performance code is to conservatively characterize the fuel temperature from a mechanical point of view. Although a fuel performance code may be used to develop fuel temperature data for use in a reactor physics code, it is intended that the use of fuel temperature data will appropriately characterize neutronic behavior.
3. Topical Report DPC-NF-2010, contained a description of the generation of fuel temperature data used as input to the neutronics code. This statement was removed, and the NRC approved Revision 2 of this topical report by letter dated June 24, 2003.
4. The fuel temperature data used within SIMULATE-3 is developed from data derived from the fuel rod thermal model within SIMULATE-3K.

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4.4 Thermal and Hydraulic Design

Thermal-hydraulic characteristics and analysis methods utilized in the licensing evaluation of the RFA are given in Reference 95.

This section will discuss the thermal-hydraulic characteristics of the RFA 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 performance and safety criteria requirements are met:

1. Fuel damage (defined as penetration of the fission product barrier, i.e., the fuel rod clad) is not expected during normal operation and operational transients (Condition I) or any transient conditions arising from faults of moderate frequency (Condition II). It is not possible, however, to preclude a very small number of rod failures. These will be within the capability of the plant cleanup system and are consistent with the plant design bases.
2. The reactor can be brought to a safe state following a Condition III event with only a small fraction of fuel rods damaged (see above definition) although sufficient fuel damage might occur to preclude resumption of operation.
3. The reactor can be brought to a safe state and the core can be kept subcritical with acceptable heat transfer (i.e., coolable) geometry following transients arising from Condition IV events.

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

4.4.1.1 Departure from Nucleate Boiling Design Basis

Basis

There will be at least a 95 percent probability that departure from nucleate boiling (DNB) will not occur on the limiting fuel rods during normal operation and operational transients and any transient conditions arising from faults of moderate frequency (Condition I and II events), at a 95 percent confidence level. Historically, this criterion has been conservatively met by adhering to the following thermal design basis: there must be at least a 95 percent probability that the minimum departure from nucleate boiling ratio (DNBR) of the limiting power rod during Condition I and II events is greater than or equal to the DNBR limit of the DNB correlation being used. The DNBR limit for the correlation is established based on the variance of the correlation such that there is a 95 percent probability with 95 percent confidence that DNB will not occur when the calculated DNBR is at the DNBR limit.

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RFA Design Discussion

The DNBR analysis limit is determined in part by the Critical Heat Flux (CHF) correlation used. DPC's analysis of the robust fuel assembly design uses the WRB-2M CHF correlation which has a correlation limit of 1.14 (Reference 97). The CHF correlation limit is the Technical Specification Safety Limit (2.1.1.1) to prevent DNB from occurring.

The design method employed to meet the DNB design basis is the Statistical Core Design Methodology (Reference 95). Uncertainties in plant operating parameters, nuclear and thermal parameters, fuel fabrication parameters, and the CHF correlation are considered statistically to determine a statistical DNBR limit (SDL). Since the parameter uncertainties are considered in determining the statistical DNBR limit, the safety analyses using the SDL are performed using values of input parameters without uncertainties. For this application the statistical DNBR value is 1.30.

General Discussion

By preventing departure from nucleate boiling (DNB), adequate heat transfer is assured between the fuel cladding and the reactor coolant, thereby preventing cladding damage as a result of inadequate cooling. Maximum fuel rod surface temperature is not a design basis as it will be within a few degrees of coolant temperature during operation in the nucleate boiling region. Limits provided by the nuclear control and protection systems are such that this design basis will be met for transients associated with Condition II events including overpower transients. There is an additional large DNBR margin at rated power operation and during normal operating transients (see item 6 in Table 4.1).

In addition to the above considerations, additional margin is included in the present safety analyses. In particular, the Design DNBR value of 1.45 or greater (WRB-2M correlation) is employed in safety analyses as the limiting value against which transients are checked. The margin available between the Design DNBR used in the safety analyses and the SDL is not required to meet the design basis discussed earlier. This margin can be used for flexibility in the design, operation, and analyses of the plant. For instance, the margin may be used for improved fuel management or increased plant availability.

For the purpose of determining the amount of DNBR margin available to offset DNBR penalties the following relationship must be applied:

$$\%\text{DNBR Margin} = \left(\frac{\text{Design DNBR Limit}}{\text{Statistical DNBR Limit}} - 1\right) \text{x}\ 100\%$$

Note: Equation above added per 2000 Update.

For example, for McGuire with the WRB-2M correlation, the statistical DNBR limit is 1.3 while the Design DNBR limit is 1.45 or greater and thus the DNBR margin is 11.5% or greater.

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% 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 for RFA analyses is given in Reference 99. By precluding UO_2 melting, the fuel geometry is preserved and possible adverse effects of molten UO_2 on the cladding are eliminated.

Discussion

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

4.4.1.3 Core Flow Design Basis

Basis

A minimum of 91.6 percent of the thermal flow rate will pass through the fuel rod region of the core and be effective for fuel rod cooling. Coolant flow through the thimble tubes as well as the leakage from the core barrel-baffle region into the core are not considered effective for heat removal.

Discussion

Core cooling evaluations are based on the thermal flow rate (minimum flow) entering the reactor vessel. A maximum of 8.4 percent of this value is allotted as bypass flow. This includes rod cluster control guide thimble cooling flow, head cooling flow, baffle leakage, and leakage to the vessel outlet nozzle.

The bypass flow calculation includes effects on other flow paths due to the overall increased hydraulic resistance of the RFA fuel due to intermediate flow mixing (IFM) grids. The core bypass flow will be verified on a cycle specific basis.

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 that additional limits are not required.

Fuel rod diametral gap characteristics, moderator-coolant flow velocity and distribution, and moderator void are not inherently limiting. Each of these parameters is incorporated into the thermal and hydraulic models used to ensure the above mentioned design criteria are met. For instance, the fuel rod diametral gap characteristics change with time (see Section 4.2.1.3.1) and the fuel rod integrity is evaluated on that basis. The effect of the moderator flow velocity and distribution (see Section 4.4.4.2.2) and moderator void distribution (see Section 4.4.2.4) are included in the core thermal (VIPRE-01) evaluation and thus affect the design bases.

Meeting the fuel clad integrity criteria covers possible effects of clad temperature limitations. As noted in Section 4.2.1.3.1, the fuel rod conditions change with time. A single clad temperature limit for Condition I or Condition II events is not appropriate since out of necessity it would be overly conservative. A clad temperature limit is applied to the loss of coolant accident (Chapter 15), control rod ejection accident, and locked rotor accident.

4.4.2 Description of Thermal and Hydraulic Design of the Core

4.4.2.1 Summary Comparison

Table 4-1 provides the design parameters for the RFA design.

Transition to RFA fuel began with cycle 15 for unit 1 and cycle 14 for unit 2.

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The RFA fuel design is based on the VANTAGE + fuel assembly design, licensed by the NRC in Reference 96. The VANTAGE + design evolved from the VANTAGE 5 & VANTAGE 5H fuel assembly designs. The RFA design used at McGuire is described in Section 4.1.

Both the average and peak linear heat generation rates (kW/ft) and heat fluxes remain the same for both fuel designs at rated thermal power.

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

The minimum DNBRs for the rated power, and anticipated transient conditions are given in Table 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 the following Sections: 4.4.2.2.1 and 4.4.2.2.2. The VIPRE-01 computer code (See References 85 and 87) 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

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<u>Robust Fuel Assembly CHF Correlation</u>

The WRB-2M CHF correlation (Reference 97) was developed by Westinghouse. The CHF correlation was developed from CHF data (241 points) on various test sections. These test sections simulated geometry and grid designs for both IFM and non-IFM heated spans. The WRB-2M CHF correlation (Reference 97) is appropriate for use in thermal-hydraulic analyses for the RFA. The WRB-2M correlation is applicable to the following range of variables:

Pressure	$1495 \leq P \leq 2425$ psia
Local Mass Velocity	$0.97 \leq G_{loc} \leq 3.1$ Mlbm/ft^2-hr
Local Quality	$-0.1 \leq X_{loc} \leq 0.29$
Heated Length	$L_h \leq 14$ feet
Grid Spacing	$10 \leq g_{sp} \leq 20.6$ inches
Equivalent Hydraulic Diameter	$0.37 \leq d_e \leq 0.46$ inches
Equivalent Heated Diameter	$0.46 \leq d_h \leq 0.54$ inches

The WRB-2M correlation will be used for McGuire RFA DNB analyses.

4.4.2.2.2 Definition of Departure from Nucleate Boiling Ratio (DNBR)

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<u>Robust Fuel Assembly Design</u>

The DNB heat flux ration (DNBR) as applied to the robust fuel assembly design for both the typical and thimble cold wall cells is:

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

where:

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

Note: The above equation was added in 2001 update.

and q”DNB,EU is the uniform critical heat flux as predicted by the WRB-2M DNB correlation (See Reference 97).

F is the flux shape factor to account for nonuniform axial heat flux distribution (See Reference 97).

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 thermal diffusion coefficient (TDC) which is defined as:

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

where:

w' = flow exchange rate per unit length, lbm/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 thermal-hydraulic analysis for determining the overall mixing effect or heat exchange rate is presented in Reference 7.

As a part of an ongoing research and development program, Westinghouse sponsored and directed mixing tests at Columbia University (Reference 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 PWR core under the following single and two phase (subcooled boiling) flow conditions:

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

TDC is determined by comparing the thermal hydraulic code predictions with the measured subchannel exit temperatures. Data for 26-inch axial grid spacing are presented in Figure 4-79 where the thermal diffusion coefficient is plotted versus the Reynolds number. TDC is found to be independent of Reynolds number, mass velocity, pressure, and quality over the ranges tested. The two-phase data (local, subcooled boiling) fell within the scatter of the single-phase data. The effect of two-phase flow on the value of TDC has been demonstrated by Cadek (Reference 13), Rowe and Angle (References 14 and 15), and Gonzalez-Santalo and Griffith (Reference 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 PWR reactor core geometry, the value of TDC increased with quality to a point and then decreased, but never below the single-phase value. Gonzalez-Santalo and Griffith showed that the mixing coefficient increased as the void fraction increased.

The data from these tests on the "R" grid showed that a design TDC value of 0.038 (for 26 inch-grid spacing) can be used in determining the effect of coolant mixing. This value for the TDC of 0.038 is also applicable to analyses of either RFA or Mark-BW fuel assemblies with the VIPRE-01 code.

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 (Reference 17). The mean value of TDC obtained from these tests was 0.059 and all data were well above the current design value of 0.038.

In addition, since the actual reactor grid spacing is approximately 20 inches for the RFA fuel design and 10 inches in the IFM grid spans for the RFA design, additional margin is available for this design, as the value of TDC increases as grid spacing decreases as per Reference 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 considers a nuclear hot channel factor (see Section 4.4.4.3.1) describing the neutron power distribution and an engineering hot channel factor, which allows for variations in flow conditions and fabrication tolerances. The engineering hot channel factors are made up of subfactors which account for the influence of the variations of fuel pellet diameter, density, enrichment and eccentricity; fuel rod diameter, pitch, and bowing; inlet flow distribution; flow redistribution; and flow mixing.

Heat Flux Engineering Hot Channel Factor, F_Q^E

The kW/ft engineering hot channel factor is used to evaluate the maximum linear heat generation rate in the core. This subfactor is determined by statistically combining the fabrication variances for the fuel pellet diameter, density, and enrichment and has a value of 1.03 at the 95 percent probability level with 95 percent confidence. No DNB penalty is taken for the short relatively low intensity heat flux spikes caused by variations in the above parameters, as well as fuel pellet eccentricity and fuel rod diameter variation (Reference 18).

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

The effect of variations in flow conditions and fabrication tolerances on the hot channel enthalpy rise is directly considered in the VIPRE-01 core thermal subchannel analysis (See Section

4.4.4.5.1) under any reactor operating condition. The items considered contributing to the enthalpy rise engineering hot channel factor are discussed below:

1. Pellet diameter, density, enrichment and fuel rod diameter:

   Variations in pellet diameter, density, enrichment, and fuel rod diameter, are considered statistically in establishing the DNBR limit (see Section 4.4.1.1) for the SCD procedure (Reference 94) employed in this application. Uncertainties in these variables are determined from sampling of manufacturing data.

Inlet Flow Maldistribution:

The effect of inlet flow maldistribution on core thermal performance 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.

Flow Redistribution:

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 non-uniform power distribution is inherently considered in the VIPRE-01 analysis for every operating condition which is evaluated.

Flow Mixing:

The subchannel mixing model incorporated in the VIPRE-01 and used in reactor design is based on experimental data. 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 82 must be accounted for in the DNBR safety analysis of Condition I and Condition II events for each plant application. Applicable generic credits for margin resulting from retained conservatism in the evaluation of DNBR and/or margin obtained from measured plant operating parameters (such as $F^N_{\Delta H}$ or core flow) which are less limiting than those required by the plant safety analysis can be used to offset the effect of rod bow.

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Robust Fuel Assembly Design

The rod bow DNB penalty is calculated using WCAP-8691, Rev. 1 methodology (Reference 82). The rod bow penalty is applied to the DNBR margin.

4.4.2.3 Linear Heat Generation Rate

The core average and maximum LHGRs are given in Table 4-1. The method of determining the maximum LHGR is given in Section 4.2.1.3.

4.4.2.4 Void Fraction Distribution

The calculated core average and the hot subchannel maximum and average void fractions are presented in Table 4-21 for operation at full power with design hot channel factors. The void models used in the VIPRE-01 computer 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, the ability of the thermal hydraulic code to represent the hydraulic conditions is important. VIPRE-01 (Reference 87) has been extensively benchmarked against measured data for a wide variety of fluid conditions.

The results of this evaluation show that because of the realistic crossflow model used in VIPRE-01, the minimum DNBR in the hot channel is adequately modeled.

4.4.2.5 Core Coolant Flow Distribution

The reactor coolant inlet flow distribution is a function of the flow profile exiting the lower internals of the reactor vessel. Once entering the fuel assemblies, the inlet flow distribution (pressure drop) and the radial core power distribution (density head) together determine the magnitude and direction of the crossflow. In the open lattice construction of a PWR core, there is lateral communication between all individual subchannels as well as fuel assemblies. Therefore while localized pressure or density differences can exist, their effect is mitigated by the inherent characteristics of fluid flow. Due to this, no significant changes occur during core life and no orificing is necessary in the reactor design.

4.4.2.6 Core Pressure Drops and Hydraulic Loads

4.4.2.6.1 Core Pressure Drops

The analytical model and experimental data used to calculate the pressure drops shown in Table 4-1 are described in Section 4.4.2.7.2. The core pressure drop includes the fuel assembly, lower core plate, and upper core plate pressure drops. The full power operation pressure drop values shown in Table 4-1 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. This section also defines and describes the thermal design flow (minimum flow) which is the basis for reactor core thermal performance and the mechanical design flow (maximum flow) which is used in the mechanical design of the reactor vessel internals and fuel assemblies. Since the best estimate flow is that flow which is most likely to exist in an operating plant, the calculated core pressure drops in Table 4-1 are based on this best estimate flow rather than the thermal design flow.

The pressure drops quoted in Table 4-1 for the RFA are based on twelve grids. Reference 98 states that the addition of three IFM grids resulted in an increase in the overall pressure drop in a VANTAGE 5 core when compared to the 17x17 OFA design. The increase in overall pressure drop for a 17x17 RFA and VANTAGE 5 cores are comparable.

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

4.4.2.6.2 Hydraulic Loads

The fuel assembly hold down springs are designed to keep the fuel assemblies in contact with the lower core plate under all Condition I and II events with the exception of the turbine overspeed transient associated with a loss of external load. The hold down springs are designed to tolerate the possibility of an over-deflection associated with fuel assembly liftoff for this case and provide contact between the fuel assembly and the lower core plate following this transient. More adverse flow conditions occur during a loss of coolant accident. These conditions are presented in Chapter 15.

Hydraulic loads at normal operating conditions are calculated considering the mechanical design flow which is described in Section 5.1 and accounting for the minimum core bypass flow based on manufacturing tolerances. Core hydraulic loads at cold plant startup conditions are based on the cold mechanical design 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 20 percent greater than the mechanical design flow, are evaluated to be approximately twice the fuel assembly weight.

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VANTAGE + fuel assemblies were separately tested for hydraulic load capability and the results are reported in Reference 96. The RFA design is similar to the VANTAGE + design with respect to the hydraulic load capability.

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 (Reference 87), with the properties evaluated at bulk fluid conditions:

$$\frac{hD_e}{K} = 0.023 \; \frac{D_e G}{\mu} \; 0.8 \; \frac{C_p \mu}{K} \; 0.4$$

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, lbm/hr-ft$_2$

μ = dynamic viscosity, lbm/ft-hr

Cp = heat capacity, BTU/lb$_m$-°F

This correlation has been shown to be conservative (Reference 87) for rod bundle geometries with pitch to diameter ratios in the range used by PWRs. This conservatism is also validated in Reference 103.

The onset of nucleate boiling occurs when the clad wall temperature reaches the amount of superheat predicted by Thom's (Reference 87) 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

The Thom corelation is also defined in Reference 103.

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 4-21). Two phase flow effects are considered in the core thermal subchannel analyses using the models as discussed in Section 4.4.4.2.3. Core and vessel pressure losses are calculated by equations of the form:

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

Note: The equation above was revised for 2000 Update.

where:

ΔP_L = unrecoverable pressure drop, lbf/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 $\frac{lb_m - ft}{lb - sec^2}$

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 4-1 for unrecoverable pressure loss across the reactor vessel, including the inlet and outlet nozzles, and across the core. The pressure drop for the vessel was calculated based on as-built vessel geometry.

Tests of the primary coolant loop flow rates were 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

There are three separate void regions considered in flow boiling in a PWR as illustrated in Figure 4-83. They are the wall void region (no bubble detachment), the subcooled boiling region (bubble detachment) and the bulk boiling region.

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Robust Fuel Assembly

In the wall void region, the point where local boiling begins is determined when the clad temperature reaches the amount of superheat predicted by Thom's (Reference 87) correlation (discussed in Section 4.4.2.7.1). The void fraction in this region is calculated using EPRI's (Reference 87) relationship. The bubble detachment point, where the superheated bubbles break away from the wall, is determined by using the EPRI subcooled boiling model (Reference 87).

The void fraction in the subcooled boiling region (that is, after the detachment point) is calculated from the EPRI (Reference 87) correlation. This correlation predicts the void fraction from the detachment point to the bulk boiling region.

The void fraction in the bulk boiling region is calculated from the EPRI (Reference 87) correlation. This model accounts for the effect of phase slip on void fraction.

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 insures 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), specific protection functions are provided as described in Section 7.2 and the use of these protection functions is described in Chapter 15. The thermal response of the fuel is discussed in Section 4.2.1.3.1.

4.4.2.9 Uncertainties in Estimates

4.4.2.9.1 Uncertainties in Fuel and Clad Temperatures

Fuel temperatures are 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.

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RFA

Uncertainties in the calculation of the fuel temperatures have been quantified by comparison of the thermal model to the inpile thermocouple measurements, References 31 through 37, by out-of-pile measurements of the fuel and clad properties, References 38 through 49, 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 50. Uncertainties are accounted for in the RFA fuel melt analysis as explained in Section 4.2.1.3.1.1.

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

4.4.2.9.2 Uncertainties in Pressure Drops

Core and vessel pressure drops based on the best estimate flow, as described in Section 5.1, are quoted in Table 4-1. The uncertainties quoted are based on the uncertainties in 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 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 (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. The WRB-2M correlation uncertainty used in DNB analyses is given in Reference 97.

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 nuclear peaking factors are accounted for by applying conservatively high values of the nuclear peaking factors and including measurement error allowances in the statistical evaluation of the limit DNBR (see Section 4.4.1.1) using the SCD procedure (References 85 and 94). In addition, engineering hot channel factors are included in the SCD procedure as discussed in Section 4.4.2.2.4.

The results of a sensitivity study (Reference 85) with VIPRE-01 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 computer code to accurately predict flow and enthalpy distributions in rod bundles is discussed in Reference 87. Studies have been performed (Reference 85) to determine the sensitivity of the minimum DNBR in the hot channel to the void fraction correlation (see also Section 4.4.2.7.3); the inlet velocity distribution assumed boundary conditions for the analysis; and the turbulent momentum factor. The results of these studies show that the minimum DNBR in the hot channel is relatively insensitive to variations in these parameters. The range of variations considered in these studies covered the range of possible variations in these parameters. An uncertainty of 5% in DNBR is included in the SCD procedure to account for any VIPRE-01 Code uncertainty.

4.4.2.9.6 Uncertainties in Flow Rates

The uncertainties associated with loop flow rates are discussed in Section 5.1. A thermal design flow is defined for use in core thermal performance evaluations which accounts for both prediction and measurement uncertainties. In addition, a maximum of 8.4 percent of the Thermal Design Flow is assumed to be ineffective for core heat removal capability because it bypasses the core through the various available vessel flow paths described in Section 4.4.4.2.1.

4.4.2.9.7 Uncertainties in Hydraulic Loads

As discussed in Section 4.4.2.6.2, hydraulic loads on the fuel assembly are evaluated for a pump overspeed transient which creates flow rates 20 percent greater than the mechanical design flow. The mechanical design flow as stated in Section 5.1 is greater than the best estimate or most likely flow rate value for the actual plant operating condition.

4.4.2.9.8 Uncertainty in Mixing Coefficient

The value of the mixing coefficient, TDC, used in VIPRE-01 analyses for this application is 0.038. The mean value of TDC obtained in the "R" grid mixing tests described in Section 4.4.2.2.3 was 0.042 (for 26-inch grid spacing). The value 0.038 is one standard deviation below the mean value; and approximately 90 percent of the data gives values of TDC greater than 0.038 (Reference 13).

The results of the mixing tests done on 17 x 17 geometry with 26 inch grid spacing, as discussed in Section 4.4.2.2.3, had a mean value of TDC of 0.059 and standard deviation of σ = 0.007. Hence the current design value of TDC is almost 3 standard deviations below the mean for 26-inch grid spacing.

In addition, since the actual grid spacing is approximately 20 inches for the RFA fuel design and 10 inches in the IFM grid spans for the RFA design, additional margin is available for this design, since the value of TDC increases as grid spacing decreases (Reference 13).

4.4.2.10 Flux Tilt Considerations

Significant quadrant power tilts are not anticipated during normal operation since this phenomenon is caused by some perturbation (for example, a dropped or misaligned rod cluster control assembly could cause changes in hot channel factors); however, these events are analyzed separately in Chapter 15. Other possible causes for quadrant power tilts include x-y xenon transients, inlet temperature mismatches, or enrichment variations within tolerances.

In addition to unanticipated quadrant power tilts as described above, other readily explainable asymmetries may be observed during calibration of the excore detector quadrant power tilt alarm. During operation, incore maps are taken at least once per month and, periodically, additional maps are obtained for calibration purposes. Each of these maps is reviewed for deviations from the expected power distributions. Asymmetry in the core, from quadrant to quadrant, is frequently a consequence of the design when assembly and/or components shuffling and rotation requirements do not allow exact symmetry preservation. In each case, the acceptability of an observed asymmetry, planned or otherwise depends solely on meeting the required accident analyses assumptions.

In practice, once acceptability has been established by review of the incore maps, the quadrant power tilt alarms and related instrumentation are adjusted to indicate zero Quadrant Power Tilt Ratio as the final step in the calibration process. This action ensures that the instrumentation is correctly calibrated to alarm in the event an unexplained or unanticipated change occurs in the quadrant to quadrant relationships between calibration intervals. Proper functioning of the quadrant power tilt alarm is significant because allowances are only made in design analyses of quadrant power tilts up to 2.0%. Finally in the event that unexplained flux tilts (as indicated by excore instrumentation) do occur, the Improved Technical Specifications (Section 3.2.4) provide appropriate corrective actions to ensure continued safe operation of the reactor.

4.4.2.11 Fuel and Cladding Temperatures

A discussion of Fuel and Cladding Temperatures and associated parameters is presented in Section 4.2. A discussion of fuel clad integrity is presented in Section 4.2.1.3.1.

4.4.3 Description of the Thermal and Hydraulic Design of the Reactor Coolant System

4.4.3.1 Plant Configuration Data

Plant configuration data for the thermal-hydraulic and fluid systems external to the core are provided in the appropriate Chapter 5, Chapter 6, and Chapter 9. Implementation of the Emergency Core Cooling System (ECCS) is discussed in Chapter 15. Some specific areas of interest are the following:

1. Total coolant flow rates for the Reactor Coolant System (RCS) and each loop are provided in Table 5-1. Flow rates employed in the evaluation of the core are presented in Section 4.4.
2. Total RCS volume including pressurizer and surge line, RCS liquid volume including pressurizer water at steady state power conditions are given in Table 5-1.
3. The flow path length through each volume may be calculated from physical data provided in the above referenced tables.
4. The height of fluid in each component of the RCS may be determined from the physical data presented in Section 5.4. The components of the RCS are water filled during power operation with the pressurizer being approximately 60 percent water filled.
5. Components of the ECCS are to be located so as to meet the criteria for net positive suction head described in Section 6.3.
6. Line length 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.
7. The parameters for components of the RCS are presented in Section 5.4, component and subsystem design.
8. The steady state pressure drops and temperature distributions through the RCS are presented in Table 5-1.

4.4.3.2 Operating Restrictions on Pumps

The minimum Net Positive Suction Head (NPSH) and minimum seal injection flow rate must be established before operating the reactor coolant pumps. With the minimum labyrinth seal injection flow rate established, the operator will have to verify that the system pressure satisfies NPSH requirements.

4.4.3.3 Power-Flow Operating Map (BWR)

Not applicable to Pressurized Water Reactors.

4.4.3.4 Temperature-Power Operating Map

The relationship between Reactor Coolant System temperature and power for nominal power operation is shown in Figure 5-19.

The above referenced figure is for general information. Calculational sources should be consulted for actual predicted behavior and/or operational limits. In addition, the figure does not reflect operation under a reduced T-average coastdown scheme.

The effects of reduced core flow due to inoperative pumps is discussed in Sections 15.2.5 and 15.3.3.4. Natural circulation capability of the system is shown in Table 15.2-2.

4.4.3.5 Load Following Characteristics

The Reactor Coolant System 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. Operation with one pump out of service requires adjustment only in Reactor Trip System setpoints as discussed in Section 7.2.

4.4.3.6 Thermal and Hydraulic Characteristics Summary Table

The thermal and hydraulic characteristics are given in Table 4-1 and Table 4-4.

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 4.4.2.2.1.

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:

1. Flow through the spray nozzles into the upper head for head cooling purposes.
2. Flow entering into the RCC guide thimbles to cool the control rods.
3. Leakage flow from the vessel inlet nozzle directly to the vessel outlet nozzle through the gap between the vessel and the barrel.
4. Flow 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 calculated value of core bypass flow for the McGuire plant does not exceed 8.4 percent of the total vessel flow. The core bypass flow will be verified on a cycle specific basis.

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 have been considered from several 1/7 scale hydraulic reactor model tests, References 24, 25, and 64, in arriving at the core inlet flow maldistribution criteria to be used in the VIPRE-01 analyses (see Section 4.4.4.5.1). VIPRE-01 analyses made, using these data, have indicated that a conservative design basis is to consider 5 percent reduction in the flow to the hot assembly.

The experimental error estimated in the inlet velocity distribution has been considered as outlined in Reference 85, where the sensitivity of changes in inlet velocity distributions to hot channel thermal performance is shown to be small.

The effect of the total flow rate on the inlet velocity distribution was studied in the experiments of Reference 24. As was expected, on the basis of the theoretical analysis, no significant variation could be found in inlet velocity distribution with reduced flow rate. All of these conclusions are valid for 17 x 17 RFA fuel.

4.4.4.2.3 Empirical Friction Factor Correlations

Two empirical friction factor correlations are used in the VIPRE-01 computer code.

The friction factor in the axial direction, parallel to the fuel rod axis, is evaluated using the Blasius relation (Reference 87). The code evaluates both the turbulent and laminar equations and selects the maximum value. The relations are:

$$f_{turbulent} = 0.32\,Re^{-0.25}$$

$$f_{laminar} = 64\,Re^{-1.0}$$

Note: The above equations were added in 2000 update.

The loss coefficient for flow in the lateral direction, normal to the fuel rod axis, is calculated by the equation:

$$K_G = 0.5\left(\frac{\text{Channel Centroid Distance}}{\text{Fuel Rod Pitch}}\right)$$

where:

centroid distance is described in Reference 87.

Extensive comparisons of VIPRE-01 predictions using these correlations to experimental data are given in Reference 87, verifying the applicability of these correlations in PWR design.

4.4.4.3 Influence of Power Distribution

The core power distribution, which is largely established at beginning-of-life by fuel enrichment, loading pattern, and core power level is also a function of variables such as control rod worth and position, and fuel depletion throughout lifetime. Radial power distributions in various planes of the core are often illustrated for general interest, however, the core radial enthalpy rise distribution as determined by the integral of power up each channel is of greater importance for DNB analyses. These radial power distributions, characterized by $F^{N}_{\Delta H}$ (defined in Section 4.3.2.2.1) as well as axial heat flux profiles are discussed in the following two sections.

4.4.4.3.1 Radial Power Distribution

Since DNB is a function of the three dimensional core power distribution and magnitude (channel integral enthalpy rise), separation of radial and axial power distributions are necessary for DNB predictions. If the core power level, coolant inlet conditions, and the limiting DNBR are fixed and an axial power distribution selected, the corresponding maximum radial fuel rod power can be calculated. When this calculation is performed for a multitude of axial peak magnitude and locations, a family of curves is generated that show the maximum allowable peaking (MAP) for a statepoint as a function of axial power distribution. The curves, usually expressed in terms of total peaking, are the locus of points where the minimum DNBR is equal to the design limit.

For the radial power distribution component, the magnitude of the peak pin is adjusted during generation of the MAP curves but the relative distribution is preserved. This distribution, called the reference radial peaking, is shown in Reference 85 and is assumed to bound, in terms of DNB performance, actual power distributions that occur during plant operations. MAP limits provide the linkage between the DNBR criteria, the core nuclear power distributions, and the reactor fluid conditions during all the steady state and transient analyses. The MAP curves define the maximum value that the Nuclear Designer can allow peaking to increase to before the 95/95 DNBR criteria is exceeded. This allows the Nuclear Designer to quickly evaluate the core loading pattern for acceptable operation during steady state nominal conditions and during design basis transients.

The MAP curves also define the relationship of increased peaking with reduced power level.

For operation at a fraction P of full power, MAP limits are adjusted by:

$$k = [1 + 0.3(1 - P)]$$

where k is the MAP adjustment factor.

The permitted relaxation of radial peaking is included in the DNB protection setpoints and allows radial power shape changes with rod insertion to the insertion limits (Reference 68), thus allowing greater flexibility in the nuclear design.

4.4.4.3.2 Axial Power Distributions

As discussed in Section 4.3.2.4, the axial power distribution can vary as a result of rod motion, 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 ex-core-nuclear detectors (as discussed in Section 4.3.2.2.7) and protect the core from excessive axial power imbalance.

The reference axial shape used in establishing core DNB limits (over-temperature ΔT protection system setpoints) is a chopped cosine shape with a peak to average value of 1.60. The Reactor Trip System provides automatic reduction of the trip setpoints on excessive axial power imbalance. To determine the magnitude of the setpoint reduction, other axial shapes skewed to the bottom and top of the core are analyzed. The course of those accidents in which DNB is a concern is analyzed in Chapter 15 assuming that the protection set points 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 (see Reference 100 for details).

The initial conditions for the accidents for which DNB protection is required are assumed to be those permissible within the axial offset control strategy employed. 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. The limiting Condition II DNBR transient is analyzed and the system response used in a VIPRE-01 subchannel model to verify acceptable DNB performance. Maximum Allowable Peaking values are calculated for various axial power distributions as described in Section 4.4.4.3.1. The fluid conditions used are taken from the most limiting DNB point during the transient. These design shape evaluations bound all normal operation axial power distributions.

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 4-1 for all loops in operation.

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

4.4.4.5 Analytical Techniques

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, as presented in the Improved Technical Specifications are not exceeded while compounding engineering and nuclear effects. The thermal design considers local variations in dimensions, power generation, flow redistribution, and mixing. VIPRE-01 is a realistic three-dimensional matrix model which has been developed to account for hydraulic and thermal effects on the enthalpy rise in the core (Reference 87). The behavior of the limiting subchannel is determined by analyzing a conservatively flat limiting assembly radial power distribution. The inlet flow velocity, inlet temperature, and exit pressure to the subchannels are given as boundary conditions. After selecting an axial power distribution and core power level, the VIPRE-01 code calculates the subchannel fluid conditions and surface heat flux of the fuel rod at discrete axial levels. Flow redistribution and mixing are calculated by the code and the local variations in fuel assembly dimensions and power generation are accounted for in the SCD procedure. In this manner, the behavior of the limiting subchannel with respect to DNBR is conservatively modeled.

4.4.4.5.2 Steady State Analysis

The VIPRE-01 computer program, as approved by the NRC (Reference 87), is used to determine coolant density, mass velocity, enthalpy, vapor void, static pressure, and DNBR distributions along parallel flow channels within a reactor core under all expected operating conditions. The VIPRE-01 Code is described in detail in References 87 and 85, including models and correlations used. In addition, a discussion on experimental verification of VIPRE-01 is given in Reference 87.

Estimates of uncertainties are discussed in Section 4.4.2.9.

4.4.4.5.3 Experimental Verification

Extensive experimental verification is presented in Reference 87.

The VIPRE-01 analysis is based on a knowledge and understanding of the heat transfer and hydrodynamic behavior of the coolant flow and the mechanical characteristics of the fuel elements. The use of the VIPRE-01 analysis provides a realistic evaluation of the core performance and is used in the thermal analyses as described above.

4.4.4.5.4 Transient Analysis

The VIPRE-01 thermal-hydraulic computer code is also used for transient analysis.

The conservation equations needed for the transient analysis are included in VIPRE-01 by adding the necessary accumulation terms to the conservation equations used in the steady-state (VIPRE-01) analysis. The input description must now include one or more of the following time dependent arrays:

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

At the beginning of the transient, the calculation procedure is carried out as in the steady state analysis. The time is incremented by an amount determined either by the user or by the program itself. At each new time step the calculations are carried out with the addition of the accumulation terms which are evaluated using the information from the previous time step. This procedure is continued until a preset maximum time is reached.

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

The VIPRE-01 Code also has the capability for evaluating fuel rod thermal response.

4.4.4.6 Hydrodynamic and Flow Power Coupled Instability

Boiling flows may be susceptible to thermohydrodynamic instabilities (Reference 70). 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 modes 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 (Reference 70) when the slope of the reactor coolant system pressure drop-flow rate curve ($\partial\Delta P/\partial G|_{internal}$) becomes algebraically smaller than the loop supply (pump head) pressure dropflow rate curve ($\partial\Delta P/\partial G|_{external}$). The criterion for stability is thus $\partial\Delta P/G|_{internal}$ $>\partial\Delta P/\partial G|_{external}$). The Westinghouse pump head curve has a negative slope ($\delta\Delta P/\delta G|_{external < 0}$) whereas the reactor coolant system pressure drop-flow curve has a positive slope ($\delta\Delta P/\delta G|_{internal > 0}$) over the Condition I and Condition II operational ranges. Thus, the Ledinegg instability will not occur.

The mechanism of density wave oscillations in a heated channel has been described by Lahey and Moody (Reference 71). Briefly, an inlet flow fluctuation produces an enthalpy perturbation. This perturbs the length and the pressure drop of the single-phase region and causes quality or void perturbations in the two-phase regions which travel up the channel with the flow. The quality and length perturbations in the two-phase region create two-phase pressure drop perturbations. However, since the total pressure drop across the core is maintained by the characteristics of the fluid system external to the core, then the two-phase pressure drop perturbation feeds back to the single-phase region. These resulting perturbations can be either attenuated or self-sustained.

A simple method has been developed by Ishii (Reference 72) for parallel closed channel systems to evaluate whether a given condition is stable with respect to the density wave type of dynamic instability. This method had been used to assess the stability of typical Westinghouse reactor designs (References 73, 74, and 75), including Virgil C. Summer, under Condition I and II operation. The results indicate that a large margin to density wave instability exists, e.g., increases on the order of 200% of rated reactor power would be required for the predicted inception of this type of instability.

The application of the method of Ishii (Reference 72) 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 and entering 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 (Reference 76) 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 crossflow 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 (Reference 77) analyzed parallel closed channels stability experiments simulating a reactor core flow. These experiments were conducted at pressures up to 2200 psia. The results showed that for flow and power levels typical of power reactor conditions, no flow oscillations could be induced above 1200 psia.

Additional evidence that flow instabilities do not adversely affect thermal margin is provided by the data from the rod bundle DNB tests. Many Westinghouse-type 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 reactor designs. A large power margin, greater than doubling rated power, exists to predicted inception of such instabilities. Analysis has been performed which shows that minor plant to plant differences in flow ratios, fuel assembly length, etc. will not result in gross deterioration of the above power margins. This evaluation is applicable to RFA fuel.

4.4.4.7 Fuel Rod Behavior Effects from Coolant Flow Blockage

Coolant flow blockages can occur within the coolant channels of a fuel assembly or external to the reactor core. The effects of fuel assembly blockage within the assembly on fuel rod behavior are more pronounced than external blockages of the same magnitude. In both cases, the flow blockages cause local reductions in coolant flow. The amount of local flow reduction, where it occurs in the reactor, and how far along the flow stream the reduction persists are considerations which will influence the fuel rod behavior. The effects of coolant flow blockages in terms of maintaining rated core performance are determined both by analytical and experimental methods. The experimental data are usually used to augment analytical tools such as computer programs similar to the VIPRE-01 program. Inspection of the DNB correlation (Section 4.4.2.2 and Reference 97 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 87, 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 reference reactor operating at the nominal full power conditions specified in Table 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 blockages in “open lattice cores” similar to the Westinghouse cores cause flow perturbations, which are local to the blockage. For instance, A. Ohtsubo (Reference 78), et al., show that the mean bundle velocity is approached asymptotically about 4 inches downstream from a flow blockage in a single flow cell. Similar results were also found for 2 and 3 cells completely blocked. P. Basmer (Reference 79), et al., 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 4-1, a reduction in local mass velocity greater than 70 percent would be required to reduce the DNBR to the design 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. Crossflow velocity above the established limits can lead to mechanical wear of the fuel rods at the grid support locations. Fuel rod wear due to flow induced vibration is considered in the fuel rod fretting evaluation for each fuel type.

4.4.5 Testing and Verification

4.4.5.1 Tests Prior to Initial Criticality

A reactor coolant flow test was performed following fuel loading, but prior to initial criticality. Coolant loop pressure drop data is obtained in this test. This data, in conjunction with coolant pump performance information, allows 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. RCS flow rate is re-measured every operating cycle.

4.4.5.2 Initial Power and Plant Operation

Core power distribution measurements are made at several core power levels. These tests are used to insure that conservative peaking factors are used in the core thermal and hydraulic analysis.

4.4.5.3 Component and Fuel Inspections

Inspections performed on the manufactured fuel are delineated in Section 4.2.1.4. Fabrication measurements critical to thermal and hydraulic analyses are obtained to verify that the engineering hot channel factors used in the design analyses (Section 4.4.2.2.4) are conservative.

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 4-86 shows the number and location of instrumented assemblies in the core. This figure is taken from Reference 101.

The core-exit thermocouples provide a backup to the flux monitoring instrumentation for monitoring power distribution. The routine, systematic, collection of thermocouple readings by the operator provides a data base. From this data base, abnormally high or abnormally low readings, quadrant temperature tilts, or systematic departures from a prior reference map can be deduced.

The movable incore neutron detector system would be used for more detailed mapping if the thermocouple system were to indicate an abnormality. These two complementary systems are more useful when taken together than either system alone would be. The incore instrumentation system is described in more detail in Section 7.7.1.9.

The incore instrumentation is provided to obtain data from which fission power density distribution in the core, coolant enthalpy distribution in the core, and fuel burnup distribution may be determined.

4.4.6.2 Overtemperature and Overpower ΔT Instrumentation

The Overtemperature ΔT trip protects the core against DNBR. The Overpower ΔT trip protects against excessive power (fuel rod rating protection).

As discussed in Section 7.2.1.1.2, factors included in establishing the Overtemperature ΔT and Overpower ΔT trip setpoints include the reactor coolant temperature in each loop and the axial distribution of core power through the use of the two section excore neutron detectors.

4.4.6.3 Instrumentation to Limit Maximum Power Output

The outputs of the nuclear instrumentation systems 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. For both units there are two combined source and intermediate range fission chambers installed on opposite "flat" portions of the core containing the primary startup (or secondary) neutron sources and are positioned with the detector centerline at an elevation corresponding to one half the core height. For both units the power range detectors include four dual section uncompensated ionization chamber assemblies installed vertically at the four corners of the core and located equidistant from the reactor vessel at all points and, to minimize neutron flux pattern distortions, within one foot of the reactor vessel. Each power range detector provides two signals corresponding to the neutron flux in the upper and in the lower sections of a core quadrant. The three ranges of detectors are used as inputs to monitor neutron flux from a completely shutdown condition up to 120 percent of full power with the capability of recording overpower excursions up to 200 percent of full power. The power range portions of the Nuclear Instrumentation system are discussed in detail in Reference 101. The source and intermediate range instrumentation is described in Reference 104.

The output of the power range channels is used for:

1. The rod speed control function,
2. To alert the operator to an excessive power unbalance between the quadrants,
3. Protect the core against rod ejection accidents and
4. Protect the core against adverse power distributions resulting from dropped rods.

Details of the neutron detectors and nuclear instrumentation design and the control and trip logic are given in Chapter 7. The limits on neutron flux operation and trip setpoints are given in the Technical Specifications.

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