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3.0 REACTOR

3.1 DESIGN BASES

The reactor is designed to meet the performance objectives specified in Section 3.1.1 without exceeding the limits of design and operation specified in Section 3.1.2.

3.1.1 PERFORMANCE OBJECTIVES

The rated power of the core was increased from 2535 MWt (initial cycle) to 2568 MWt for TMI-1 as of cycle 7 (Reference 90). The reactor is designed to operate at the reference design core power of 2568 MWt with sufficient design margins to accommodate transient operation and instrument error without damage to the core and without exceeding the pressure at the relief valve settings for the Reactor Coolant System. The physics and the core thermal and hydraulic design information presented in this section are for the reference design core power level of 2568 MWt (see Section 1.1).

The fuel rod cladding is designed to maintain its integrity for the anticipated operating transients throughout core life. The effects of gas release, fuel dimensional changes, and corrosion-induced or irradiation-induced changes in the mechanical properties of cladding are considered in the design of fuel assemblies.

The reactor is operated in a rods out, feed and bleed mode. Core reactivity control is by soluble boron shim supplemented by 61 full length, Ag-In-Cd control rod assemblies (CRAs). Burnable poison rod assemblies (BPRAs) may also be used to control core reactivity. In Cycle 10 uranium-gadolinia ($\text{UO}_2/\text{Gd}_2\text{O}_3$) burnable poison, integral to the fuel pellets in selected fuel rods, was introduced for enhanced reactivity and power peaking control. Prior to Cycle 19, in addition to the full length control rods, 8 Inconel part-length axial power shaping rod assemblies (APSRAs) were available for additional control of axial power distribution. Starting with Cycle 19, the APSRSs were determined to be unnecessary and were removed from service.

Control rod worth with the most reactive CRA stuck in the fully withdrawn position is sufficient to shut down the reactor in the hot condition with at least a 1 percent delta-k/k subcritical margin.

In the cold condition, when the reactor is cooled down to ambient temperatures, the shutdown capability with at least 1 percent delta-k/k subcritical margin is maintained with soluble boron shim.

The rate at which reactivity can be added by boron dilution or CRA withdrawal, and the reactivity worth of CRAs is limited to ensure that credible reactivity accidents cannot cause a transient capable of damaging the reactor coolant system or causing significant fuel failure.

3.1.2 LIMITS

The cycle-specific parameter limits that are applicable for the current cycle and help to assure that the fuel limits described below are met have been placed in the Core Operating Limits Report (COLR). The cycle-specific core operating limits are determined for each reload cycle in accordance with Technical Specification Section 6.9.5.

3.1.2.1 Nuclear Limits

The core has been designed to the following nuclear limits:

- a. Fuel has been designed for maximum burnups that do not exceed those established by the methods and criteria in References 98, 110 and 116.
- b. The power Doppler coefficient is negative. However, the control system is capable of compensating for reactivity changes resulting from either positive or negative nuclear coefficient.
- c. The core will have sufficient excess reactivity to produce the design power level and lifetime without exceeding the control capacity or shutdown margin.
- d. Controlled reactivity insertion rates have been limited to a maximum value of 1.09×10^{-4} delta-k/k/s for a single regulating CRA group withdrawal, a maximum value of 9.27×10^{-4} delta-k/k/s for all 61 CRAs, and 1.6×10^{-5} delta-k/k/s for soluble boron removal.
- e. Reactor control and maneuvering procedures will not produce peak-to-average power distributions greater than the Nuclear Power Factors listed in Table 3.2-12.

3.1.2.2 Reactivity Control Limits

The control system and operational procedures will provide adequate control of the core reactivity and power distribution. The following control limits apply:

- a. Sufficient control will be available to produce an adequate shutdown margin.
- b. The shutdown margin will be maintained throughout core life with the CRA of highest worth stuck out of the core.
- c. CRA withdrawal rate limits the reactivity insertion rate to a maximum of 1.09×10^{-4} delta-k/k/s for a single regulating group and a maximum of 9.27×10^{-4} delta-k/k/s for all 61 CRAs. Boron dilution is limited to a reactivity insertion rate of 1.6×10^{-5} delta-k/k/s.

3.1.2.3 Thermal And Hydraulic Limits

The reactor core is designed to meet the following limiting thermal and hydraulic conditions:

- a. No central melting in the fuel at the design overpower value of 112 percent of the reference core design power level (2568 MWt).
- b. A 95 percent confidence that at least 95 percent of the fuel rods in the core are in no jeopardy of experiencing a departure from nucleate boiling (DNB) during continuous operation at the design overpower value.
- c. The minimum allowable DNBR during normal operation and anticipated transients is 1.30 with the BAW-2 correlation for Mark-B fuel, 1.18 with the BWC correlation for Mark-BZ type fuel, and 1.132 with the BHTP correlation for Mark-B-HTP type fuel. The

Statistical Core Design methodology (Reference 126) uses a DNB Statistical Design Limit (SDL) which treats core state and bundle uncertainties statistically for a realistic assessment of core DNB protection. The SDL's for BWC and BHTP correlations are equivalent to the traditional DNBR limit for these correlations (see Section 3.2.3.2.2.1).

- d. The generation of net steam in the hottest core channels is permissible, but steam voids will be below the threshold for flow instabilities.

3.1.2.4 Mechanical Limits

3.1.2.4.1 Reactor Internals

The reactor internal components are designed to withstand the stresses resulting from startup, steady-state operation with one or more reactor coolant pumps running, and shutdown conditions. No damage to the reactor internals will occur as a result of loss of power to the reactor coolant pumps.

Reactor internals are fabricated primarily from SA-240 (Type 304) material and designed within the allowable stress levels permitted by the ASME Code, Section III, for normal reactor operation and transients. Structural integrity of all core support assembly circumferential welds is assured by compliance with ASME Code Sections III and IX, radiographic inspection acceptance standards and welding qualification.

The core support structure is designed as a Class I structure, as defined in Chapter 5, to resist the effects of seismic disturbances. The basic design guide for the seismic analysis is in Reference 1.

Lateral deflection and torsional rotation of the lower end of the core support assembly is limited in order to prevent excessive deformation resulting from seismic disturbance, thereby assuring insertion of CRAs. Core drop in the event of failure of the normal supports is limited by core support lugs so that the CRAs do not disengage from the fuel assembly guide tubes (see Section 3.2.4.1).

The structural internals are designed to maintain their functional integrity in the event of any loss of coolant accident (LOCA). The dynamic loading resulting from the pressure oscillations because of a LOCA will not prevent CRA insertion.

Internals vent valves are provided to relieve pressure resulting from steam generation in the core following a postulated reactor coolant inlet pipe rupture, so that the core will be rapidly recovered by coolant.

3.1.2.4.1.1 Allowable Stresses

The loading combinations and corresponding stress criteria, including the analytically predicted values of internals deflection for the combined maximum seismic and LOCA loadings, are given in Reference 2. Additional criteria for stresses due to flow-induced vibratory loads are given in Reference 3.

3.1.2.4.1.2 Methods of Load Analysis Employed for Reactor Internals and Core

Static or dynamic analysis is used as appropriate. In general, dynamic analysis is used for earthquakes and the subcooled portion of the LOCA. For the relatively steady-state portion of the LOCA, a static analysis is used.

Where it is indicated that substantial coupling, i.e., interrelationship, exists between major components of the Nuclear Steam Supply System (NSSS), such as the steam generator, the piping, and the vessel, the dynamic analysis includes the response of the entire coupled system. However, where coupling is found to be small, the component or groups of components are treated independently of the overall system.

The dynamic analysis for LOCA uses predicted pressure-time histories as input to a lumped-mass model. For earthquakes, actual earthquake records normalized to appropriate ground motion are used as input to the model. The output from the analysis is in the form of internal motions (displacements, velocities, and accelerations), motions of individual fuel assemblies, impact loads between adjacent fuel assemblies, and impact loads between peripheral fuel assemblies and the core shroud. Motions of the reactor vessel, internals, and core have been confirmed using a time history excited lumped-mass solution.

In addition, seismic analysis is also performed using a modal superposition and response spectra approach.

For the simultaneous occurrence of LOCA and the maximum earthquake, both time history excitations are input to the system simultaneously such that maximum structural motions, indicating maximum stresses, are obtained. Outputs are those mentioned above.

The output from the lumped mass model and additional information such as pressure-time histories on separate internals and core components (including control rods) are used to calculate stresses and deflections. These stresses and deflections are compared to the allowable limits for the various loading combinations to insure that they are less than these allowables. The allowable stress limits are shown in Section 4.1.2.

3.1.2.4.2 Core Components

3.1.2.4.2.1 Fuel Assemblies

Fuel assemblies are designed for structural adequacy and reliable performance during core operation, handling, and shipping. Design criteria for core operation include steady-state and transient conditions under combined effects of flow-induced vibration, temperature gradients, and seismic disturbances.

Spacer grids, located along the length of the fuel assembly, position fuel rods in a square array and are designed to maintain fuel rod spacing during core operation, handling, and shipping. Spacer grid to fuel rod contact loads are established to minimize fretting but to allow axial relative motion resulting from fuel rod irradiation growth and differential thermal expansion.

The fuel assembly upper end fitting is indexed to the plenum assembly by the grid immediately above the fuel assemblies to assure proper alignment of the fuel assembly guide tubes to the

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control rod guide tube. The guidance of the CRA is designed such that these assemblies will never be disengaged from the fuel assembly guide tubes during operation.

The fuel rod cladding is designed to withstand strain resulting from combined effects of reactor pressure, fission gas pressure, fuel expansion, and thermal and irradiation growth. Clad strain resulting from normal and abnormal operating conditions is limited as follows:

- a. Stresses not relieved by small material deformation are limited so as not to exceed either the yield strength of the material or 75 percent of the stress rupture life of the material. An example of such a stress is the circumferential membrane stress in the clad due to internal or external pressure.
- b. Stresses relieved by small material deformation are permitted to exceed the yield strength. Strain limits for this stress condition are established based on low-cycle fatigue techniques, not to exceed 90 percent of material fatigue life. Evaluation of cyclic loading is based on conservative estimates of the number of cycles to be expected. An example of this type of stress is the thermal stress resulting from thermal gradients across the clad thickness.
- c. Combinations of these two types of stresses, in addition to the individual treatment outlined above, are evaluated on the low-cycle fatigue basis of item b) above. Clad plastic strain due to diameter increases resulting from fuel swelling, thermal ratcheting and creep, and the effects of internal gas pressure is limited to 1 percent.
- d. Minimum clad collapse pressure margins are as follows:
 - 1) Ten percent margin over system design pressure, on short time collapse, at fuel rod end voids.
 - 2) End voids must not collapse (must be either free standing or have adequate support) on a long time basis.
 - 3) Ten percent margin over system operating pressure, on short time collapse, at hot spot average temperature of the clad wall.
 - 4) Clad must be freestanding at design pressure on a short time basis at approximately 733F hot spot temperature averaged through the clad wall.

3.1.2.4.2.2 Control Rod Assemblies (CRAs)

Absorber material cladding used on the Control Rod Assemblies (CRAs) and when used when used, Burnable Poison Rod Assemblies (BPRAs) is designed to the same criteria as the fuel rod clad, as applicable to absorber material characteristics. Clearance is provided between the rods of these assemblies and the fuel assembly guide tubes to permit coolant flow to limit operating temperature of the absorber materials. In addition, this clearance is designed to permit rod motion as required during reactor operation under any condition including seismic disturbances.

Excessive stress in the CRA components during trip of the rod drive mechanism is prevented by use of conservative design stress limits and by hydraulic snubbing to minimize shock.

3.1.2.4.2.3 Orifice Rod Assembly (ORA)

Orifice Rod Assemblies (ORAs) serve to limit bypass flow through empty guide tubes. ORAs are not used in the current core design.

3.1.2.4.3 Control Rod Drive Mechanisms

3.1.2.4.3.1 Shim Safety Drive

The shim safety control rod drives provide CRA insertion and withdrawal rates consistent with the required reactivity changes for reactor operational load changes. This rate is based on the worths of the various rod groups, which have been established to limit power-peaking flux patterns to design values. The maximum reactivity addition rate is specified to limit the magnitude of a possible nuclear excursion resulting from a control system or operator malfunction. The normal insertion and withdrawal velocity has been established as 30 inches per minute.

The drive provides a trip of the CRA which results in a rapid shutdown of the reactor for conditions that cannot be handled otherwise by the Reactor Control System. The trip set point is based on the results of various reactor emergency analyses, including instrument and control delay times and the amount of reactivity that must be inserted before deceleration of the CRA occurs. The maximum travel time for a 3/4 insertion (104 inches travel) on a trip command of a CRA has been established as 1.66 seconds (hot full flow) and 1.40 seconds (hot no flow).

The control rod drives can be coupled and uncoupled to their respective CRAs without any withdrawal movement of the CRA.

The components containing reactor coolant pressure are designed to meet the requirements of the ASME Code, Section III, Nuclear Vessels, for Class 1 appurtenances (1974 Edition with no Addenda, and 1971 Edition with Summer Addenda).

Materials selected for the control rod drive are capable of operating within the specified reactor environment for the life of the mechanism without any deleterious effects. Adequate clearances are provided between the stationary and moving parts of the control rod drives so that the CRA trip time to full insertion will not be adversely affected by mechanical interference under all operating conditions and seismic disturbances.

3.1.2.4.3.2 Deleted

3.2 REACTOR DESIGN

3.2.1 GENERAL SUMMARY

The core design characteristics are listed in Table 3.2-1, Core Design Data.

The sections that follow present the nuclear (Section 3.2.2), thermal hydraulic (Section 3.2.3), and mechanical (Section 3.2.4) design and evaluation of the core. Within these sections, certain nuclear, thermal-hydraulic, and mechanical design parameters were determined for the initial core. Certain of the initial cycle values represented by curves, graphs, or numerical values are not recalculated for each reload cycle, but are representative of the current core. Such information has been retained and is identified as "initial core". Significant parameters, however, are recalculated for each reload core cycle. These parameters are contained in each cycle specific "Reload Report". The reload analysis for each cycle ensures that key safety parameters, limits, and set points are consistent with the assumptions used in the safety analysis of Chapter 14.

For Cycles 12 and 13 only, the reload safety analyses were done using the licensee's NRC-approved methodologies instead of AREVA (formerly B&W, FCF, and FRA-ANP) methods. The licensee's methods included core physics (Reference 112), core thermal-hydraulics (Reference 113) and core operating limits (Reference 114). Fuel analyses continued to be done by AREVA NRC-approved methods.

Some changes in the design analysis between the initial cycle and the current cycle are summarized below.

- a. Current cycle fuel assemblies have higher initial theoretical density and a correspondingly higher linear heat rate capability (see Section 3.2.3.2.3.7, Item c).
- b. Current thermal hydraulic analyses use two critical heat flux correlations, rather than the W-3 correlation used in the initial cycle (see Section 3.2.3.1.1.2) or the BAW-2 correlation used for Mark-B fuel (References 15 and 16). The current correlations used are the BWC for Mark-BZ type fuel and for Mark-B-HTP fuel below the first HTP grid (Reference 84) and the BHTP for Mark-B-HTP fuel at the first HTP grid and above (Reference 129), as applicable to the specific fuel designs in the core.
- c. Current cycle thermal hydraulic analysis (Reference 128) used an increased value for system flow; that is, 107.0 percent of Cycle 1 design flow (see Section 3.2.3.1.1.2).
- d. The reference design radial x local peaking factor $(F \Delta h)^N$ was reduced from 1.78 to 1.71 (see Section 3.2.3.1.1.3) and later increased to 1.80 with the application of Statistical Core Design methodology (see Section 3.2.3.2.2.1) in Cycle 15.
- e. Fuel densification effects were included in the fuel thermal analysis prior to Cycle 2 reload (see Section 3.2.3.2.3.7, Item b).
- f. The thermal design overpower was reduced from 114 percent to 112 percent of the reference core design power level (see Section 3.2.3.2.3.7, Item b).
- g. Current fuel rod behavior analyses are done using the TACO series computer codes (References 82, 99 and 100) and the COPERNIC fuel performance code (Reference 133). Initial cycle analyses were performed using TAFY (see Section 3.2.3.1.2). The

TACO codes are used in LOCA initialization analyses. All other fuel rod behavior analyses are performed using COPENIC. These include prediction of cladding transient strain and fuel centerline melt limits, end-of-life rod internal pressures in-reactor and rod internal pressures in the spent fuel pool, cladding creep collapse initialization, cladding fatigue, thermal conditions, and cladding oxidation.

- h. In the first cycle the reactivity was controlled mainly by control rod assemblies (CRAs) and burnable poison rod assemblies (BPRAs). In the second cycle no BPRAs were used and the reactivity was mainly controlled by CRAs. Beginning with Cycle 4, CRAs were not used for the purpose of controlling excess reactivity and the boron feed-and-bleed operational mode was introduced (Reference 59). As of Cycle 6, BPRAs were reintroduced due to conversion to extended-cycle designs; boron feed-and-bleed operation is maintained (Reference 81). As of Cycle 10 uranium-gadolinia (UO_2/Gd_2O_3) integral burnable poison was introduced (Reference 101).
- i. Current cycle DNB analysis is based on cross flow models, which predict flow redistribution effects in an open lattice reactor core (References 79 and 80). This change was introduced in Cycle 6 and provides significant improvement in the DNBR margins relative to the former closed-channel modeling used for Cycle 1 through Cycle 5.
- j. The reference design axial peaking factor (F_z) has increased from 1.5 to 1.65 cosine (Reference 81).
- k. As of Cycle 13, the effect of axial blankets on axial power shapes has been accounted for in TACO3 and GDTACO calculations, and in COPENIC calculations starting from Cycle 19.
- l. As of Cycle 15, the Statistical Core Design methodology (Reference 126) has been incorporated into core DNB analyses.

3.2.2 NUCLEAR DESIGN AND EVALUATION

This section presents the evaluation of significant core parameters and shows that the basic design of the core satisfies the performance limits and objectives of Sections 3.1.2.1 and 3.1.2.2.

3.2.2.1 Nuclear Characteristics Of The Design

A listing of the Nuclear Design Data representative of the core characteristics is given in Table 3.2-2.

3.2.2.1.1 Excess Reactivity

The excess reactivity associated with various initial cycle core conditions is given in Table 3.2-3.

The minimum critical mass, with and without xenon and samarium poisoning may be specified as a single assembly or as multiple assemblies in various geometric arrays. The unit fuel assembly has been investigated for comparative purposes. A single cold, clean assembly containing a maximum probable enrichment of 5.0 weight percent is subcritical in water (Reference 89). Two assemblies side by side are supercritical under these conditions.

3.2.2.1.2 Reactivity Control Distribution

The reactivity control distribution for the current cycle is given in Table 3.2-4.

The critical boron concentration over core life for the current cycle is given in Table 3.2-5.

3.2.2.1.3 Shutdown Reactivity Analysis

The ability to shut down the core under hot conditions is demonstrated by the data given in Table 3.2-6. In this analysis the core is evaluated at the beginning of cycle (BOC) and the end of cycle (EOC) for shutdown capability. The table shows that, with the highest worth CRA stuck out, the core can be maintained in a subcritical condition with operating boron concentrations. Minimum shutdown margin for the current cycle is above the 1 percent delta-k/k requirement.

Under conditions where a cooldown to reactor building ambient temperature is required, concentrated soluble boron will be added to the reactor coolant to produce a shutdown margin of at least 1 percent delta-k/k.

The following conservatisms were applied for the shutdown calculation:

- a. Poison material depletion allowance
- b. Ten percent uncertainty on control rod worth
- c. Xenon transient allowance.

Boron concentrations for refueling conditions are established to ensure a shutdown margin of at least 1 percent delta-k/k is available with all control rods removed from the core as required by Technical Specification 1.2.6, and a shutdown margin of approximately 5 percent delta-k/k is available with all control rods inserted except for the maximum worth rod in accordance with NRC Bulletin 89-03.

3.2.2.1.4 Control Rod Groups for Operation

The rod worth calculations for Cycles 1 through 9 were performed with the FLAME 3, PDQO7, and NOODLE computer programs as applicable for each cycle (References 6, 7, and 83, respectively). For Cycles 10 and 11, the NEMO code (Reference 102) was used for rod worth analyses. For Cycles 12 and 13, CASMO-3/SIMULATE-3 was used (Reference 112). As of Cycle 14, rod worth calculations reverted to the NEMO code.

Figure 3.2-1 shows the control rod group locations for current cycle operation. The control rod worths of the transient rod bank (group 7) at hot full power (HFP) are given in Table 3.2-4. The worths of the control rod groups at hot zero power (HZIP) are given in Table 3.2-7.

The location of the ejected rod for BOC and EOC analysis is shown in Figure 3.2-2.

The location of the stuck rod for BOC and EOC analysis is shown in Figure 3.2-3.

3.2.2.1.5 Reactivity Coefficients

Reactivity coefficients form the basis for studies involving normal and abnormal reactor operating conditions. These coefficients have been investigated as part of the core analysis and are described below as to function and overall range of values.

3.2.2.1.5.1 Doppler Coefficient

The Doppler coefficient of reactivity reflects the change in reactivity as a function of fuel temperature. A rise in fuel temperature results in an increase in the effective absorption cross section of the fuel (the Doppler broadening of the resonance peaks) and a corresponding reduction in neutron production.

The Doppler coefficients at the beginning and at the end of current cycle are given in Table 3.2-9.

For the previous FCF physics methods, temperature- dependent resonance integrals which include Doppler broadening were calculated based on Reference 8. A comparison of calculated to experimental resonance integrals is presented in Figure 3.2-4. The curves for $r = 0.5$ are representative of this core.

3.2.2.1.5.2 Moderator Void Coefficient

The moderator void coefficient relates the change in neutron multiplication to the presence of voids in the moderator. The expected range of void coefficients for a core representative of the initial cycle is shown in Figure 3.2-5.

3.2.2.1.5.3 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 opposite in sign and considerably smaller when compared to the moderator temperature coefficient.

3.2.2.1.5.4 Moderator Temperature Coefficient

The moderator temperature coefficient relates a change in neutron multiplication to the change in reactor coolant temperature. Reactors using soluble boron as a reactivity control have a less negative moderator temperature coefficient than do cores controlled solely by movable or fixed Control Rod Assemblies (CRAs). The major temperature effect on the coolant is a change in density. An increasing coolant temperature produces a decrease in water density and an equal percentage reduction in boron concentration. The concentration change results in a positive reactivity component by reducing the absorption in the coolant. The magnitude of this component is a function of the total reactivity held by soluble boron. Distributed poisons (burnable poison rods or inserted control rods) have a negative effect on the moderator coefficient for a specified boron concentration. For example, the moderator coefficient for a system with 1200 ppm boron in the coolant and one percent rod worth inserted will be more negative than for a system with 1200 ppm boron and no rods inserted. Depending on the core size, core loading, and power density, a plant may or may not require additional distributed

poisons to yield the appropriate moderator temperature coefficient as determined by the safety analysis and the stability analysis of the core.

In the first cycle the reactivity was controlled mainly by CRAs and BPRAs. In the second cycle the reactivity was mainly controlled by CRAs. Similar control of excess reactivity was exercised in Cycle 3. Beginning with Cycle 4 and throughout the current cycle, CRAs are not used for the purpose of controlling excess reactivity and boron feed-and-bleed operational mode has been introduced.

The physics startup program included measurements of the moderator temperature coefficient. The threshold value of moderator temperature coefficient for azimuthal Xenon instability is 1×10^{-4} (delta-k/k)/°F. The limiting moderator coefficient is the value used in the safety analysis in Chapter 14, i.e., $+0.9 \times 10^{-4}$ (delta-k/k)/°F.

The moderator temperature coefficients at BOC and EOC conditions for the current cycle are given in Table 3.2-9.

3.2.2.1.5.5 Power Coefficient

The power coefficient α_p , is the fractional change in neutron multiplication per unit change in core power level. The two most significant factors that determine the power coefficient are the moderator temperature coefficient and Doppler coefficient. The power coefficient can be written as:

$$\alpha_p = \alpha_m \frac{dT_m}{dp} + \alpha_f \frac{dT_f}{dp}$$

Where α_m = moderator temperature coefficient

α_f = fuel Doppler coefficient

$\frac{dT_m}{dp}, \frac{dT_f}{dp}$ = change in moderator and fuel temperatures per unit change in core power.

The integrated power coefficient (power deficit) over the range between HZP and HFP for the current cycle is given in Table 3.2-9.

3.2.2.1.6 Reactivity Insertion Rates

Using a CRA group worth of 1.5 percent delta-k/k and a 30 in/min drive speed in conjunction with the reactivity response given in Figure 7.2-1 yields a maximum reactivity insertion rate of 1.09×10^{-4} delta-k/k/s for a single regulating group (also, see Section 14.1.2.3). If a CRA worth of 12.9% delta-k/k (all 61 CRAs) is used, the maximum reactivity insertion rate is 9.27×10^{-4} delta-k/k/s (Section 14.1.2.3). The maximum reactivity insertion rate for soluble boron removal is 1.6×10^{-5} delta-k/k/s (see Section 14.1.2.4).

3.2.2.1.7 Power Decay Curves

Figure 3.2-6 displays the initial core beginning of life power decay curves for the CRA worths corresponding to the 1 percent hot shutdown margin with and without a stuck rod. The power

decay is initiated by the trip of the CRA with a 300 msec delay from initiation to start of CRA motion. The time required for insertion of a CRA two-thirds of the distance into the core is 1.4 seconds. Since the most accurate position indication is obtained from the zone reference switch at the 75 percent inserted position, this position is used instead of the two-thirds inserted position for data gathering. The acceptance criterion is 1.4 seconds corrected to a 75 percent inserted position (by rod insertion versus time correlation) is 1.66 seconds. (Reference Technical Specification No. 4.7.1.1).

3.2.2.2 Nuclear Evaluation

Analytical models and their application are discussed in this section. Core instabilities associated with xenon oscillation are also described.

3.2.2.2.1 Analytical Models

Reactor design calculations are made using a large number of computer codes. FCF analytical models for core analyses are discussed in topical report BAW-10118A, "Core Calculational Techniques and Procedures" (Reference 9).

3.2.2.2.1.1 Fuel Cycle Analysis

Fuel cycle design for the current core was done with the NEMO core physics code (Reference 102). The fuel assembly arrangement for the current cycle is shown in Figure 3.2.7. The nuclear design data is given in Table 3.2-2.

3.2.2.2.1.2 Control of Power Distributions

Beginning with Cycle 4, the core has been designed to operate mainly in a feed and bleed mode. Although there will be some control rod banks inserted, insertion will be small during steady state operation. Prior to Cycle 19, Axial Power Shaping Rods were available for controlling axial xenon effects. In Cycle 19, the APSRAs were determined to be unnecessary and were removed from service. Axial power distribution control during operation with the APSRAs removed is accomplished by adjusting regulating rod group position, as required, to prevent or damp xenon oscillations.

DNB and/or fuel melt design limits have been analyzed to establish the allowable limits on axial imbalance consistent with the thermal design case described in Section 3.1.2.3. Imbalance trip set points have been established for the current cycle, as given in the Core Operations Limits Report, for a Reactor Protection System function (Over Power Trip Based on Flow and Imbalance) which will ensure that DNB and fuel temperature limits will not be exceeded.

These trip set points are calculated using NRC approved codes and methods and provide core protection against excessive axial power imbalance up to and including the design overpower level.

During startup testing, incore instrumentation is used to establish that the out of core imbalance is consistent with the actual incore imbalance data.

3.2.2.2.1.3 Power Maldistributions

a. Misaligned Control Rods

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The reactor has a control function to protect against a rod out of step with its group. The position of each rod is compared to the average of the group. If a fault is detected at power levels above 60 percent of rated power, a rod withdrawal inhibit is activated. If a rod is dropped, the plant is run back to 60 percent of rated power.

Radial power tilts may be detected with the incore or out of core instrumentation.

b. Azimuthal Xenon Oscillations

The reactor is predicted to have a substantial margin to threshold for azimuthal xenon oscillations. Therefore, this mode is not considered a power peaking problem. A detailed description of previous FCF analysis is found in topical report BAW-10010, Part I, "Stability Margin for Xenon Oscillations" (Reference 10). The conclusions of this analysis remain representative for the current core.

c. Fuel Misloading

Misloading the fuel pins in an assembly is prevented by loading controls and procedures. Each fuel rod is identified by an enrichment code and the design of the reactor is such that typically one enrichment is used per assembly. An exception to this practice was introduced in Cycle 10 when a limited number of urania-gadolinia fuel rods were used in selected assemblies. The Gd rods contain lower U235 enrichments than the rest of the rods in the host assembly (see Table 3.2-2). A second exception to this practice was the introduction of enriched axial blankets in Cycle 13 where the top and bottom sections of all fuel rods are loaded with a lower U235 enrichment than the nominal central section of the rods. The manufacturing process relies on administrative procedures and quality control checks to ensure that fuel rods are placed in the proper assembly and that axial blanket regions are loaded properly.

Gross fuel assembly misplacement in the core is prevented by administrative core loading procedures and the prominent display of identification markings on the upper end fitting of each assembly. The fuel handling bridges and grapple mechanisms are designed for accurate indexing and positioning. To ensure proper placement of fuel assemblies, all movement is communicated to the Control Room and verified.

The fuel loading operations are performed in accordance with a predetermined and reviewed load sequence. After fuel loading has been completed, the core loading is verified by visually surveying the core and recording the fuel assembly numbers versus core location. This record is then compared to the core loading plan by a person other than the one making the survey.

The procedures will specify how the fuel assemblies are to be oriented in the spent fuel pool and in the reactor with respect to fixed references. In order to identify the fuel assemblies in the core loading verification mentioned above, it is necessary for the observer to look at the fuel assembly identification number, which is located on only one side of the assembly.

After refueling is completed, the response of the incore detectors is compared to calculations. Even if an assembly were out of position, operators could monitor the incore detectors to determine whether radial power tilts were developing. Upon return to power operation after refueling, and to an even greater extent upon initial increase to power, operators carefully

monitor both incore and out-of-core detectors to ensure that core symmetry exists within technical specifications limits.

3.2.2.2.1.4 Control Rod Analysis

Babcock & Wilcox has developed a procedure for the analysis of the reactivity worth of small cylindrical Ag-In-Cd control rods. The procedure has been verified against a set of 14 critical experiments in which the variables included number of rods per cluster, arrangement of rods within the cluster, number of clusters in the core, and soluble boron concentration.

Approximately half of the experiments included water holes to simulate withdrawn rods. The comparison of calculated and experimental reactivity worths is shown in Table 3.2-10.

The experiments were performed at the B&W Critical Experiment Laboratory with lattices of aluminum-clad uranium oxide fuel rods. Enrichment of the fuel was 2.46 weight percent Uranium-235. The Ag-In-Cd control rods used in the experiments had an absorber diameter of 0.400 inches. Geometrical arrangements of the control rods were chosen to encompass the reference design for the power cores.

Experimental rod worths were determined by calibration against soluble boron concentration. The critical soluble poison concentration was determined for each configuration with rods in and again with rods out. (Soluble poison concentrations given in the Table 3.2-10 are for the rods-in situation). Soluble poison concentrations were measured with an absolute accuracy of ± 5 ppm and a precision of ± 3 ppm. References 11 and 12 describe the experiments in detail.

The FCF analytical method used in this analysis is based upon the PDQ code with coefficients generated by the B&W LIFET program used at the time. Key features include the allowance for interference and overlap effects between resonances and isotopes in the Ag-In-Cd rod, and calculation of the relative fluxes in the control rod and surrounding fuel in an 80-group thermal model.

3.2.2.2.2 Xenon Stability Analysis and Control

Modal and digital analysis of the core indicated that the tendency towards xenon instability in the axial mode could exist for a given combination of events. Therefore, eight part-length APSRAs were included in the original design. When utilized, they were positioned during operation to maintain an acceptable distribution of power for any particular operating condition in the core, thereby reducing the tendency for axial oscillations.

Analysis also indicated that the core is stable azimuthally over the entire fuel cycle.

Modal Analysis indicated that the core is stable with regard to radial oscillations.

A detailed description of the xenon analysis performed on the core can be found in Reference 10.

The foregoing results are valid while the APSRs are in the core. However, current reload designs have been shown to be axially stable (Reference 132) and no longer include APSRAs. For operation with APSRs removed, the following criterion applies:

Axial power oscillations induced by an axial xenon oscillation shall be naturally damped.

During the core reload or redesign analysis, a design xenon transient is simulated. If the simulation shows that the xenon oscillation is not naturally dampened, regulating rods would be used to damp any induced xenon oscillations.

3.2.3 THERMAL AND HYDRAULIC DESIGN AND EVALUATION

For Cycles 1 through 11 core thermal-hydraulics reload analyses were done using FRA-ANP NRC-approved methodologies. For Cycles 12 and 13 the thermal-hydraulic design was done using the licensee's NRC-approved VIPRE-01 core T-H methodology described in Reference 113. As of Cycle 14, core T-H analyses reverted back to FRA-ANP methods. FRA-ANP's NRC-approved Statistical Core Design methodology was implemented as of Cycle 15.

3.2.3.1 Thermal And Hydraulic Characteristics

A summary of the Thermal and Hydraulic design data is given in Table 3.2-11.

3.2.3.1.1 Fuel Assembly Heat Transfer Design

3.2.3.1.1.1 Design Criteria

The criterion for the heat transfer design is to be safely below departure from nucleate boiling (DNB) heat flux at the design overpower. The analysis is described in detail in Section 3.2.3.2.2, Statistical Core Design Technique.

The input information for the statistical core design technique and for the evaluation of individual hot channels is as follows:

- a. Heat transfer critical heat flux equations and data correlations
- b. Nuclear power factors
- c. Engineering hot channel factors
- d. Core flow distribution hot channel factors
- e. Maximum reactor overpower

3.2.3.1.1.2 Critical Heat Flux Correlation

The initial cycle thermal-hydraulics analyses were based on the W-3 critical heat flux correlation presented in References 13 and 14. Beginning with cycle 2 the W-3 correlation was replaced by the BAW-2 critical heat flux correlation, documented in References 15 and 16, for reload licensing analysis. The BAW-2 correlation is a realistic prediction of the burnout phenomenon and predicts DNB and the location of DNB for axially uniform and non-uniform heat flux distributions. In applying the BAW-2 correlation, the following modifications to Cycle 1 DNB data are used.

- a. The limiting design DNBR of 1.30 is used, corresponding to a 95 percent confidence level of a 95 percent probability that DNB will not occur.

- b. The pressure range applicable to the correlation has been extended downward from 2000 to 1750 psig.

In addition, the design overpower was changed from 114 percent to 112 percent in Reference 45 and the primary system flow was taken to be 106.5 percent of Cycle 1 design flow. As of Cycle 20, the primary system flow was taken to be 107.0 percent of Cycle 1 design flow to reflect replacement steam generators. Figure 3.2-9 shows a typical variation of DNBRs with the hot unit cell versus active core height as derived using the BAW-2 CHF correlation for Cycle 5.

In Cycle 7 Mark-BZ type reload fuel assemblies, which replaced the Intermediate Inconel spacer grids in Mark-B fuel assemblies with Zircaloy grids, were introduced (References 84 and 85). A new critical heat flux correlation, BWC, was applied to the Mark-BZ type fuel (Reference 86). The transient design limit DNBR for the BWC correlation is 1.18. When used in conjunction with the Statistical Core Design methodology (Reference 111) a DNB Statistical Design Limit (SDL) is used, which is equivalent to the traditional DNBR limit of 1.18 for the BWC correlation.

As of Cycle 17, Mark-B-HTP type reload fuel assemblies, which replaced the spacer grids with higher pressure drop Zircaloy HTP (intermediate and top) and Inconel HMP (bottom) grids, were introduced. A new critical heat flux correlation, BHTP, was applied to the Mark-B-HTP type fuel at and above the first HTP grid (Reference 129); the BWC correlation still applies below the first. The transient design limit DNBR for the BHTP correlation is 1.132. The SCD methodology uses a DNB Statistical Design Limit (SDL) (Reference 134), which is equivalent to the traditional DNBR limit of 1.132 for the BHTP correlation.

As of Cycle 19, all fuel assemblies are the Mark-B-HTP type and only the BWC and BHTP correlations are used. However, certain analytical data and results from the initial cycle have not been recalculated for the reload and are presented as being representative information for the current cycle. These are identified as "initial cycle."

The design equations for the BAW-2 CHF correlation are as follows:

BAW-2 Critical Heat Flux Correlation for Uniform Axial Flux Profiles

$$q_u = (a - bD_e) \left[A_1(A_2G)^{A_3 + A_4(P-2000)} - (A_9)(G)(X)(H_{fg}) \right] A_5(A_6G)^{A_7 + A_8(P-2000)}$$

Where:

q_u = uniform critical heat flux, Btu/h-ft²

P = pressure, psia

G = mass velocity, lb/h-ft²

X = quality

D_e = equivalent diameter, inches

H_{fg} = latent heat of vaporization, Btu/lb

a = 1.15509

b = 0.40703

A_1 = 0.37020×10^8

A_2 = 0.59137×10^{-6}

$$A_3 = 0.83040$$

$$A_4 = 0.68479 \times 10^{-3}$$

$$A_5 = 12.710$$

$$A_6 = 0.30545 \times 10^5$$

$$A_7 = 0.71186$$

$$A_8 = 0.20729 \times 10^{-3}$$

$$A_9 = 0.15208$$

BAW-2 Critical Heat Flux Correlation for Non-uniform Axial Flux Profiles

$$q''_e = q''_u / F_D$$

Where:

$$q''_e = \text{non-uniform critical heat flux, Btu/h-ft}^2$$

$$q''_u = \text{uniform critical heat flux, Btu/h-ft}^2$$

$$F_D = K \frac{C}{D q'' \int_0^{L_{CHF}} \exp(-C(L_{CHF}-z)) dz}$$

$$C = A_{21} \frac{(1-x)^{A_{22}}}{(G/10^6)^{A_{23}}}$$

$$L_{CHF} = \text{critical heat flux location, inches}$$

$$q'' = \text{local heat flux, Btu/h-ft}^2$$

$$K_D = 1.02508$$

$$A_{21} = 0.24867$$

$$A_{22} = 7.82293$$

$$A_{23} = 0.45758$$

$$z = \text{distance from inception of local boiling to } L_{CHF}, \text{ inches}$$

The measured-to-predicted critical heat flux for this correlation is compared in Figure 4.4-10 of Reference 15 which describes the BAW-2

Correlation in more detail. The BAW-2 correlation is limited to the following ranges of hydraulic conditions:

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$$\text{Mass velocity, lbm/h-ft}^2 \quad 0.77 \times 10^6 \leq G \leq 4.0 \times 10^6$$

$$\text{Quality, percent} \quad -3 \leq x \leq +22$$

$$\text{Pressure, psia} \quad 1,750 \leq P \leq 2,450$$

$$\text{Equivalent diameter, inches} \quad 0.2 \leq D \leq 0.524$$

The BWC CHF Correlation

The BWC CHF correlation was developed for both 17 x 17 Mark C fuel and 15 x 15 Mark BZ zircaloy grid design. The correlation, q''_{BWC} is given by (Reference 86).

$$q''_{\text{BWC}} = \frac{A_5[(A_1 G)^{A_3 + A_4(P - 2000)} - A_8 H_{fg} G x]}{[A_2 G^{A_6 + A_7(P - 2000)}] F}$$

$$F = \frac{C q''_{av}}{q''_L (1 - e^{-CL})} \int_0^L [\phi(Z)] e^{-C(L-Z)} dz$$

$$C = \frac{B_1(1 - x)^{B_2}}{(G/10^6)^{B_3}}$$

Where:

$$q_c'' = \text{critical heat flux, Btu/h-ft}^2$$

$$G = \text{local mass velocity, lbm/h-ft}^2$$

$$P = \text{pressure, psia}$$

$$x = \text{local quality}$$

$$H_{fg} = \text{latent heat of vaporization, Btu/lbm}$$

$$L = \text{axial location of CHF, inches}$$

$$Z = \text{axial location, inches}$$

$$q''_{av} = \text{rod average heat flux, Btu/h-ft}^2$$

$$\phi(z) = \text{local average heat flux ratio}$$

$$q''_L = \text{local heat flux, Btu/h-ft}^2$$

TMI-1 UFSAR

The coefficients are given in Reference 86.

Applicable range of system conditions for the BWC correlation is:

Pressure, psia 1600 to 2600

Mass Velocity,
 10^6 LBM/H-FT² 0.43 to 3.8

Quality (Local), % -20 to +26

After the critical heat flux (CHF) has been evaluated, the DNB ratio is calculated as follows:

$$\text{DNBR} = \frac{\text{CHF}}{q_s'' F_q''}$$

Where:

q_s'' = actual heat flux (Local)
Btu/hr ft²

F_q'' = hot channel factor on local heat flux

The BHTP CHF Correlation

The BHTP CHF correlation was developed for the HTP grid design for 14x14 through 17x17 fuel using the LYNXT computer code (Reference 79). The correlation, q''_{BHTP} is given by (Reference 129).

The form of the correlation function and the coefficients used in the correlation are given in Reference 129.

Applicable range of system conditions for the BHTP correlation is:

Pressure, psia 1385 to 2425 (Correlation can be used up to 2600 psia,
with LYNXT calculations using an input of 2425
psia between 2425 and 2600 psia)

Mass Velocity,
 10^6 LBM/hr-ft² 0.492 to 3.549

Quality (Local), % No lower limit to +51.2

After the critical heat flux (CHF) has been evaluated, the DNB ratio is calculated as follows:

$$\text{DNBR} = \frac{\text{CHF}}{q_s'' F_q''}$$

Where:

q_s'' = actual heat flux (Local)
Btu/hr ft²

F_q'' = hot channel factor on local heat flux

3.2.3.1.1.3 Nuclear Power Factors

The heated surfaces in every flow channel in the core are examined for heat flux limits. The heat input to the fuel rods in a coolant channel is determined from a nuclear analysis of the core and fuel assemblies. The results of this analysis for the initial cycle and the current cycle are presented in Table 3.2-12.

The axial distribution of power expressed as P/P_A for two conditions of reactor operation have been evaluated. The first condition is an inlet peak resulting from partial insertion of a CRA group for transient control following a power level change. This condition results in the maximum local heat flux and maximum linear heat rate. The second power shape is a symmetrical cosine which is indicative of the power distribution with xenon override rods withdrawn. Both of these flux shapes have been evaluated for thermal DNB limitations. The limiting condition is the cosine power distribution with a 1.65 P/P_A axial peak. The inlet peak shape has a larger maximum value. However, the position of the cosine peak farther up the channel results in a less favorable flux-to-enthalpy relationship. This effect has been demonstrated in DNB tests of non-uniform flux shapes. The cosine axial shape has been used to determine individual channel DNB limits and to make the associated statistical analysis.

The nuclear factor for total radial x local rod power, $F_{\text{delta-h}}$, is calculated for each rod in the core. A distribution curve of the fraction of the core fuel rods operating above various peaking factors is shown on Figure 3.2-10 for a typical fuel cycle condition with the fuel rod maximum design peaking factor.

Additional axial power distributions have been analyzed to determine the allowable power in the upper half of the channel. Figure 3.2-11 shows the position versus allowable peak for DNBR conditions equivalent to symmetrical cosine distribution. The initial cycle maximum design radial-local of 1.78 has been used on Figure 3.2-11. Figure 3.2-12 presents the allowable conditions when the radial-local power factor is 1.65 instead of 1.78. A comparison of Figures 3.2-11 and 3.2-12 shows the additional allowable outlet peak for less than maximum design radial-local. As shown in Table 3.2-11, the design $F_{\text{delta-h}}$ for the current cycle is 1.80.

3.2.3.1.1.4 Engineering Hot Channel Factors

Power peaking factors obtained from the nuclear analysis are based on mechanically perfect fuel assemblies. Engineering hot channel factors are used to describe variations in fuel loading, fuel and clad dimensions, and flow channel geometry from perfect physical quantities and dimensions.

The application of hot channel factors is described in detail in Section 3.2.3.2.2, Statistical Core Design Technique. The factors are determined statistically from fuel assembly as built or specified data where F_Q is a heat input factor, F_Q'' is a local heat flux factor at a hot spot, and F_A is a flow area reduction factor describing the variation in coolant channel flow area. Several subfactors are combined statistically to obtain the final values for F_Q , F_Q'' , and F_A . These

subfactors are given in Table 3.2-13. The factor, the coefficient of variation, the standard deviation, and the mean value are tabulated.

3.2.3.1.1.5 Core Flow Distribution Hot Channel Factors

The physical arrangement of the reactor vessel internals and nozzles results in a non-uniform distribution of coolant flow to the various fuel assemblies. Reactor internal structures above and below the active core are designed to minimize unfavorable flow distribution. A 1/6 scale model test of the reactor and internals was performed to demonstrate the adequacy of the internal arrangements. The results of the test have confirmed the adequacy of the design values used. Model test results are given in Reference 17.

A flow distribution factor is determined for each fuel assembly location in the core. The factor is expressed as the ratio of fuel assembly flow to average fuel assembly flow. The finite values of the ratio are greater or less than 1.0, depending on the position of the assembly being evaluated. The flow in the central fuel assemblies is in general larger than the flow in the outermost assemblies because of the inherent flow characteristics of the reactor vessel.

The flow distribution factor is related to a particular fuel assembly location and the quantity of heat being produced in the assembly. A flow-to-power comparison is made for all of the fuel assemblies. The worst condition in the hottest fuel assembly is determined by applying model test isothermal flow distribution data and heat input effects at power as outlined in Section 3.2.3.2.3. Two assumptions for flow distribution have been made in the thermal analysis of the core as follows:

- a. For the maximum design condition and for the analysis of the hottest channel, all fuel assemblies receive minimum flow for the worst power condition.
- b. For the most probable design conditions predicted, average flows have been assigned for each fuel assembly consistent with location and power.

3.2.3.1.1.6 Maximum Reactor Design Overpower

Core performance is assessed at the maximum design overpower of 112 percent of the reference design core power level of 2568 MWt. The maximum overpower will not be exceeded under normal operating conditions.

3.2.3.1.1.7 Maximum Design Conditions

The maximum design condition is analyzed at the overpower limit of 112 percent, as stated above. This condition also assumes that the worst nuclear, thermal, and mechanical conditions exist simultaneously in a particular subchannel. If this channel meets all the thermal design criteria, then it can be safely assumed that all other channels in the core will be no worse thermally than this limiting channel. The maximum design conditions are represented by the following assumptions:

- a. The limiting fuel assembly possesses the fuel pin with the maximum value of $F_{\text{delta-h}}$ (as determined from examination of the referenced design radial peaking distribution and from examination of the maximum, nominal, and minimum fuel assembly spacing to find the effects on the local peaking distribution).

- b. The maximum value of F_z nuclear (max/avg axial fuel rod heat input) is determined for the limiting steady-state condition such that sufficient margin is provided to accommodate asymmetrical axial power shape considerations which will permit core operation with both positive and negative power imbalance. The maximum errors on core pressure and inlet temperature are applied in the most conservative manner.
- c. Every channel in the core is assumed to have the nominal pressure drop associated with the core flow conditions.
- d. The limiting fuel assembly is penalized 5 percent in isothermal flow such that it receives only 95 percent of the flow associated with an average core bundle at 100 percent of system flow (four pump operation).
- e. The limiting fuel assembly is assumed to have only 98 percent of the nominal assembly area due to the proximity of the adjacent fuel assemblies.
- f. The limiting assembly also contains spacer grid form loss coefficients which are based upon minimum spacing and worst case grillage flow area tolerances.
- g. On the subchannel types with maximum values of $F_{\text{delta-h}}$ three different hot channel factors are applied:
 - 1) The channel is assumed to have a reduced flow area, represented by the flow area reduction factor, F_A , of less than 1.0 (see Table 3.2-11).
 - 2) The fuel pin will have the greatest heat output by virtue of the local heat flux factor, F_Q , which increases the local surface heat flux, (see Table 3.2-11).
 - 3) The power peaking factor, F_Q , which increases the overall fuel pin power rating, will be greater than 1.0 (see Table 3.2-11).

3.2.3.1.1.8 Most Probable Design Conditions

In general, the most probable design condition is defined as occurring at the nominal power rating (100 percent) of the core. In addition, this analysis assumes that nominal specified conditions prevail in the core for the reference design peaking conditions. Thus, the most probable design conditions are assumed to be the same as the maximum design conditions with the following exceptions:

- a. The limiting fuel pin in the hottest assembly is assumed to have a nominal value of $F_{\text{delta-h}}$ nuclear, whereas the relative assembly powers remain the same as shown for the maximum design condition.
- b. The limiting fuel assembly is assumed to have no flow maldistribution, nor is there a flow area penalty for the peripheral flow channels due to assembly spacing. In addition, spacer grid form loss coefficients are based on nominal spacing.

- c. Hot channel factors are not applied to the subchannels; thus, they have a nominal flow area and the maximum calculated value of heat input; F_A and F_Q are assigned values of 1.0.
- d. No pressure or core inlet temperature errors are taken into consideration.

3.2.3.1.1.9 Hot Channel Performance for Four-Pump Operation

The hottest unit cell with all surfaces heated has been examined for hot channel factors, departure from nucleate boiling ratios (DNBRs), and quality for a range of reactor powers. The cell has been examined for the maximum value of $F_{\text{delta-h}}$ nuclear. The hot channel was assumed to be located in a fuel assembly with minimum flow. The heat generated in the fuel is 97.3 percent of the total nuclear heat. The remaining 2.7 percent is assumed to be generated in the coolant as it proceeds up the channel within the core and is reflected as an increase in delta-T of the coolant.

Operating pressure and inlet temperature error bands are reflected in the total core and hot channel thermal margin calculations in the direction producing the lowest DNBRs or highest qualities. The engineering hot channel factors used in the design analysis are described in Section 3.2.3.2.3. The DNBR versus power is shown in Figure 3.2-13. The hot channel exit quality for various powers is shown on Figure 3.2-14. The engineering hot channel factors and summary results are listed in Table 3.2-14. Figure 3.2-13, Figure 3.2-14 and Table 3.2-14 reflect "initial cycle" analysis.

3.2.3.1.1.10 Hot Channel Performance Summary for Partial Pump Flow

The power limitations imposed on the reactor because of the loss of one or more pumps has been examined by studying DNBRs and quality in the hot channel for a range of reactor powers. The system parameters used in the analysis are the same as the ones discussed in Section 3.2.3.1.1.9 above. A constant reactor vessel average temperature of 5790F was used to determine inlet temperature. The DNB ratio versus power for the flow conditions caused by loss of pumps is given in Table 3.2-15 and Figure 3.2-15. The hot channel quality at the minimum DNBR point at the same conditions is shown in Figure 3.2-16.

Two limits have been placed on the analysis. One is the recommended minimum DNBR of 1.3 and the other is a quality limit of 15 percent at the point of the minimum DNBR. This limit has been used because the limits of the correlation are 15 percent quality at the DNB location. Table 3.2-15 summarizes the power limitations on reactor operation at 2120 psig as defined by the hot channel conditions. The overpower margins and system limitations are discussed in Section 4.1.1.3, Partial Loop Operation. The one-pump power level DNBRs and quality are given for the maximum desired power of 30 percent rather than for the above limiting conditions. The steady-state rated power level is determined by dividing the maximum design overpower by the desired overpower margin. Figures 3.2-15, 3.2-16 and Table 3.2-15 reflect initial cycle analysis.

3.2.3.1.1.11 Hot Channel Performance Summary for Loss of All Coolant Flow

A reduction in the reactor coolant flow rate can occur from mechanical failures or from a loss of electrical power to the pumps. With four independent pumps available, a mechanical failure in one pump will not affect operation of the others. With the reactor at power, the effect of loss of

coolant flow is a rapid increase in coolant temperature due to reduction of heat removal capability. This temperature increase could result in DNB if corrective action were not taken immediately. An analysis, reported in Reference 18, for 4-pump coastdown or locked rotor incident, was performed to determine hot channel DNB peaking factors. The parameters of the Densification Report, (Reference 19) were used in this analysis. The results showed the DNBR remained above 1.3 (W-3) for the four-pump coastdown and the fuel cladding temperature remained below criteria limits for the locked rotor transient. For this particular case, the W-3 DNB was bounding, and, therefore, results using the BAW-2 correlation would be within this bounding limit.

3.2.3.1.2 Application of Computer Codes to Fuel and Clad Thermal Calculations

Fuel and cladding thermal conditions are determined for fuel rods prepressurized with helium. Fuel pin and clad temperatures and pressures, fuel densification and swelling, fission gas release, cladding creep and gap closure were calculated by the TAFY (Reference 20) computer code for the initial cycle and TACO-2 (Reference 82), TACO-3 (Reference 99) and GDTACO (Reference 100) for the current cycle. TACO-2 and TACO-3 are advanced versions of the TACO code discussed in 3.2.3.1.2.2 below. Starting with Cycle 19, the COPERNIC fuel performance code (Reference 133) is utilized along with the TACO family of codes.

3.2.3.1.2.1 TAFY

This section is maintained for historical purposes only.

The fuel temperature and gas pressure computer code TAFY was developed to calculate fuel temperatures, expansion, densification, equiaxed and columnar grain growth, center piping of fuel pellets, fission gas release, and fission gas pressure.

The radial expansion of the fuel pellet is computed from the mean fuel temperature and the average coefficient of linear expansion for the fuel over the temperature range considered. This model combined with the model for calculating the heat transfer coefficient was compared with the model developed by Reference 22. The difference in fuel growth for the two calculation models was less than the experimental scatter of data.

The fuel may be divided into as many as 30 radial and 70 axial increments for the analysis. An iterative solution for the temperature distribution is obtained, and the thermal conductivity of the fuel is input as a function of temperature. The relative thermal expansion of the fuel and cladding is taken into account when determining the temperature drop across the gap between the fuel and cladding surfaces.

The temperature drop across the gap is calculated using a gap conductance model based on the methods reported in Reference 23. The model, which has the capability of calculating gap conductance before fuel to clad contact as well as after contact is an extension of the methods suggested by Reference 24. This fuel-to-clad heat transfer is a function of gap width, gas conductivity, mean conductivity of the interface materials, mean surface roughness, material hardness, and fuel-to-clad contact pressure. Before total fuel-to-clad contact is made, a fraction of the fuel, based on fuel OD and gap size, is in contact with the clad. A constant pressure is applied to this fraction to simulate the effects of fuel cracking, (References 25, 26, and 27).

A polynomial fit relationship for fuel thermal conductivity is used. The B&W reference design (Reference 28) curve, illustrated in Figure 3.2-17 is a modification of the relationship presented in Reference 29. The curve yields a conservative integrated thermal conductivity of 93 W/cm with relatively little increase in conductivity beyond 3000°F.

An earlier version of TAFY was used to establish the first core parameters. The differences between the two versions are as follows:

- a. The earlier version adjusted the fuel density to account for fuel migration due to grain growth and centerline melt. This correction will tend to increase the thermal conductivity value used for the fuel. No such density correction was made for the present version of TAFY.
- b. The earlier version predicts lower gap conductance.

Figures A-1 through A-4 of Reference 20 show comparison between the results of the two versions of TAFY.

3.2.3.1.2.2 TACO

The computer code TACO provides a calculational tool to conservatively predict fuel pin temperature and pressure. TACO includes models for fuel and cladding temperature distribution, fuel densification and swelling, fission gas release, cladding creep, and gap closure.

The temperature distribution in cylindrical fuel rods is determined from solution to the heat diffusion equation. The fuel thermal conductivity is presented in TACO by a polynomial with temperature as a variable. This polynomial is corrected for density changes by the Maxwell-Eucken relationship. The thermal conductivity for the UO₂ fuel is based on Reference 28.

The fuel pellet thermal expansion model assumes linear expansion and is discussed in the TACO topical report, Reference 21. The coefficients of linear expansion for fuel and for cladding are discussed in the TACO topical report.

For the calculation of the fuel-cladding interface coefficient, the fuel pellet is assumed to be concentric within the cladding. This model is based on Reference 23 in conjunction with Reference 24.

The time dependent in reactor densification and swelling is described, along with supporting data, in Reference 30. Densification is assumed to continue until the maximum density is reached. The density is then assumed constant until the remaining pellet porosity is filled, after which the pellet is assumed to swell at the rate of $7 \times 10^{-23} \text{ cm}^3/\text{fission}$.

The effects of cladding creepdown, thermal expansion, elastic deformation, and stress free irradiation growth are considered in the TACO code. The radial, hoop, and axial strain components due to each of these effects are calculated and added.

Cladding creep will affect the dimensions of the cladding through the mechanism of uniform cladding creepdown. The approach used in TACO parallels the model described in Reference 31.

Cladding dimensional changes are also affected by irradiation. The axial expansion associated with cladding irradiation affects the fuel pin's plenum volume, which in turn affects the internal pin pressure. The data reported in Reference 32 are used for the irradiation growth model in TACO.

Utilizing photographs of transverse cross sections of irradiated fuel pins, a model has been developed that describes the fraction of gap closure attributable to pellet cracking and relocation. The fractional gap closure is given in terms of burnup and average linear heat rate which is incorporated in the calculation of the gap conductance.

The fission gas release correlation is taken from experimental data, (Reference 28) describing the fraction of gas released as a function of the volumetric average temperature. The amount of fission gas release is found by multiplying the amount produced by the release fraction corresponding to the temperature in a particular fuel segment. The NRC burnup enhancement model is used at burnups greater than 20,000 MWd/MTU to include the effects of burnup on fission gas release. The total gas released is obtained by summing over all fuel segments.

The design criterion on fuel temperature is that no melting should occur at any time in life during normal operation and incidents of moderate frequency. The linear heat rate at which fuel melting occurs, fuel rod internal pressures, and fuel temperatures are predicted by TACO. Uncertainties and conservatisms associated with this prediction are as follows:

- a. The pin power history is conservatively chosen to envelop all limiting pins during core life. The axial power and burnup shapes are varied several times during core life to realistically model actual fuel pin effects.
- b. The burnup determination contains a 10 percent uncertainty. This is in addition to the conservatism in defining the local peaking factor (hot rod to assembly average) described above.
- c. Nominal fuel pellet and clad dimensions used for the analysis reported herein are given in Table 3.2-16. Nominal dimensions are used in combination with conservative assumptions for fuel rod power history and fuel densification for the calculation of maximum fuel temperatures and internal pressure as well as the linear heat generation rate limit corresponding to centerline fuel melt.
- d. TACO uses a polynomial relationship for fuel thermal conductivity. The B&W reference design curve yields a conservative integrated thermal conductivity of 93 W/cm to fuel melting with relatively little increase in conductivity beyond 3000F.
- e. Data concerning the effect of burnup on the melting temperature of UO_2 at high burnup. The two sets of data by Reference 33 predict EOC (43,000 MWd/MTU) melting points of about 5000F and 4800F, respectively. The more conservative data, which shows a linear reduction from 5080F at BOC to 4800F at 43,000 MWd/MTU, are used to determine fuel melting in B&W's fuel temperature analysis.

- f. In addition to the above items, the following additional conservatisms are imposed on the fuel temperature calculations:
- (1) No fuel pellet grain restructuring is considered.
 - (2) Closed fuel porosity is assumed.
 - (3) Conservative values of cladding surface roughness and fuel pellet sorbed gas content are applied.
 - (4) An isotropic densification of 3.5 percent TD for diametric and axial changes is used in fuel temperature analyses. This value is a conservative estimate of the upper bound on fuel densification.
- g. Separate fuel densification assumptions are used to be conservative for both fuel temperature and internal pin pressure analysis. Maximum densification is conservative for the calculation of fuel temperatures because it results in the maximum pellet clad gap. Minimum densification is conservative for pin pressure calculations because this results in the minimum plenum volume increase. The densification assumptions made are based on a review of re-sinter data taken on B&W fuel and bounds for the maximum and minimum expected. The maximum densification assumption is based on individual pellet effects and is conservative for fuel temperature and fuel melt limit analysis. The minimum densification assumption is based on average pellet effects and is conservative for pin pressure calculation.

3.2.3.1.2.3 TAFY/TACO Differences

a. Gap Conductance

TACO and TAFY use the same correlation for gap conductance (References 23 and 24), but TACO does not include the interfacial pressure term. This is conservative and therefore an acceptable approach. Both TACO and TAFY introduce a radiation heat transfer term to account for radiation between the surfaces when it is not negligible.

b. Fuel Restructuring

Both TACO and TAFY contain fuel restructuring models to account for columnar grain growth. For TACO, restructuring is not expected to occur during normal operating conditions, and therefore this model is not to be used. For TAFY, when columnar grain growth or centerline melt occurs, a center void is formed, and the fuel temperatures are calculated using a hollow cylinder model and the related heat transfer equation.

TAFY has an option for no restructuring; NRC's acceptance of TAFY is based on the use of this option.

c. Fuel Densification and Swelling

For TACO, the combined effects of in-reactor fuel densifications and swelling are considered and modeled as an integral part of the same equation, as described in BAW-10038P (Reference 21). In TACO, no equiaxed grain growth is assumed to occur.

(The densification penalty based on NRC guidelines is discussed in Section 3.2.3.2.3.7 Fuel Temperature).

A graphical representation of fuel swelling is shown on Figure 1 of Reference 20 and used as a model in TAFY to calculate the fuel volume increase resulting from swelling. Fuel densification in TAFY as columnar grain growth and centerline melt phenomena are handled as discussed above in Item b above.

d. Fuel and Cladding Temperature Distribution

TACO - The temperature distribution is found from solutions to the heat diffusion equation with the aid of the heat flow integral SKdt for fuel temperature. The transient term dT/dt is negligible and therefore assumed equal to zero. Values for conductivity coefficient, for this solution, correspond to fuel at theoretical densities. For densities less than theoretical, a correction based on the relationship developed (Reference 34) is used.

TAFY - The solution used for the radial temperature distribution is the same as for TACO. However, the density correction term is somewhat different, as reported in BAW-10044 (Reference 20).

e. Gap Closure From Pellet Cracking

TACO - Contains an empirical model for gap closure due to fuel relocation. This model is based on gap-width and crack-width measurements on transverse sections of irradiated fuels.

TAFY - No model for gap closure due to fuel relocation. This is conservative and therefore should be considered an acceptable approach.

f. Cladding Pressure Effects and Creepdown

TACO - The TACO correlation tends to slightly underpredict the internally pressurized test results. In addition, the TACO correlation for creep strains depends exponentially upon the generalized stress in the fuel cladding. Since the generalized stresses are lower for externally pressurized rods than for internally pressurized rods at the same hoop stress, the predicted creep strains will be lower in the TACO expression for externally pressurized fuel rods. This effect enhances the under prediction when the correlation is applied to fuel rods. The net effect is that the empirical constants in the correlation significantly underpredict the creep in actual fuel rods. Since the underpredicted creep rate yields larger pellet to cladding gaps, the cladding correlation appears conservative for fuel thermal performance calculations and is therefore adequate for its application in TACO.

TAFY - Fission gas pressures in PWR fuel pins may reach 2000 psi. Since the external pin pressure generally will be above 2000 psi, the net pressure differential on the cladding may range from about 2000 psi inward to nearly zero during lifetime. Thus, it is important to consider these effects on the cladding diameter, especially as they pertain to fuel-cladding contact pressure. Using the thin cylinder approach, the change in inner diameter of the cladding due to pressure is calculated by $\Delta d_{ic} = d_{ic}^2 \Delta P / E_c (d_{oc} -$

d_{ic}) where d_{ic} and d_{oc} are the inner and outer diameters of the cladding, E_c is the modulus of elasticity in psi, and ΔP is the pressure differential across the cladding (a positive differential is directed outward). Note that the TAFY method is not creep rate dependent.

g. Fission Gas Production and Release

Both TACO and TAFY use the same approach regarding fission gas production and release up to burnups of 20,000 MWd/MTU, in that the fission gas release is based on the results reported in Reference 35. This method assumes that temperature is the only factor in controlling fission gas release. TAFY uses the same correlation above this burnup level. For TACO, NRC's evaluation of TACO, dated March 1977, indicated that the NRC staff considers the use of B&W fission gas release model appropriate for burnups up to 20,000 MWd/MTU; however, the model is acceptable for licensing calculations only when modified according to Equation A, for burnups greater than 20,000 MWd/MTU.

Eq. (A)

$$F(Bu, T) = f(T) + \frac{1 - E^{-0.435 \times 10^{-4} (Bu - 20,000)}}{1 + \frac{0.665}{f(T)} E^{-1.107 \times 10^{-4} (Bu - 20,000)}}$$

Where:

T = fuel temperature, C;

Bu = burnup, MWd/mtU;

$f(T) = -0.323 + 0.4077 \times 10^{-3} T, > 0.01$

3.2.3.1.2.4 COPERNIC

The COPERNIC computer code is the fuel performance code for fuel rod design and analysis of slightly enriched uranium dioxide fuels and uranium-gadolinia fuels with the advanced cladding material, M5. The COPERNIC code can be used for a broad range of fuel rod design, analysis and optimization tasks. Its primary focus is fuel rod licensing-type analyses, which include fuel rod internal gas pressure, LOCA initialization, fuel melt, cladding strain, creep collapse initialization, and cladding peak oxide thickness analysis.

The COPERNIC is an amalgam of individual phenomenological models tied together by a master program that integrates the geometric and temporal solution. These individual phenomenological models simulate the various mechanisms at work in a fuel rod - heat generation, heat transfer and thermal expansion in the fuel and cladding; fuel densification and swelling; fuel fracture and relocation; fission gas generation and release; pellet and cladding stresses and strains; and cladding corrosion and hydriding.

The thermal models include the coolant-rod heat transfer, the fuel-cladding gap conductance, the fuel thermal conductivity, the heat transfer gap closure, and the fuel radial power

distribution. The COPENIC fuel thermal conductivity model is composed of a phonon term and a high-temperature electronic term. The phonon term considers both a burnup degradation function and a radiation degradation term. The fuel thermal conductivity is corrected by a temperature-dependent porosity factor.

The effects of pellet and cladding dimension change are considered in the COPENIC code. The COPENIC code calculates the total pellet strains from solid swelling, gaseous swelling, densification, thermal expansion, and relocation models. The total cladding strains are calculated from the thermal, creep, elastic, and high stress creep models.

Two fission gas release models operate within the COPENIC code: a steady-state model and a transient model that tracks the fuel response to rapid power changes. The COPENIC steady-state model has two parts: an athermal knockout-recoil component and thermally activated diffusion component, leading to a grain boundary accumulation, saturation, and release. The transient gas release model consists of an enhanced diffusion model for short times, and a burst model that involves controlled release of the grain boundary gas inventory on a time basis related to the current diffusion coefficient.

The waterside cladding corrosion model in COPENIC determines the growth of the oxide layer that forms on the outer surface of the fuel rod cladding as function of environmental condition. The corrosion model is formulated with pre-transition and post-transition corrosion relationships.

3.2.3.1.3 End-of-Life Clad Transients

An investigation was carried out for the initial cycle analyzing the ability of the cladding to withstand various end-of-life transients which, though not considered normal, could occur during the life of the plant. The specific transients examined were loss of flow at 108 percent power and power excursions up to 114 percent of the reference design core power level (2568 MWt). The latter value is the maximum power attainable with a flux trip point setting of 107.5 percent. The effects of internal cladding pressure and system pressure on the integrity of the cladding during the normal shutdown for refueling and due to depressurization transients were also examined.

For the flow coastdown analysis, temperatures for the fuel and cladding during a coastdown from 108 percent power were obtained. Even starting from this overpower condition, there was no rise in temperature in either the fuel or cladding following the loss of pumping power. The fact that the pumps have been designed to include a rotational inertia equivalent to 70,000 lb-ft² allows them to provide sufficient flow after the loss of power to avoid temperature increases and to maintain the DNBR for the hot channel at a value higher than the DNBR for continuous operation at the maximum overpower condition.

A power excursion transient to 114 percent was also considered. Since the DNB analysis has been done for steady-state operation at this power level, DNB was not a consideration. For this transient it was expected that a greater release rate of fission gases and, consequently, a greater internal pressure and stress of the cladding during the excursion would occur. The analysis of the internal pressure buildup and stress and strain in the cladding at 114 percent overpower was carried out with conservative assumptions. At the end of life, the calculated internal gas pressure is considerably below the design internal pressure. The tensile stress due to the maximum calculated fission gas pressure is less than 10 percent of yield strength.

During a normal reduction in power or a cooldown for refueling, the internal gas temperature and pressure decrease. An investigation of depressurization transients indicates that when coolant pressure is conservatively assumed to drop instantaneously to zero, the cladding stress due to internal pressure is less than yield strength. The design internal pressure in a hot fuel rod is 3300 psi. Maximum calculated fission gas pressure is considerably less than this even at 114 percent power in a high burnup rod as described in Section 3.2.3.2.3.8. The internal pressure required to cause clad stress equal to the yield strength at the hot spot in the core at maximum overpower conditions (maximum average clad temperature 711F) is 4500 psi. It is concluded that any conceivable depressurization transient cannot damage the clad.

3.2.3.2 Thermal And Hydraulic Evaluation

3.2.3.2.1 Introduction

Summary results for the characteristics of the reactor design are presented in Section 3.2.3.1. The statistical core design technique employed in the design represents a refinement in the methods for evaluating pressurized water reactors. Corresponding single hot channel DNB data were presented to relate the new method with previous criteria. A comprehensive description of the new technique is included in this section to permit a rapid evaluation of the methods used.

A detailed evaluation and sensitivity analysis of the design has been made by examining the hottest channel in the reactor for DNB ratio, quality, and fuel temperature.

3.2.3.2.2 Statistical Core Design Technique

The core thermal design is based on a statistical core design technique developed by B&W. [Note: This technique should not be confused with the Statistical Core Design (SCD) methodology (Reference 111) described in Section 3.2.3.2.2.1] The method reflects the performance of the entire core and provides insight into the reliability of the calculation. The statistical core design technique considers all parameters that affect the safe and reliable operation of the reactor core. By considering each fuel rod, the method rates the reactor on the basis of the performance of the entire core. The result then provides a good measure of the core safety and reliability since the method provides a statistical statement for the total core. This statement also reflects the conservatism or design margin in the calculation.

A reactor safe operating power has been determined by the ability of the coolant to remove heat from the fuel material. The criterion that best measures this ability is the DNB, which involves the individual parameters of heat flux, coolant temperature rise, and flow area. The DNB criterion is commonly applied through the use of the DNBR. This is the minimum ratio of the DNB heat flux (as computed by the DNB correlation) to the surface heat flux. The ratio is a measure of the margin between the operating power and the power at which a DNB might be expected to occur in that channel. The DNBR varies over the channel length, and it is the minimum value of the ratio in the channel of interest that is used.

The calculation of DNB heat flux involves the coolant enthalpy rise and coolant flow rate. The coolant enthalpy rise is a function of both the heat input and the flow rate. It is possible to separate these two effects; the statistical hot channel factors required are a heat input factor, F_Q , and a flow area factor, F_A . In addition, a statistical heat flux factor, F_Q'' , is required; the heat flux factor statistically describes the variation in surface heat flux. The DNBR is most limiting

when the burnout heat flux is based on minimum flow area (small F_A) and maximum heat input (large F_Q), and when the surface heat flux is large (large F_Q''). The DNB correlation is provided in a best-fit form, i.e., a form that best fits all of the data on which the correlation is based. To afford protection against DNB, the DNB heat flux computed by the best-fit correlation is divided by a DNB factor (BF) greater than 1.0 to yield the design DNB surface heat flux. The basic relationship involves as parameters statistical hot channel

$$\text{DNBR} = \frac{Q''_{\text{DNB}}}{\text{BF}} \times f(F_A, F_Q) \times \frac{1}{Q''_{\text{surface}} \times F_Q''}$$

and DNB factors. The DNB factor (BF) above is usually assigned a value of unity when reporting DNBRs so that the margin at a given condition is shown directly by a DNBR greater than 1.0, i.e., 1.30 in the hot channel.

Selected heat transfer data are analyzed to obtain a correlation. Since thermal and hydraulic data generally are well represented with a Gaussian (normal) distribution, see Figure 3.2-18, mathematical parameters that quantitatively rate the correlation can be easily obtained for the histogram. These same mathematical parameters are the basis for the statistical burnout factor (BF).

In analyzing a reactor core, the statistical information required to describe the hot channel subfactors may be obtained from data on the as-built core, from data on similar cores that have been constructed, or from the specified tolerances for the proposed core. The design factors are shown graphically on Figures 3.2-19 and 3.2-20.

All the plots have the same characteristic shape whether they are for subfactors, hot channel factors, or burnout factor. The factor increases with either increasing population or confidence. The value used for the statistical hot channel and burnout factor is a function of the percentage of confidence desired in the result, and the portion of all possibilities desired, as well as the amount of data used in determining the statistical factor. A frequently used assumption in statistical analyses is that the data available represent an infinite sample of that data. The implications of this assumption should be noted. For instance, if limited data are available, such an assumption leads to a somewhat optimistic result. The assumption also implies that more information exists for a given sample than is indicated by the data; it implies 100 percent confidence in the end result. The B&W calculation procedure does not make this assumption, but rather uses the specified sample size to yield a result that is much more meaningful and statistically rigorous. The influence of the amount of data, for instance, can be illustrated easily as follows: Consider the heat flux factor which has the form

where:

$$F_Q'' = 1 + K\sigma F_Q''$$

F_Q'' = the statistical hot channel factor for heat flux

K = a statistical multiplying factor

$\sigma F_Q''$ = the standard deviation of the heat flux factor, including the effects of all the subfactors

If $\sigma F_Q'' = 0.05$ for 300 data points, then a K factor of 2.608 is

required to protect 99 percent of the population (initial core).

The value of the hot channel factor then is:

$$F_Q = 1 + (2.608 \times 0.050) = 1.1304$$

and will provide 99 percent confidence for the calculation.

If, instead of using the 300 data points, it is assumed that the data represent an infinite sample, then the K factor for 99 percent of the population is 2.326. The value of the hot channel factor in this case is:

$$F_Q = 1 + (2.326 \times 0.050) = 1.1163$$

which implies 100 percent confidence in the calculation. The values of the K factor used above are taken from Reference 36. The same basic techniques can be used to handle any situation involving variable confidence, population, and number of points.

The statistical factors are then used to determine the minimum fraction of rods protected or that are in no jeopardy of experiencing a DNB at each nuclear power peaking factor. Since this fraction is known, the maximum fraction in jeopardy is also known. It should be recognized that every rod in the core has an associated DNBR that is substantially greater than 1.0, even at the design overpower and that theoretically no rod can have a statistical population factor of 100 percent, no matter how large its DNBR.

Because both the fraction of rods in jeopardy at any particular nuclear power peaking factor and the number of rods operating at that peaking factor are known, the total number of rods in jeopardy in the whole core can be obtained by simple summation. The calculation is made as a function of power and the plot of rods in jeopardy versus reactor overpower is obtained. The summation of the fraction of rods in jeopardy at each peaking factor summed over all peaking factors can be made in a statistically rigorous manner only if the confidence for all populations is identical. If an infinite sample is not assumed, the confidence varies with population. To form this summation, then, a conservative assumption is required. The B&W total core model assumes that the confidence for all rods is equal to that for the least-protected rod, i.e., the minimum possible confidence factor is associated with the entire calculation.

Based on the maximum design conditions, the result of the foregoing technique performed for the initial cycle was this statistical statement:

There is at least a 99 percent confidence that at least 99.96 percent of the rods in the core are in no jeopardy of experiencing a DNB, even with continuous operation at the design overpower.

The maximum design conditions are represented by these assumptions:

- a. The maximum design values of $F_{\text{delta-h}}$ (nuclear maximum/average total fuel rod heat input) are obtained by examining the maximum, nominal, and minimum fuel assembly spacing and determining the worst value for rod peaking.
- b. The maximum value of F_Z (nuclear maximum/average axial fuel rod heat input) is determined for the limiting transient or steady-state condition.

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- c. Every coolant channel in the core is assumed to have less than the nominal flow area represented by engineering hot channel factors, F_A , less than 1.0.
- d. Every channel is assumed to receive the minimum flow associated with core flow maldistribution.
- e. Every fuel rod in the core is assumed to have a heat input greater than the maximum calculated value. This value is represented by engineering hot channel heat input factors, F_Q and F_Q'' , which are greater than 1.0.
- f. Every channel and associated fuel rod has a heat transfer margin above the experimental best-fit limits reflected in DNBRs greater than 1.0 at maximum overpower conditions.

The statistical core design technique may also be used in a similar manner to evaluate the entire core at the most probable mechanical and nuclear conditions to give an indication of the most probable degree of fuel element jeopardy. The result of the technique based on the most probable design conditions leads to a statistical statement which is a corollary to the maximum design statement. For the initial cycle the statement is:

There is at least a 99 percent confidence that at least 99.993 percent of the rods in the core are in no jeopardy of experiencing a DNB, even with continuous operation at the design overpower.

The most probable initial core design conditions are assumed to be the same as the maximum design conditions with these exceptions:

- a. Every coolant channel is assumed to have the nominal flow area ($F_A = 1.0$).
- b. Every fuel rod is assumed to have (1) the maximum calculated value of heat input and (2) F_Q and F_Q'' are assigned values of 1.0.
- c. The flow in each coolant channel is based on a power analysis without flow maldistribution factors.
- d. Every fuel rod is assumed to have a nominal value for $F_{\text{delta-h nuclear}}$.

The full meaning of the maximum and most probable design statements requires additional comment. As to the 0.04 percent or 0.007 percent of the rods not included in the statements, statistically it can be said that no more than 0.04 percent or 0.007 percent of the rods will be in jeopardy, and that in general the number in jeopardy will be fewer than 0.04 percent or 0.007 percent. The statements do not mean to specify a given number of DNBRs, but only acknowledge the possibility that a given number could occur for the overpower conditions assumed. Analyses for 100 percent rated power conditions show that essentially none of the fuel rods are subject to a DNB.

3.2.3.2.2.1 Statistical Core Design Methodology

The Statistical Core Design (SCD) methodology (Reference 111 and 112) is a thermal-hydraulic analysis technique that gives additional DNBR margin by statistically combining core state and fuel assembly uncertainties, while retaining the criterion that the core is protected by designing to avoid departure from nucleate boiling. The traditional method of treating uncertainties is to

assume the worst level of each uncertainty simultaneously. Applying statistical techniques allows for a realistic assessment of core DNB protection.

The SCD methodology consists of identifying the variables that are important to the DNB analysis (i.e., fraction of reactor thermal power, fraction of nominal RCS flow, system pressure, core subcooled inlet temperature, and hot pin radial peaking factor), their uncertainties, and their uncertainty distributions. The individual uncertainties are propagated through a thermal-hydraulic model in order to obtain an overall uncertainty on the calculated DNBR.

Once the DNBR uncertainty is determined, a Statistical Design Limit (SDL) is established to replace the traditional CHF correlation limit DNBR. Subsequently, the thermal-hydraulic codes are run with nominal input conditions, and the resulting DNBR is compared to the SDL to determine the core DNBR margin. Variables not treated in deriving the SDL continue to be input at their most adverse allowable levels.

A Statistical Design Limit (SDL) was determined for the BWC CHF correlation for B&W 177 FA plants using the LYNXT family of thermal-hydraulic codes. This limit provides 95 percent protection at a 95 percent confidence level against hot pin DNB. The corresponding core-wide protection on a pin-by-pin basis using real peaking distributions is greater than 99.9 percent. The SDL (BWC) is equivalent to the traditional DNBR limit of 1.18 (BWC), which only accounts for DNBR correlation uncertainty. Similarly, a SDL was determined for the BHTP CHF correlation, which is equivalent to the traditional DNBR limit of 1.132 (BHTP). The uncertainty parameters that were used in the SCD development of the SDL are listed in Table 3.2-11.

In order to retain margin to offset effects not treated in the SDL development (such as transition core effects, deviations in uncertainty values from those incorporated in the SDL, or other cycle-specific emergent issues), a more conservative Thermal Design Limit (TDL) of 1.50 is used as the basis for thermal-hydraulic analyses using SCD. In addition, a portion of the DNB margin gained by switching from the non-SCD core thermal-hydraulic methodology to the SCD methodology was used to justify a higher design radial-local peaking factor of 1.80 (vs. 1.714). The higher factor of 1.80 was chosen to provide more cycle design flexibility and less restrictive core operating limits for normal operations.

3.2.3.2.3 Evaluation of the Thermal and Hydraulic Design

3.2.3.2.3.1 Hot Channel Coolant Quality and Void Fraction

An evaluation of the hot channel coolant conditions provides additional confidence in the thermal design. Sufficient coolant flow has been provided to ensure low quality and void fractions. The quality in the hot channel versus reactor power is shown on Figure 3.2-14. The sensitivity of channel outlet quality with pressure and power level is shown by the 2185 and 2120 psig system pressure conditions examined. These calculations were made for the maximum design value of $F_{\text{delta-h}}$. Additional calculations for a 10 percent increase in $F_{\text{delta-h}}$ were made at maximum overpower. The significant results of both calculations are summarized in Table 3.2-17.

The conditions of Table 3.2-17 were determined with all of the hot channel factors applied. Additional calculations were made for unit cell channels without engineering hot channel factors to show the coolant conditions more likely to occur in the reactor core. A nominal value for $F_{\text{delta-h}}$ was examined with and without fuel assembly flow distribution hot channel factors at 2185 psig as shown on Figure 3.2-21. These results show that the exit qualities from the

hottest cells are lower than the maximum design conditions. Figures 3.2-14, 3.2-21 and Table 3.2-17 reflect analysis performed for the initial cycle.

3.2.3.2.3.2 Core Void Fraction

The core void fractions were calculated at 100 percent rated power for the normal operating pressure of 2185 psig and for the minimum operating pressure of 2120 psig. The influence of core fuel assembly flow distribution was checked by determining the total voids for both 100 and 95 percent total core flow for the two pressure conditions. The results are presented in Table 3.2-17.

The most conservative condition of 95 percent flow at 2120 psig results in an acceptable void volume in the core. Conservative maximum design values were used to make the calculation.

The void program uses a combination of Reference 37 model with Reference 38 correlation between void fraction and quality. The Bowring model considers three different regions of forced convection boiling. They are:

a. Highly Subcooled Boiling

In this region, the bubbles adhere to the wall while moving upward through the channel. This region is terminated when the subcooling decreases to a point where the bubbles break through the laminar sublayer and depart from the surface. The highly subcooled region starts when the surface temperature of the clad reaches the surface temperature predicted by the Jens and Lottes equation. The highly subcooled region ends when:

Eq. (A)

$$T_{\text{sat}} - T_{\text{bulk}} = \frac{\eta \phi}{V}$$

Where:

ϕ = local heat flux, Btu/hr-ft²

η = $1.863 \times 10^{-5} (14 + 0.0068p)$

V = velocity of coolant, ft/sec

p = pressure, psia

The void fraction in this region is computed in the same manner as Reference 39, except that the end of the region is determined by Equation (A) rather than by a vapor layer thickness. The nonequilibrium quality at the end of the region is computed from the void fraction as follows:

Eq. (B)

$$x_d^* = \frac{1}{1 + \rho_f/\rho_g(1/a_d-1)}$$

Where:

x_d^* = nonequilibrium quality at end of Region 1

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$$a_d = \text{void fraction at } T_{\text{sat}} - T_{\text{bulk}} = \frac{\eta \phi}{V}$$

$$\rho_f = \text{liquid component density, lb/ft}^3$$

$$\rho_g = \text{vapor component density, lb/ft}^3$$

b. Slightly Subcooled Boiling

In this region, the bubbles depart from the wall and are transported along the channel (condensation of the bubbles is neglected). This region extends to a point where the thermodynamic quality is equal to the apparent quality. In general, this is the region of major concern in the design of pressurized water reactors.

The nonequilibrium quality in this region is computed from the following formula:

Eq. (C)

$$x^* = x_d^* + \frac{P_h}{m h_{fg}(1 + \epsilon)} \int_0^Z (\phi - \phi_{SP}) dz$$

Where:

$$x^* = \text{nonequilibrium quality in Region 2}$$

$$h_{fg} = \text{latent heat of vaporization, Btu/lb}$$

$$\frac{1}{1 + \epsilon} = \text{fraction of the heat flux above the single-phase heat flux that actually goes to producing voids}$$

$$\phi^{SP} = \text{single-phase heat flux, Btu/hr-ft}^2$$

$$m = \text{mass flow rate, lb/hr}$$

$$P_h = \text{heated perimeter, ft}$$

$$Z = \text{channel distance, ft}$$

The void fraction in this region is computed from

Eq. (D)

$$a = \frac{x^*}{C_o [x^* + \rho_f - \rho_g (1 - x^*)] + \frac{38.3 A_f \rho_g [\sigma g g_c (\rho_f - \rho_g)]^{1/4}}{m \left[\frac{\rho_f}{\rho_f(t)^2} \right]}} \quad (D)$$

Where:

$$G = \text{acceleration due to gravity, ft/sec}^2$$

g_c = constant in Newton's Second Law

$$g_c = 32.17 \frac{\text{lb m ft}}{\text{lb f sec}^2}$$

C_o = Zuber's distribution parameter

A_f = flow area, in²

σ = surface tension

a = void fraction

Equation (D) results from rearranging equations found in Reference 42 and assuming bubbly turbulent flow in determining the relative velocity between the vapor and the fluid. Zuber has shown that Equation (D) results in a better prediction of the void fraction than earlier models based on empirical slip ratios.

c. Bulk Boiling

In this region, the bulk temperature is equal to the saturation temperature, and all the energy transferred to the fluid results in net vapor generation. Bulk boiling begins when the thermodynamic (heat balance) quality, x , is greater than the nonequilibrium quality, x^* . The void fraction in this region is computed using Equation (D) with the thermodynamic quality, x , replacing x^* .

3.2.3.2.3.3 Coolant Channel Hydraulic Stability

Flow regime maps of mass flow rate and quality were constructed in order to evaluate channel hydraulic stability. The confidence in the design is based on a review of both analytical evaluations (References 40-43) and experimental results obtained in multiple rod bundle burnout tests. Bubble-to-annular and bubble-to-slug flow limits proposed by Reference 41 are consistent with the B&W experimental data in the range of interest. The analytical limits and experimental data points have been plotted to obtain the maps for the four different types of cells in the reactor core. These are shown in Figures 3.2-22, 3.2-23, 3.2-24 and 3.2-25. The experimental data points represent the exit conditions in the various types of channels just previous to the burnout condition for a representative sample of the data points obtained at design operating conditions in the nine rod burnout test assemblies. In all of the bundle tests, the pressure drop, flow rate, and rod temperature traces were repeatable and steady and did not exhibit any of the characteristics associated with flow instability.

Values of hot channel mass velocity and quality at 114 and 130 percent of the reference core design power (2568 MWt) for both maximum design and most probable conditions are shown on the maps. These representative operating points are within the bounds suggested by Baker. Experimental data points for the reactor geometry with much higher qualities than the operating conditions have not exhibited unstable characteristics.

3.2.3.2.3.4 Hot Channel DNB Comparisons

The DNBR ratios for the hottest channel in the initial cycle were determined using the W-3 correlation, and the results are shown on Figure 3.2-13. DNBRs are shown for the design 1.50 axial maximum/ average symmetrical cosine flux shape from 100 to 140 percent of the reference core design power level. The W-3 DNBR at the maximum design over power level of 114 percent of the reference core design power (2568 MWt) is 1.55. This compares with the suggested W-3 design value of 1.3. A ratio of 1.3 is reached at 122.5 percent power at an exit quality of 9.2 percent, which is within the prescribed quality limits of the correlation.

The sensitivity of the DNBR with F_z nuclear also was examined from 100 to 140 percent power. A cosine flux shape with an F_z of 1.80 and $F_{\text{delta-h}}$ of 1.78 resulted in a W-3 DNBR of 1.30 at 114 percent power.

The influence of change in $F_{\text{delta-h}}$ was determined by analyzing the hot channel for an $F_{\text{delta-h}}$ of 1.96. This value is 10 percent above the maximum design value of 1.78. The resulting W-3 DNBR ratio is 1.23 at 114 percent power. This value is well above the correlation best fit values of 1.0 for the severe conditions assumed.

Table 3.2-18 shows typical values of the hot channel DNBR from 100 to 112 percent of reference design power (2568 MWt) based on the BHTP correlation which is used for the current cycle.

3.2.3.2.3.5 Reactor Flow Effects

Another significant variable to be considered in evaluating the design is the total system flow. Conservative values for system and reactor pressure drop have been determined to ensure that the required system flow is obtained in the as-built plant. The reactor vessel model test and the production pump tests have confirmed the design conditions.

The reactor core flow and power capability were evaluated by determining the steady-state power DNBRs versus flow. Analyses were made for: (1) variations of power capability with total reactor flow for a constant DNBR of 1.30, (2) DNBRs for design flow with variations in hot channel mixing coefficients, and (3) DNBRs for gross flow variations of ± 10 percent. The results are indicated on Figures 3.2-26 and 3.2-27. For the analysis shown on Figure 3.2-26 for design hot channel condition, the flow was determined that would give a DNBR of 1.30 for a range of reactor powers. This analysis shows, for example, that a DNBR of 1.30 can be maintained in the hot channel at 114 percent power with a total reactor flow of 118×10^6 lb/hr as compared with the available design flow of 131.3×10^6 lb/hr. The results shown by line 2 in Figure 3.2-27 are the DNBR for rated flow of 131.3×10^6 lb/hr versus power. The limiting condition is 122.5 percent power for a DNBR of 1.30. Lines 1 and 3 show the DNBRs versus power where the total system flow has been varied by ± 10 percent. Adequate DNBRs can be maintained with a substantial reduction in Reactor Coolant System flow. Figures 3.2-26 and 3.2-27 reflect analysis performed for the initial cycle.

The foregoing sensitivity analyses were made using a fuel assembly design mixing coefficient of 0.02. A sensitivity analysis for a range of coefficients was made for the rated flow condition. The results are shown by Lines 4 and 5 of Figure 3.2-27 and discussed in more detail in Section 3.2.3.2.3.10, Evaluation of the DNBRs in the Unit, Wall, Control Rod, and Corner Cells.

The difference between the reactor system flow and the reactor core flow is the bypass flow. The bypass flow is defined as that part of the flow that does not contact the active heat transfer

surface area. This part of the flow exists primarily through three different paths. These paths are (1) through the core shroud, (2) through the control rod guide tubes and instrument tubes, and (3) between all interfaces separating the inlet and outlet regions.

The bypass flow rates are determined through an iterative process. The total core flow is obtained by taking the difference between the total system flow and an assumed bypass value. Pressure drops are now calculated between the vessel inlet and outlet. These pressure drops are then used to calculate the flow rates through all the predefined bypass paths. The process is repeated until the calculated bypass value is equal to the assumed bypass value.

The flow through the control rod guide tubes is calculated with all the control rods assumed to be in the fully withdrawn position. Flow through the interfaces separating the inlet and outlet plenums is calculated by assuming the maximum tolerances at hot conditions to result in the minimum flow resistance. Additional conservatism is applied to determining the bypass flow by adding an allowance of 50 percent of the calculated value to account for any uncertainties. The resulting bypass through the various paths is given in Table 3.2-19.

3.2.3.2.3.6 Reactor Inlet Temperature Effects

The influence of reactor inlet temperature on power capability at design flow was evaluated. A variation of one degree F in reactor inlet temperature will result in a power capability change of 0.6 percent at a given DNBR.

3.2.3.2.3.7 Fuel Temperature

a. Method of Calculation

Fuel pin and clad temperatures and pressures, fuel densification and swelling, fission gas release, cladding creep, and gap closure are calculated by the computer codes TAFY and TACO. The description and differences between the two codes is discussed in Section 3.2.3.1.2. Starting with Cycle 19, the COPENIC fuel performance code is utilized. A description of this code is provided in Section 3.2.3.1.2.4. This section is maintained for historical purposes only.

b. Fuel Densification Consideration

As a result of the guidelines set forth in Reference 44, the fuel pin performance was reanalyzed to determine what penalties, if any, would have to be imposed on the plant operation. In Reference 19, a generic densification penalty model for power spiking was developed for the reference design core power level of 2568 MWt. In Reference 45, the densification penalties were applied to modify the TMI-1 setpoints. These modifications ensure that the thermal design criteria are not exceeded. The modifications to the RPS reflect a reduction in the design overpower from 114 to 112 percent of rated power and a minor reduction in allowable imbalance limits.

The modifications to the plant operation and analysis were implemented as of Cycle 2 (Reference 18).

Inspection of operating data and irradiated fuel assemblies from several pressurized water reactors has shown that fuel densification has caused gaps to form within the fuel

rod cladding. In cores containing unpressurized fuel rods (not the case for TMI-1, which is prepressurized), the cladding adjacent to these gaps has suffered local collapse in a significant number of cases. In the case of TMI-1, the concern as it affected the thermal analysis was with:

- (1) Increase in the axial and local core power density. Densification reduces the active level length of the core and raises the average power density. Also, as fuel density increases and pellet length decreases, some pellets become fixed in place and cause gaps to form in the pellet stack. The presence of a gap produces localized power spikes both in the fuel column containing the gap and in the surrounding fuel columns.
- (2) Densification of the fuel pellet increases the radial gap between the fuel and the clad with an apparent reduction in the gap conductance.

To account for the above concerns, the TAFY thermal code was rerun prior to Cycle 2 reload with the following assumptions:

- (1) In the equiaxed zone 3 percent porosity is assumed but is not used in the calculation; that is, the input value for fuel density is used and therefore no credit is taken in the calculation for increased thermal conductivity of UO_2 for the higher fuel density.
- (2) The option in the code for no restructuring of fuel has been used in the analysis.
- (3) The gap conductance was reduced by 25 percent.

The results of the analysis are presented in Reference 45, and the generic evaluation of fuel densification is presented in Reference 19.

For Cycle 2, the nominal heat rate at 2535 MWt increased from 5.583 kW/ft to 5.693 kW/ft due to densification. The central fuel melting limit, (linear heat rate capability) for Cycle 2, changed from 22.2 kW/ft before densification to 19.6 kW/ft after.

The thermal model considerations as well as the effects on power distribution as discussed above are factored into the present core parameters.

c. Fuel Center Temperature Results at Beginning and End of Cycle

The results of the analysis for center temperatures for the initial cycle are shown on Figures 3.2-28 and 3.2-29 for beginning-of-cycle (BOC) and end-of-cycle (EOC) conditions.

The results for a typical post-initial cycle are shown in Figure 3.2-30.

Average fuel temperatures at nominal linear heat rates for the current cycle are shown in Table 3.2-11. All values are from the COPENIC analysis of the specific fuel batch.

The BOC and EOC gas conductivity values of 0.14 and 0.05 Btu/hr ft °F, Reference 33, were used to establish fuel melting temperatures.

A sensitivity analysis for initial cycle fuel was made for a range of cold diametral clearances to show the effect of clearance on fuel center temperature. Temperatures for the nominal design clearance, the maximum design clearance (0.0085 inches), and the maximum possible (0.0095 inches) are shown on Figures 3.2-28 and 3.2-29.

The calculated EOC center fuel temperatures are higher than the BOC values because of the reduction in the conductivity of the gas in the gap. The effect is apparent even though the fuel-to-clad diametral gap decreases. The calculation includes the effect of fuel swelling due to irradiation and accounts for the flux depression on the center of the rod because of the self-shielding effect of UO_2 (non-uniform power generation). The effect of clad irradiation and thermal expansion is also considered.

The transient analyses at accident and normal conditions have been made using the design fuel thermal conductivity curve (Figure 3.2-17) to reflect a conservative value for the maximum average temperature and stored energy in the fuel. Use of this curve results in a higher temperature and, therefore, a lower Doppler coefficient, since it decreases with temperature. Thus, the resultant Doppler effect is also conservative.

d. Fuel Center and Average Temperature Variations with Fuel Burnup

Maximum fuel temperature conditions are affected by the fuel-to-clad heat transfer coefficient. The coefficient is determined by fuel-to-clad clearance and gas conductivity. Fuel swelling due to burnup decreases the clearance and results in improved heat transfer; however, the conductivity of the gas decreases with the addition of xenon and krypton gas to the helium fill gas. A combination of these effects for BOC and EOC conditions was described in Item c above. It was conservatively assumed that the peak power could be obtained in a fuel rod with the maximum burnup. It is not likely that the peak power will be experienced in a rod with any significant fuel depletion; however, an additional sensitivity analysis of fission gas conductivity and fuel growth from zero to maximum burnup has been made for the maximum design cold diametral clearance of 0.0085 inches. This analysis shows that the worst combination of gas conductivity and fuel-to-clad clearance occurs at the maximum burnup-or-end of cycle. Center and average fuel temperature peak at end of cycle. The fuel center temperature for a fuel rod with the maximum design diametral clearance, maximum enrichment, and maximum linear heat rate will change with burnup as shown in the upper curve on Figure 3.2-31. A maximum design linear heat rate of 17.63 kW/ft for the 100 percent power condition was calculated for the initial core. The lower curve on Figure 3.2-31 is a comparison of the fuel center temperature at end of cycle and beginning of cycle as a function of heat rate.

e. Equilibrium Cycle Average Fuel Temperatures

An analysis was done to show equilibrium average fuel conditions in the core. A typical fuel cycle end-of-cycle condition was used to determine the fraction of fuel at a given average condition. The results are shown in Figure 3.2-32 for 100 percent reference core design power level of 2568 MWt. The average fuel temperature in the core is 1280F. A typical reactor power distribution at end-of-cycle as shown on Figure 3.2-33

was used to obtain fuel rod heat rates. A symmetrical cosine axial power distribution with a 1.50 maximum/average value was used to predict the axial heat rate distribution. It was assumed that 97.3 percent of the power is generated in the fuel. The core radial power, assembly local power, and fuel rod axial power distributions were used to obtain the temperature distribution for this analysis.

The B&W design thermal conductivity was used to provide conservative values for fuel conductivity. The maximum powers occurred in fuel assemblies with one and two cycles of operation, as shown on Figure 3.2-33, and the assemblies with the highest burnup did not exceed 1.087 times the average power for the typical case analyzed. Typical 6 and 10 kW/ft rod radial temperature profiles are shown on Figure 3.2-34.

3.2.3.2.3.8 Fission Gas Release

Chapter 5 of Reference 133 details fission gas release in the COPERNIC code. The initial and the most recent versions of TAFY as well as TACO, discussed in Section 3.2.3.1.2, use the following method for fission gas release calculations. As stated, this model is acceptable for burnups up to 20,000 MWt/MTU. A correction factor to account for increased releases for higher burnups, as shown in Equation (A) of Section 3.2.3.1.2, has been used in the latest analysis.

The fission gas release is based on results reported in Reference 35. Additional data from References 47, 48 and 49 have been compared with the suggested release rate curve. The release rate curve (Reference 35) is representative of the upper limit of release data in the temperature region of most importance. A maximum internal pressure of 3300 psi is used to determine the clad stresses reported in Section 3.2.4.2.1.2.

The design values for fission gas released from the fuel and for the maximum clad internal pressure were determined by analyzing various operating conditions and assigning suitable margins for possible increases in local or average burnup in the fuel. A detailed analysis of the design assumptions for fission gas release and the relationship of burnup, fuel growth, and initial diametral clearance between the fuel and clad are summarized in the following paragraphs. An evaluation of the effect of having the fuel pellet internal voids available as gas holders is also included.

a. Design Assumptions

1) Fission Gas Release Rates

The fission gas release rate was calculated as a function of fuel temperature at 112 percent of rated power when the TAFY code was used. The fission gas release curve and the supporting data are shown on Figure 3.2-35. Most of the data are on or below the design release rate curve. A release rate of 51 percent is used for the portion of the fuel above 3000F. The fuel temperatures were calculated using the B&W design fuel thermal conductivity curve which yields conservatively high values for fuel temperatures. For TACO, the same procedure is used, except as modified and described in Section 3.2.3.1.2.

2) Axial Power and Burnup Assumptions

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The temperature conditions in the fuel are determined for the most severe axial power peaking expected to occur. Two axial power shapes have been evaluated to determine the maximum release rates. These are 1.50 and 1.70 maximum/average shapes, as shown on Figure 3.2-36. The quantity of gas released is found by applying the temperature-related release rates to the quantities of fission gas produced along the length of the fuel rod.

The quantity of fission gas produced in a given axial location is obtained from reactor core axial region equilibrium burnup studies. Three curves showing the axial distribution of burnup as a local-to-average ratio along the fuel rod are shown on Figure 3.2-36. Values at 100 and 300 days of operation and end-of-life are shown.

The end-of-cycle axial burnup distribution is the condition with the maximum fission gas inventory. The average burnup at the end of cycle in the hot fuel rod is 40,900 MWd/MTU, which has been determined as follows:

Calculated hot bundle average burnup, MWd/MTU	35,400
Hot fuel rod burnup factor	1.05
Margin for calculation accuracy	1.10
Hot rod maximum average burnup, MWd/MTU	40,900

The local burnup along the length of the fuel rod is the product of the hot rod maximum average value given above and the local-to-average ratio shown on Figure 3.2-36. The resulting hot rod local maximum burnup for the end-of-cycle condition is about 44,950 MWd/MTU.

3) Hot Rod Power Assumptions

Maximum fuel temperature was determined as a function of fuel burnup at the maximum design heat rate by operating continuously at the rated power level throughout the cycle. Conservative fuel and clad properties and gas conditions were used to determine the fuel-to-clad heat transfer coefficient. A study of the power distribution in the core through several cycles to equilibrium conditions shows the assembly average burnups as a function of power for all assemblies. The power-burnup history for fuel rods is also determined by considering the local peaking factors. A conservative margin for calculation accuracy was included in the reference design power history. Fission gas release and internal rod pressure were determined for rated and maximum overpower conditions.

4) Fuel Growth Assumptions

The fuel growth was calculated as a function of burnup as indicated in Section 3.2.4.2.1. Fuel pellet dimensions in the thermal temperature and gas release models were increased to the end-of-cycle conditions as determined above.

5) Gas Conductivity and Contact Heat Transfer Assumptions

The quantity of fission gas released is a function of fuel temperature and fuel burnup. The temperatures are influenced by three factors: (1) the conductivity of the fission gas in the gap between the fuel and clad, (2) the diametral clearance between fuel and clad, and (3) the heat transfer conditions when the fuel expands enough to contact the clad. Burnup is influenced by two factors: (1) the power history of the fuel and (2) the initial fuel concentration.

Gas conductivity varied with burnup in the analysis. Diametral clearances of 0.0025 to 0.0065 inch reflecting minimum and maximum design clearances after fuel growth were analyzed. The contact heat transfer coefficients were calculated as suggested in Reference 23 and are illustrated on Figure 3.2-37. The gap conductance is plotted as a function of heat rate for two fuel-to-clad gap clearance conditions to show the dependence of fuel-to-clad heat transfer on this parameter. Heat transfer models presented in the literature (References 28 and 50) suggest that gap conductance is higher than the design values used in this analysis.

b. Summary of Results

The reference design power history was used to determine the maximum internal fuel rod pressure and corresponding fission gas release rate. Pressures and rates were determined for various cold diametral clearances and axial power peaking shapes.

Fission gas release rate results are shown on Figure 3.2-38. The highest release rate as shown on Figure 3.2-38 is for 1.70 maximum/average power shape, with an end-of-cycle axial burnup shape and closed fuel porosity. The increase in release rate with diametral clearance results from higher fuel temperatures. The release rate at the minimum clearance, 0.0045 inches, is 5.0 percent. This condition is equivalent to the minimum gap after irradiation growth and produces the maximum clad stress (maximum sized pellets with minimum internal diameter cladding). The release rate is 16.0 percent for the maximum design diametral clearance (0.0085 inch).

An additional case was examined to check the sensitivity of the calculations to axial power shapes. The results are shown on Figure 3.2-38. The effect of open fuel porosity on fission gas release is also shown on Figure 3.2-38.

Maximum internal pressure due to the release of fission product gases is shown on Figure 3.2-39. Internal clad pressure is plotted as a function of cold diametral gap for the 1.50 maximum/ average and 1.70 maximum/average axial power shapes with the expected end-of-cycle burnup distribution. The lower curve for the 1.70 maximum/average power shape assumed that 7.5 percent of the fuel volume is available to hold the released gas (open porosity). The remaining curves correspond to closed porosity. The present design condition being used to determine the maximum internal pressure assumes a closed-pore condition with all released gas contained outside the fuel pellets in the spaces between the expanded dished ends of the pellets, the radial gaps, if any, and the void spaces at the end of the fuel rod. The effects of fuel densification and grain growth described in Section 3.2.3.2.3.7 are included in the

analysis. The expected maximum internal pressures are not strongly influenced by the axial power shape.

The allowable design internal pressure of 3300 psi is well above the maximum values of internal pressures calculated for open or closed pores, and the maximum internal pressure should only occur with the maximum design diametral clearance condition. An increase in maximum fuel burnup can be tolerated within the prescribed internal pressure design limits.

It has been indicated in Reference 22 and in AECL-1598 that the UO_2 fuel is plastic enough to flow under low stresses when the temperature is above 1800°F. That fraction of the fuel below this temperature may retain a large portion of the original porosity and act as a fission gas holder. The hottest axial locations producing the highest clad stresses will have little if any fuel below 1800°F. However, the end of the fuel rods will have some fuel below this temperature.

The approximate fraction of the fuel below 1800°F at maximum overpower for a 1.5 axial power shape is as follows for various cold diametral clearances. The bundle average powers shown on Figure 3.2-33 were used to determine the heat rates.

<u>Clearance (in.)</u>	<u>Percent of Fuel Below 1800F (percent)</u>
0.0045	79
0.0070	69
0.0085	62

The retention of fuel porosity in the low-temperature and low-burnup regions will result in modest reductions in internal gas pressure.

Gas pressure at rated and overpower conditions is shown on Figure 3.2-39. The overpower condition is not expected to occur except for brief periods during operating transients.

3.2.3.2.3.9 Hot Channel Factors Evaluation

a. Rod Pitch and Bowing

A flow area reduction factor is determined for the as-built fuel assembly by taking channel flow area measurements and statistically determining an equivalent hot channel flow area reduction factor. Interior channel measurements and measurements of the channels formed by the outermost fuel rods with adjacent assemblies have been analyzed. Coefficients of variation for each type of channel have been determined.

In the analytical solution for a channel flow, each channel flow area is reduced over its entire length by the F_A factors shown on Figure 3.2-20 for the desired population protected at a 99 percent confidence. The hot channels have been analyzed using

values for 99 percent population protected, or F_A in the interior cells of 0.98 and F_A in the wall cells of 0.97^Δ, as listed in Table 3.2-14.

Special attention is given to the influence of water gap variation between fuel assemblies when determining rod powers. Nuclear analyses have been made for the nominal, maximum, and minimum spacing between adjacent fuel assemblies. The nominal and maximum hot assembly fuel rod powers are shown on Figures 3.2-40 and 3.2-41. The hot channel nuclear power factor ($F_{\text{delta-h nuclear}}$) of 1.78 discussed in Section 3.2.3.1.1.3 is based on Figure 3.2-41 for the worst water gap between fuel assemblies. The factor of 1.783 is a product of the hot assembly factor of 1.68 times the 1.061 hot rod factor. This power factor is assigned to the hottest unit cell rod which is analyzed for burnout. Peaking factors for other channels are obtained in a similar manner. In all cases, the combined flow spacing and power peaking producing the lowest DNBR is used.

b. Fuel Pellet Diameter, Density, and Enrichment Factors

These variations in the pellet size, density, and enrichment are reflected in coefficients of variation. These variations have been obtained from the measured or specified tolerances and combined statistically to give a power factor on the hot rod. For 99 percent confidence and 99 percent population conditions, this factor, F_Q , is 1.011 and is applied as a power increase over the full length of the hot channel fuel rod. The local heat flux factor, F_Q'' , for similar conditions is 1.014. These hot channel values are given in Table 3.2-11. The corresponding values of F_Q and F_Q'' with 99.99 percent population protected are 1.025 and 1.03, respectively. A conservative value of F_Q'' of 1.03 for 99 percent confidence and 99.99 percent population is used for finding the maximum fuel linear heat rates as shown in Section 3.2.3.1.2.1.

These factors are used in the direct solution for channel enthalpies and are not expressed as factors on enthalpy rise as is often done.

c. Flow Distribution Effects

1) Inlet Plenum Effects

The inlet plenum effects have been determined from the 1/6 scale model flow test. It has been conservatively assumed that the flow in the hot bundle position is 5 percent less than average bundle flow under isothermal conditions corresponding to the model flow test conditions. An additional reduction of flow due to hot assembly power is described below.

2) Redistribution of Adjacent Channels of Dissimilar Coolant Conditions

The hot fuel assembly flow is less than the flow through an average assembly at the same core pressure drop because of the increased pressure drop associated with a higher enthalpy and quality condition. This effect is allowed for by making a direct calculation for the hot assembly flow. The combined effects of upper and lower plenum flow conditions and heat input to the hot assemblies have been used to determine hot assembly flow. The worst flow maldistribution effect has been assumed in the design, and the minimum hot assembly flow has been

calculated to be 87 percent of the average assembly flow at an overpower of 114 percent of the reference core design power (2568 MWt).

Actual hot assembly flows are calculated rather than applying an equivalent hot channel enthalpy rise factor.

3) Physical Mixing of Coolant Between Channels

The flow distribution within the hot assembly is calculated with a mixing code that allows an interchange of heat between channels. Mixing coefficients have been determined from multirod mixing tests. The fuel assembly, consisting of a 15 x 15 array of fuel rods, is divided into unit, wall, control rod, and corner cells as shown on Figure 3.2-40. The mixed enthalpy for every cell is determined simultaneously so that the ratio of cell to average assembly enthalpy rise (enthalpy rise factor) and the corresponding local enthalpy are obtained for each cell. Typical enthalpy rise factors are shown on Figures 3.2-40 and 3.2-41 for the hot and surrounding cells.

3.2.3.2.3.10 Evaluation of the DNBRs in the Unit, Wall, Control Rod, and Corner Cells

a. DNB Results at Rated Flow

The DNBRs in the hot unit cell at the maximum design condition described in Section 3.2.3.1 are shown on Figure 3.2-13. The relationship shown is based on the application of the W-3 correlation.

An additional sensitivity analysis of the assembly corner, wall, i.e., peripheral, and control rod cells has been made for the worst combination of fuel assembly spacing and power peaking.

The sensitivity of the assembly design with respect to variations of mass velocity (G), channel spacing, mixing intensity, and local peaking on the DNBRs in the fuel assembly channels has been evaluated by analyzing the most probable conditions and the postulated maximum design condition. The summary results are given in Table 3.2-20. The unit cell DNBRs are repeated for comparison. All of the DNBRs are for an overpower level of 114 percent of the reference core design power level (2568 MWt) using the initial cycle W-3 correlation.

The DNBRs in all channels are high enough to ensure a confidence- population relationship equal to or better than that outlined in Section 3.2.3.1.1 for the hot unit cell channel. All of the wall, corner, and control rod cells have DNBRs equal to or greater than that of the unit cell hot channel. This results from a more favorable flow to power ratio in these cells associated with relatively larger flow areas.

The DNBRs were obtained by comparing the fuel rod local heat fluxes and channel coolant conditions with the limitations predicted by the correlation. Typical results are shown on Figures 3.2-42 and 3.2-43 for the most probable and maximum design conditions in the unit cell.

b. Fuel Rod Power Peaks and Cell Coolant Conditions

The most probable case local-to-average rod powers and the local-to-average exit enthalpy rise ratios are shown on Figure 3.4-40 for the hot corner, wall control rod, and unit cells in the hot fuel assembly. Values shown are for nominal water gaps between the hot fuel assembly and adjacent fuel assemblies with nominal flow to the hot fuel assembly, and with a minimum intensity of turbulence, β ,* equal to 0.02.

The maximum design case local-to-average rod powers (nuclear peaking factor) and exit enthalpy rise factors in the hot fuel assembly are shown on Figure 3.2-41. The factors were determined for this case with the minimum water gap between the hot fuel assembly and adjacent assemblies, with minimum flow to the hot fuel assembly, and with a minimum assumed intensity of turbulence, β , equal to 0.02. An evaluation of minimum, nominal, and maximum spacing between assemblies showed the minimum to have the lowest DNBRs.

A mixing coefficient of 0.02 was used for both most probable and maximum design case analyses. The influence of mixing coefficients is shown on Figure 3.2-27, for values ranging from 0.01 to 0.06. The value of 0.02 is sufficiently conservative for design evaluation. The conditions analyzed to obtain the DNBRs for various values of the mixing coefficients shown on Figure 3.2-27 were outlined previously in Section 3.2.3.2.3.9, Hot Channel Factors Evaluation.

c. Fuel Assembly Power and Rated Flow Conditions

The most probable and maximum design cases were run at 114 percent reactor power with the nominal and worst flow factors shown in Section 3.2.3.1.1.3. The 1.50 modified cosine axial power shape of Figure 3.2-36 was used to describe the worst axial condition.

The hot assembly flow under most probable conditions without a flow maldistribution effect is 96 percent of the average assembly flow, and the reduction in flow is due entirely to heat input effects. The hot assembly flow under the maximum design conditions is 87 percent of the average assembly flow and considers the worst combined effects of heat input and flow maldistribution.

*The intensity of turbulence, β , is defined as V'/V where V' is the transverse component of the fluctuating turbulent velocity, and V is the coolant velocity in the axial direction. This method of computing mixing is described by Reference 51.

3.2.3.2.3.11 Removal of Orifice Rod Assemblies

Fuel assemblies not containing control rods, BPRAs or neutron sources originally had Orifice Rod Assemblies (ORAs) installed in the guide tubes to minimize core bypass flow. Anomalous mechanical behavior of the BPRA and ORA latching mechanisms in operating plants made it prudent to remove the ORAs. All ORAs have been removed in the current cycle.

Since the number of control rods is fixed at 61, the number of vacant assembly locations for bypass flow depends upon the number of BPRAs used for the cycle. The number of BPRAs in the current core is given in Table 3.2-26; bypass flow is shown on Table 3.2-19.

To offset the effect of the increased core bypass flow on the thermal-hydraulic design, the reference design radial x local peaking factor ($F_{\text{delta-h}}$) has been reduced from 1.78 to 1.71. This reduction in $F_{\text{delta-h}}$ is conservative with respect to the maximum predicted peak pin power factor for the current cycle. Subsequently, application of the Statistical Core Design methodology provided additional DNB margin such that the design $F_{\text{delta-h}}$ was increased to 1.80. In addition, design changes to fuel assembly guide tubes have reduced core bypass flow. Present reactor core safety limits have been reevaluated based on the increased $F_{\text{delta-h}}$ and the current core bypass flow.

3.2.3.2.4 Flux Flow Trip Set Point

- a. To determine the flux flow trip set point that is necessary to meet the hot-channel DNBR criteria, several calculation steps are required.

These steps involve such things as the determination of steady- state operating conditions, fuel densification effects, and transient calculations.

1) Thermal-Hydraulic Conditions During Normal Operation

The hot channel thermal-hydraulic conditions are calculated for design conditions at 108 percent of the rated power of 2568MWt. The power level of 108 percent includes operation at 102 percent of rated power plus a maximum power level measurement error of 6 percent (4 percent neutron flux error and a 2 percent heat balance error). This serves as the benchmark calculation from which the densification penalty and the transient effects can be determined by the RADAR Computer Code (Reference 52). The steady state analysis was performed using the TEMP Computer (Reference 53) with the appropriate hot channel factors, coolant inlet temperature and system pressure errors, and a 5 percent hot assembly flow maldistribution factor applied. The design flow rate of 131.32×10^6 lb/hr (88,000 gpm/pump) was used for first-cycle analysis. For second-cycle analysis, the reanalysis used 106.5 percent of the design flow rate, based on system flow measurements made during the first cycle. For both cycles, the hot assembly power distribution consisted of a 1.78 radial local nuclear peaking factor ($F_{\text{delta-h}}$) with a 1.5 cosine axial flux shape. Incorporation of the increased flow rate into the analysis was accompanied by a corresponding increase in the reactor coolant inlet temperature, from 554°F to 555.6°F for the nominal, rated power condition (2568 MWt). Neither the increase in system flow nor the increase in inlet temperature represented a change in the operation of the plant.

As a result of pump and Reactor Coolant System tests, a majority of the orifice plugs were removed from peripheral fuel assemblies prior to startup of TMI-1 and other similar B&W 177 FA plants. This was done to preclude operation with excessive coolant flow through the reactor core. The result was an increase in the maximum core bypass (or leakage) flow conservatively estimated to be 2.3 percent (from 6.04 percent to 8.34 percent of total Reactor Coolant System flow). This increased leakage was not accounted for in those analyses based on design flow (Cycle 1) because it was a direct result of the higher system flow. For those analyses based upon 106.5 percent design flow, the increased

leakage was taken into account; thus, for an increase of 6.5 percent in system flow, the corresponding core flow increase was 4.2 percent.

2) Densification Effects

The fuel densification penalty applied to the hot channel for Cycle 1 operation was determined by methods discussed in Reference 54.

3) Effect on Open Vent Valve Assumption

It was conservatively assumed that one core barrel vent valve is stuck open. This assumption reduces the effective core flow rate by 4.6 percent and results in a corresponding reduction in minimum DNB. For second cycle analysis, the effect of this assumption is a reduction in predicted minimum DNBR for the 108 percent overpower, maximum design case from 2.00 to 1.85. This value represents the initial minimum DNBR for the transient analysis described below.

The RADAR computer code was used to analyze two isolated channels, representing an average subchannel and the hot subchannel. Primary result for the nominal subchannel calculation is pressure drop versus time for the average subchannel.

The hot subchannel and its associated fuel rod was modeled in the same manner as the first channel with appropriate hot channel factors added. Input power to this channel is higher than that of channel 1 by the maximum design radial x local power factor of 1.783 plus an added factor to account for the densification penalty. The flow rate in this subchannel is calculated for both initial and transient conditions so that the hot channel pressure drop always matches that of the average channel. The result was a more severe hot channel transient than would be indicated if the transient core flow function were applied directly to the hot channel.

4) Transient Hot Channel Conditions During a Loss of Flow

The flux flow trip set point is derived to protect the core during a one-pump coastdown. A one-pump coastdown is analyzed because redundant pump monitors are provided which will provide DNB protection for all other pump coastdowns, including coastdowns while the plant is in partial pump operation. The pump monitor logic will not cause a reactor trip for the loss of one pump from four-pump operation.

The initial hot channel DNBR was set equal to the steady-state value with densification and open vent valve effects included. The RADAR output in the form of hot channel DNBR versus time was the basis for establishing the flux flow ratio trip set point.

5) Rod Bowing Effects

Analysis was performed with the COBRA III-C code to determine the effect of a fuel rod bowing into the hot channel and reducing the flow area of that channel.

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The results demonstrate that rod bow of the magnitude predicted is adequately compensated for by the flow area reduction factor. Rod bow away from the hot channel was also analyzed. In this analysis, the effect of a power spike was added to the hot rod in the area of the minimum DNBR. This analysis also demonstrated that the DNBR results conservatively account for the effects of fuel rod bowing.

b. Procedure For Determining Flux Flow Set Point

The determination of the flux flow set point is accomplished in four basis steps. The result of these steps is designed to yield a value of the flux flow ratio that will prevent the minimum hot channel DNBR from going below the limiting design DNBR for the coastdown for which protection is required. These steps are as follows:

1) Total Time Determination

From a plot of minimum DNBR versus time, find the time that yields a DNBR of 1.3 for the maximum power level (108 percent) for the maximum number of pumps lost for which the flux flow trip must provide protection (one pump).

2) Coasting Time Determination

The total time to reach a DNBR of 1.3 minus a conservative value of the total trip delay time gives the maximum allowable coasting time prior to trip initiation.

3) Minimum Flow Determination

From a plot of flow versus time for the coastdown of interest, the percent flow for the maximum allowable coasting time is found. This yields the flow at which trip must be initiated.

4) Flux Flow Ratio Calculation

The maximum allowable flux flow ratio is the maximum real power level of interest (108 percent) minus the power level measurement error (6 percent) divided by the minimum flow.

c. Calculation Results

The analysis showed that a DNBR of 1.3 (W-3) is reached at about 3.35 sec; this yields a flux flow ratio of 1.08. This is the value presented in the Technical Specifications for densified fuel. Using the BAW-2 correlation, the limiting design DNBR (1.32 for Cycle 1, 1.30 for Cycle 2) is reached at 5.45 sec for both cases. Using the method defined in Item b above, with a trip delay of 1.3 sec, the maximum allowable flux flow ratio is then 1.12.

The method defined in Item b) above, was refined slightly to include the effect of "DNBR turnaround." This effect results from the fact that some finite time is required after control rod motion starts before the minimum DNBR is reached. For TMI-1 set point

analysis, this effect can be conservatively accounted for by adding 0.5 sec to the trip delay time. Using a value of 1.9 sec for trip delay, the maximum flux flow set point would then be reduced from 1.12 to 1.11. Current cycle analysis assures that the flux flow setpoint remains conservative.

It should be emphasized that the above described analyses were based on the assumption that one vent valve is stuck open. This assumption reduced the effective core flow by 4.6 percent. Elimination of this conservative assumption has the effect of increasing the calculated allowable flux flow set point by approximately 0.04 for a closed channel analysis. Thus conservatism of the Technical Specifications value (1.08) is ensured. Current cycle analysis has eliminated the vent valve penalty thus increasing the conservatism of the setpoint which remains at 1.08.

3.2.3.2.5 Evaluation of Internals Vent Valve

A vapor lock problem could arise if water is trapped in the steam generator blocking the flow of steam from the top of the reactor vessel to a cold leg leak. Under this condition, the steam pressure at the top of the reactor would rise and force the steam bubbles through the water leg in the bottom of the steam generator. This same differential pressure that develops a water leg in the steam generator will develop a water leg in the reactor vessel which could lead to uncovering of the core.

The most direct solution to this problem is to equalize the pressure across the core support shield, thus eliminating the depression of the water level in the core. This was accomplished by installing vent valves in the core support shield to provide direct communication between the top of the core and coolant inlet annulus. These vent valves open on a very low pressure differential to allow steam generated in the core to flow directly to the leak from the reactor vessel. Although the flow path to the steam generator is blocked, this is of no consequence because there is an adequate flow path to remove the steam being generated in the core.

During the vent valve conceptual design phase, criteria were established for valves for this service. The design criteria were: (1) functional integrity, (2) structural integrity, (3) remote handling capability, (4) individual part-capture capability, (5) functional reliability, (6) structural reliability, and (7) leak integrity throughout the design life. The design criteria resulted in the selection of the hinged-disc (swing-disc) check valve, which was considered suitable for further development.

Because of the unique purpose and application of this valve, B&W recognized the need for a complete detailed design and development program to determine valve performance under nuclear service conditions. This program included both analytical and experimental methods of developing data. The program is discussed in detail in Reference 55. The final design of the valve is discussed in Section 3.2.4.1.2.8.

It was concluded that vent valve performance will not be impaired during the course of an accident because disc free-motion part stresses remain within allowable limits, disc structural integrity is maintained, vessel pressure boundary integrity is maintained, and plastic deformation of the disc seating surface improves the venting function.

3.2.4 MECHANICAL DESIGN

3.2.4.1 Reactor Internals

Reactor internal components include the plenum assembly and the core support assembly. The core support assembly consists of the core support shield, vent valves, core barrel, lower grid, flow distributor, incore instrument guide tubes, and thermal shield. Figure 3.2-44 shows the reactor vessel, reactor vessel internals arrangement, and the reactor coolant flow path. Figure 3.2-45 shows a cross section through the reactor vessel, and Figure 3.2-46 shows the core flooding arrangement.

Reactor internal components do not include fuel assemblies, CRAs, surveillance specimen assemblies, or incore instrumentation. Fuel assemblies and control rod assemblies are described in Section 3.2.4.2, control rod drives in Section 3.2.4.3, surveillance specimen assemblies in Section 4.4.5, and incore instrumentation in Section 7.3.3.

The core internals are designed to meet the stress requirements of the ASME Code, Section III, during normal operation and transients. Additional criteria and analysis are given in Reference 3. A detailed stress analysis of the internals under accident conditions has been completed and is reported in Reference 2. This report analyzes the internals in the event of a major loss of coolant accident (LOCA) and for the combination of LOCA and seismic loadings. It is shown that although there is some internals deflection, failure of the internals will not occur because the stresses are within established limits. These deflections would not prevent CRA insertion because the control rods are guided throughout their travel, and the guide-to-fuel assembly alignment cannot change because positive alignment features are provided between them and the deflections do not exceed allowable values. All core support circumferential weld joints in the internals shells are inspected to the requirements of the ASME Code, Section III.

3.2.4.1.1 Plenum Assembly

The plenum assembly is located directly above the reactor core and is removed as a single component before refueling. It consists of a plenum cover, upper grid, CRA guide tube assemblies, and a flanged plenum cylinder with openings for reactor coolant outlet flow. The plenum cover is constructed of a series of parallel flat plates intersecting to form square lattices and has a perforated top plate and an integral flange at its periphery. The cover assembly is attached to the plenum cylinder top flange. The perforated top plate has matching holes to position the upper end of the CRA guide tubes. Lifting lugs are provided for remote handling of the plenum assembly. These lifting lugs are welded to the cover grid.

The CRA guide tubes are welded to the plenum cover top plate and bolted to the upper grid. CRA guide assemblies provide CRA guidance, protect the CRA from the effects of coolant cross flow, and provide structural attachment of the grid assembly to the plenum cover.

Each CRA guide assembly consists of an outer tube housing, a mounting flange, 12 perforated slotted tubes, and four sets of tube segments which are oriented and attached to a series of castings so as to provide continuous guidance for the CRA full stroke travel. The outer tube housing is welded to a mounting flange, which is bolted to the upper grid. Design clearances in the guide tube accommodate misalignment between the CRA guide tubes and the fuel assemblies. Final design clearances were established by tolerance studies and Control Rod Drive Line Facility (CRDL) prototype test results. The test results are described in Section 3.3.3.4.

The plenum cylinder consists of a large cylindrical section with flanges on both ends to connect the cylinder to the plenum cover and the upper grid. Holes in the plenum cylinder provide a flow path for the coolant water. The upper grid consists of a perforated plate which locates the lower end of the individual CRA guide tube assembly relative to the upper end of a corresponding fuel assembly. The grid is bolted to the plenum lower flange. Locating keyways in the plenum assembly cover flange engage the reactor vessel, the reactor closure head control rod drive penetrations, and the core support assembly. The bottom of the plenum assembly is guided by the inside surface of the lower flange of the core support shield.

3.2.4.1.2 Core Support Assembly

The core support assembly consists of the core support shield, core barrel, lower grid assembly, flow distributor, thermal shield, in-core instrument guide tubes, surveillance specimen holder tubes, and internals vent valves. Static loads from the assembled components and fuel assemblies and dynamic loads from CRA trip, hydraulic flow, thermal expansion, seismic disturbances, and LOCA loads are all carried by the core support assembly.

The core support assembly components are described in the following sections.

3.2.4.1.2.1 Core Support Shield

The core support shield is a flanged cylinder which mates with the reactor vessel opening. The forged top flange rests on a circumferential ledge in the reactor vessel closure flange. The core support shield lower flange is bolted to the core barrel. The inside surface of the lower flange guides and aligns the plenum assembly relative to the core support shield. The cylinder wall has two nozzle openings for coolant flow. These openings are formed by two forged rings, which seal to the reactor vessel outlet nozzles by the differential thermal expansion between the stainless steel core support shield and the carbon steel reactor vessel. The nozzle seal surfaces are finished and fitted to a predetermined cold gap providing clearance for core support assembly installation and removal. At reactor operating temperature, the mating metal surfaces are in contact to make a seal without exceeding allowable stresses in either the reactor vessel or internals. Eight vent valve mounting rings are welded in the cylinder wall. Internals vent valves are installed in the core support shield cylinder wall to control steam flow from the core following a postulated cold leg (reactor coolant inlet) pipe rupture as described in Section 3.2.4.1.2.8.

3.2.4.1.2.2 Core Barrel

The core barrel supports the fuel assemblies, lower grid, flow distributor, and incore instrument guide tubes. The core barrel consists of a flanged cylinder, a series of internal horizontal former plates bolted to the cylinder, and a series of vertical baffle plates bolted to the inner surfaces of the horizontal formers to produce an inner wall enclosing the fuel assemblies. The core barrel cylinder is flanged on both ends. The upper flange of the core barrel cylinder is bolted to the mating lower flange of the core support shield, and the lower flange is bolted to the lower grid assembly. All bolts are lock welded after final assembly. Coolant flow is downward along the outside of the core barrel cylinder and upward through the fuel assemblies contained in the core barrel. A small portion of the coolant flows upward through the space between the core barrel outer cylinder and the inner baffle plate wall. Coolant pressure in this space is maintained lower than the core coolant pressure to avoid tension loads on the bolts attaching the plates to the horizontal formers.

3.2.4.1.2.3 Lower Grid Assembly

The lower grid assembly provides alignment and support for the fuel assemblies, supports the thermal shield and flow distributor, and aligns the incore instrument guide tubes with the fuel assembly instrument tubes. The lower grid consists of two grid structures, separated by short tubular columns, and surrounded by a forged flanged cylinder. The upper structure is a perforated plate, while the structure consists of a machined forging.

The top flange of the forged cylinder is bolted to the lower flange of the core barrel.

A perforated flat plate located midway between the two grid structures aids in distributing coolant flow prior to entrance into the core. Alignment between fuel assemblies and incore instruments is provided by pads bolted to the upper perforated plate.

3.2.4.1.2.4 Flow Distributor

The flow distributor is a perforated dished head with an external flange which is bolted to the bottom flange of the lower grid. The flow distributor supports the incore instrument guide tubes and distributes the inlet coolant entering the bottom of the core.

3.2.4.1.2.5 Thermal Shield

A cylindrical stainless steel thermal shield is installed in the annulus between the core barrel cylinder and reactor vessel inner wall. The thermal shield reduces the incident gamma absorption internal heat generation in the reactor vessel wall and thereby reduces the resulting thermal stresses. The thermal shield upper end is restrained against inward and outward vibratory motion by restraints bolted to the core barrel cylinder. The lower end of the thermal shield is shrunk-fit on the lower grid flange and secured by 120 high strength bolts.

3.2.4.1.2.6 Surveillance Specimen Holder Tubes

Surveillance specimen holder tubes were installed on the core support assembly outer wall to contain the surveillance specimen assemblies. The tubes extended from the top flange of the core support shield down toward the lower end of the thermal shield. The holder tube had a 4 inch offset to place the center line of the specimens approximately 2 1/4 inches from the vessel inside wall. Reference 56 describes the holder tubes and specimen capsules in detail.

All surveillance specimen holder tubes have been removed from the reactor vessel. The tubes had been damaged as a result of vibration of the specimen trains and, as a result, the structural integrity of these tubes could not be ensured for continued operation. The justification for operating without the tubes is provided in Reference 57. This change does not reduce the margin of safety as defined in the Technical Specification.

3.2.4.1.2.7 InCore Instrument Guide Tube Assembly

The incore instrument guide tube assemblies guide the incore instrument assemblies from the instrument penetrations in the reactor vessel bottom head to the instrument tubes in the fuel assemblies.

Horizontal clearances are provided between the reactor vessel instrument penetrations and the instrument guide tubes in the flow distributor to accommodate misalignment. Fifty two incore instrument guide tubes are provided and are designed so they will not be affected by the core drop described in Section 3.2.4.1.

3.2.4.1.2.8 Internal Vent Valves

Internal vent valves are installed in the core support shield to prevent a pressure imbalance which might interfere with core cooling following a postulated inlet pipe rupture. Under all normal operating conditions, the vent valve will be closed. In the event of the pipe rupture in the cold leg of the reactor loop, the valve will open to permit steam generated in the core to flow directly to the leak and will permit the core to be rapidly recovered and adequately cooled after emergency core coolant has been supplied to the reactor vessel. The design of the internals vent valve is shown on Figure 3.2-47.

Each valve assembly consists of a hinged disc, valve body with sealing surfaces, split-retaining ring, and fasteners. Each valve assembly is installed into a machined mounting ring integrally welded in the core support shield wall. The mounting ring contains the necessary features to retain and seal the perimeter of the valve assembly. Also, the mounting ring includes an alignment device to maintain the correct orientation of the valve assembly for hinged-disc operation. Each valve assembly will be remotely handled as a unit for removal or installation. Valve component parts, including the disc, are of captured design to minimize the possibility of loss of parts to the coolant system, and all operating fasteners include a positive locking device. The hinged disc includes a device for remote inspection of disc function. Vent valve materials are given in Table 3.2-21.

The vent valve materials were selected on the basis of their corrosion resistance, surface hardness, anti-galling characteristics, and compatibility with mating materials in the reactor coolant environment.

The arrangement consists of eight 14 in inside diameter vent valve assemblies installed in the cylindrical wall of the internals core support shield (refer to Figure 3.2-44). The valve centers are coplanar and are 42 in above the plane of the reactor vessel coolant nozzle centers. In cross section, the valves are spaced around the circumference of the core support shield wall.

The hinge assembly consists of a shaft, two valve body journal receptacles, two valve disc journal receptacles, and four flanged shaft journals (bushings). Loose clearances are used between the shaft and journal inside diameters and between the journal outside diameters and their receptacles.

The hinge assembly is shown and the clearance gaps are identified on Figure 3.2-48. The bushing clearances are listed in Table 3.2-22.

The valve disc hinge journal contains integral exercise lugs for remote operation of the disc with the valve installed in the core support shield.

The hinge assembly provides eight loose rotational clearances to minimize any possibility of impairment of disc-free motion in service. In the event that one rotational clearance should bind in service, seven loose rotational clearances would remain to allow unhampered disc free

motion. In the worst case, at least four clearances must bind or seize solidly to adversely affect the valve disc free motion.

In addition, the valve disc hinge loose clearances permit disc self- alignment so that the external differential pressure adjusts the disc seal face to the valve body seal face. This feature minimizes the possibility of increased leakage and pressure-induced deflection loadings on the hinge parts in service.

The external side of the disc is contoured to absorb the impact load of the disc on the reactor vessel inside wall without transmitting excessive impact loads to the hinge parts as a result of a LOCA.

3.2.4.2 Core Components

The complete core has 177 fuel assemblies arranged in a square lattice to approximate the shape of a cylinder. All fuel assemblies are essentially similar in mechanical construction and are mechanically interchangeable in any core location. There are 61 CRAs. Depending on cycle design requirements, an Orifice Rod Assembly or a Burnable Poison Rod Assembly may be installed in fuel assemblies not containing a CRA. When used the Orifice Rod Assemblies (ORAs) limit guide tube bypass coolant flow through the fuel assembly guide tubes and the Burnable Poison Rod Assemblies (BPRAs) to ensure a negative moderator temperature coefficient. ORAs have been removed from the core (see Section 3.2.3.2.3.11).

3.2.4.2.1 Fuel Assemblies

3.2.4.2.1.1 Fuel Assembly Description

a. General

The fuel is sintered low-enriched UO_2 cylindrical pellets. The pellets are clad in either Zircaloy-4 or M5 (an NRC-approved, zirconium-based alloy) tubing and sealed by Zircaloy-4 or M5 end caps, welded at each end. The clad, fuel pellets, end caps, and fuel support components form a fuel rod. Two hundred and eight fuel rods, sixteen control rod guide tubes, one instrumentation tube assembly, seven segmented spacer sleeves, eight spacer grids, and two end fittings make up the basic fuel assembly (Figure 3.2-49). The guide tubes, spacer grids, and end fittings form a structural cage to arrange the rods and tubes in a 15 x 15 array. The center position in the assembly is reserved for instrumentation. Control rod guide tubes are located in 16 locations of the array. Fuel assembly components, materials, and dimensions are given in Table 3.2-16.

Substitution of Zircaloy-4 or stainless steel filler rods for fuel rods in fuel assemblies is permitted if justified by cycle-specific reload analyses, using an NRC-approved methodology. NRC-approved methodology includes those methodologies described in the FSAR and as referenced in Technical Specification Section 6.9.5.2 for establishing core operating limits. This requirement ensures conformance to the existing design limits and that safety analyses criteria are met before operation during the next fuel cycle. Flexibility to deviate from the number of fuel rods per assembly is desirable to permit timely removal of fuel rods that are found to be leaking during a refueling outage or are determined to be probable sources of future leakage. This improvement in the fuel performance program will provide for reductions in future

occupational radiation exposure and plant radiological releases. Additional design details are discussed in Section 3.2.4.2.1.1.d.

b. Fuel Rods

All fuel rods are internally prepressurized with helium. The fuel is in the form of sintered and ground pellets of low-enriched UO_2 . As of Cycle 10, fuel in selected rods may contain urania-gadolinia ($\text{UO}_2/\text{Gd}_2\text{O}_3$) integral burnable poison. As of Cycle 13, all fuel rods contain enriched axial blankets where the top and bottom sections of pellets are of a lower U235 enrichment than the nominal central section. Pellet ends are dished to minimize differential thermal expansion between the fuel and cladding. Radial growth of the fuel during burnup is accommodated by the pellet porosity, radial clearance between the pellets and the cladding, and by a small amount of permanent strain in the cladding. Fuel growth is calculated by the method given in Reference 58.

Below each fuel column is a spring spacer which axially locates the bottom of the fuel column and separates the fuel from the lower fuel rod end cap. This spring is designed to deflect at high column loads to prevent excessive axial strain in the cladding.

Above the fuel column is a spring spacer that separates the fuel from the fuel rod upper end cap. This spacer maintains the fuel column in place during shipping and handling. In operation, the spacer permits axial differential growth and thermal expansion between the fuel and the clad. This spacer also provides radial fuel rod cladding support.

Depending on the specific Mark B fuel rod design, metallic and ceramic spacers are located between the fuel pellets and the spring spacers to thermally insulate and separate fuel pellets from the spacers.

Fission gas release from the fuel is vented to voids within the pellets to the radial gap between the pellets and the cladding, and to the void spaces at top and bottom ends of the fuel rods.

c. Fuel Assembly

1) General

All fuel assemblies are similar in concept and are mechanically interchangeable. Mark B4 Fuel assemblies introduced in Cycle 2 incorporated minor modifications to the end fitting primarily to reduce fuel assembly pressure drop and to increase holddown margin. In Cycle 7 the Mark B4Z assembly was implemented which replaced the intermediate Inconel spacer grids with zircaloy grids. As of Cycle 8, the Mark B8 assembly was implemented with several design improvements including: a) reconstitutable upper end fittings; b) elimination of BPRA retainers; c) annealed guide tubes; d) higher burnup capability; and, e) debris-resistant fuel rod lower end plugs. The Mark B8 also has zircaloy intermediate spacer grids. The Mark B8V (Cycle 9) added a shot-peened coiled holddown spring, grippable upper fuel rod end plugs and bullet-nosed lower fuel rod end plugs. In Cycle 10, the Mark B9 assembly was introduced with a removable lower end fitting, lower guide tube bypass flow and an optimized fuel rod with larger pellet diameter to improve thermal performance. In Cycle 11 the Mark B10 assembly was introduced with a new cruciform leaf-type FA holddown spring in the upper end fitting to replace the helical coil spring design for better reliability and increased holddown force and with an improved reconstitutable guide tube upper

attachment nut. In Cycle 14 the Mark B12 assembly was introduced with new design features including: a) low corrosion, low growth M5 cladding and guide tubes; b) fine mesh debris filter; c) redesign cruciform holddown spring to reduce holddown force; and, d) heavier uranium loading. The Mark B12 design was shown to meet all design criteria in reference 120. In Cycle 17 the Mark B-HTP assembly was introduced with new design features including: a) low corrosion M5 top and intermediate HTP spacer grids; b) Inconel bottom HMP spacer grid; and, c) a no-direct-line-of-sight FUELGUARD debris filter. The Mark B-HTP design was shown to meet all design criteria in Reference 130.

The fuel assembly shown on Figure 3.2-49 is typical of the design used in the initial loading of the core. It is of the canless type where the eight spacer grids, end fittings, and the guide tubes form the basic structure. Fuel rods are supported at each spacer grid by contact points integral with the wall of the cell boundary. The guide tubes are permanently attached to the upper and lower end fittings. Use of similar material in the guide tubes and fuel rods results in minimum differential thermal expansion.

2) Spacer Grids

Spacer grids are constructed from strips which are slotted and fitted together in egg-crate fashion. Each grid has 32 strips, 16 perpendicular to 16, which form the 15 x 15 lattice. The square walls formed by the interlaced strips provide support for the fuel rods in two perpendicular directions.

3) Lower End Fitting

The lower end fitting positions the assembly in the lower core grid plate. The lower ends of the fuel rods rest on the grid of the lower end fitting. Penetrations in the lower end fitting are provided for attaching the control rod guide tubes and for access to the instrumentation tube. Depending on the specific Mk B fuel assembly design, lower end fittings may contain a fine mesh debris filter, as the Mark B12 fuel design, or a no-direct-line-of-sight debris filter, as the Mark B-HTP fuel design.

Reference 121 addressed the impact of the fine mesh filter on LOCA and safety analysis design basis requirements. The fine mesh was determined to have no impact on the potential for core inlet debris blockage during normal operation or for safety analysis events that do not result in reactor building sump recirculation; the design particulate size that can pass through the makeup and purification system is an order of magnitude smaller than the debris filter mesh size so particulates will pass through the filter. A LOCA was identified as the most severe event that would introduce larger debris into the RCS during reactor building sump recirculation; Reference 121 concluded that long-term cooling would be readily provided in the event of a cold leg or hot leg break. A similar evaluation was performed for the no-direct-line-of-sight FUELGUARD debris filter in Reference 131, which also concluded that long-term cooling would be readily provided in the event of a cold leg or hot leg break.

4) Upper End Fitting

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The upper end fitting positions the upper end of the fuel assembly in the upper core grid plate structure and provides means for coupling the handling equipment. An identifying number on each upper end fitting provides positive identification.

Integral with each upper end fitting are a holddown spring and spider to provide a positive holddown margin to opposite hydraulic forces.

In response to an NRC concern, an evaluation of core operation with broken springs has been performed and shows that the continued safe operation of B&W plants with such springs can be ensured (References 87 and 88).

Penetrations in the upper end fitting grid are provided for the guide tubes.

5) Guide Tubes

The zircaloy or M5 guide tubes provide continuous guidance to the control rods when inserted in the fuel assembly during operation and provide the structural continuity for the fuel assembly. Welded to each end of a guide tube are flanged and threaded sleeves, which secure the guide tubes to each end fitting by lock-welded or crimped nuts depending on whether the design is reconstitutable. Transverse location of the guide tubes is provided by the spacer grids.

6) Instrumentation Tube

This zircaloy or M5 tube serves as a channel to guide, position, and contain the incore instrumentation within the fuel assembly. The instrumentation probe is guided up through the lower end fitting to the desired core elevation. It is retained axially at the lower end fitting by a retainer sleeve.

7) Spacer Sleeves

On the Mark-B design, the spacer tube segments fit around the instrument tube between spacer grids and prevent axial movement of the spacer grids during primary coolant flow through the fuel assembly. In Cycle 12 a design change was made to the Mark B10 assembly spacer grid retention system that allows the spacer sleeves to positively capture the grid insert tubes above and below the grid to improve prevention of axial grid movement (Reference 111). Spacer sleeves were eliminated on the Mark-B-HTP design, which prevents axial grid movement by welding the spacer grids to guides tubes.

d. Recaging/Reconstitution

To reduce fission product releases to the reactor coolant system, methods have been developed to remove leaking or suspect fuel rods from Mark B fuel assemblies that will be reinserted in the reload core. Reconstitution is accomplished using a specially designed assembly with a removable upper end fitting. When defective rods are detected and need to be removed, the UEF is unfastened, the rods removed and replaced with dummy fuel rods (stainless steel or zircaloy) and the UEF refastened.

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Recaging is used when the original assembly structure is no longer usable. The UEF is removed, all reusable intact fuel rods are transferred to a new assembly structural cage and a new UEF is attached. The damaged rods remain in the original assembly structure and are replaced in the new assembly by dummy fuel rods. Recaging can be performed for both older Mark B4 assembly designs (non-reconstitutable) and the newer, reconstitutable Mark B8 design.

The Mark B reconstitutable and recage assemblies and fuel rods meet all previous fuel mechanical design criteria as described in Section 3.1 of the FSAR. Effects of the recaging process on fuel rod mechanical and thermal-hydraulic performance have been evaluated and margin demonstrated to all limiting criteria.

Generic justification for the replacement of up to ten (10) fuel rods in a Mark B assembly was approved in Reference 103. In addition, use of any reconstituted or recaged assemblies in which fuel rods have been replaced with substitute rods must be justified by cycle-specific reload analyses using NRC approved methodologies to demonstrate that existing design limits and safety criteria are met for the next cycle.

e. Cycle 10 Fuel Degradation/Repair

During the 11R refueling outage (1995) fuel inspections of all 177 Cycle 10 fuel assemblies indicated a total of nine (9) failed fuel rods in four (4) first-burn (Batch 12) assemblies. All failed rods were in the peripheral rows of the fuel. Visual inspections revealed that these rods, as well as adjacent rods that had no through-wall failure indications, had a distinctive crud pattern (DCP) exhibiting an intense dark and light mottled appearance. Eddy current inspections of intact intense DCP rods showed that some had indications of up to 70% wall-thinning in the upper spans while others had no such indications. Rods with less intense DCP showed no wall-thinning.

All fuel assemblies scheduled for reinsertion in Cycle 11 were visually inspected. Those with rods with intense DCP were taken to the fuel repair station, the upper end fitting removed, and the DCP rods eddy current inspected for wall-thinning. No non-peripheral rods were found with the DCP. All peripheral rods with an E/C indication greater than 0% were removed and replaced with interior donor rods of similar burnups, from the same assembly or (for several rods) from another Batch 12 assembly, to avoid power peaking mismatches. The interior donor rods were replaced with stainless steel dummy rods to fill the water holes.

A total of 105 Batch 12 peripheral fuel rods in 23 fuel assemblies had some wall-thinning indication; a total of 290 peripheral rods were E/C inspected in 28 assemblies. A total of 35 non-peripheral rods were inspected and showed no DCP and no wall-thinning. To reduce repair work during the outage, 8 symmetric Batch 12A assemblies (initial enrichment of 4.0 wt/o U235) were discharged and replaced with fresh inventory assemblies (Mk-B10 design @ 4.0 wt/o U235). Nineteen other reconstituted Batch 12 assemblies were reinserted for Cycle 11 with a total of 87 stainless steel rods (varying from 1 to 10 rods per assembly). A Cycle 11 Batch 11E assembly with a broken spacer grid also was discharged with its 3 symmetric assemblies; these were replaced with similar Batch 11 assemblies.

These changes required a core reload redesign analysis. The analysis accounted for the effects of the stainless steel dummy rods. All reconstitutions were limited to a

maximum of 10 replacement rods per assembly and analyzed using USNRC-approved methods in accordance with Reference 103. Results of the redesign verification showed that no core operating limits established for the original cycle design needed to be changed for the redesign due to the replacement rods and new core shuffle, and that all nuclear, mechanical, thermal and safety characteristics remained within design and safety criteria. The Westinghouse Lead Test Assemblies and BWFC Advanced Cladding demonstration assemblies, discussed in f and g below, also were verified as acceptable for the redesign. The redesign verification results are documented in detail in Reference 108.

Special safety assessments, also discussed in Ref. 108, were done for the reinserted Batch 12 fuel to confirm that the Cycle 10 degradation phenomenon is not a safety concern for Cycle 11. The visual inspection and E/C process showed that only a minority (105 of 290) of DCP rods had any wall-thinning; all measured rods with less intense DCP showed no wall-thinning. This screening process removed all rods likely to have any significant wall degradation. The Cycle 10 pattern of 9 through-wall failed rods occurring at intervals over the entire cycle indicates that the phenomenon is not catastrophic. Extensive failures of the reinsertion fuel, therefore, are not likely. Further, the thermal condition of Batch 12 fuel will be less severe in Cycle 11 during its second burn; no second-burn rods in Cycle 10 were observed to have the DCP.

Root cause evaluations including hot cell exams were performed and documented in Reference 118. Although the root cause of the cladding degradation failures could not be definitively determined, the most probable scenario developed was that of a crud layer forming on the clad, sufficiently thick to entrap a thin vapor layer. The vapor layer would have increased the thermal resistance of the crud layer significantly resulting in extremely high cladding temperatures, localized cladding corrosion, and ultimately, cladding failure. To avoid recurrence of the degradation in the fresh (hotter) fuel in Cycle 11 (Batch 13) certain corrective actions were taken. These include: a) reducing the Cycle 11 maximum assembly power peaking at fresh fuel interfaces below that of Cycle 10; b) reduction of beginning-of-cycle soluble boron concentration from that of Cycle 10 (by about 150 ppmB) to increase BOC pH; and c) improved lithium/boron control consistent with EPRI modified chemistry recommendations to maintain BOC pH greater than or equal to 6.9. These actions are expected to eliminate the conditions under which the degradation can occur. DCP was not observed following Cycle 11 and 12 operations indicating the corrective actions were successful.

f. Westinghouse Lead Test Assemblies

Four Westinghouse replacement Lead Test Assemblies (LTA) were inserted with the Cycle 11 reload fuel batch for three cycles of operation.

To qualify an alternative fuel supplier for TMI-1, four LTA's had been inserted with the Cycle 9 reload (1991) to be burned for three consecutive cycles. During the 10R outage (1993) UT fuel inspections determined that a total of 24 LTA fuel rods had failed during Cycle 9. The LTA's were discharged and a root cause investigation begun. Visual inspections and flow testing determined that the cause of failure was self-excited flow-induced vibration (FIV) similar to that which had been found at Salem-2 and Beaver Valley-1 early in 1993. The FIV led to grid-to-rod fretting failures. Westinghouse had

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developed a design change to mitigate the FIV for their 17 x 17 fuel. The change consisted of 900 rotation of alternate intermediate spacer grids.

A prototype LTA with rotated grids was tested at TMI operating flows by Westinghouse. Results showed that the higher-mode, larger-amplitude vibrations of concern exhibited by the original aligned-grid (non-rotated) LTA were eliminated. To improve corrosion performance at long residence times the advanced zirconium alloy ZIRLO was used in place of the original Zircaloy 4 material for assembly components and rod cladding.

The basic design of the (original) LTA is described in Reference 93; the replacement LTA changes and improvements are described in Reference 106. The LTA incorporates features adapted from current Westinghouse advanced fuel assemblies which include:

- 15 x 15 array
- 0.422 inch OD fuel rod design
- Leaf-type holddown springs
- Removable improved-design top nozzle
- Inconel upper and lower spacer grids
- Rotated alternate mid-grids
- ZIRLO mid-grids with flow mixing vanes
- ZIRLO fuel rod cladding
- ZIRLO guide tubes/instrument tube
- Debris-resistant fuel rod lower end plug
- Debris-filter, removable bottom nozzle

LTA characteristics with respect to the BWFC Mark B fuel are:

- Smaller fuel rod diameter
- Slightly lower uranium loading
- Better uranium utilization
- Slightly higher reactivity
- Lower U235 enrichment
- Compatible hydraulics
- Matching spacer grid elevations
- Somewhat higher flow resistance
- Somewhat higher pressure drop
- Greater DNBR margin

The LTA was designed in accordance with Westinghouse criteria and methods developed for similar PWR fuel assemblies and applied to high burnup designs. These methodologies have all been approved by the USNRC (References 94, 95, and 107). LTA mechanical, thermal and material design criteria are consistent with the USNRC Standard Review Plan guidelines. Nuclear, thermal-hydraulic, hydraulic and safety analyses criteria were established to ensure compatibility with the B&W Fuel Company Mark B fuel assemblies that comprise the rest of the core. The LTA also is consistent with all UFSAR fuel design bases and meets all currently applicable safety analysis requirements. Other design requirements included:

- The LTA must meet all design criteria to a fuel assembly average burnup of 60 GWD/MTU and peak fuel rod average of 65 GWD/MTU.

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- The LTA must meet all safety requirements and allow reactor operation within currently licensed limits.
- The LTA will not set any core operating or safety limits.
- The LTA must be compatible with the resident BWFC MkB assemblies for introduction in transition cycles.
- The LTA must have an in-core residence capability of 650 ± 15 EFPD per cycle with a maximum total lifetime of 2070 EFPD at full power or six calendar years at full system pressure and flow.

Safety evaluations for use of the LTA's in TMI-1 were performed by Westinghouse (Reference 96) and GPUN (References 97 and 108) and confirmed all requirements will be met and that their use would not adversely affect plant safety.

g. Advanced Cladding Assemblies

As TMI cycle lengths, fuel burnups and fuel in-core residence time increase and the RCS chemical environment becomes more aggressive (e.g., higher lithium concentrations), enhanced corrosion of the Zr-4 fuel rod cladding and other assembly components has become a concern. To help ensure that fuel will continue to meet all design and performance criteria for future cycles a program was developed with BWFC to irradiate two advanced zirconium-based alloy claddings designed to have greater corrosion resistance than the current Zr-4 claddings. These non-Zircaloy alloys, designated M4 and M5, have been successfully irradiated in European reactors and in McGuire-1.

The TMI program consists of a total of 16 demonstration rods, 8 each of M4 and M5. Four rods of each alloy were placed in the peripheral rows of two Cycle 11 Batch 13F MkB10-Gd fuel assemblies. They will be burned for three 2-year cycles, Cycles 11, 12 and 13, with a projected residence time of 1950 ± 45 EFPD and rod burnups to 53 GWD/T.

Use of the rods in TMI was evaluated in Ref. 109. The rods were analyzed by BWFC for bounding conditions for the specific Cycle 11 cycle design and projected Cycle 12 and 13 designs. Locations in the 13F FA's were selected to control power peaking and ensure sufficient margin to the Cycle 11 hot pin at all times in the cycle, as well as conservative margins to all LOCA, CFM and DNB-based core operating limits. The thermal analyses confirmed acceptable results for LOCA initialization, CFM and internal pin pressure criteria. The mechanical analyses confirmed acceptable results for cladding corrosion, creep collapse, stress, strain fatigue, rod growth, rod bow and material compatibility. Expected LOCA performance also was evaluated and judged to be acceptable for stored energy, high-temperature oxidation and strain/rupture considerations based on the power peaking margins established.

The demo rod power peaking was confirmed for Cycles 12 and 13 actual core designs to ensure the evaluation results remain bounded. Post irradiation exams performed after Cycles 11, 12, and 13 demonstrated satisfactory performance of the demo rods.

The demonstration program was extended to Cycle 14 as four of the thrice-burned M5-clad rods were reconstituted into a host assembly to achieve burnups in excess of 62 GWd/mtU. The high burnup demo rods were shown to meet all design criteria in Reference 120 and the high burnup program was accepted by the NRC in Reference 122.

3.2.4.2.1.2 Fuel Assembly Evaluation

a. General

The basis for the design of the fuel rod is discussed in Section 3.1.2.4. Materials testing and actual operation in reactor service with zircaloy cladding have demonstrated that zircaloy-4 and M5 material has sufficient corrosion resistance and mechanical properties to maintain the integrity and serviceability required for design burnup. The justification to increase design rod average burnup for Mark-B fuel to 62,000 MWd/Mtu was provided in Reference 117. The acceptability of M5 material (including evaluations of material properties, stress, strain, corrosion, growth, pressure effects, creep, and collapse) was established in Reference 123 and approved for use at TMI-1 in Reference 124.

b. Clad Stress and Strain

The cladding of fuel rods is subjected to external hydrostatic pressure, gradually increasing internal pressure, thermal stresses, vibration, and to the effects of differential expansion of the fuel and cladding caused by thermal expansions and by fuel growth due to irradiation effects. In addition, the properties of the cladding are influenced by thermal and irradiation effects. The analysis of these effects is discussed below.

Stress analysis for cladding is based on several conservative assumptions that make the actual margins of safety greater than those calculated. For example, it is assumed that the clad with the thinnest wall, the smallest fuel-clad gap, and the greatest ovality permitted by the specification is operating in the region of the core where performance requirements are most severe. Fission gas release rates, fuel growth, and changes in mechanical properties with irradiation are based on a conservative evaluation of currently available data.

c. Pressure Effects

- 1) Beginning-of-cycle power conditions clad stresses due to external and internal pressure and considerably below the yield strength. Circumferential stresses due to external pressure, calculated using those combinations of clad dimensions, ovality, and eccentricity that produce the highest stress, are shown in Table 3.2-23.

The maximum compressive stress in the expansion void at the system design pressure is the sum of compressive membrane stress and compressive bending stress due to ovality at the clad OD. Stress conditions are listed for beginning of cycle.

In the heat producing zone, the stress and temperature are such that the clad material may creep enough to allow an increase in clad ovality until further creep is restrained by support from the fuel. If fuel clad contact occurs, the clad is subject to cyclic stresses and strains which are a result of power and pressure transients. To minimize clad fatigue damage, all fuel rods will be internally pressurized with helium. Fatigue analyses, based on conservative assumptions, show that the design limits previously specified (see Section 3.1.2.4.2) are met for pressurized fuel rods.

2) End-of-Life Power Conditions

At the end-of-cycle, fission gas pressure does not exceed operating pressure (Section 3.2.3.2); however, an internal pressure of 3300 psi has been selected as the design basis. At this pressure, the differential would result in a circumferential tensile stress at normal operating pressure.

This stress value shown in Table 3.2-23 is about 1/4 of the yield strength and, therefore, is not a potential source of short time burst. The possibility of stress rupture burst has been investigated using finite-difference methods to estimate the long-time effects of the increasing design pressure on the clad. The predicted pressure-time relationship produces stresses that are less than 1/3 of the stress levels that would produce stress rupture at the end-of-cycle. Outpile stress-rupture data were used, but the greater than 3:1 margin on stress is more than enough to account for decreased stress rupture strength due to irradiation.

3) Deleted

4) Fuel Burnup, Temperature, and Gas Release Conditions

The total production of fission gas and maximum internal clad pressure is based on the analysis of fuel rod power and burnup histories resulting from fuel depletion and fuel cycling. The fission gas release is based on temperature versus release fraction, as shown on Figure 3.2-38. Fuel temperatures are calculated for small radial and axial increments. The total fission gas release is calculated by integrating the incremental releases.

Fuel burnup, temperature, and gas release conditions are determined by evaluating the following factors for the most conservative conditions:

- (a) Gas conductivity at the end-of-life with fission gas present;
- (b) Influence of the pellet-to-clad radial gap and contact heat transfer coefficient on fuel temperature and release rate;
- (c) Unrestrained radial and axial thermal growth of the fuel pellets relative to the clad;
- (d) Hot rod local peaking factors;
- (e) Radial distribution of fission gas production in the fuel pellets;

- (f) The fuel temperatures used to determine fission gas release and internal gas pressure have been calculated at the reactor rated power and maximum design overpower condition. Fuel temperature, total free gas volume, fission gas release, and internal gas pressure have been evaluated for a range of initial diametral clearances as defined in the densification report. This evaluation shows that the highest internal pressure results when the maximum design diametral gap is assumed because of the resulting high average fuel temperature (Figure 3.2-39). The release rate increases rapidly with an increase in fuel temperature, and unrestrained axial growth reduces the relatively cold gas end plenum volumes. A conservative thermal expansion model is used to calculate fuel temperatures as a function of initial cold diametral clearance as outlined in Sections 3.2.3.1.2 and 3.2.3.2.3.7; and,
- (g) Nuclear calculations of power peaks, and power burnup histories, are considered in evaluating fuel and cladding performance.

d. Collapse Margins

Short-time collapse tests have demonstrated a clad collapsing pressure in excess of 4000 psi at expansion void maximum temperature. Collapse pressure margin is approximately 2.2. Extrapolation to hot spot average clad temperature indicates a collapse pressure of 3500 psi and a margin of 1.9, which exceeds requirements. Outpile creep-collapse tests have demonstrated that the clad meets the long-time (creep-collapse) requirement. However, backup radial support has been provided in the upper end void to ensure clad dimensional stability in the event that in-pile creep rates are sufficiently high to allow creep collapse of unsupported cladding. Test results summarized in Section 3.3.3.3.1 show the end void spacers are capable of providing backup support. The results of the tests show that creep collapse of the bottom end void will not occur because the clad temperature is about 900F lower than that in the upper void region. The spacer in the bottom end void is therefore not required to provide radial support. Its geometry, however, is similar to the upper spacer, and it therefore provides added assurance of clad dimensional stability at the bottom void region.

For the current cycle fuel load, the power history for the most limiting assembly was used to calculate the fast neutron flux level for the energy range above 1 MeV. The collapse burnup for the most limiting assembly was conservatively determined to be greater than the maximum projected burnup. The creep collapse analysis was performed based on the conditions set forth in Reference 31.

e. Fuel Irradiation Growth and Fuel-Clad Differential Thermal Expansion

The results of test and the operation of zircaloy-clad UO_2 fuel rods indicate that the rods can be safely operated to the point where total permanent strain is 1.50 percent, or higher, in the temperature range applicable to PWR cladding (Reference 62). The design allowable stain is 1 percent (see Section 3.1.2.4). Fuel rod parameters pertinent to fuel swelling considerations are given in Table 3.2-16.

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The capability of zircaloy-clad UO_2 fuel in solid rod form to perform satisfactorily in service has been demonstrated through operation of the SA-1 assembly in the Dresden and Shippingport cores and through results of their supplementary development programs, up to approximately 45,000 MWd/MTU.

As outlined below, existing experimental information supports the various individual parameters and operating conditions for the maximum design burnup of 55,000 MWd/MTU.

1) Application of Experimental Data to Design Adequacy of the Clad-Fuel Initial Gap to Accommodate Clad-Fuel Differential Thermal Expansion

(a) Experimental Work

Six rabbit capsules, each containing three Zirconium-2 clad rods of 5 inches fuel length, were irradiated in the Westinghouse Test Reactor, (Reference 63), at power levels up to 24 kW/ft. The 94 percent theoretical density (TD) UO_2 pellets (0.430 OD) had initial clad-fuel diametral gaps of 6, 12, and 25 mils. No dimensional changes were observed. Central melting occurred at 24 Kw/ft only in the rods that had the 25 mil initial gap.

Two additional capsules were tested, (Reference 64). The specimens were similar to those described above except for length and initial gap. Initial gaps of 2, 6, and 12 mils were used in each capsule. In the A-2 capsule, three 38 inches long rods were irradiated to 3459 MWd/MTU at 19 kW/ft maximum. In the A-4 capsule, four 6 in long rods were irradiated to 6250 MWd/MTU at 22.2 kW/ft maximum. No central melting occurred in any rod but diameter increases up to 3 mils in the A-2 capsule and up to 1.5 mils in the A-4 capsule were found in the rods with the 2 mil initial gap.

(b) Application

In addition to demonstrating the adequacy of zircaloy-clad UO_2 pellet rods to operate successfully at the power levels of interest (and without central melting), these experiments demonstrate that the design initial clad-fuel gap of 4.5 of 8.5 mils is adequate to prevent unacceptable clad diameter increase due to differential thermal expansion between the clad and the fuel at beginning of life. A maximum local diametral increase of less than 0.001 inch is indicated for fuel rods having the minimum initial gap, operating at the maximum overpower condition.

2) Adequacy of the Available Voids to Accommodate Differential Expansion of Clad and Fuel, Including the Effects of Fuel Swelling

(a) Experimental Work

Zircaloy-clad, UO_2 pellet-type rods have performed successfully in the Shippingport reactor up to approximately 40,000 MWd/MTU. Bettis

Atomic Power Laboratory (Reference 38) has irradiated plate-type UO_2 fuel (96 to 98 percent TD) up to 127,000 MWd/MTU and at fuel center temperatures between 1300°F and 3800°F. This work indicates fuel swelling rates of 0.16 percent $\text{V}/10^{20}$ f/cc until fuel internal voids are filled, then 0.7 percent $\text{delta-V}/10^{20}$ f/cc after internal voids are filled. This point of breakaway appears to be independent of temperature over the range studied and dependent on clad restraint and the void volume available for collection of fission products. The additional clad restraint and greater fuel plasticity (from higher fuel temperatures) of rod-type elements tend to reduce these swelling effects by providing greater resistance to radial swelling and lower resistance to longitudinal swelling than was present in the plate-type test specimens.

This is confirmed in part by the work of Frost, Bradbury, and Griffiths of Harwell (Reference 65) in which 1/4 inch diameter UO_2 pellets clad in 0.020 inch stainless steel with a 2 mil diametral gap were irradiated to 53,300 MWd/MTU at a fuel center temperature of 3180F without significant dimensional change.

In other testing (Reference 66) 0.150 inch OD, 82 to 96 percent TD oxide pellets (20 percent Pu, 80 percent U) clad with 0.016 inch stainless steel with 6 to 8 mil diametral gaps have been irradiated to 77,000 MWd/MTU at fuel temperatures high enough to approach central melting without apparent detrimental results. Comparable results were obtained on rods swaged to 75 percent TD and irradiated to 100,000 MWd/MTU.

(b) Application

Clad strain due to reactor operating conditions is calculated as follows:

Fuel irradiation induces swelling is determined using an empirical model based on the Bettis Atomic Power Laboratory data, (Reference 58). The fuel swelling model accounts for the portion of swelling which is accommodated by fuel porosity. Initial external fuel swelling will occur at 0.16 percent $\text{delta-V}/10^{20}$ f/cc until the fuel pores are filled. For the maximum fuel density of 94 percent of theoretical, this occurs at a burnup of 11.1×10^{20} f/cc (45,300 MWd/MTU). After the fuel pores are closed, the fuel will swell at a rate of 0.7 percent $\text{delta-V}/10^{20}$ f/cc until the maximum local peak design burnup of 13.5×10^{20} f/cc (55,000 MWd/MTU) is reached. The design burnup exceeds, of course, the actual calculated local peak burnup.

The fuel is assumed to swell uniformly in all directions, conservatively neglecting axial plastic flow into the pellet end dishes. For uniform fuel swelling in all directions, the percent increase in diameter is one third the percent volumetric swelling rate. If the fuel cracks, the crack voids will also be available to accommodate fuel growth. Fuel-clad differential thermal expansion in going from cold conditions to power is calculated as described in Section 3.2.3.1.2.

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Studies of clad strain for various fuel-clad gaps indicate that the rod with the minimum gap will experience the greatest clad strain, in spite of its improved gap conductivity. Clad permanent strain reaches a maximum at the end-of-life.

The clad plastic strain for maximum fuel density (94 percent of theoretical), maximum local design burnup (55,000 MWd/MTU), and minimum fuel-clad gap is 0.7 percent. Fuel rods with nominal gaps, nominal density, and average burnup will not grow sufficiently to cause any tensile loop stress or strain in the cladding.

(c) Fuel Swelling Studies at B&W

Performance of B&W test fuel rods irradiated up to burnups of 55,000 MWd/MTU support the design criteria used for TMI-1 fuel rods.

f. Effect of Zircaloy Creep

The effect of zircaloy creep on the amount of fuel rod growth due to fuel swelling has been investigated. Clad creep has the effect of producing a nearly constant total pressure on the clad ID by permitting the clad diameter to increase as the fuel diameter increases. Based on out-of-pile data (Reference 67) 1 percent creep will result in 10,000 hours (corresponding approximately to the end-of-life diametral swelling rate) from a stress of about 22,000 psi at the 733F average temperature through the clad at the hot spot. At the start of this higher swelling period (roughly the last 1/3 of the core life), the Reactor Coolant System pressure would more or less be balanced by the rod internal pressure; so the total pressure to produce the clad stress of 22,000 psi would have to come from the fuel. Contact pressure would be 2400 psi. At the end of life, the rod internal design pressure exceeds the system pressure by about 1100 psi, so the clad fuel contact pressure would drop to 1300 psi. Assuming that irradiation produces a 3:1 increase in creep rates, the clad stress for 1 percent strain in 10,000 hours would drop to about 15,000 psi. Contact pressures would be 1800 psi at the beginning of the high swelling period 700 psi at the end of life. Since the contact pressure was assumed to be 825 psi in calculating the contact coefficient used to determine the fuel pellet thermal expansion, there is only a short period at the very end of life (assuming the 3:1 increase in creep rates due to irradiation) when the pellet is slightly hotter than calculated. The effect of this would be a slight increase in pellet thermal expansion and therefore in clad strain.

g. Overall Assembly

1) Assurance of Control Rod Assembly Free Motion

The 0.058 inch diametral clearance between the control rod guide tube and the control rod is provided to cool the control rod and to ensure adequate freedom to insert the control rod. As indicated below, studies have shown that fuel rods will not bow sufficiently to touch the guide tube. Thus, the guide tube will not undergo deformation caused by fuel rod bowing effects. Initial lack of straightness of fuel rod and guide tube, plus other adverse tolerance conditions, conceivably could reduce the 0.088 inch nominal gap between fuel rod and guide

tube to a minimum of about 0.038 inch, including amplification of bowing due to axial friction loads from the spacer grid. The maximum expected flux gradient of 1.176 across a fuel rod will produce a temperature difference of 10°F, which will result in a thermal bow of less than 0.002 inch.

Under these conditions, for the fuel rod to touch the guide tube, the thermal gradient across the fuel rod diameter would have to be on the order of 300°F.

The effect of a DNB occurring on the side of a fuel rod adjacent to a guide tube would result in a large temperature difference. In this case, however, investigation has shown that the clad temperature would be so high that insufficient strength would be available to generate a force of sufficient magnitude to cause a significant deflection of the guide tube. In addition, the guide tube would experience an opposing gradient that would resist fuel rod bowing and its internal cooling would maintain temperatures much lower than those in the fuel rod cladding, thus retaining the guide tube strength.

2) Vibration

The semi-empirical expression developed by Burgreen, (Reference 68) was used to calculate the flow-induced vibratory amplitudes for the fuel assembly and fuel rod. The calculated amplitude is less than 0.010 inch for the fuel assembly and less than 0.005 inch for the fuel rod. The fuel rod vibratory amplitude correlates with the measured amplitude obtained from a test on a 3 x 3 fuel rod assembly. In order to substantiate this conservatively calculated amplitude for the fuel assembly, a direct measurement has been obtained for a full-size prototype fuel assembly during testing of the assembly in the Control Rod Drive Line Facility (CRDL) at the B&W Research Center, Alliance, Ohio. The maximum assembly amplitude determined by measurement was 0.005 inch.

3) Loading During Depressurization Transient Following an Accident

An analysis (Reference 69) of the fuel assembly for loads caused by the depressurization transient following an instantaneous reactor coolant pipe rupture and/or seismic excitation has been conducted.

The analysis investigated (1) the extent of horizontal contact between fuel assemblies primarily at the mid-span grid spacers and (2) the vertical contact of fuel assemblies with internals between the end fittings and grid plates.

The results show that, (1) the level of permanent distortion suffered during the design basis earthquake (DBE) or DBE plus LOCA did not prevent control rod insertion, (2) the reference fuel assembly design can withstand the horizontal contact loads, (3) the compressive loads in the guide tubes do not exceed 85 percent of the static Euler buckling load, and, (4) that the end spacer grid assembly is adequate for the maximum anticipated loads.

4) Demonstration

In addition to the specific items discussed above, the overall mechanical performance of the fuel assembly and its individual components has been and is continuing to be demonstrated in an extensive experimental program in the CRDL (see Section 3.3.3.2).

3.2.4.2.2 Control Rod Assembly (CRA)

Each CRA (Figure 3.2-50) has 16 control rods, a stainless steel spider, and a female coupling. The 16 control rods are attached to the spider by means of a nut threaded to the upper shank of each rod. After assembly, all nuts are lock welded. The control rod drive is coupled to the CRA by a bayonet-type connection. Full-length guidance for the CRA is provided by the control rod guide tube of the upper plenum assembly and by the fuel assembly guide tubes. The CRAs and guide tubes are designed with adequate flexibility and clearances to permit freedom of motion within the fuel assembly guide tubes throughout the stroke.

Each control rod has a section of neutron absorber material. The absorber material is an alloy of silver-indium-cadmium (Ag-In-Cd). The initial-core CRAs were replaced in Cycles 9 and 10 with the new Extended-Life CRA. The ELCRA is clad in Inconel 625 tubing. Inconel end pieces are welded to the tubing to form a water and pressure-tight container for the absorber material. The tubing provides the structural strength of the control rods and prevents corrosion of the absorber material. A tube spacer similar to that in the fuel assembly is used to prevent absorber motion within the cladding during shipping and handling and to permit differential expansion in service.

Principal data pertaining to the CRA are given in Table 3.2-24.

A CRA prototype of the original B&W design was extensively tested at reactor temperature, pressure, and flow conditions in the B&W test loop at their Alliance Research Laboratory. For test program description and results, refer to Reference 70.

The control rods are designed to withstand all operating loads, including those resulting from hydraulic force, thermal gradients, and reactor trip deceleration. The ability of the original design control rod clad to resist collapse has been established in a test program on cold-worked stainless steel tubing. Because the Ag-In-Cd alloy poison does not yield a gaseous product under irradiation, internal pressure, and swelling of the absorber material will not cause excessive stressing or stretching of the clad.

Because of their length and the possible lack of straightness over the entire length of the rod, some interference between control rods and the fuel assembly guide tubes is expected. However, the parts involved, especially the control rods, are flexible and only small friction drag loads result. Similarly, thermal distortions of the control rods are small because of the low heat generation and adequate cooling. Consequently, CRAs will not encounter significant frictional resistance to their motion in the guide tubes.

The ELCRA design was evaluated in Reference 97 for use in TMI-1 and determined to meet all design and safety criteria.

3.2.4.2.3 Axial Power Shaping Rod Assembly (APSRA)

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Starting with Cycle 19, the APSRAs were determined to be unnecessary and were removed from service. The following is historical information.

Each APSRA, Figure 3.2-51, has 16 axial power shaping rods, a stainless steel spider, and a female coupling. The 16 rods are attached to the spider by means of a nut threaded to the upper shank of each rod. After assembly, all nuts are lock welded. The axial power shaping rod drive is coupled to the APSRA by a bayonet connection. The female couplings of the APSRA and CRA have slight dimensional differences to ensure that each type of rod can only be coupled to the correct type of drive mechanism.

When the APSRA is inserted into the fuel assembly, it is guided by the guide tubes of the fuel assembly. Full-length guidance of the APSRA is provided by the control rod guide tube of the upper plenum assembly. At the full out position of the control rod drive stroke, the lower end of the APSRA remains within the fuel assembly guide tube to maintain the continuity of guidance throughout the rod travel length. The APSRAs are designed to permit maximum conformity with the fuel assembly guide tube throughout travel.

The APSR as of Cycle 6 is an improved design. Each axial power shaping rod has a section of Inconel neutron absorber material clad in cold-worked, type 304 stainless steel tubing. This tubing provides the structural strength for the axial power shaping rods and prevents corrosion of the absorber material. The absorber section is sealed by both an internal plug and an end plug shown on Figure 3.2-51. The section of tubing above the absorber is vented so it is always filled with borated water and consequently the pressure differential across the tube wall is negligible.

These axial power shaping rods are designed to withstand all operating loads including those resulting from hydraulic forces and thermal gradients. The ability of the axial power shaping rod clad to resist collapse due to the system pressure has been demonstrated by an extensive collapse test program on stainless steel tubing. APSR collapse lifetime has been increased by use of thicker cladding, prepressurization and tighter allowable ovality tolerances. Because the Inconel alloy does not yield gaseous products under irradiation, internal pressure is not generated within the clad. Swelling of the absorber material is negligible and will not cause unacceptable clad strain.

As of Cycle 12, the APSRA design was changed to ensure proper latchup with the Mark-B10 fuel assembly and to increase the rod lifetime. The latchup improvement consisted of lengthening the female coupling and related dimensional changes. The extended lifetime was achieved by adjusting the internal rod plenum volume. The Mk-B10 APSRA changes were evaluated in Reference 111.

Pertinent data on the APSRA are given in Table 3.2-25.

Because of their great length and unavoidable lack of straightness, some slight mechanical interference between axial power shaping rods and the fuel assembly guide tubes must be expected. However, the parts involved are flexible and result in very small friction drag loads. Similarly, thermal distortions of the rods are small because of the low heat generation and adequate cooling. Consequently, the APSRAs will not encounter significant frictional resistance to their motion in the guide tubes.

3.2.4.2.4 Burnable Poison Rod Assembly (BPRA)

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The number of BPRAs in each cycle can vary with the cycle design. The number in the current cycle core is given in Table 3.2-26.

Each BPRA (see Figure 3.2-52) has up to 16 burnable poison rods, a stainless steel spider. The rods are attached to the spider. The BPRA is inserted into the fuel assembly guide tubes through the upper end fitting. For all Mark B4-type fuel assemblies, the retainers described in Section 3.2.4.2.6.1 are used to lock the BPRA into the fuel assembly. Starting with the Mark B8 fuel assembly, an improved BPRA spider design is used that positively captures the BPRA between the fuel assembly upper end fitting and the upper core grid plate, thus eliminating the need for a separate retainer.

Each burnable poison rod has a section of sintered $\text{Al}_2\text{O}_3\text{-B}_4\text{C}$ pellets which serves as burnable poison. The length of this section is varied depending on fuel stack length and the presence of axial blankets to enhance neutron efficiency and to control axial power peaking. The burnable poison is clad in cold-worked Zircaloy-4 tubing and Zircaloy-4 upper and lower end pieces. The end pieces are welded to the tubing to form a water and pressure-tight container for the absorber material. The Zircaloy-4 tubing provides the structural strength of the burnable poison rods.

In addition to their nuclear function, the BPRAs also serve to minimize guide tube bypass coolant flow. Pertinent data on the BPRA is given in Table 3.2-26.

The burnable poison rods are designed to withstand all operating loads including those resulting from hydraulic forces and thermal gradients. The ability of the burnable poison rod clad to resist collapse due to the system pressure and internal pressure has been demonstrated by an extensive test program on cold-worked Zircaloy-4 tubing.

3.2.4.2.5 Orifice Rod Assembly (ORA)

The ORAs have been removed from the present core. See Section 3.2.3.2.3.11 for a further discussion.

Each ORA (Figure 3.2-53) has 16 orifice rods, a stainless steel spider, and a coupling mechanism. The coupling mechanism provides a means for positive coupling between the ORA and the fuel assembly holddown latch when the orifice rods are inserted into the fuel assembly. The necked-down section of the rod permits lateral movement in order to facilitate the installation of the orifice assembly into the guide tubes in the fuel assembly. The ORA serves to limit bypass flow through empty guide tubes. Pertinent data on the ORA is given in Table 3.2-27. The retainer described in Section 3.2.4.2.6.2 below must be used with the ORA design.

3.2.4.2.6 Burnable Poison Rod Assembly and Orifice Rod Assembly Retainers

3.2.4.2.6.1 Burnable Poison Rod Assembly Retainers

These BPRA retainers are not required on the Mark B8 or later fuel assembly designs and are not used in the current core.

The retainer shown in Figures 3.2-54 and 3.2-55 is designed to be used with the Mark B4 type fuel assemblies to provide a positive holddown against lift forces acting on the BPRA (when BPRAs are used). The design achieves a minimum positive holddown margin of 35 pounds

(approximately 500 percent margin) against the lift provided under reactor conditions (Reference 71). In service, the upper reactor internal (upper core plate) is placed over the feet of the load arm assembly to apply the load to the retainer, and ensures positive capture.

3.2.4.2.6.2 Orifice Rod Assembly Retainers

These ORA retainers are not used in the current core.

The BPRA retainer device has been evaluated (Reference 71) and is suitable for use on a standard Orifice Rod Assembly (ORA) modified for use with a primary neutron source. Some spider arms and orifice rods must be removed from the standard orifice rod assembly to minimize drag so that an adequate margin to lift off is maintained.

3.2.4.2.7 Quality Control Program for Core Components

B&W equipment specifications require that core components be fabricated under an approved quality control program. This includes shop quality control provisions, which are approved by B&W quality assurance personnel, and special process procedures, which are approved by B&W design personnel.

The B&W Commercial Nuclear Fuel Plant manufactures core components under a controlled manufacturing system which includes complementary written process procedures and inspection provisions. These fabrication activities are supported by quality control provisions, e.g., document control, control of special processes, inprocess and final inspection, gage control, and corrective action.

3.2.4.3 Control Rod Drives

3.2.4.3.1 Description

The Control Rod Drive Mechanism (CRDM) positions the control rod within the reactor core and indicates the location of the control rod with respect to the reactor core. The speed at which the control rod is inserted or withdrawn from the core is consistent with the reactivity change requirements during reactor operation. For conditions that require a rapid shutdown of the reactor, the shim safety drive mechanism releases the CRA and supporting CRDM components, permitting the CRA to move by gravity into the core. The reactivity is reduced during such a rod insertion at a rate sufficient to control the core under any operating transient or accident condition. The control rod is decelerated at the end of the rod trip insertion by a buffer assembly in the CRDM upper housing. The buffer assembly supports the control rod in the fully inserted position. Criteria applicable to drive mechanisms for the control shim rod assemblies are given below. Additional requirements for the mechanisms which actuate only control shim rod assemblies are also given below.

3.2.4.3.1.1 General Design Criteria

a) Single Failure

No single failure shall inhibit the protective action of the control rod drive system. The effect of a single failure shall be limited to one CRDM.

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b) Uncontrolled Withdrawal

No single failure or sequence of dependent failures shall cause uncontrolled withdrawal of any CRA.

c) Equipment Removal

The disconnection of plug-in connectors, modules, and subassemblies from the protective circuits shall be annunciated or shall cause a reactor trip.

d) Position Indication

Continuous position indication, as well as an upper and lower position limit indication, shall be provided for each CRDM. The accuracy of the position indicators shall be consistent with the tolerance set by reactor safety analysis.

e) Drive Speed

The control rod drive control system shall provide a single uniform mechanism speed. The drive controls, or mechanism and motor combination, shall have an inherent speed limiting feature. The design speed of the mechanism for both insertion and withdrawal is given in Table 3.2-28. Speed limiting is further described and maximum rod speed from the motor is given in Section 7.2.2.3 b).

f) Mechanical Stops

Each CRDM shall have positive mechanical stops at both ends of the stroke or travel. The stops shall be capable of receiving the full operating force of the mechanisms without failure.

g) Control Rod Positioning

The control rod drives shall provide for controlled withdrawal or insertion of the control rods out of or into the reactor core to establish and hold the power level required.

3.2.4.3.1.2 Additional Design Criteria

a. The following criterion is applicable only to the mechanisms which actuate CRAs:

b. CRA Trip

The shim safety drives are capable of rapid insertion or trip for emergency reactor conditions.

3.2.4.3.2 Control Rod Drive Mechanisms

The control rod drive mechanisms provide for controlled withdrawal or insertion of the CRAs out of or into the core and are capable of rapid insertion or trip. The drive mechanisms are

hermetically sealed, reluctance motor-driven screw units. The CRDM data are listed in Table 3.2-28.

3.2.4.3.2.1 Shim Safety Drive Mechanism

The shim safety drive mechanism consists of a motor tube which houses a lead screw, its rotor assembly, and a buffer. The top end of the motor tube is closed by a closure and vent assembly. An external motor stator surrounds the motor tube (a pressure housing) and position indication switches are arranged outside the motor tube extension.

The control rod drive output element is a non-rotating translating lead screw coupled to the control rod. The screw is driven by separate antifriction roller nut assemblies attached to segment arms which are rotated magnetically by a motor stator located outside the pressure boundary. Current impressed on the stator causes the separating roller nut assembly halves to close and engage the lead screw. Mechanical springs disengage the roller nut halves from the screw in the absence of a current. For rapid insertion, the nut halves separate to release the screw and control rod, which move into the core by gravity. A hydraulic buffer assembly within the upper housing decelerates the moving CRA to a low speed a short distance above the CRA full-in position. The final CRA deceleration energy is absorbed by the down-stop buffer spring. The CRDM is a totally sealed unit with the roller nut assemblies and segment arms magnetically driven by the stator coil through the motor tube pressure housing wall. The lead screw assembly is connected to the control rod by a bayonet type coupling. An anti-rotation device (torque taker) prevents rotation of the lead screw while the drive is in service. A closure and vent assembly is provided at the top of the motor tube housing to permit access to couple and release the lead screw assembly from the control rod. The top end of the lead screw assembly is guided by the buffer piston and its guide. Two of the six phase stator housing windings are energized to maintain the control rod position when the drive is in the holding mode.

The CRDM is shown on Figures 3.2-56 and 3.2-57. Subassemblies of the CRDM are described as follows:

a. Motor Tube

The motor tube is a three-piece welded assembly designed and manufactured in accordance with the requirements of the ASME Code, Section III, for a Class 1 nuclear pressure vessel appurtenance. Materials conform to ASTM or ASME, Section II, Material Specifications. All welding shall be performed by personnel qualified under ASME Code, Section IX, Welding Qualifications. The motor tube wall between the rotor assembly and the stator is constructed of stainless steel. This region of the motor tube is of low alloy steel clad on the inside diameter with stainless steel or with Inconel. The upper end of the motor tube functions only as a pressurized enclosure for the withdrawn lead screw and is made of stainless steel transition-welded to the upper end of the stainless steel motor section. The lower end of the low alloy steel tube section is welded to a stainless steel machined forging which is flanged at the face which contacts the vessel control rod nozzle. Double gaskets, which are separated by a ported test annulus, seal the flanged connection between the motor tube and the reactor vessel.

b. Motor

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The motor is a synchronous reluctance unit with a slip-on stator. The rotor assembly is described in Item f. of this section. The stator is a four-pole arrangement with water cooling coils wound on the outside of its casing. The stator is encapsulated after winding to establish a sealed unit. It is six-phase star-connected for operation in a pulse-stepping mode and advances 15 mechanical degrees per step. The stator assembly is mounted over the motor tube housing as shown on Figure 3.2-56.

c. Plug and Vent Valve

The upper end of the motor tube is closed by a closure insert assembly containing a vapor bleed port and vent valve. The vent valve and insert closure have double seals. The insert closure is retained by a closure nut which is threaded to the inside of the motor tube. The sealing load for the closure is applied by jackscrews threaded through the closure nut.

d. Actuator

The actuator consists of the translating lead screw, lead screw nut assembly, and the torque taker assembly on the screw. The actuator lead screw travel is 139 inches.

e. Lead Screw

The lead screw has a lead of 0.750 inches. The thread is double lead with a single pitch spacing of 0.375 inches. Thread lead error is held to 0.0005 inch maximum in any 6 inches for uniform loading with the roller nut assemblies. The thread form is a modified ACME with a flank angle that allows the roller nut to disengage without lifting the screw.

f. Rotor Assembly

The rotor assembly consists of a ball-bearing-supported rotor tube carrying and limiting the travel of a pair of segment arms. Each of the two arms carries a pair of ball-bearing-supported roller (nut) assemblies which are skewed at the lead screw helix angle for engagement with the lead screw. The current in the motor stator (two of a six-winding stator) causes the arms that are pivoted in the rotor tube to move radially toward the motor tube wall to the limit provided, thereby engaging the four roller nuts with the centrally located lead screw. Also, four separating springs mounted in the segment arms keep the rollers disengaged when the power is removed from the stator coils. A second radial bearing mounted to the upper end of the rotor tube has its outer race pinned to both segment arms, thereby synchronizing their motion during engagement and disengagement. When a three phase rotating magnetic field is applied to the motor stator, the resulting force produces rotor assembly rotation.

g. Torque Tube and Torque Taker

The torque tube is a separate tubular assembly containing a keyway that extends the full length of the lead screw travel. The tube assembly is secured against rotation and in elevation by the lower end of the closure assembly and a retaining ring. The lower end of the tube assembly houses the buffer and is the down stop. The torque taker on the lead screw contacts the torque tube cap for the upper mechanical stop.

The torque taker assembly consists of the position indicator permanent magnet, the buffer piston, and a positioning key. The torque taker key fixed at the top of the lead screw is mated with the torque tube keyway to provide both radial and tangential positioning of the lead screw.

h. Buffer Assembly

The buffer assembly is capable of decelerating the translating mass from the unpressurized terminal velocity to zero velocity without applying greater than ten times the gravitational force on the control rod. The water buffer consists of a piston fixed to the top end of the screw shaft and a cylinder which is fixed to the lower end of the torque tube. Twelve inches above the bottom stop, the piston at the top of the screw enters the cylinder. Guiding is accomplished because the piston and torque key are in a single part, and the cylinder and keyway are in a single mating part. As the piston travels into the cylinder, water is driven into the center of the lead screw through holes in the upper section which produce the damping pressure drop. The number of holes presented to the buffer chamber is reduced as the rod moves into the core so that the damping coefficient increases as the velocity reduces, thereby providing an approximately uniform deceleration. A large helical buffer spring is used to take the kinetic energy of the drive line at the end of the water buffer stroke. The buffer spring accepts a 5 fps impact velocity of the drive line and control rod with an instantaneous overtravel of 1 inch past the normal down stop. The inclusion of this buffer spring permits practical clearances in the water buffer.

i. Lead Screw Guide/Thermal Barrier

The lead screw guide bushing acts as a primary thermal barrier and as a guide for the screw shaft. As a primary thermal barrier, the bushing allows only a small path for free convection of water between the mechanism and the closure head nozzle. Fluid temperature in the mechanism is largely governed by the flow of water through this bushing. The diametral clearance between screw shaft and bushing is large enough to preclude jamming the screw shaft and small enough to hold the free convection to an acceptable value. In order to obtain trip travel times of acceptably small values, it is necessary to provide auxiliary flow paths around the guide bushing. This larger flow area reduces the pressure differential required to drive water into the mechanism to equal the screw displacement, thus limiting hydraulic drag.

j. Position Indications

Two methods of position indication are provided: one, an absolute position indicator and the other, a relative position indicator. The absolute position transducer consists of a series of magnetically operated reed switches mounted in a tube parallel to the motor tube extension. Each switch is hermetically sealed. Switch contacts close when a permanent magnet mounted on the upper end of the lead screw extension comes in close proximity. As the lead screw (and the CRA) moves, switches operate sequentially, producing an analog voltage proportional to position. Additional reed switches are included in the same tube with the absolute position transducer to provide full withdrawal and insertion signals. The relative position indication for each rod is calculated by the Digital Control Rod Drive Control System controller based on the sequence and number of power pulses it generates to drive the motor stator windings in and out.

k. Motor Tube Design Criteria

The motor tube design complies with Section III of the ASME Boiler and Pressure Vessel Code for a Class 1 appurtenance. The operating transient cycles, which are considered for the stress analysis of the reactor pressure vessel, are also considered in the motor tube design.

Quality standards relative to material selection, fabrication, and inspection are specified to ensure safety functions of the housings essential to accident prevention. Materials conform to ASTM or ASME, Section II, Material Specifications. All welding shall be performed by personnel qualified under ASME Code, Section IX, Welding Qualifications. These design and fabrication procedures establish quality assurance of the assemblies to contain the reactor coolant safely at operating temperature and pressure.

In the highly unlikely event that a pressure barrier component or the control rod drive assembly does fail catastrophically, i.e., rupture completely, the following results would ensue:

1) Control Rod Drive Nozzle

The assembly would be ejected upward as a missile until it was stopped by the missile shield over the reactor. This upward motion would have no adverse effect on adjacent assemblies.

2) Motor Tube

The failure of this component anywhere above the lower flange would result in a missile-like ejection into the missile shielding over the reactor. This upward motion would have no adverse effect on adjacent mechanisms (Reference 72).

3.2.4.3.2.2 Deleted

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TABLE 3.2-1
(Sheet 1 of 1)

CORE DESIGN DATA

Reactor

Design heat output, MWt	2,568
Vessel coolant inlet temperature, °F at rated power	554.3
Vessel coolant outlet temperature, °F at rated power	603.7
Core operating pressure, psia	2,200
Reactor coolant flow, design flow %	107.0 ^(a)

Core and Fuel Assemblies ^(b)

Total No. of fuel assemblies incore	177
No. of fuel rods per fuel assembly	208
No. of control rod guide tubes per assembly	16
No. of incore instr. positions per fuel assembly	1
Fuel rod outside diameter, inches	0.430
Cladding thickness, inches	0.025
Fuel rod pitch, inches	0.568
Fuel assembly pitch spacing, inches	8.587
Unit cell metal-water ratio (volume basis)	0.82
Cladding material	M5

^(a) Including instrument uncertainty, a minimum design flow of 104.5% is protected in DNB-related analyses.

^(b) Data is for Mark-B-HTP Fuel Assemblies.

TMI-1 UFSAR

TABLE 3.2-2
(Sheet 1 of 2)

NUCLEAR DESIGN DATA^{a, b} (Current Core)

Fuel Assembly Volume Fractions

Fuel	
Moderator	0.306
Zircaloy	0.583
M5	0.005
Void	0.095
	<u>0.011</u>
	1.000

Total U (BOC)

Metric Tons	86.2
-------------	------

Core Dimensions, inches

Equivalent Diameter	128.9
Active Height (current cycle)	See Table 3.2-11

Unit Cell H₂O/U Atomic Ratio (Fuel Assembly)

Cold	2.71
Hot	1.98

Effective Full-Power Lifetime, days

Current Cycle	680
---------------	-----

Fuel Irradiation, MWd/MTU

Current Cycle Average	20,261
-----------------------	--------

Core Average Burnup, MWd/MTU

Current Cycle (at 715 EFPD)	38,667
-----------------------------	--------

^(a) Data is for Mark B12 and Mark B-HTP Fuel Assemblies.

^(b) Source: TMI-1 Cycle 21 Reload Report (Reference 110).

TMI-1 UFSAR

TABLE 3.2-2
(Sheet 2 of 2)
NUCLEAR DESIGN DATA^b
(Current Core)

Initial Enrichments

Batch ID	Base Enrichment (wt.% ²³⁵ U)	Zone-Loaded Enrichment (wt.% ²³⁵ U)	Gadolinia Rods No. x wt.% Gd ₂ O ₃	Gad Rod Enrichment (wt.% ²³⁵ U)
20A2	1.40	----	None	----
21C1 (a, c)	4.40	4.10	12 x 3.0 8 x 8.0	3.70 2.50
21C3 (a, c)	4.40	4.10	12 x 3.0 8 x 8.0	3.70 2.50
21D (a, c)	4.82	4.40	12 x 2.0 8 x 8.0	4.10 2.90
21E2 (a, c)	4.90	4.40	16 x 2.0	4.10
21F2 (a, c)	4.95	4.40	12 x 2.0	4.10
22A2 (a, c)	4.57	4.20	12 x 3.0 8 x 8.0	3.80 2.50
22B (a, c)	4.76	4.40	12 x 2.0 8 x 8.0	3.80 2.85
22C (a, d)	4.76	4.40	12 x 3.0 8 x 8.0	3.80 2.85
22D (a, c)	4.90	4.57	12 x 2.0	4.20
22E (a, c)	4.90	4.57	16 x 2.0	4.20
23A (a, c)	4.10	3.80	None	----
23B (a, c)	4.10	3.80	12 x 3.0 8 x 8.0	3.40 2.50
23C (a, c)	4.30	3.80	None	----
23D (a, c)	4.30	3.80	16 x 2.0 4 x 6.0	3.40 2.50
23E (a, c)	4.50	4.10	None	----
23F (a, c)	4.50	4.10	8 x 2.0	3.80
23G (a, c)	4.50	4.10	16 x 2.0	3.80
23H (a, c)	4.50	4.10	12 x 2.0 8 x 8.0	3.80 2.50

Burnable Poison Data

Integral BP Concentration

Number of
BP Pins

Wt %
(Gd₂O₃ in UO₂)

1840 Burned

2.0, 3.0, or 8.0

736 Fresh

2.0, 3.0, 6.0, or 8.0

Control Data

Total Worth of full-length CRAs (Current Cycle)

BOC (HZP), % delta-k/k (0 EFPD)

7.635

EOC (HZP), % delta-k/k

8.535

- These fuel assemblies have six-inch upper and lower axial blankets enriched to 2.5 wt% ²³⁵U for the non-gadolinia fuel rods; all gadolinia-bearing fuel rods have 9.9 inch upper and lower axial blankets enriched to 2.5 wt% ²³⁵U.
- Source: TMI-1 Cycle 21 Reload Report (Reference 110)
- The zone-loaded enrichment in these assemblies refers to the enrichment of the three rods in each assembly corner, i.e., a total of 12 fuel rods per fuel assembly.
- The zone-loaded enrichment in these assemblies refers to the enrichment of the three rods in each assembly corner plus 16 interior rods, i.e., a total of 28 fuel rods per fuel assembly.

TMI-1 UFSAR

TABLE 3.2-3
(Sheet 1 of 1)

EXCESS REACTIVITY CONDITIONS (Initial Cycle)

Effective Multiplication, k_{eff} ^{a,d}

Cold, 70 °F, clean	1.257
Hot, 532 °F, clean, zero power	1.194
Hot, 580 °F, clean, full power	1.168
Hot, 580 °F, full power, equilibrium xenon and samarium	1.122

Single Fuel Assembly^b

Hot	0.77
Cold ^c	0.87

^a First cycle at beginning-of-cycle (BOC) reflects burnable poison holddown.

^b Based on highest probable enrichment of 3.5 wt %.

^c A center-to-center assembly pitch of 21 in. is required for this k_{eff} in cold, unborated water with no xenon or samarium.

^d Values were originally for Cycle 1 but are representative of the current cycle.

TMI-1 UFSAR

TABLE 3.2-4
(Sheet 1 of 1)
CYCLE REACTIVITY CONTROL DISTRIBUTION^a
(Current Cycle)

Critical boron - BOC, ppm

HZP, (no Xe, 0 EFPD)	2095	
HFP, (eq Xe, 4 EFPD)	1459	

Critical boron - EOC, ppm

HZP, (no Xe)	559	
HFP, (eq Xe)	-126	

Control rod worths - HFP, BOC % delta-k/k

Group 5	1.467	
Group 6	0.955	
Group 7 ^c	1.084	

Control rod worths - HFP, EOC % delta-k/k

Group 5	1.576	
Group 6	1.022	
Group 7 ^c	1.158	

Max ejected rod worth - HZP, % delta-k/k
(Groups 5 to 7 inserted)

BOC (no Xe)	0.384 ^b	
EOC (eq Xe)	0.582 ^b	

Max stuck rod worth - HZP, % delta-k/k

BOC (no Xe)	1.278 ^b	
EOC (no Xe)	1.688 ^d	

^a Source: Physics Manual, TMI-1 Cycle 21 (Reference 104), unless noted.

^b Core location N12

^c Group 7 is transient rod bank.

^d Core location O11

TMI-1 UFSAR

TABLE 3.2-5
(Sheet 1 of 1)

HFP CRITICAL BORON CONCENTRATION OVER CORE LIFE^a
(Current Cycle)

<u>Core Life (EFPD)</u>	<u>Boron Concentration (ppm)</u>	
4	1459	
200	1221	
400	685	
600	107	
715	-126	

^a Source: Physics Manual, TMI-1 Cycle 21 (Reference 104).

TMI-1 UFSAR

TABLE 3.2-6
(Sheet 1 of 1)

SHUTDOWN REACTIVITY ANALYSIS^c (Current Cycle)

<u>Available Rod Worth</u>	<u>BOC, % delta-k/k</u>	<u>EOC,^(a) % delta-k/k^(a)</u>	
Total Rod Worth, HZP ^(b)	7.635	8.535	
Reduction of Worth due to Poison			
Material Depletion	-0.161	-0.211	
Maximum Stuck Rod Worth, HZP	<u>-1.309</u>	<u>-1.694</u>	
Net Worth	6.165	6.630	
Less 10% Uncertainty	<u>-0.617</u>	<u>-0.663</u>	
Total Available Worth	5.548	5.967	
Required Rod Worth			
Power Deficit, HFP to HZP	1.593	2.888	
Maximum Allowable Inserted Rod Worth	0.383	0.604	
Off-Nominal Flux Distribution Allowance	0.370	0.370	
Cycle 18 Shutdown Flexibility Allowance	<u>0.050</u>	<u>0.050</u>	
Total Required Worth	2.396	3.912	
Shutdown Margin			
Total Available Worth minus Total Required Worth	3.152	2.055	

NOTE: Required Shutdown Margin is 1.00 %Δk/k.

Notes:

^(a) 680 EFPD

^(b) HZP denotes hot zero power (532F T_{avg}); HFP denotes hot full power (581F T_{avg})

^(c) Source: TMI-1 Cycle 21 Reload Report, (Reference 110).

TMI-1 UFSAR

TABLE 3.2-7
(Sheet 1 of 1)

CONTROL ROD GROUP WORTHS AT HZP ^a
(Current Cycle)

<u>Group(s)</u> <u>Inserted</u>	<u>Worth (% delta-k/k)</u>	
	<u>BOC</u>	<u>EOC</u>
1 to 4	4.474	5.209
5	1.288	1.380
6	0.894	0.883
7	0.975	1.059
1 to 7	7.631	8.530

^a Source: Physics Manual, TMI-1 Cycle 21 (Reference 104), HFP equilibrium Xenon.

TABLE 3.2-8

DELETED

TMI-1 UFSAR

TABLE 3.2-9
(Sheet 1 of 1)

REACTIVITY COEFFICIENTS^a (Current Cycle)

Doppler coeff - BOC, 10^{-5} (delta-k/k/ °F) 100% power (No Xe)	-1.67	
Doppler coeff - EOC, 10^{-5} (delta-k/k/ °F) 100% power (eq Xe)	-1.82	
Moderator coeff - HFP, 10^{-4} (delta-k/k/ °F)		
BOC (No Xe, 1940 ppm)	-0.46	
EOC (eq Xe, 0 ppm)	-3.45	
Boron worth - HFP, ppm/% delta-k/k		
BOC (4 EFPD)	165	
EOC (eq Xe)	129	
Xenon worth HFP, % delta-k/k		
BOC (4 EFPD)	2.42	
EOC (equil)	2.64	
Power deficit, HZP to HFP, % delta-k/k		
BOC (4 EFPD)	-1.59	
EOC (eq Xe)	-2.89	
^(a) Source: TMI Cycle 21 Reload Report (Referenced 110).		

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TABLE 3.2-10
(Sheet 1 of 1)

EXPERIMENTAL AND CALCULATED ROD AND ROD ASSEMBLY COMPARISON

Core No.	Rods per Clusters & Type	Clusters per core	Soluble Boron (ppm)	Control Rod Worth (% delta-k/k)		
				Experimental	Calculate	
4-K	(43)16 Ag-Cd-In	1	1375	1.39	0.09	1.41
4-E	(39)20 Ag-Cd-In	1	1365	1.52	0.09	1.53
4-F	(39) 9 Ag-Cd-In	4	1219	3.36	0.09	3.35
5-B	(39) 4 Ag-Cd-In	4	1232	2.02	0.09	2.03
5-C	(39)12 Ag-Cd-In	2	1167	2.36	0.09	2.37
5-D	(39)16 Ag-Cd-In	2	1118	2.86	0.09	2.89
5-E	(39)20 Ag-Cd-In	2	1082	3.06	0.09	3.12
4-E	(39)20 B ₄ C	1	1358	1.6	0.06	1.63
4-F	(39) 9 B ₄ C	4	1200	3.65	0.06	3.67
4-K	(43)16 B ₄ C	1	1359	1.52	0.06	1.50
14-3	(46)24 B ₄ C	1	1160	2.05	0.07	2.09
14-2	(46)24 Ag-Cd-In	1	1289	1.97	0.07	1.97

TMI-1 UFSAR

TABLE 3.2-11
(Sheet 1 of 4)

THERMAL AND HYDRAULIC DATA ^a

Heat Transfer and Fluid Flow at Design Power

Total heat transfer surface incore (ft ²)	49,505
Average heat flux (Btu/hr-ft ² , power 100%)	172,700
Maximum heat flux (Btu/hr-ft ² , power 100%)	512,800
Average power density incore (kW/l)	83.96
Average thermal output of fuel rod (kW/ft)	5.70
Maximum thermal output of fuel rod (kW/ft)	17.63 ^c
Maximum cladding surface temperature (°F)	654 ^c
Average core fuel temperature (°F)	(see sheet 4)
Maximum fuel central temperature at hot spot(°F)	4,220 ^c
Total reactor coolant flow (lb/hr)	140.87 x 10 ⁶
Core flow area (effective for heat transfer) (ft ²)	49.65
Core coolant average velocity (fps)	15.3
Coolant outlet temperature at hot channel (°F)	645.06
Active Fuel Length (inches)	(see Sheet 4)
Core Bypass Flow	
Best Estimate (%)	6.06
System Pressure (psia)	2,200
Design Power Level (MWt)	2,568

TMI-1 UFSAR

TABLE 3.2-11
(Sheet 2 of 4

THERMAL AND HYDRAULIC DATA^a

Power Distribution

Maximum/average power ratio, radial x local (nuclear $F_{\text{delta-h}}^N$)	1.80
Maximum/average power ratio, axial (F_z^N nuclear)	1.65
Overall power ratio (F_q nuclear) = $F_z^N \times F_h^N$	2.97
Power generated in fuel and cladding (%)	97.3

Hot Channel Factors

(See Sheet 3 for currently used uncertainty factors)

DNB Data

CHF correlation	BHTP
DNBR Modeling	Cross Flow
Design overpower (% design power)	112
DNB ratio at design overpower (112% design power)	≥ 2.06
DNB ratio at initial cond. power (102% design power)	≥ 2.27
DNBR Thermal Design Limit (TDL)	1.50

TMI-1 UFSAR

TABLE 3.2-11
(Sheet 3 of 4)

THERMAL AND HYDRAULIC DATA^a

Uncertainty Parameters Used in Statistical Core Design Analysis

<u>State Variable</u>	<u>Uncertainty Variable</u>	<u>Uncertainty</u>
Core Power	Heat Balance	2%
RCS Pressure	Pressurized Uncertainty	65 psia
Core Flow	RCS Flow Uncertainty	2.5%
Core Subcooled Inlet Temperature	Inlet Temperature Uncertainty from RTD String Error	2°F

Note: The table above includes plant instrumentation uncertainties only. Additional analysis uncertainties are described in Reference 134.

TMI-1 UFSAR

TABLE 3.2-11
(Sheet 4 of 4)

THERMAL AND HYDRAULIC DATA^a

Fuel Thermal Parameters

	All Batches Mark-B-HTP
Number of Assemblies	177
Pellet diameter ^b , inches	0.3735
Fuel stack height ^b , inches	143.0
Nominal LHR at 2568 Mwt, kW/ft	5.70
LHR to center fuel melt, kW/ft	25.16 @ 50 MWd/mtU 25.36 @ 1000 MWd/mtU 25.24 @ 10000 MWd/mtU 24.54 @ 15000 MWd/mtU 24.09 @ 20000 MWd/mtU 23.61 @ 25000 MWd/mtU 23.13 @ 30000 MWd/mtU 22.17 @ 40000 MWd/mtU 20.99 @ 50000 MWd/mtU 19.57 @ 62000 MWd/mtU

^a Current Cycle design values, except as noted
^b Undensified and cold
^c Initial cycle value

TMI-1 UFSAR

TABLE 3.2-12
(Sheet 1 of 1)

NUCLEAR POWER FACTORS

1. The nominal nuclear peaking factors for the worst time in the initial cycle core life are:

$$F_{\text{delta-h}} = 1.77$$

$$F_z = 1.70$$

$$f_q = 3.01$$

2. The design nuclear peaking factors for the worst time in core life are:

Initial Cycle

$$F_{\text{delta-h}} = 1.78$$

$$F_z = 1.70$$

$$F_q = 3.03$$

Current Cycle

$$F_{\text{delta-h}} = 1.80$$

$$F_z = 1.65$$

$$F_q = 2.97$$

Where:

$$F_{\text{delta-h}} = \text{maximum/average radial power ratio (radial x local nuclear)}$$

$$F_z = \text{maximum/average axial power ratio (nuclear)}$$

$$F_q = F_{\text{delta-h}} \times F_z \text{ (nuclear total)}$$

The nominal values are the maximum values calculated with nominal spacing of fuel assemblies. The design values are obtained by examining maximum, nominal, and minimum fuel assembly spacing and determining the worst values for the combined effect of flow and rod peaking.

TMI-1 UFSAR

TABLE 3.2-13
(Sheet 1 of 1)

COEFFICIENTS OF VARIATION

CV No.	Deviation	Standard Mean Value of Variable (σ)	Coefficient of Variable (x)	of Variable ($\sigma\sqrt{x}$)
1	Flow Area Interior bundle cells	0.00190	0.17740	0.01072
	Peripheral bundle cells	0.00346	0.21546	0.01608
2	Local Rod Diameter	0.000647	0.430	0.00151
3	Average Rod Diameter (die-drawn, local and average same)	0.000647	0.430	0.00151
4	Local Fuel Loading			0.00698
	Subdensity	0.000647	0.935	0.00088
	Subfuel area (diameter effect)	0.000094	0.1075	0.00088
5	Average Fuel Loading			
	Subdensity	0.00485	0.935	0.00519
	Sublength	0.26294	1.44	0.00183
	Subfuel area (diameter effect)	0.000094	0.1075	0.00088
6	Local Enrichment	0.00421	2.30	0.00183
7	Average Enrichment	0.00421	2.30	0.00183

Enrichment values are for worst case normal assay batch; maximum variation occurs for minimum enrichment.

TABLE 3.2-14
(Sheet 1 of 1)HOT CHANNEL DATA AND PERFORMANCE FOR FOUR-PUMP OPERATION*
(Initial Cycle)

Engineering Hot Channel Factors

$$FQ = 1.011$$

$$FQ'' = 1.014$$

$$F_A = 0.98 \text{ (interior cells)}$$

$$F_A = 0.97 \text{ (wall cells)}$$

Performance Summary*

<u>Reactor Design Power^a (%)</u>	<u>DNB Ratio (W-3)</u>	<u>Exit Quality (%)</u>
100	2.00	0.9
107.5 (trip setting)	1.75	3.4
114 (maximum power)	1.55	5.8
122.5	1.30	9.2

Hot Channel Statistical Statement

The DNB ratio in the hot channel at the maximum overpower of 114 percent is 1.55, which corresponds to a 99 percent confidence that at least 99.34 percent of the fuel channels of this type are in no jeopardy of experiencing a DNB.

^a The reference design core power level is 2568 MWt

TMI-1 UFSAR

TABLE 3.2-15
(Sheet 1 of 1)

HOT CHANNEL PERFORMANCE VS PUMPS IN SERVICE
(Initial Cycle)

<u>Reactor Coolant Pumps Operating</u>	<u>3 Pumps</u>	<u>2 Pumps (2 Loops)</u>
Hot Channel DNBR at Maximum Design Overpower	1.30	1.40
Hot Channel Quality at Minimum DNBR Point, %	7.0	15.0
Reactor Coolant Flow, % of Rated	74.7	49.0
Maximum Design Overpower, % of Reference Design Power (2568 MWt)	101.5	77.0

TMI-1 UFSAR

TABLE 3.2-16
(Sheet 1 of 2)

FUEL ASSEMBLY COMPONENT DIMENSIONS ^(a)

<u>Item</u>	<u>Material</u>	<u>Dimensions (inches)</u>
<u>Fuel Rod</u>		
Fuel	UO ² sintered pellets ^(b)	See Table 3.2-11 for diameters
Fuel Clad	M5	0.430 OD x 154.075 long
Fuel Rod Pitch		0.568
Active Fuel Length		See Table 3.2-11
Nominal Fuel-to-Clad Gap (BOC)		0.00325

^(a) Data for Mark-B-HTP Fuel Assemblies.

^(b) 96% theoretical density fuel (undensified and cold).

TMI-1 UFSAR

TABLE 3.2-16
(Sheet 2 of 2)

FUEL ASSEMBLY COMPONENT DIMENSIONS^(a)

<u>Item</u>	<u>Material</u>	<u>Dimensions (inches)</u>
<u>Fuel Assembly</u>		
Fuel Assembly Pitch		8.587
Overall Length		165.835
Control Rod Guide tube wall	M5	0.530 OD x 0.016
Instrumentation tube (Mark-B-HTP)	M5	0.493 OD x 0.400 ID for bottom 135" of tube)
End Fittings	Stainless Steel (castings)	
Spacer Grid Bottom: Inconel-718 Intermediate/Top: M5		<div><div><u>Interior Rib</u></div><div>0.0125</div></div> <div><u>Exterior Rib</u></div> <div>0.025</div>
		<div><div>0.014</div><div>0.026</div></div>

TMI-1 UFSAR

TABLE 3.2-17
(Sheet 1 of 1)

HOT CHANNEL AND CORE AVERAGE COOLANT CONDITIONS (Initial Cycle)

<u>Reactor Design Power^c(%)</u>	<u>F delta-h</u>	<u>Quality(%)</u>	<u>Exit Void Fraction(%)</u>	<u>Operating Pressure (psig)</u>
100	1.78	(-)1.3 ^a	0.9 ^b	2185
114	1.78	3.4	12.8	2185
128	1.78	8.7	31.2	2185
114	1.96	7.1	32.2	2185
110	1.78	0.9	2.8	2120
114	1.78	5.8	26.3	2120
128	1.78	11.3	39.8	2120
114	1.96	9.5	41.1	2120

Core Average Void Fraction

<u>Flow (%)</u>	<u>Pressure (psig)</u>	<u>Core Void Fraction (%)</u>
100	2185	0.06
100	2120	0.194
95	2185	0.201
95	2120	0.770

^a Negative indication of quality denotes subcooling

^b Subcooled voids

^c The reference design core power level is 2568MWt

TMI-1 UFSAR

TABLE 3.2-18
(Sheet 1 of 1)

TYPICAL HOT CHANNEL DNB (BHTP) AS A FUNCTION OF POWER ^a

<u>Percent Reference Design Power 2568 MWt</u>	<u>Hot Channel DNB Ratio (BHTP)</u>	
102	2.27	
112	2.06	
System Flow = 107% of 88,000 gpm/pump (nominal flow) ^b		
Core Bypass = 6.26%		
Radial - Local Peak = 1.800		
^a Cycle 20 values given as typical		
^b SCD analysis includes 2.5% flow measurement uncertainty; protecting a minimum flow of 104.5% of 88,000 gpm/pump		

TMI-1 UFSAR

TABLE 3.2-19
(Sheet 1 of 1)

REACTOR CORE BYPASS FLOW
(Current Cycle)

	<u>Path</u>	<u>System Flow</u> ^a (%)	
1.	Shroud		
2.	Control rod guide tubes and instrument guide tubes		
3.	Inlet to outlet interfaces		
4.	Shroud gap		
5.	Assumed allowance		
6.	Open fuel assemblies		
7.	Total design bypass	6.26 ^b	

^a Based on the design system flow of 134.02×10^6 lb/hr

^b Best estimate total bypass is assumed for the SCD-based design DNBR analysis that supports current COLR protective limits.

TMI-1 UFSAR

TABLE 3.2-20
(Sheet 1 of 1)

DNB RATIOS IN THE FUEL ASSEMBLY CHANNELS (W-3) (Initial Core)

Most Probable Conditions

<u>Cell Type</u>	<u>G (lb/hr-ft² x 10⁻⁶)</u>	<u>DNBR (W-3) (114% power)^a</u>
Unit	2.51	1.82
Corner	2.58	1.86
Wall (peripheral)	2.56	1.90
Control rod	2.40	1.97

Maximum Design Conditions

Unit	2.26	1.55
Corner	2.14	1.66
Wall (peripheral)	2.20	1.65
Control rod	2.16	1.69

^a The overpower level is based on a reference design core power level of 2568 MWt

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TABLE 3.2-21
(Sheet 1 of 1)

INTERNAL VENT VALVE MATERIALS

<u>Valve Part Name</u>	<u>Material and Form</u>	<u>Material Specific</u>
Valve Body	304 SS Casting ^a	ASTM A351-CF8
Valve Disc	304 SS Casting ^a	ASTM A351 - CF-8
Disc Shaft	431 SS Bar ^b	ASTM A276 Type Cond. T
Shaft Bushings	Stellite No. 6	
Retaining Rings (Top and Bottom)	15-5 pH (H 1100) SS Forgings	AMS 5658
Ring Jackscrews	"A-286 Superalloy" SS ^c	AMS 5737C
Jackscrew Bushings	431 SS Bar	ASTM A276 Type Cond. A
Misc Fasteners, Covers, Locking Devices, etc.	304 SS Plate Bar etc.	ASTM A240, ASTM A276

^a Carbide solution annealed, C_{max}0.08%,Co_{max} 0.2%.

^b Heat treated and tempered to Brinell Hardness Number (BHN) range of 290-320

^c Heat treated to produce a BHN of 248 minimum

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TABLE 3.2-22
(Sheet 1 of 1)

VENT VALVE SHAFT AND BUSHING CLEARANCES

Clearance Gaps are Illustrated in Figure 3.2-48

A. Cold Clearance Dimensions at 70F

Bushing ID	1.500 to 1.505
Shaft OD	<u>1.490</u> to <u>1.485</u>
	0.010 to 0.020 Clearance (Gaps 1, 2, 7, and 8)

Body ID	2.000 to 2.005
Bushing OD	<u>1.997</u> to <u>1.995</u>
	0.003 to 0.010 Clearance (Gaps 3, 4, 5, and 6)

Bushing End Clearance (Gaps 9 and 10)

Body Lugs	5.765 to 5.780
Disc Hub	<u>4.750</u> to <u>4.740</u>
	1.015 to 1.040
	<u>0.996</u> to <u>0.992</u>
	0.019 to 0.048 End Clearance (Gaps 9 and 10)

Bushing Flange	0.249 x 4 = 0.996
	0.248 x 4 = 0.992

B. Hot Clearance Differential Change From 70 to 580F

Linear coefficient of thermal expansion of the materials
for a temperature change of 70 to 600F.

Shaft: A276 type (431)	6.7×10^{-6} in./in./°F
Bushing: Stellite #6	8.1×10^{-6}
Bodies: CF8 Stainless	9.82×10^{-6}

delta-T = 580 - 70 = 510F

delta-D = D (delta-T)

Shaft	= 1.5	(6.7×10^{-6})	510 = 0.0051
Bushing ID	= 1.5	(8.1×10^{-6})	510 = <u>0.0062</u>
			+0.0011 Increase

Bushing OD	= 2	(8.1×10^{-6})	510 = 0.0083
Body ID	= 2	(9.82×10^{-6})	510 = <u>0.0100</u>
Bushing Endplay Hot			+0.0017 Increase

CF8 Body	delta-L = 1	(9.82×10^{-6})	510 = 0.0050
Stellite #6	= 1	(8.1×10^{-6})	510 = <u>0.0041</u>
Bushing Flange			+0.0009 Increase

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TABLE 3.2-23
(Sheet 1 of 3)

CLAD CIRCUMFERENTIAL STRESSES* (Initial Cycle)

Case	P-External (psig)	P-Internal (psig)
1. Beginning of life - pre-operational hot standby- 0% power	2,185 ¹ 2,500 ²	690 690
2. Beginning of life - void section of clad - 100% power ⁶	2,185 2,500	1,340 1,340
3. Beginning of life - void section of clad - 114% power ⁶	2,185 2,500	1,380 1,380
4. Beginning of life - fueled section of clad - 100% power ⁶	2,185 2,500	1,340 1,340
5. Beginning of life - fueled section of clad - 114% power ⁶	2,185 2,500	1,380 1,380
6. End of life - hot standby 0% power	2,185 2,500	725 725
7. End of life - fueled section clad - 100% power ⁶ - design internal pressure	2,185 2,500	3,300 ³ 3,300
8. End of life - fueled section of clad - 114% power ⁶ - design internal pressure	2,185 2,500	3,300 3,300
9. End of life - fueled section of clad - 100% power ⁶	2,185 2,500	2,160 2,160
10. End of life - fueled section of clad - 114% power ⁶	2,185 2,500	2,300 2,300
11. End of life - immediately after shutdown 2,500 1,450	2,185	1,450
12. End of life at clad temperature of 425°F	1,725	1,270

* Updated numbers are available in the TMI-1 Densification Report (Reference 19)

See notes 1 - 6 on Sheet 3 of 3

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TABLE 3.2-23
(Sheet 2 of 3)

CLAD CIRCUMFERENTIAL STRESSES*

	<u>T_{Clad}</u> <u>(F)</u>	<u>Bending</u> <u>Membrane</u>	<u>Yield⁵</u> <u>Total</u> <u>(psi)</u>	<u>Ultimate⁵</u> <u>Strength</u> <u>(psi)</u>	<u>Strength</u> <u>(psi)</u>
1.	532	-18,800	-18,800	48,000	57,000
	532	-23,700	-23,700	48,000	57,000
2.	650	-10,100	-10,000	45,000	50,000
	650	-14,200	-14,200	45,000	50,000
3.	650	- 9,600	- 9,600	45,000	50,000
	650	-13,700	-13,700	45,000	50,000
4.	723	-10,100	-13,700	42,000	44,000
	723	-14,200	-17,800	42,000	44,000
5.	733	- 9,600	-13,700	41,500	43,500
	733	-13,700	-17,800	41,500	43,500
6.	532	-18,300	-18,300	48,000	57,000
	532	-23,100	-23,100	48,000	57,000
7.	704	+10,100 ⁴	+16,200	43,000	46,000
	704	+7,200 ⁴	+12,100	43,000	46,000
8.	711	+10,100 ⁴	+16,500	43,000	46,000
	711	+7,200 ⁴	+12,400	43,000	46,000
9.	704	-300	-3,000	43,000	46,000
	704	-3,900	-6,600	43,000	46,000
10.	711	+1,100 ⁴	+4,400	43,000	46,000
	711	-2,300	-5,300	43,000	46,000
11.	535	-8,700	-8,800	48,000	57,000
	535	-12,700	-12,800	48,000	57,000
12.	425	-5,300	-5,300	50,000	62,500

* Updated numbers are available in the TMI-1 Densification Report (Reference 19)

See notes 1 - 6 on Sheet 3 of 3

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TABLE 3.2-23
(Sheet 3 of 3)

CLAD CIRCUMFERENTIAL STRESSES*

¹ System operating pressure

² System design pressure

³ Fuel rod clad internal design pressure

⁴ Pressure stress only

⁵ Cladding is specified with 45,000 psi minimum yield strength and 10-percent minimum elongation, both at 650F. Minimum room temperature strengths are approximately 75,000 psi yield strength 0.2 percent offset) and 85,000 psi ultimate tensile strength

⁶ The reference design core power level is 2568 MWt

* Updated numbers are available in the TMI-1 Densification Report
(Reference 19)

TABLE 3.2-24
(Sheet 1 of 1)CONTROL ROD ASSEMBLY DATA

<u>Item</u>	<u>Data</u>
Number of CRAs	61
Number of control rods per assembly	16
Outside diameter of control rod, inches	0.441
Cladding thickness, inches	0.0225
Cladding material	Inconel 625
End plug material	Inconel 625
Spider material	SS Grade CF3M
Poison material	80% Ag, 15% In, 5% Cd
Female coupling material	Type 304 SS, annealed
Length of poison section, inches	139
Stroke of control rod, inches	139

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TABLE 3.2-25
(Sheet 1 of 1)

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TABLE 3.2-26
(Sheet 1 of 1)

BURNABLE POISON ROD ASSEMBLY DATA

<u>Item</u>	<u>Data</u>
Number of BPRA in core	None
Number of burnable poison rods per assembly	16
Outside diameter of burnable poison rod, inches	0.430
Cladding thickness, inches	0.035
Cladding material	Zircaloy-4, cold-worked
End plug material	Zircaloy-4, annealed
Poison material	B ₄ C in Al ₂ O ₃ matrix
Length of poison section, inches	123.2 ^a
Spider material	SS, Grade CF3M
Coupling mechanism material	Type 304 SS, annealed

^a Increased from 121" in Cycle 14 with introduction of Mark B12 fuel design.

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TABLE 3.2-27
(Sheet 1 of 1)

ORIFICE ROD ASSEMBLY DATA

<u>Item</u>	<u>Data</u>
Number of orifice rod assemblies	None for current cycle
Number of orifice rods per assembly	16
Outside diameter of orifice rod, inches	0.480
Orifice rod material	Type 304 SS, annealed
Spider material	SS, Grade CF3M
Coupling mechanism material	Type 304 SS, annealed, and 17-4PH, Condition H 1100

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TABLE 3.2-28
(Sheet 1 of 1)

CONTROL ROD DRIVE MECHANISM DESIGN DATA

<u>Mechanism Function</u>	<u>Shim Safety</u>	
Type	Roller Nut	
Quantity	61	
Location	Top-Mounted	
Direction of Trip	Down	
Velocity of Normal Withdrawal and Insertion, inches per minute	30	
Maximum Travel Time for Trip		
2/3 Insertion, s	1.40 ^a	
3/4 Insertion, s	1.52 ^a	
Length of Stroke, inches	139	
Design Pressure, psig	2500	
Design Temperature, °F	650	
Weight of Mechanism (approx), lb	977	
^a Does not include 0.21 seconds of non-travel time delay for CRD breaker opening (0.08 seconds) and stator unlatch (0.13 seconds).		

3.3 TESTS AND INSPECTIONS

3.3.1 NUCLEAR TESTS AND INSPECTION

3.3.1.1 Critical Experiments

An experimental program (References 73, 74, and 75) to verify the relative reactivity worth of the control rod assemblies (CRAs) has been completed. Detailed testing established the worth of the CRA under various conditions similar to those for the reference core. These parameters include control rod arrangement in a CRA, fuel enrichments, fuel element geometry, CRA materials, and soluble boron concentration in the moderator.

Gross and local power peaking were also studied, and three dimensional power peaking data were taken as a function of CRA insertion. Detailed peaking data were also taken between fuel assemblies and around the water holes left by withdrawn CRA. The experimental data have been analyzed and were used to bench mark the analytical models used in the design.

3.3.1.2 Zero Power, Approach To Power, And Power Testing

Boron worth and CRA worth are determined by physics tests at the beginning of each core cycle. The boron worth and CRA worth at a given time in core life will be based on CRA position indication and calculated data as adjusted by operating data.

The reactor coolant is analyzed in the laboratory periodically to determine the boron concentration, and the reactivity held in boron is calculated from the concentration and the reactivity worth of boron.

The method of maintaining the hot shutdown margin (hence ejected-CRA margin) is related to operational characteristics (load patterns) and to the power peaking restrictions on CRA patterns at power. The CRA pattern restrictions ensure that sufficient reactivity is always fully withdrawn to provide adequate shutdown with the stuck-CRA margin. Power peaking as related to CRA patterns and shutdown margin is predicted by calculations.

Operation under power conditions is normally monitored by incore instrumentation, and the resulting data is analyzed.

3.3.2 THERMAL AND HYDRAULIC TESTS AND INSPECTION

3.3.2.1 Reactor Vessel Flow Distribution And Pressure Drop Test

A 1/6 scale model of the reactor vessel and internals has been tested to evaluate:

- a. The flow distribution to each fuel assembly of the reactor core and to develop any necessary modifications to produce the desired flow distribution.
- b. Fluid mixing between the vessel inlet nozzle and the core inlet, and between the inlet and outlet of the core.
- c. The overall pressure drop between the vessel inlet and outlet nozzles, and the pressure drop between various points in the reactor vessel flow circuit.

- d. The internals vent valves for closing behavior and for the effect on core flow with valves in the open position.

The reactor vessel, flow baffle, and core barrel models were made of clear plastic to allow use of visual flow study techniques. All parts of the model except the core are geometrically similar to those in the production reactor. However, the simulated core was designed to maintain dynamic similarity between the model and production reactor.

Each of the 177 simulated fuel assemblies contains a calibrated flow nozzle. The test loop is capable of supplying cold water (80F) to three inlet nozzles and hot water (180F) to the fourth. Temperature was measured in the inlet and outlet nozzles of the reactor model and at the inlet and outlet of each of the fuel assemblies. Static pressure taps were located at suitable points along the flow path through the vessel. This instrumentation provided the data necessary to accomplish the objectives set forth for the tests. The results of the test were reported in Reference 17.

3.3.3 FUEL ASSEMBLY, CONTROL ROD ASSEMBLY, AND CONTROL ROD DRIVE MECHANICAL TESTS AND INSPECTION

To demonstrate the mechanical adequacy and safety of the fuel assembly, CRA, and control rod drive, a number of functional tests have been performed.

3.3.3.1 Prototype Testing

A full-scale prototype fuel assembly, CRA, and control rod drive have been tested in the Control Rod Drive Line (CRDL) Facility located at the B&W Research Center, Alliance, Ohio. This full-sized loop is capable of simulating reactor environmental conditions of pressure, temperature, and coolant flow. To verify the mechanical design, operating compatibility, and characteristics of the entire control rod drive fuel assembly system, the drive was stroked and tripped approximately 200 percent of the expected operating life requirements.

A portion of the testing was performed with maximum misalignment conditions. Equipment was available to record and verify data such as fuel assembly pressure drop, vibration characteristics, and hydraulic forces; and to demonstrate control rod drive operation and verify scram times. All prototype components were examined periodically for signs of material fretting, wear, and vibration fatigue to ensure that the mechanical design of the equipment met reactor operating requirements. Test results are given in Reference 76.

3.3.3.2 Model Testing

Many functional improvements have been incorporated in the design of the fuel assembly as a result of model tests. For example, the spacer grid to fuel rod contact area was fabricated to 10 times reactor size and tested in a loop simulating the coolant flow Reynolds number of interest. Thus, visually, the shape of the fuel rod support areas was optimized with respect to minimizing the severity of flow vortices and pressure drop. A nine-rod (3 x 3) assembly using stainless steel spacer grid material was tested at reactor conditions (640°F, 2200 psi, 13 fps coolant flow) for 210 days. Two full-sized canned fuel assemblies with stainless steel spacer grids were tested at reactor conditions, one for 40 days and the other for 22 days. A prototype canless fuel assembly using Inconel 718 spacer grids was tested for approximately 90 days,

approximately half of that time at reactor conditions. The principal objectives of these tests were to evaluate fuel assembly and fuel rod vibration and/or fretting wear resulting from flow-induced vibration. Vibratory amplitudes were found to be very small and, with the exception of a few isolated instances which were attributed to pretest spacer grid damage, no unacceptable wear was observed.

3.3.3.3 Component And/Or Material Testing

3.3.3.3.1 Fuel Rod Cladding

Extensive short-time collapse testing was performed on Zircaloy-4 tube specimens as part of the B&W overall creep-collapse testing program. Initial test specimens were 0.436 inch OD with wall thicknesses of 0.020 inch, 0.024 inch, and 0.028 inch. Ten 8 inch long specimens of each thickness were individually tested at 600°F at slowly increasing pressure until collapse occurred. Collapse pressures for the 0.020 inch wall thickness specimens ranged from 1800 to 2200 psig, the 0.024 inch specimens ranged from 2800 to 3200 psig, and the 0.028 inch specimens ranged from 4500 to 4900 psig. The material yield strength of these specimens ranged from 65,000 to 72,000 psi at room temperature and was 35,900 psi at 680°F.

Additional Zircaloy-4 short-time collapse specimens were prepared from material with a yield strength of 78,000 psi at room temperature and 48,500 psi at 615°F. Fifteen specimens having an OD of 0.410 inch and an ID of 0.365 inch (0.0225 inch nominal wall thickness) were tested at 615°F at increasing pressure until collapse occurred. Collapse pressures ranged from 4470 to 4960 psig.

Creep-collapse testing was performed on the 0.436 inch OD specimens. Twelve specimens of 0.024 inch wall thickness and 30 specimens of 0.028 inch wall thickness were tested in a single autoclave at 680°F and 2050 psig. During this test, two 0.024 inch wall specimens collapsed during the first 30 days and two collapsed between 30 and 60 days. None of the 0.028 inch wall specimens had collapsed after 60 days. Creep-collapse testing was then performed on thirty 0.410 inch OD by 0.365 inch ID (0.0225 inch nominal wall) specimens for 60 days at 615°F and 2140 psig. None of these specimens collapsed, and there were no significant increases in ovality after 60 days.

The results of the 60 day creep-collapse testing on the 0.410 inch OD specimens showed no indication of incipient collapse. The 60 day period for creep-collapse testing was used because it exceeds the point of primary creep of the material yet is sufficiently long to enter the stage when fuel rod pressure begins to build up during reactor operation, i.e., past the point of maximum differential pressure that the cladding would be subjected to in the reactor.

These tests were followed by additional creep-collapse tests in which 60 specimens of variable wall thickness were subjected to pressure of 2085 psi at 385°F until collapse occurred. The cladding wall thickness was 0.0285, 0.0263, 0.0251, and 0.0240 inches. The cladding thickness included the range of tolerances for production cladding, and the pressure represented the fuel rod maximum pressure differential at operating conditions. The temperature was selected to conservatively approximate in-pile creep rates. It was found that the 0.024 inch wall specimens collapsed in less than a month, and several 0.0263 inch wall specimens collapsed in less than three months. In view of the unknown increase of in-pile creep rates as compared with out-of-pile creep rates, it was decided to provide backup support

for the cladding in the upper end void regions where cladding temperatures of 650°F occur in hot channels.

A spring spacer has been designed as a backup spacer, which provides radial support to the cladding without causing excessive axial restraint to fuel expansion. Analytical results have indicated that the spacer can withstand the shipping acceleration of the fuel pellets without permanent deformation. Tests have been performed to demonstrate that the spring spacer will provide the necessary backup support to the cladding. The spacers were enclosed in production zircaloy cladding and subjected to 2500 psi at 750°F. This represents the design system external pressure for the cladding and the normal operating temperature of the spacer. Post-autoclave examinations revealed that the cladding was adequately supported.

3.3.3.3.2 Fuel Assembly Structural Components

The structural characteristics of the fuel assemblies that are pertinent to loadings resulting from normal operation, handling, earthquake, and accident conditions were investigated experimentally in test facilities such as the CRDL Facility. Structural characteristics such as natural frequency and damping were determined at the relatively high (up to approximately 0.800 inch) amplitude of interest in the seismic and LOCA analyses. Natural frequencies and amplitudes resulting from flow-induced vibration were measured at various temperatures and flow velocities, up to reactor operating conditions.

3.3.3.4 Control Rod Drive Tests And Inspection

3.3.3.4.1 Control Rod Drive Developmental Tests

The prototype control rod drive mechanism was tested at the B&W Research Center at Alliance, Ohio. Wear characteristics of critical components indicated that material compatibility and structural design of these components would be adequate for the design life of the mechanism. The trip time for the mechanism as determined under test conditions of reactor temperature, pressure, and flow was well within the specification requirements (Reference 76).

3.3.3.4.2 Production Tests

The following control rod drive mechanism production tests were performed either on the drives installed or on drives manufactured to the same specifications:

- a. Ambient Tests
 - Coupling tests
 - Operating speeds
 - Position indication
 - Trip tests
- b. Operational Tests
 - Operating speeds
 - Position indication
 - Partial and full-stroke cycles
 - Partial and full-stroke trip cycles

The finished control rod drive mechanism was proof-tested as a complete system, i.e., mechanisms, motor control, and system control working as a system. This proof-testing was above and beyond the developmental testing performed in the product development stages.

3.3.4 INTERNALS TESTS AND INSPECTIONS

The internals upper and lower plenum hydraulic design was evaluated and guided by the results from the 1/6 scale model flow test which is described in Subsection 3.3.2.1. The test results have guided the design to obtain minimum flow maldistribution. Test data allowed verification of vessel flow and pressure drops.

The effects of internals misalignment were evaluated on the basis of the test results from the CRDL tests described in Subsection 3.3.3.4. These test results, correlated with the internals guide tube design, insure that the CRA can be inserted at specified rates under conditions of maximum misalignment.

Internals shop fabrication quality control tests, inspection, procedures, and methods are similar to those for the pressure vessel described in Section 4.3.11. The internals surveillance specimen holder tubes and the material irradiation program are described in Section 4.4.5.

3.3.4.1 Internals Non-Destructive Testing

A listing is included in the following sections for all internals non-destructive examinations and inspections with applicable codes or standards applicable to all core structural support material of various forms. In addition, one or more of these examinations were performed on materials or processes which are used for functions other than structural support (i.e., alignment dowels, etc.) so that virtually 100 percent of the completed internals materials and parts are included in the listing. Internals raw materials were purchased to ASME Code Section II or ASTM material specifications. Certified material test reports were obtained and retained to substantiate the material chemical and physical properties. All internals materials were purchased and obtained to a low cobalt limitation. The ASME Code Section III, as applicable for Class A vessels, was generally specified as the requirement for reference level non-destructive examination and acceptance. In isolated instances when ASME III could not be applied, the appropriate ASTM Specifications for non-destructive testing were imposed. All welders performing weld operations on internals were qualified in accordance with ASME Code Section IX, applicable edition and addenda. The primary purpose of the following list of non-destructive tests was to locate, define, and determine the size of material defects to allow an evaluation of defect acceptance, rejection, or repair. Repaired defects were similarly inspected as required by applicable codes.

3.3.4.1.1 Ultrasonic Examination

- a. Wrought or forged raw material forms were 100 percent inspected throughout the entire material volume to ASME III, Class A.
- b. Personnel conducting these examinations were trained and qualified.

3.3.4.1.2 Radiographic Examination (includes X-ray or radioactive sources)

- a. Cast raw material forms were 100 percent inspected to ASME III, Class A, or to ASTM.
- b. All circumferential full-penetration structural weld joints that support the core were 100 percent inspected to ASME III, Class A.
- c. All radiographs were reviewed by qualified personnel who were trained in their interpretation.

3.3.4.1.3 Liquid Penetrant Examination

- a. Cast form raw material surfaces were 100 percent inspected to ASME III, Class A, or to ASTM.
- b. Full penetration non-radiographic or partial penetration structural welds were inspected by examination of the root and the cover passes to ASME III, Class A.
- c. All circumferential full-penetration structural weld joints that support the core had cover passes inspected to ASME III, Class A.
- d. Personnel conducting these examinations were trained and qualified.

3.3.4.1.4 Visual (5X Magnification) Examination

This examination was performed in accordance with and results accepted on the basis of a B&W Quality Control Specification which complies with NAV-SHIPS 250-1500-1. Each entire weld pass and adjacent base metal were inspected prior to the next pass from the root to and including the cover passes.

- a. Partial-penetration non-radiographically or non- ultrasonically feasible structural weld joints were 100 percent inspected to the above specification.
- b. Partial or full-penetration attachment weld joints for non-structural materials or parts were 100 percent inspected to the above specification.
- c. Partial or full-penetration weld joints for attachment of mechanical devices which lock and retain structural fasteners.
- d. Personnel conducting these examinations were trained and qualified.

After completion of shop fabrication, the internals components were shopfitted and assembled to final design requirements. The assembled internals components underwent a final shop fitting and alignment of the internals with the as-built dimensions of the reactor vessel. Dummy fuel and CRAs were used to ensure that ample clearance exists between the fuel and internals structures guide tubes to allow free movement of the CRA throughout its full stroke length in various core locations. Fuel assembly mating fit was checked at all core locations. The dummy fuel and CRAs were identical to the production components except that they were

manufactured to the most adverse tolerance space envelope and they contained no fissionable or absorber materials.

All internal components can be removed from the reactor vessel to allow inspection of all vessel interior surfaces. Internals components surfaces can be inspected when the internals are removed to the canal underwater storage location.

3.3.4.1.5 Internals Vent Valves Tests

The internals vent valves were designed to relieve the pressure generated by steaming in the core following a LOCA so that the core will remain sufficiently cooled. The valves were designed to withstand the forces resulting from rupture of either a reactor coolant inlet or outlet pipe. To verify the structural adequacy of the valves to withstand the pressure forces and perform the venting function, the following tests were performed:

- a. A full-size prototype valve assembly (valve disc retaining mechanism and valve body) was hydrostatically tested to the maximum pressure expected to result during the blowdown.
- b. Sufficient tests were conducted at zero pressure to determine the frictional loads in the hinge assembly, the inertia of the valve disc, and the disc rebound resulting from impact of the disc on the seat so that the valve response to cyclic blowdown could be determined analytically.
- c. A prototype valve assembly was pressurized to determine the pressure differential required to cause the valve disc to begin to open. A determination of the pressure differential required to open the valve disc to its maximum open position was simulated by mechanical means.
- d. A prototype valve assembly was successfully installed and removed remotely in a test stand to confirm the adequacy of the vent valve handling tool.
- e. A 1/6 scale model valve disc closing force (excluding gravity) test as described in Section 3.3.2.1.
- f. The full-size prototype valve's response to vibration was determined experimentally to verify prior analytical results which indicated that the valve disc would not move relative to the body seal face as a result of vibration caused by transmission of core support shield vibrations. The prototype valve was mounted in a test fixture which duplicated the method of valve mounting in the core support shield. The test fixture with valve installed was attached to a vibration test machine and excited sinusoidally through a range of frequencies which encompassed those which may reasonably be anticipated for the core support shield during reactor operation. The relative motion between the valve disc and seat was monitored and recorded during testing. The test results indicated that there was no relative motion of the valve to its seat for conditions simulating operating conditions. After no relative motion was observed or recorded during testing, the valve disc was manually forced open during testing to observe its response. The disc closed with impact on its seat, rebounded open, and resealed without any adverse effects to valve seal surfaces, characteristics, or

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performance. From the oscillograph record, the natural frequency of the valve disc was conservatively calculated as approximately 1500 cps, whereas the range of frequencies for the primary system (including internal components) has been established as 15 to 160 cps. These frequencies are separated by an ample margin to conclude that no relative motion between the valve disc and its seat will occur during normal reactor operation.

Each production valve was subjected to tests described in Items b and c above except that no additional analysis was performed in conjunction with the test described in Item b above.

The valve disc, hinge shaft, shaft journals (bushings), disc journal receptacles, and valve body journal receptacles have been designed to withstand without failure the internal and external differential pressure loadings resulting from a LOCA. These valve materials were non-destructively tested and accepted in accordance with the ASME Code III requirements for Class A vessels as a reference quality level.

During scheduled refueling outages after the reactor vessel head and the internals plenum assembly have been removed, the vent valves are accessible for visual and mechanical inspection. A hook tool is provided to engage with the valve disc exercise lug described in Section 3.2.4.1.2.8. With the aid of this tool, the valve disc is manually exercised to evaluate the disc freedom. The hinge design incorporates special features, as described in Section 3.2.4.1.2.8, to minimize the possibility of valve disc motion impairment during its service life. With the aid of the hook tool, the valve disc sealing faces can be examined for surface irregularities. In the unlikely event that a hinge part should fail during normal operation, the most significant indication of such a failure would be a change in the free-disc motion as a result of altered rotational clearances. Remote installation and removal of the vent valve assemblies, if required, is performed with the aid of the vent valve handling tool which includes unlocking and operating features for the retaining ring jackscrews.

3.3.4.2 Internals Vibration Tests

Ocone 1 was the prototype reactor from which vibration test data (in situ) was obtained to verify the design adequacy of the TMI-1 internals to withstand the effects of flow-induced vibration. A vibration analysis and test program for the prototype reactor internals was submitted by References 77 and 78.

3.4 REFERENCES

1. U. S. Atomic Energy publication, Nuclear Reactors and Earthquakes TID-7024.
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