3.0 UNIT 1 REACTOR

3.1 UNIT 1 SUMMARY DESCRIPTION

All fuel rods are pressurized with helium during fabrication to reduce stresses and strains and to increase fatigue life. The fuel rods are cold worked, partially annealed Zircaloy tubes containing slightly enriched uranium dioxide fuel.

The fuel assembly is a canless type with the basic assembly consisting of the rod cluster control guide thimbles fastened to the grids, and to the top and bottom nozzles. The fuel rods are supported at several points along their length by the spring-clip grids.

Full length rod cluster control assemblies (RCCAs) are inserted into the guide thimbles of the fuel assemblies. The absorber sections of the RCCAs are fabricated of silver-indium-cadmium alloy sealed in stainless steel tubes.

The control rod drive mechanisms for the full length RCC assemblies are of the magnetic latch type. The latches are controlled by three magnetic coils. They are so designed that upon a loss of power to the coils, the RCCAs are released and fall by gravity to shut down the reactor.

The reactor was initially supplied with fuel from Westinghouse Electric Corp. (W). Reload fuel for Cycles 2 through 7 was supplied by Exxon Nuclear Co (ENC). Cycles 8 through 17 reload fuel was supplied by Westinghouse Electric Corp. The latest information regarding the current fuel cycle may be found in Sub-Chapter 3.5.

In addition to this summary description, this chapter contains: a description of the mechanical components of the reactor and reactor core, including Cycle 1 W fuel assemblies, reactor internals and control rod mechanisms (Sub-Chapter 3.2); a description of the Cycle 1 nuclear design for the W fuel (Sub-Chapter 3.3); a description of the Cycle 1 thermohydraulic design (Sub-Chapter 3.4); and a description of the current core design (Sub-Chapter 3.5).

The information contained in this chapter is principally concerned with the nuclear fuel and reactor internals design and therefore does not necessarily reflect the same information as that used in the safety analysis. For information concerning safety analysis, Chapter 14 should be consulted.

3.1.1 Performance Objectives

The current licensed thermal power limit is 3304 MWt. Calculations indicate that hot channel factors are considerably less than those used for design purposes in this application. The thermal and hydraulic design, and accident analyses in Chapter 14 were performed to support operation at this power level. These analyses identify design/safety limits for a potential uprating.

The initial reactor core fuel loading was designed to yield the first cycle nominal burnup of 16,666 MWD/MTU, and the Cycle 2 through 7 reload designs yield an nominal cycle burnup of 10,000 MWD/MTU. Reload designs for Cycles 8 through 17, yield nominal cycle burnups of between 15,000 and 19,442 MWD/MTU. The fuel rod cladding is designed to maintain its integrity for the anticipated 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 the fuel assemblies.

RCCAs are employed to provide sufficient reactivity control to terminate any credible power transient prior to reaching the applicable design minimum departure from nucleate boiling ratio (DNBR) (see Section 3.5.3). This is accomplished for the current cycle by ensuring sufficient RCCA worth to shut the reactor down by at least 1.3% in the hot condition with the most reactive RCCA stuck in the fully withdrawn position.

Redundant equipment is provided to add soluble poison to the reactor coolant in the form of boric acid to maintain shutdown margin when the reactor is cooled to ambient temperatures.

Experimental measurements from critical experiments or operating reactors, or both, are used to validate the methods employed in the design. During design, nuclear parameters are calculated for various operational phases and, where applicable, are compared with design limits to show that an adequate margin of safety exists.

In the thermal hydraulic design of the core, the maximum fuel and clad temperatures during normal reactor operation and at 118% overpower have been conservatively evaluated and found to be consistent with safe operating limitations.

3.1.2 Application of Principal Design Criteria

The reactor control and protection system is designed to actuate a reactor trip for any anticipated combination of plant conditions, when necessary, to ensure a minimum DNBR equal to or greater than the applicable design value for the fuel.

The integrity of fuel cladding is ensured by preventing excessive fuel swelling, excessive clad heating, and excessive cladding stress and strain. This is achieved by designing the fuel rods so that the following conservative limits are not exceeded during normal operation or any anticipated transient condition:

a. Minimum DNBR equal to or greater than the applicable design value for the fuel. For the current cycle, the design values are given in Section 3.5.3.

b. Fuel center temperature below melting point of UO2.

c. For W fuel for the initial core and ENC reload fuel, internal gas pressure less than the nominal external pressure (2250 psia), even at the end of life. For W reload fuel in the current cycle, the rod internal gas pressure shall remain below the value which causes the fuel-cladding diametral gap to increase due to outward cladding creep during steady-state operation.

d. Clad stresses less than the Zircaloy yield strength.

e. Clad strain less than 1%.

f. Cumulative strain fatigue cycles less than 80% of design strain fatigue life for ENC fuel. Cumulative strain fatigue cycles are less than the design fatigue life for W reload fuel in the current cycle.

The ability of fuel designed and operated to these criteria to withstand postulated normal and abnormal service conditions is shown by analyses described in Chapter 14 to satisfy the demands of plant operation well within applicable regulatory limits.

The reactor coolant pumps provided for the plant are supplied with sufficient rotational inertia to maintain an adequate flow coastdown and prevent core damage in the event of a simultaneous loss of power to all pumps.

In the unlikely event of a turbine trip from full power without an immediate reactor trip, the subsequent reactor coolant temperature increase and volume insurge to the pressurizer results in a high pressurizer pressure trip and thereby prevents fuel damage for this transient.

A loss of external electrical load of 50% of full power or less is normally controlled by RCCA insertion, together with a controlled steam dump to the condenser, to prevent a large temperature and pressure increase in the reactor coolant system. In this case, the overpower-overtemperature protection would guard against any combination of pressure, temperature, and power, which could result in a DNBR ratio less than the applicable design value during the transient.

In neither the turbine trip nor the loss-of-flow events do the changes in coolant conditions provoke a nuclear power excursion because of the large system thermal inertia and relatively small void fraction. Protection circuits actuated directly by the coolant conditions identified with core limits are therefore effective in preventing core damage.

The shutdown groups are provided to supplement the control groups of RCCAs to make the core at least 1.3 percent subcritical at the hot zero power condition (k_{eff} = 0.987) following trip from any credible operating condition, assuming the most reactive RCC assembly is in the fully withdrawn position.

Sufficient shutdown capability is also provided to ensure the DNBR remains above the limiting value, assuming the most reactive rod to be in the fully withdrawn position for the most severe anticipated cooldown transient associated with a single active failure, e.g., accidental opening of a steam bypass, or relief valve, or safety valve stuck open. This is achieved by the combination of RCCAs and boric acid addition. The design minimum shutdown margin is 1.3 percent, assuming the maximum worth RCCA is in the fully withdrawn position, and allowing 10% uncertainty in the RCCA worth calculations.

Manually controlled boric acid addition is used to maintain the shutdown margin for the long-term conditions of xenon decay and plant cooldown. Redundant equipment is provided to guarantee the capability of adding boric acid to the reactor coolant system.

The amount of boric acid in the boric acid tank is sufficient to maintain the reactor subcritical by the necessary shutdown margin at hot conditions following a reactor trip from all credible operating conditions. The control rods provide the necessary shutdown margin immediately following a reactor trip from full power conditions, assuming that the most reactive RCCA is fully withdrawn. The boric acid tank contains sufficient borated water to compensate for subsequent xenon decay. The flow rate of boric acid from the boric acid tank is sufficient to follow the highest burnout rate of xenon following reactor startup from peak xenon conditions. Boric acid is pumped from the boric acid tanks by one of two boric acid transfer pumps to the suction of one of three charging pumps which inject boric acid into the reactor coolant. Any charging pump and either boric acid transfer pump can be operated from diesel generator power on loss of station power.

Alternately, boric acid solution at lower concentration can be supplied from the refueling water storage tank. This solution can be transferred directly by the charging pumps or alternately by the safety injection pumps.

The reduced boric acid concentration lengthens the time required to achieve equivalent shutdown.

The reactor control system employs RCCAs. A portion of the RCCAs are designated shutdown rods and are fully withdrawn during power operation. The remaining rods comprise the control groups, which are used to control reactivity changes due to load changes and to control reactor coolant temperature. The rod cluster drive mechanisms are wired into preselected groups, and are therefore prevented from being withdrawn in other than their respective groups. The rod drive mechanism is of the magnetic latch type and the coil actuation is sequenced to provide variable speed rod travel. The maximum reactivity insertion rate is analyzed in the detailed plant analysis assuming two of the highest worth groups to be accidentally withdrawn at maximum speed, yielding reactivity insertion rates of the order of 7.5 x 10^{-4} Δk/k/sec, which is well within the capability of the overpower-overtemperature protection circuits to prevent core damage.

No single credible mechanical or electrical control system malfunction can cause a rod cluster to be withdrawn at a speed greater than 77 steps per minute (~48.125 inches per minute).

3.1.3 Safety Limits

The reactor is capable of meeting the performance objective throughout core life under both steady state and transient conditions without violating the integrity of the fuel elements. Thus the release of unacceptable amounts of fission products to the coolant is prevented.

The limiting conditions for operation established in the Technical Specifications specify the functional capacity of performance levels permitted to assure safe operation of the facility.

Design parameters, which are pertinent to safety limits, are specified below for the nuclear, control, thermal and hydraulic, and mechanical aspects of the design.

3.1.3.1 Nuclear Limits

The equations and curves which show the F_Q limits as a function of power and fuel height are defined in the Core Operating Limits Report and in Section 3.2.2 of the Cook Nuclear Plant Unit 1 Technical Specifications.

For any condition of power level, coolant temperature, and pressure which is permitted by the control and protection system during normal operation and anticipated transients, the hot channel power distribution is such that the minimum DNBR is greater than or equal to the applicable design value given in Section 3.5.3.

3.1.3.2 Reactivity Control Limits

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

a. A minimum hot shutdown margin as shown in the Technical Specifications is available assuming a 10% uncertainty in the RCCA worth calculation.

b. This shutdown margin is maintained with the most reactive RCCA in the fully withdrawn position.

c. The shutdown margin is maintained at ambient temperature by the use of soluble poison.

3.1.3.3 Thermal and Hydraulic Limits

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

a. The minimum allowable DNBR during normal operation, including anticipated transients, is not less than the applicable DNBR design limit. For the current cycle, design limit is given in Section 3.5.3.

b. No fuel melting during any anticipated operating condition.

To maintain fuel rod integrity and prevent fission product release, it is necessary to prevent clad overheating under all operating conditions. This is accomplished by preventing a departure from nucleate boiling (DNB) which causes a large decrease in the heat transfer coefficient between the fuel rods and the reactor coolant resulting in high clad temperatures.

The ratio of the heat flux causing DNB at a particular core location, as predicted by the W-3 and WRB-1 correlations, to the existing heat flux at the same core location is the DNB ratio. The applicable design limit DNBR for W and ENC fuel corresponds to a 95% probability at a 95% confidence level that DNB does not occur and is chosen to maintain an appropriate margin to DNB for all operating conditions.

3.1.3.4 Mechanical Limits

A discussion of specific reactor components is presented in the sections that follow. RCCA insertion credit for criticality control at the time ECCS-recirculation is aligned from cold leg injection to hot leg injection following a large cold leg break has been credited, as discussed in the unit-specific Chapter 14.3-1, "Large Break LOCA Analysis." Chapter 14.3-1 for each unit also references the analysis of the reactor upper internal guide tubes and fuel assembly grids under composite loads from LOCA blowdown and seismic conditions, which supports RCCA insertion credit.

3.1.3.4.1 Reactor Internals

The reactor internal components are designed to withstand the stresses resulting from startup, steady state operation with any number of pumps running, and shutdown conditions. No damage to the reactor internals occurs as a result of loss of pumping power.

Lateral deflection and torsional rotation of the lower end of the core barrel is limited to prevent excessive movements resulting from seismic disturbances and thus prevent interference with rod control cluster assemblies. Core drop in the event of failure of the normal supports is limited so that the RCCAs do not disengage from the fuel assembly guide thimbles.

The internals are further designed to maintain their functional integrity in the event of a major loss-of-coolant accident. The dynamic loading resulting from the pressure oscillations because of a loss-of-coolant accident does not cause sufficient deformation to prevent RCCA insertion.

3.1.3.4.2 Fuel Assemblies

The fuel assemblies are designed to perform satisfactorily throughout their lifetime. The loads, stresses, and strains resulting from the combined effects of flow induced vibrations, earthquakes, reactor pressure, fission gas pressure, fuel growth, thermal strain, and differential expansion during both steady state and transient reactor operating conditions have been considered in the design of the fuel rods and fuel assembly. The assembly is also structurally designed to withstand handling and shipping loads prior to irradiation, and to maintain sufficient integrity at the completion of design burnup to permit safe removal from the core, subsequent handling during cooldown, shipment and fuel reprocessing.

The fuel rods are supported at seven locations along their length within the fuel assemblies by grid assemblies, which are designed to maintain control of the lateral spacing between the rods throughout the design life of the assemblies. The magnitude of the support loads provided by the grids is established to minimize possible fretting without overstressing the cladding at the points of contact between the grids and fuel rods and without imposing restraints of sufficient magnitude to result in buckling or distortion of the rods.

An eighth grid, located just above the bottom nozzle, was added to the fuel design beginning with the Cycle 15 (Region 17) core. This grid is designed to work in conjunction with the bottom nozzle and bottom end plug of the fuel rod to mitigate the probability of debris-induced fuel rod fretting failures. Also eight lead test assemblies (LTAs) were introduced in the Cycle 15 (Region 17) core design that contain three intermediate flow mixing (IFM) grids in addition to the seven structural grids and one debris grid. To accommodate pressure drops due to addition of IFM grids, the seven structural grids in the LTAs have a low pressure drop design. The grid design of the LTAs was further implemented in the design of the cycle 17 (Region 19) and may be implemented for future reloads.

The fuel rod cladding is designed to withstand operating pressure loads without rupture and to maintain encapsulation of the fuel throughout the design life.

3.1.3.4.3 Rod Cluster Control Assemblies (RCCAs)

The criteria used for the design of the cladding on the individual absorber rods in the RCCAs are similar to those used for the fuel rod cladding. The cladding is designed to be free standing under all operating conditions and will maintain encapsulation of the absorber material throughout the absorber rod design life. Allowance for wear during operation is included for the RCCA cladding thickness.

Adequate clearance is provided between the absorber rods and the guide thimbles which position the rods within the fuel assemblies so that coolant flow along the length of the absorber rods is sufficient to remove the heat generated without overheating of the absorber cladding.

The clearance is also sufficient to compensate for any misalignment between the absorber rods and guide thimbles and to prevent mechanical interference between the rods and guide thimbles under any operating conditions.

3.1.3.4.4 Control Rod Drive Assembly

Each RCCA drive assembly is designed as a hermetically sealed unit to prevent leakage of reactor coolant. All pressure-containing components are designed to meet the requirements of the ASME Boiler and Pressure Vessel Code, Section III, Nuclear Power Plant Components for Class I Vessels.

The rod drive assemblies for the full-length rods provide RCCA 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 are 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. Also, the RCCA drive assemblies for the full length rods provide a fast insertion rate during a "trip" of the RCCAs which results in a rapid shutdown of the reactor for conditions that cannot be handled by the reactor control system.

3.2 MECHANICAL DESIGN

3.2.1 Mechanical Design and Evaluation

The reactor core and reactor vessel internals are shown in cross-section in Figure 3.2.1-1 and in elevation in Figure 3.2.1-2. The core, consisting of the fuel assemblies, control rods, source rods, integral fuel and/or discrete burnable absorbers, and potentially guide thimble plugging devices, provides and partially controls the heat source for the reactor operation. Beginning with Cycle 18, thimble plugs are no longer required to reside in the core, but they have been used in previous cycles. The internals, consisting of the upper and lower core support structure, are designed to support, align, and guide the core components, direct the coolant flow to and from the core components, and to support and guide the in-core instrumentation. A listing of the core mechanical design parameters for the initial core is given in Table 3.2.1-1. Design parameters for reload fuel are presented in Table 3.5.1-1.

The fuel assemblies are arranged in a roughly circular cross-sectional pattern. The assemblies are all similar in configuration, but contain fuel of different enrichments depending on the location of the assembly within the core.

The fuel is in the form of low enriched uranium dioxide ceramic pellets. The pellets are stacked to an active height of 144 inches within Zircaloy-4 tubular cladding which is plugged and seal welded at the ends to encapsulate the fuel. The fuel rods are internally pressurized with helium during fabrication. The enrichments of the fuel for the various regions in the first core are given in Table 3.2.1-1. Heat generated by the fuel is removed by demineralized light water which flows upward through the fuel assemblies and acts as both moderator and coolant.

The initial core is divided into regions of three different enrichments. The loading arrangement for the initial cycle is indicated in Figure 3.2.1-3. The loading arrangement for an example cycle may be found in Figure 3.5.2-1 and Table 3.5.2-1.

The control rods, designated as Rod Cluster Control Assemblies (RCCA), consist of groups of individual absorber rods, which are held together by a spider at the top end and actuated as a group. In the inserted position, the absorber rods fit within hollow guide thimbles in the fuel assemblies. The guide thimbles are an integral part of the fuel assemblies and occupy locations within the regular fuel rod pattern where fuel rods have been deleted. In the withdrawn position, the absorber rods are guided and supported laterally by guide tubes, which form an integral part of the upper core support structure. Figure 3.2.1-4 shows a typical rod cluster control assembly.

As shown in Figure 3.2.1-2, the fuel assemblies are positioned and supported vertically in the core between the upper and lower core plates. The core plates are provided with pins, which index into closely fitting mating holes in the fuel assembly top and bottom nozzles. The pins maintain the fuel assembly alignment, which permits free movement of the control rods from the fuel assembly into the guide tubes in the upper support structure without binding or restriction between the rods and their guide surface.

Operational or seismic loads imposed on the fuel assemblies are transmitted through the core plates to the upper and lower support structures and ultimately to the internals support ledge at the pressure vessel flange in the case of vertical loads or to the lower radial support and internals support ledge in the case of horizontal loads. The internals also provide a form fitting baffle surrounding the fuel assemblies which confines the upward flow of coolant in the core area to the fuel bearing region.

Reactor Internals

Design Description

The reactor internals are designed to support and orient the reactor core fuel assemblies and control rod assemblies, absorb the control rod dynamic loads and transmit these and other loads to the reactor vessel flange, provide a passageway for the reactor coolant, and support in-core instrumentation. The reactor internals are shown in Figure 3.2.1-2.

The internals are designed to withstand the forces due to weight, preload of fuel assemblies, control rod dynamic loading, vibration, and earthquake acceleration. These internals are analyzed in a manner similar to Connecticut Yankee, San Onofre, Zorita, Saxton and Yankee. Under the loading conditions, including conservative effects of design earthquake loading, the structure satisfies stress values prescribed in Section III, ASME Boiler and Pressure Vessel Code.

The reactor internals are equipped with bottom-mounted in-core instrumentation supports. These supports are designed to sustain the applicable loads outlined above.

The components of the reactor internals are divided into three parts consisting of the lower core support structure (including the entire core barrel and thermal shield), the upper core support structure and the in-core instrumentation support structure.

Lower Core Support Structure

The major containment and support member of the reactor internals is the lower core support structure, shown in Figure 3.2.1-5. This support structure assembly consists of the core barrel, the core baffle, and lower core plate and support columns, the thermal shield, the intermediate diffuser plate and the bottom support plate which is welded to the core barrel. All the major material for this structure is Type 304 Stainless Steel. The core support structure is supported at its upper flange from a ledge in the reactor vessel head flange and its lower end is restrained in its transverse movement by a radial support system attached to the vessel wall. Within the core barrel are axial baffle and former plates, which are attached to the core barrel wall and form the enclosure periphery of the assembled core. The lower core plate is positioned at the bottom level of the core below the baffle plates and provides support and orientation for the fuel assemblies.

The lower core plate is a 2" thick member through which the necessary flow distributor holes for each fuel assembly are machined. Fuel assembly locating pins (two for each assembly) are also inserted into this plate. Columns are placed between this plate and the bottom support plate of the core barrel in order to provide stiffness and to transmit the core load to the bottom support plate. Intermediate between the support plate and lower core support plate is positioned a perforated plate to diffuse uniformly the coolant flowing into the core.

The one piece thermal shield is fixed to the core barrel at the top with rigid bolted connections. The bottom of the thermal shield is connected to the core barrel by means of axial flexures. This bottom support allows for differential axial growth of the shield/core barrel but restricts radial or horizontal movement of the bottom of the shield. Rectangular tubing in which material samples can be inserted and irradiated during reactor operation are welded to the thermal shield and extend to the top of the thermal shield. These samples are held in the rectangular tubing by a preloaded spring device at the top and bottom.

The lower core support structure and principally the core barrel serve to provide passageways and control for the coolant flow. Inlet coolant flow from the vessel inlet nozzles proceeds down the annulus between the core barrel and the vessel wall, flows on both sides of the thermal shield, and then into a plenum at the bottom of the vessel. It then turns and flows up through the lower support plate, passes through the intermediate diffuser plate and then through the lower core plate. The flow holes in the diffuser plate and the lower core plate are arranged to give a very uniform entrance flow distribution to the core. After passing through the core the coolant enters the area of the upper support structure and then flows generally in the radial direction to the core barrel outlet nozzles and directly through the vessel outlet nozzles.

A small amount of water also flows between the baffle plates and core barrel to provide additional cooling of the barrel. Similarly, a small amount of the entering flow is directed into the vessel head plenum to provide cooling of the head. Both these flows eventually are directed into the upper support structure plenum and exit through the vessel outlet nozzles.

Vertically downward loads from weight, fuel assembly preload, control rod dynamic loading and earthquake acceleration are carried by the lower core plate partially into the lower core plate support flange on the core barrel shell and partially through the lower support columns to the lower core support and thence through the core barrel shell to the core barrel flange supported by the vessel head flange. Transverse loads from earthquake acceleration, coolant cross flow, and vibration are carried by the core barrel shell to be distributed to the lower radial support to the vessel wall, and to the core barrel flange. Transverse acceleration of the fuel assemblies is transmitted to the core barrel shell by direct connection of the lower core plate to the barrel wall and by a radial support type connection of the upper core plate to flat sided pins pressed into the core barrel.

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

Radial and axial expansions of the core barrel are accommodated but transverse movement of the core barrel is restricted by this design. With this system, cycle stresses in the internal structures are within the ASME Section III limits. This eliminates any possibility of failure of the core support.

In the event of downward vertical displacement of the internals, energy-absorbing devices limit the displacement by contacting the vessel bottom head. The load is transferred through the energy devices of the internals.

The energy absorbers, cylindrical in shape, are contoured on their bottom surface to the reactor vessel bottom head geometry. Their number and design are determined so as to limit the forces imposed to less than yield. Assuming a downward vertical displacement, the potential energy of the system is absorbed mostly by the strain energy of the energy absorbing devices.

The free fall in the hot condition is on the order of 1/2 inch, and there is an additional strain displacement in the energy absorbing devices of approximately 3/4 inch. Alignment features in the internals prevent cocking of the internals structure during this postulated drop.

The control rods are designed to provide assurance of control rod insertion capabilities under this assumed drop of internals condition. The drop distance of about 1¼ inch is not enough to cause the tips of the RCC assemblies to come out of the guide tubes in the fuel assemblies.

Upper Core Support Assembly

The upper core support assembly, shown in Figure 3.2.1-6, consists of the top support plate, deep beam sections, and upper core plate between which are contained 48 support columns and 61 guide tube assemblies. The support columns establish the spacing between the top support plate, deep beam sections, and the upper core plate and are fastened at top and bottom to these plates and beams. The support columns transmit the mechanical loading between the two plates and serve the supplementary functions of supporting thermocouple guide tubes. The guide tube assemblies, shown on Figure 3.2.1-7, sheath and guide the control rod drive shafts and control rods and provide no other mechanical functions. They are fastened to the top support plate and are guided by pins in the upper core plate for proper orientation and support. Additional guidance for the control rod drive shafts is provided by the control rod shroud tube which is attached to the upper support plate and guide tube. Flow restrictors are installed in the guide tubes at core locations D6, D10, F4, F12, K4, M6 and M10. The part length CRDM drive shafts were eliminated at these locations.

The upper core support assembly, which is removed as a unit during refueling operation, is positioned in its proper orientation with respect to the lower support structure by flat-sided pins pressed into the core barrel which in turn engage in slots in the upper core plate. At an elevation in the core barrel where the upper core plate is positioned, the flatsided pins are located at angular positions of 0º, 90º, 180º and 270º. Four slots are milled into the core plate at the same positions. As the upper support structure is lowered into the main internals, the slots in the plate engage the flat-sided pins in the axial direction. Lateral displacement of the plate and of the upper support assembly is restricted by this design. Fuel assembly locating pins protrude from the bottom of the upper core plate and engage the fuel assemblies as the upper assembly is lowered into place. Proper alignment of the lower core support structure, the upper core support assembly, the fuel assemblies and control rods is thereby assured by this system of locating pins and guidance arrangement. The upper core support assembly is restrained from any axial movements by a large circumferential spring which rests between the upper barrel flange and the upper core support assembly and is compressed by the reactor vessel head flange.

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

In-Core Instrumentation Support Structures

The in-core instrumentation support structures consist of an upper system to convey and support thermocouples penetrating the vessel through the head and a lower system to convey and support flux thimbles penetrating the vessel through the bottom.

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

In addition to the upper in-core instrumentation, there are reactor vessel bottom penetration tubes which admit the retractable, partially chrome plated cold worked stainless steel flux thimble tubes that are pushed upward into the reactor core. Thimble guide tube conduits extend from the bottom of the reactor vessel down through the concrete shield area and up to a thimble seal table. The minimum bend radii are about 144 inches and the trailing ends of the thimble tubes (at the seal table) are extracted approximately 15 feet during refueling of the reactor in order to avoid interference within the core. The thimble tubes are closed at the leading ends and serve as the pressure barrier between the reactor pressurized water and the containment atmosphere.

Mechanical seals between the retractable thimbles and the conduits are provided at the seal line. During normal operation, the retractable thimbles are stationary and move only during refueling or for maintenance, at which time a space of approximately 15 feet above the seal line is cleared for the retraction operation. Chapter 7 contains more information on the layout of the in-core instrumentation system.

The in-core instrumentation support structure is designed for adequate support of instrumentation during reactor operation and is rugged enough to resist damage or distortion under the conditions imposed by handling during the refueling sequence.

Evaluation of Core Barrel and Thermal Shield

The internals design is based on analysis, test and operational information. Troubles in previous Westinghouse PWR's have been evaluated and information derived has been considered in this design. For example, in this design Westinghouse uses a one-piece thermal shield, which is attached rigidly to the core barrel at one end and flexured at the other. The early designs that malfunctioned were multi-piece thermal shields that rested on vessel lugs and were not rigidly attached at the top.

Early core barrel designs that have malfunctioned in service, now abandoned, employed threaded connections such as tie rods, joining the bottom support to the bottom of the core barrel, and a bolted connection that tied the core barrel to the upper barrel. The malfunctioning of core barrel designs in earlier service was believed to have been caused by the thermal shield, which was oscillating, thus creating forces on the core barrel. Other forces were induced by unbalanced flow in the lower plenum of the reactor. In today's RCC design there are no fuel followers to necessitate a large bottom plenum in the reactor. The elimination of these fuel followers has enabled Westinghouse to build a shorter core barrel.

The Connecticut Yankee, Indian Point #2 and the Zorita reactor core barrels are of the same construction as the D. C. Cook reactor core barrel. Deflection measuring devices employed in the Connecticut Yankee reactor during the hot-functional tests, and deflection and strain gauges employed in the Zorita reactor during the hot-functional test have provided important information that has been used in the design of the present day internals, including that for Zion. When the Connecticut Yankee thermal shield was modified to the same design as for Southern California Edison, it, too, operated satisfactorily as was evidenced by the examination after the hot-functional tests. After these hot-functional tests on all of these reactors, a careful inspection of the internals was provided. All the main structural welds were examined, nozzle interfaces were examined for any differential movement, upper core plate inside supports were examined, the thermal shield attachments to the core barrel including all lockwelds on the devices used to lock the bolt were checked; no malfunctions were found.

Substantial scale model testing was performed at Westinghouse. This included tests which involved a complete full scale fuel assembly which was operated at reactor flow, temperature and pressure conditions. Tests were run on a 1/7th scale model of the Indian Point Unit 2 reactor. Measurements taken from these tests indicate very little shield movement, on the order of a few mils when scaled up to Indian Point Unit 2. Strain gauge measurements taken on the core barrel also indicate very low stresses. Testing to determine thermal shield excitation due to inlet flow disturbances has been included. Information gathered from these tests was used in the design of the thermal shield and core barrel.

In order to provide further confirmation of the internals design, Indian Point Unit 2 had deflection gauges mounted on the thermal shield top and bottom for the hot-functional tests. Six such gauges were mounted in the top of the thermal shield equidistant between the fixed supports and eight located at the bottom, equidistant between the six flexures, and two next to flexure supports. The internals inspection, just before the hot-functional tests, included looking at mating bearing surfaces, main welds and welds used on bolt locking devices. At the conclusion of the hot-functional tests, measurement readings were taken from the deflectometers on the shield and the internals were re-examined at all key areas for any evidence of malfunction. It can be concluded from the testing programs, analyses and the experience gained from Indian Point Unit 2, that the design as employed on this plant is adequate.

To facilitate the replacement of broken/loose barrel-former bolts (bolts A4, A5 and A6 at the top former plate directly below the thermal shield support block located at the 22.5° azimuth position), access holes were cut through the thermal shield at the bolt hole locations. These holes remain in the thermal shield. An analysis was performed by Westinghouse and it showed that the effect of the thermal shield holes would not result in any adverse effects in the thermal shield or core barrel integrity (see Attachment #27 of Reference #6 for details).

Fuel Assembly and Core Components

Fuel assembly and core components for the initial core are described in the following subsections. The current Westinghouse Company reload fuel is described in Section 3.5.

Design Description

Westinghouse Fuel Assembly

All of the Westinghouse fuel assemblies which have been in the core were of similar design. The overall configuration of the fuel assemblies is shown in Figures 3.2.1-8 and 3.2.1-9. The assemblies are square in cross-section, nominally 8.426 inches on a side, and have an overall height of 160.1 inches.

The fuel rods in a fuel assembly are arranged in a square array with 15-rod locations per side and a nominal centerline-to-centerline pitch of 0.563 inch between rods. Of the total possible 225-rod locations per assembly, 20 are occupied by guide thimbles for the RCCA rods and one for in-core instrumentation. The remaining 204 locations contain fuel rods. In addition to fuel rods, a fuel assembly is composed of a top nozzle, a bottom nozzle, 7 grid assemblies, 20 absorber rod guide thimbles, and one instrumentation thimble.

An eighth grid, located just above the bottom nozzle, was added to the fuel design beginning with the Cycle 15 (Region 17) core. This grid is designed to work in conjunction with the bottom nozzle and bottom end plug of the fuel rod to mitigate the probability of debris-induced fuel rod fretting failures. Also, eight lead test assemblies (LTAs) were introduced in the Cycle 15 (Region 17) core design that contain three intermediate flow mixing (IFM) grids in addition to the seven structural grids and one debris grid. To accommodate pressure drops due to addition of IFM grids, the seven structural grids in the LTAs have a low pressure drop design. The grid design of the LTAs was further implemented in the design of the Cycle 17 (Region 19) and may be implemented for future reloads.

The guide thimbles in conjunction with the grid assemblies and the top and bottom nozzles comprise the basic structural fuel assembly skeleton. The top and bottom ends of the guide thimbles are secured to the top and bottom nozzles respectively. The grid assemblies, in turn, are fastened to the guide thimbles at each location along the height of the fuel assembly at which lateral support for the fuel rods is required. Within this skeletal framework the fuel rods are contained and supported and the rod-to-rod centerline spacing is maintained along the assembly.

Bottom Nozzle

The bottom nozzle is a square box-like structure, which controls the coolant flow distribution to the fuel assembly and functions as the bottom structural element of the fuel assembly. The nozzle, which is square in cross-section, is fabricated from Type 304 stainless steel parts consisting of a perforated plate, four angle legs, and four pads or feet. The angle legs, are fastened to the plate forming a plenum space for coolant inlet to the fuel assembly. The perforated plate serves as the bottom end support for the fuel rods. The bottom support surface for the fuel assembly is formed under the plenum space by the four pads, which are welded to the corner angles.

Coolant flow to the fuel assembly is directed from the plenum in the bottom nozzle upward to the interior of the fuel assembly and to the channel between assemblies. The ligaments in the perforated plate are positioned below the fuel rods and are sized so that the fuel rods cannot pass through the holes in the plate.

The RCC guide thimbles, which carry axial loads imposed on the assembly, are fastened to the bottom nozzle perforated plate. These loads, as well as the weight of the assembly, are distributed through the nozzle to the lower core support plate. Indexing and positioning of the fuel assembly in the core is controlled through two holes in diagonally opposite pads which mate with locating pins in the lower core plate. Lateral loads imposed on the fuel assembly are also transferred to the support structures through the locating pins.

Top Nozzle

The top nozzle is a box-like structure, which functions as the fuel assembly upper structural element and forms a plenum space where the heated fuel assembly discharge coolant is mixed and directed toward the flow holes in the upper core plate. The nozzle is comprised of an adapter plate enclosure, top plate, tow clamps, four leaf springs, and assorted hardware. All parts with the exception of the springs and their hold down bolts are constructed of Type 304 stainless steel. The springs are made from age hardened Inconel 718 and the bolts from Inconel 600.

The adapter plate is square in cross-section, and is perforated by machined slots to provide for coolant flow through the plate. At assembly, the control guide thimbles are fastened through individual bored holes in the plate. Thus, the adapter plate acts as the fuel assembly top end plate, and provides a means of distributing evenly among the guide thimbles any axial loads imposed on the fuel assemblies.

The nozzle enclosure is actually a square thin walled tubular shell, which forms the plenum section of the top nozzle. The bottom end of the enclosure is pinned and welded to the periphery of the adapter plate, and the top end is welded to the periphery of the top plate.

The top plate is square in cross-section with a square central hole. The hole allows clearance for the RCC absorber rods to pass through the nozzle into the guide thimbles in the fuel assembly and for coolant exit from the fuel assembly to the upper internals area. Two pads containing axial through-holes which are located on diametrically opposite corners of the top plate provide a means of positioning and aligning the top of the fuel assembly. As with the bottom nozzle, alignment pins in the upper core plate mate with the holes in the top nozzle plate.

Hold down forces of sufficient magnitude to oppose the hydraulic lifting forces on the fuel assembly are obtained by means of the leaf springs, which are mounted on the top plate. The springs are fastened in pairs to the top plate at the two corners where alignment holes are not used and radiate out from the corners parallel to the sides of the plate. Fastening of each pair of springs is accomplished with a clamp which fits over the ends of the springs and two bolts (one per spring) which pass through the clamp and spring, and thread into the top plate.

At assembly, the spring mounting bolts are torqued sufficiently to preload against the maximum spring load and then lockwelded to the clamp, which is counter-bored to receive the bolt head.

The spring load is obtained through deflection of the spring by the upper core plate. The spring form is such that it projects above the fuel assembly and is depressed by the core plate when the internals are loaded into the reactor. The free end of the spring is bent downward and captured in a key slot in the top plate to guard against loose parts in the reactor in the event (however remote) of spring fracture. In addition, the fit between the spring and key slot and between the spring and its mating slot in the clamp are sized to prevent rotation of either end of the spring into the control rod path in the event of spring fracture.

In addition to its plenum and structural functions, the nozzle provides a protective housing for components, which mate with the fuel assembly. In handling a fuel assembly with a control rod inserted, the control rod spider is contained within the nozzle. During operation in the reactor, the nozzle protects the absorber rods from coolant cross flows in the unsupported span between the fuel assembly adapter plate and the end of the guide tube in the upper internals package. Plugging devices which fill the ends of the fuel assembly thimble tubes at unrodded core locations, and the spiders which support the source rods and burnable poison rods are all contained within the fuel top nozzle.

Guide Thimbles

The control rod guide thimbles in the fuel assembly provide guided channels for the absorber rods during insertion and withdrawal of the control rods. They are fabricated from a single piece of Zircaloy-4 tubing, which is drawn to two different diameters. The larger inside diameter at the top (.515 inch) provides a relatively large annular area for rapid insertion during a reactor trip and to accommodate a small amount of upward cooling flow during normal operations. The bottom portion of the guide thimble is of reduced diameter (.454 inch) to produce a dashpot action when the absorber rods near the end of travel in the guide thimbles during a reactor trip. The transition zone at the dashpot section is conical in shape so that there are no rapid changes in diameter in the tube.

Flow holes are provided just above the transition of the two diameters to permit the entrance of cooling water during normal operation, and to accommodate the outflow of water from the dashpot during reactor trip.

The dashpot is closed at the bottom by means of a welded end plug. The end plug is fastened to the bottom nozzle during fuel assembly fabrication.

The top ends of the guide thimbles are fitted through individual bored holes in the plate and welded to the plate around the circumference of each hole.

Grids

The spring clip grid assemblies consist of individual slotted straps which are assembled and interlocked in an "egg-crate" type arrangement and then furnace brazed to permanently join the straps at their points of intersection. Details such as spring fingers, support dimples, mixing vanes, and tabs are punched and formed in the individual straps prior to assembly.

Two types of grid assemblies are used in the fuel assembly. One type having mixing vanes which project from the edges of the straps into the coolant stream is used in the high heat region of the fuel assemblies for mixing of the coolant. A grid of this type is shown in Figure 3.2.1-10. Grids of the second type, located at the bottom and top ends of the assembly, are of the non-mixing type. They are similar to the mixing type with the exception that mixing vanes are not used on the internal straps.

The spacing between grids is shown on Figure 3.2.1-9. The variation in span lengths is the result of optimization of the thermal-hydraulic and structural parameters. The grids are fastened securely to each guide thimble.

The outside straps on all grids contain mixing vanes which, in addition to their mixing function, aid in guiding the grids and fuel assemblies past projecting surfaces during handling or loading and unloading the core. Additional small tabs on the outside straps and the irregular contour of the straps are also for this purpose.

For the fuel assemblies supplied by Westinghouse, Inconel 718 has been chosen for the grid material because of its corrosion resistance and high strength properties. After the combined brazing and solution annealing temperature cycle, the grid material is age hardened to obtain the material strength necessary to develop the required grid spring forces.

Fuel Rods

The fuel rods consist of uranium dioxide ceramic pellets contained in a slightly cold worked and partially annealed Zircaloy-4/ZIRLO® / Optimized ZIRLO™ tubing which is plugged and seal welded at the ends to encapsulate the fuel. Sufficient void volume and clearances are provided within the rod to accommodate fission gases released from the fuel, differential thermal expansion between the cladding and the fuel, and fuel swelling due to accumulated fission products without overstressing of the cladding or seal welds. Shifting of the fuel within the cladding is prevented during handling or shipping prior to core loading by a carbon steel helical compression spring, which bears on the top of the fuel.

At assembly, the pellets are stacked in the cladding to the required fuel height. The compression spring is then inserted into the top end of the fuel and the end plugs pressed into the ends of the tube and welded. All fuel rods are internally pressurized with helium during the welding process.

A hold-down force of approximately six times the weight of the fuel is obtained by compression of the spring between the top end plug and the top of the fuel pellet stack.

Beginning with Cycle 15 (Region 17), the plenum spring was redesigned. The spring has a variable pitch, which allows for reduced plenum volume taken up by the spring while maintaining required pellet stack hold-down force. Additional plenum volume was needed to offset the reduction caused by the longer bottom end plug that is integral to the new debris resistant features introduced in Region 17.

The fuel pellets are right circular cylinders consisting of slightly enriched uranium-dioxide powder, which has been compacted by cold pressing and then sintered to the required density. The ends of each pellet are dished slightly to allow the greater axial expansion at the center of the pellets to be taken up within the pellets themselves and not in the overall fuel length. The plenum spring was slightly lengthened starting with the Cycle 17 (Region 19) reload to maintain appropriate hold-down force on the fuel pellet stack after a slight increase in fuel rod length.

A limited number of fuel rods may be replaced with substitutions of zirconium alloy, zircaloy-4, ZIRLO™, or stainless steel filler rods, in accordance with the NRC-approved methodology in Reference 7.

For the first core, the pellets in the outer region have a density of approximately 10.3 gm/cc (94% of theoretical density) while those in the two inner regions (checkerboard pattern, see Figure 3.2.1-3) have densities of 10.4 gm/cc corresponding to 95% of theoretical density. Lower pellet densities are used to compensate for the effects of the higher burnup which the fuel in the outer region will experience.

A different fuel enrichment as listed in Table 3.2.1-1 was used for each of the three regions in the first core loading.

Each fuel assembly was identified by means of a serial number engraved on the upper nozzle. The fuel pellets were fabricated by a batch process so that only one enrichment region was processed at any given time. The serial numbers of the assemblies and corresponding enrichment were documented by the manufacturer and verified prior to shipment.

Each assembly was assigned a specific core loading position prior to insertion. A record was then made of the core loading position, serial number and enrichment. Prior to core loading, two independent checks were made to ensure that this assignment was correct.

During initial core loading and subsequent refueling operations, detailed written handling and check-off procedures are utilized throughout the sequence. The initial core was loaded in accordance with the core loading diagram which shows the location for each of the three enrichment types of fuel assemblies used in the loading (similar to Figure 3.2.1-3) together with the serial number of the assemblies in the region.

Rod Cluster Control Assemblies

The control rods or rod cluster control assemblies (RCCA) each consist of a group of individual absorber rods fastened at the top end to a common hub or spider assembly. These assemblies, one of which is shown in Figure 3.2.1-4 are provided to control the reactivity of the core under operating conditions. These assemblies contain full-length absorber material. Design parameters for the RCCA's are specified in Table 3.2.1-1.

The absorber material used in the control rods is a silver-indium-cadmium alloy, which is essentially "black" to thermal neutrons and has sufficient additional resonance absorption to significantly increase its worth. The alloy is in the form of extruded single length rods, which are sealed in stainless steel tubes to prevent the rods from coming in direct contact with the coolant.

The overall control rod length is such that when the assembly has been withdrawn through its full travel, the tip of the absorber rods remain engaged in the guide thimbles so that alignment between rods and thimbles is always maintained. Since the rods are long and slender, they are relatively free to conform to any small misalignments with the guide thimble. Prototype tests have shown that the RCC assemblies are very easily inserted and not subject to binding even under conditions of severe misalignment.

The spider assembly is in the form of a center hub with radial vanes supporting cylindrical fingers from which the absorber rods are suspended. Handling detents, and detents for connection to the drive shaft, are machined into the upper end of the hub. A spring pack is assembled into a skirt integral to the bottom of the hub to stop the RCC assembly and absorb the impact energy at the end of a trip insertion. The radial vanes are joined to the hub, and the fingers are joined to the vanes by furnace brazing. A centerpost which holds the spring pack and its retainer is threaded into the hub within the skirt and welded to prevent loosening in service. All components of the spider assembly are made from Type 304 stainless steel except for the springs which are Inconel X-750 alloy and the retainer which is of 17-4 pH material.

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

In construction, the silver-indium-cadmium rods are inserted into coldworked stainless steel tubing which is then sealed at the bottom and the top by welded end plugs. Sufficient diametral and end clearance are provided to accommodate relative thermal expansions and to limit the internal pressure to acceptable levels.

The bottom plugs are made bullet-nosed to reduce the hydraulic drag during a reactor trip and to guide smoothly into the dashpot section of the fuel assembly guide thimbles. The upper plug is threaded for assembly to the spider and has a reduced end section to make the joint more flexible.

Stainless steel clad silver-indium-cadmium alloy absorber rods are resistant to radiation and thermal damage thereby ensuring their effectiveness under all operating conditions. Rods of similar design have been successfully used in the Saxton, SELNI and Indian Point I reactors.

Part Length Rod Cluster Control Assembly

Part length control rods are currently not installed in Unit 1 of the Donald C. Cook Plant for the following reasons:

a. No credit is taken for their presence in the safety analysis performed by the vendor. Therefore the decision not to mount them does not constitute a safety issue.

b. The reactor's Operating License and Technical Specifications preempt their use.

c. Unit 1 of the Donald C. Cook Nuclear Plant has successfully load followed and controlled artificially created large xenon oscillations without the use of these rods in previous cycles when they were installed.

The part length CRDMs and associated anti-rotation devices have been eliminated.

Neutron Source Assemblies

Four neutron source assemblies were utilized in the initial core. These consisted of two assemblies with four secondary source rods and two assemblies with one primary source rod each. The source rods in each secondary assembly were fastened to a spider at the top end. The primary source rods were attached to a burnable poison assembly.

In the core, the neutron source assemblies were inserted into the RCC guide thimbles in fuel assemblies at unrodded locations. The location and orientation of the assemblies in the core is shown in Figure 3.2.1-11.

The primary and secondary source rods both utilize the same type of cladding material as the absorber rods (cold-worked Type 304 stainless steel tubing, with 0.019 in. thick walls) into which the sources are inserted. The secondary source rods contain Sb-Be pellets stacked to a height of 121.74 inches. Each primary source rod contains a Cf capsule 2 inches long at a neutron strength of approximately 4 x 10^8 neutrons/sec. Design criteria for the source rods are: the cladding is free standing, internal pressures are always less than reactor operating pressure, and internal gaps and clearances are provided to allow for differential expansions between the source material and the cladding.

Plugging Devices

Plugging devices may be used to limit bypass flow through the RCC guide thimbles in fuel assemblies, which do not contain either control rods, source assemblies, or burnable poison rods. The plugging devices consist of a flat spider plate with short rods suspended from the bottom surface and a spring pack assembly and mixing device attached to the top surface.

At installation in the core, the plugging devices fit with the fuel assembly top nozzles and rest on the adapter plate. The short rods project into the upper ends of the thimble tubes to reduce the bypass flow area. The spring pack is compressed by the upper core plate when the upper internals package is lowered into place. Similar short rods are also used on the source assemblies to fill the ends of all vacant fuel assembly guide thimbles.

All components in the plugging device, except for the springs, are constructed from Type 304 stainless steel. The springs (one per plugging device) are wound from an age hardened nickel base alloy to obtain higher strength.

Burnable Poison Rods

The burnable poison rods are statically suspended and positioned in vacant RCC thimble tubes within the fuel assemblies at non-rodded core locations. The poison rods in each fuel assembly are grouped and attached together at the top end of the rods by a flat spider plate, which fits with the fuel assembly top nozzle and rests on the top adapter plate.

The spider plate (and the poison rods) are held down and restrained against vertical motion through a spring pack, which is attached to the plate and is compressed by the upper core plate when the reactor upper internals package is lowered into the reactor. This ensures that the poison rods cannot be lifted out of the core by flow forces.

The poison rods consist of borosilicate glass tubes contained within Type 304 stainless steel tubular cladding which is plugged and seal welded at the ends to encapsulate the glass. The glass is also supported along the length of its inside diameter by a thin wall Type 304 stainless steel tubular inner liner. A typical burnable poison rod is shown in longitudinal and transverse cross-sections in Figure 3.2.1-12.

The rods are designed in accordance with the standard Westinghouse fuel rod design criteria; i.e., the cladding is free standing at reactor operating pressures and temperatures and sufficient cold void volume is provided within the rods to limit internal pressures to less than the reactor operating pressure assuming total release of all helium generated in the glass as a result of the B_{10} (n, a) reaction. The large void volume required for the helium is obtained through the use of glass in tubular form, which provides a central void along the length of the rods. A more detailed discussion of the burnable poison rod design is found in WCAP-9000.(Reference 3)

Based on available data on the properties of borosilicate glass and on nuclear and thermal calculations for the rods, gross swelling or cracking of the glass tubing is not expected during operation. Some minor creep of the glass at the hot spot on the inner surface of the tube is expected with the inner liner. The inner liner is provided to maintain the central void along the length of the glass and to prevent the glass from slumping or creeping into the void as a result of softening at the hot spot. The wall thickness of the inner liner is sized to provide adequate support in the event of slumping but to collapse locally before rupture of the exterior cladding if large volume changes due to swelling or cracking should possibly occur.

The top end of the inner liner is open to receive the helium, which diffuses out of the glass.

Evaluation of Core Components

Fuel Evaluation

The fission gas release and the associated buildup of internal gas pressure in the fuel rods is calculated by the FIGHT code based on experimentally determined rates. The increase of internal pressure in the fuel rod due to this phenomenon is included in the determination of the maximum cladding stresses at the end of core life when the fission product gap inventory is a maximum.

The maximum allowable strain in the cladding, considering the combined effects of internal fission gas pressure, external coolant pressure, fuel pellet swelling and clad creep is limited to less than 1 per cent throughout core life. The associated stresses are below the yield strength of the material under all normal operating conditions.

To assure that manufactured fuel rods meet a high standard of excellence from the standpoint of functional requirements, many inspections and tests are performed both on the raw materials and the finished product. These tests and inspections include analysis, elevated temperature, tensile testing of fuel tubes, dimensional inspection, X-ray of both end plug welds, ultrasonic testing and helium leak tests.

In the event of cladding defects, the high resistance of uranium dioxide fuel pellets to attack by hot water protects against fuel deterioration or decrease in fuel integrity. Thermal stress in the pellets, while causing some fracture of the bulk material during temperature cycling, does not result in pulverization or gross void formation in the fuel matrix. As shown by operating experience and extensive experimental work in the industry, the thermal design parameters conservatively account for any changes in the thermal performance of the fuel element due to pellet fracture.

The consequences of a breach of cladding are greatly reduced by the ability of uranium dioxide to retain fission products including those, which are gaseous or highly volatile. This retentiveness decreases with increasing temperature or fuel burnup, but remains a significant factor even at full power operating temperature in the maximum burnup element.

A survey of high burnup uranium dioxide (Reference 1) fuel element behavior indicates that for an initial uranium dioxide void volume, which is a function of the fuel density, it is possible to conservatively define the fuel swelling as a function of burnup. The fuel swelling model considers the effects of burnup, temperature distribution, and internal voids. Investigations carried out in the Westinghouse fuel irradiation programs supplemented by data obtained from the Yankee Fuel Evaluation Program, the CVTR Post-Irradiation Examinations and the Saxton Plutonium Program have resulted in a design in which all three regions will have pellet densities of 94% or 95%, a pellet diameter of 0.3659 inches and a diametral gap of 0.0075 inches. This results in a more reliable design due to a higher beginning of life helium pre-pressurization level which delays fuel-clad contact and moisture retention is reduced.

The integrity of fuel rod cladding so as to retain fission products or fuel material is directly related to cladding stress and strain under normal operating and overpower conditions. Design limits and damage limits (cladding perforation) in terms of stress and strain are as follows:

> Clad Stress - Under normal operational conditions I and II, the clad stresses are less than the clad yield stress, with due consideration for temperature and irradiation effects. While

the clad has some capability for accommodating plastic strain, the yield stress has been accepted as a conservative design basis.

Clad Tensile Strain - Under nominal operational conditions I and II, the clad tensile strain is less than 1%. This limit is consistent with proven practice.

For most of the fuel rod life the actual stresses and strains are considerably below the design limits. Thus, significant margins exist between actual operating conditions and the damage limits.

The other parameters having an influence on cladding stress and strain and the relationship of these parameters to the damage limits are as follows:

1. Internal gas pressure:

   The internal gas pressure required to produce cladding stresses equal to the damage limit under normal operating conditions is well in excess of the maximum design pressure. The maximum design internal pressure under nominal conditions is 2250 psia which is equal to the coolant pressure. The end of life internal gas pressure depends upon the initial pressure, void volume, and fuel rod power history, however it does not exceed the design limit of 2250 psia.

2. Cladding temperature:

   The strength of the fuel cladding is temperature dependent. The minimum ultimate strength reduces to the design yield strength at an average cladding temperature of approximately 850°F. The maximum average cladding temperature during normal operating conditions is given in Table 3.4.1-1.

3. Burnup:

   Fuel burnup results in fuel swelling which produces cladding strain. The strain damage limit is not expected to be reached until the peak burnup reaches approximately 65,000 MWD/MTU. The peak pellet burnup for fuel in equilibrium cycling is expected to be 50,000 MWD/MTU. The design equilibrium average burnup for a fuel assembly is about 30,000 MWD/MTU.

4. Fuel temperature and kw/ft:

   At zero burnup, cladding damage is calculated to occur at 31 kw/ft based upon cladding strain reaching the damage limit. At this power rating 17% of the pellet central region is expected to be in the molten condition. The maximum thermal output at rated power is 15.85 kw/ft (F_Q = 2.32).

Evaluation of Burnable Poison Rods

The burnable poison rods are located in the core inside RCC assembly guide thimbles and held down in place by attachment to a spider assembly compressed beneath the upper core plate and hence cannot be the source of any reactivity transient. Due to the low heat generation rate, and the conservative design of the poison rods, there is no possibility for release of the poison as a result of helium pressure or clad heating during accident transients including loss of coolant.

Two burnable poison rods of reduced length but similar in design to those to be used in the Indian Point Plant Unit 3 Reactor were exposed to inpile test conditions in the Saxton Test Reactor.

A visual examination of the rods was made in early June 1968, and a visual and profilometer examination was made on July 30, 1968 after an exposure of 1900 effective full power hours (~25% B^{10} depletion). The rods were found to be in excellent condition and profilometry results showed no dimensional variation from the original new condition.

An experimental verification of the reactivity worth calculations for borosilicate glass tubing is presented in WCAP-9000 (Reference 3).

Effects of Vibration and Thermal Cycling on Fuel Assemblies

Analyses of the effect of cyclic deflection of the fuel rods, grid spring fingers, RCC control rods, and burnable poison rods due to hydraulically induced vibrations and thermal cycling show that the design of the components is conservative.

In the case of the fuel rod grid spring support, the amplitude of a hydraulically induced motion of the fuel rod is extremely small (~.001), and the stress associated with the motion is significantly small (~100 psi). Likewise, the reactions at the grid spring due to the motion is much less than the preload spring force and contact is maintained between the fuel clad and the grid spring and dimples. Fatigue of the clad and fretting between the clad and the grid support is not anticipated.

The effect of thermal cycling on the grid-clad support is merely a slight relative movement between the grid contact surfaces and the clad, which is gradual in nature during heat-up and cool-down. Since the number of cycles of the occurrence is small over the life of a fuel assembly (~3 years), negligible wear of the mating parts is expected.

In-core operation of assemblies in the Yankee Rowe and Saxton reactors using similar clad support have verified the calculated conclusions. Additional test results under simulated reactor environment in the Westinghouse Reactor Evaluation Center also support these conclusions.

The dynamic deflection of the full-length control rods and the burnable poison rods is limited by their fit with the inside diameter of either the upper portion of the guide thimble or the dashpot (.0765 in. diametral clearance at guide thimble; .0145 in. diametral clearance at the dashpot). With this limitation, the occurrence of truly cyclic motion is questionable. However, an assumed cycle deflection through the available clearance gap results in an insignificantly low stress in either the clad tubing or in the flexure joint at the spider or retainer plate. The above consideration assumes the rods are supported as cantilevers from the spider, or the retainer plate in the case of the burnable poison rods.

A calculation, assuming the rods are supported by the surface of the dashpots and at the upper end by the spider or retainer, results in a similar conclusion.

Control Rod Drive Mechanism

Design Description

a) Full Length Rods

The control rod drive mechanisms are used for withdrawal and insertion of the RCCA's into the reactor core and to provide sufficient holding power for stationary support.

Fast total insertion (reactor trip) is obtained by simply removing the electrical power allowing the rods to fall by gravity.

The complete drive mechanism, shown in Figure 3.2.1-13, consists of the internal (latch) assembly, the pressure vessel, the operating coil stack, the drive shaft assembly, and the rod position indicator coil stack.

Each assembly is an independent unit, which can be dismantled or assembled separately. Each mechanism pressure housing is threaded onto an adapter on top of the reactor pressure vessel and seal welded. The operating drive assembly is connected to the control rod (directly below) by means of a grooved drive shaft. The upper section of the drive shaft is suspended from the working components of the drive mechanism. The drive shaft and control rod remain connected during reactor operation, including tripping of the rods.

Main coolant fills the pressure containing parts of the drive mechanism. All working components and the shaft are immersed in the main coolant.

Three magnetic coils, which form a removable electrical unit and surround the rod drive pressure housing induce magnetic flux through the housing wall to operate the working components. They move two sets of latches, which lift, lower and hold the grooved drive shaft.

The three magnets are turned on and off in a fixed sequence by solid-state switches for the full-length rod assemblies.

The sequencing of the magnets produces step motion over the 144 inches of normal control rod travel.

The mechanism develops a lifting force approximately two times the static lifting load. Therefore, extra lift capacity is available for overcoming mechanical friction between the moving and the stationary parts. Gravity provides the drive force for rod insertion and the weight of the whole rod assembly is available to overcome any resistance.

The mechanisms are designed to operate in water at 650ºF and 2485 psig. The temperature at the mechanism head adapter will be much less than 650ºF because it is located in a region where there is limited flow of water from the reactor core, while the pressure is the same as in the reactor pressure vessel.

A multi-conductor cable connects the mechanism's operating coils to the 125 volt d-c power supply.

Latch Assembly

The latch assembly contains the working components, which withdraw and insert the drive shaft and attached control rod. It is located within the pressure housing and consists of the pole pieces for three electromagnets. They actuate two sets of latches, which engage the grooved section of the drive shaft.

The upper set of latches move up or down to raise or lower the drive rod by 5/8 inch. The lower set of latches have a maximum 1/16 inch axial movement to shift the weight of the control rod from the upper to the lower latches.

Pressure Vessel

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

Operating Coil Stack

The operating coil stack is an independent unit, which is installed on the drive mechanism by sliding it over the outside of the pressure housing. It rests on a pressure housing flange without any mechanical attachment and can be removed and installed while the reactor is pressurized.

The three operator coils are made of round copper wire, which is insulated, with a double layer of filament type glass yarn.

The design operating temperature of the coils is 200°C. The average coil temperature can be determined by resistance measurement. Forced air cooling along the outside of the coil stack maintains a coil casing temperature of approximately 120°C or lower.

Drive Shaft Assembly

The main function of the drive shaft is to connect the control rod to the mechanism latch. Grooves for engagement and lifting by the latches are located throughout the 144 in. of control rod travel. The grooves are spaced 5/8 inch apart to coincide with the mechanism step length and have 45° angle sides.

The drive shaft is attached to the control rod by the coupling. The coupling has two flexible arms, which engage the grooves in the spider assembly.

A 1/4 inch diameter disconnect rod runs down the inside of the drive shaft. It utilizes a locking button at its lower end to lock the coupling and control rod. At its lower end, there is a disconnect assembly. For remote disconnection of the drive shaft assembly from the control rod, a button at the top of the drive rod actuates the connect/disconnect assembly.

During plant operation, the drive shaft assembly remains connected to the control rod at all times. It can be attached and removed from the control rod only when the reactor vessel head is removed.

Position Indicator Coil Stack

The position indicator coil stack slides over the rod travel housing section of the pressure vessel. It detects drive rod position by means of a cylindrically wound differential transformer which spans the normal length of the rod travel (144 inches).

Drive Mechanism Materials

All parts exposed to the reactor coolant, such as the pressure vessel, latch assembly and drive rod, are made of metals, which resist the corrosive action of the water.

Three types of metals are used exclusively: stainless steel, Inconel X, and cobalt based alloys. Wherever magnetic flux is carried by parts exposed to the main coolant, 400 series stainless steel is used. Cobalt based alloys are used for the pins, latch tips, and bearing surfaces.

Inconel X is used for the springs of both latch assemblies and Type 304 stainless steel is used for all pressure containment. Hard chrome plating provides wear surfaces on the sliding parts and prevents galling between mating parts (such as threads) during assembly.

Outside of the pressure vessel, where the metals are exposed only to the reactor plant containment environment and cannot contaminate the main coolant, carbon and stainless steels are used. Carbon steel, because of its high permeability, is used for flux return paths around the operating coils. It is zinc-plated 0.001 inch thick to prevent corrosion.

Principles of Operation

The drive mechanisms shown schematically in Figure 3.2.1-13 withdraw and insert their respective control rods as electrical pulses are received by the operator coils.

ON and OFF sequence, repeated by switches in the power programmer causes either withdrawal or insertion of the control rod. Position of the control rod is indicated by the differential transformer action of the position indicator coil stack surrounding the rod travel housing. The differential transformer output changes as the top of the ferromagnetic drive shaft assembly moves up the rod travel housing.

Generally, during plant operation, the drive mechanisms hold the control rods withdrawn from the core in a static position, and only one coil, either the movable gripper coil, or the stationary gripper coil is energized on each mechanism.

Control Rod Withdrawal: The control rod is withdrawn by repeating the following sequence:

1. Moveable Gripper Coil - ON

   The movable gripper armature raises and swings the movable gripper latches into the drive shaft groove.

2. Stationary Gripper Coil - OFF

   Gravity causes the stationary gripper latches and armature to move downward until the load of the drive shaft is transferred to the movable gripper latches. Simultaneously, the stationary gripper latches then swing out of the shaft groove.

3. Lift Coil - ON

   The 5/8 inch gap between the lift armature and the lift magnet pole closes and the drive rod raises one step length.

4. Stationary Gripper Coil - ON

   The stationary gripper armature raises and closes the gap below the stationary gripper magnetic pole, and swings the stationary gripper latches into a drive shaft groove. The latches contact the shaft and lift it 1/16 inch. The load is so transferred from the movable to the stationary gripper latches.

5. Movable Gripper Coil - OFF

   The movable gripper armature separates from the lift armature under the force of the spring and gravity. Three links, pinned to the movable gripper armature, swing the three movable gripper latches out of the groove.

6. Lift Coil - OFF

   The gap between the lift armature and the lift magnet pole opens. The movable gripper latches drop 5/8 inch to a position adjacent to the next groove.

Control Rod Insertion:

The sequence for control rod insertion is similar to that for control rod withdrawal:

1. Lift Coil - ON

   The movable gripper latches are raised to a position adjacent to a shaft groove.

2. Movable Gripper Coil - ON

   The movable gripper armature raises and swings the movable gripper latches into a groove.

3. Stationary Gripper Coil - OFF

   The stationary gripper armature moves downward and swings the stationary gripper latches out of the groove.

4. Lift Coil - OFF

   Gravity and spring force separates the lift armature from the lift magnet pole and the control rod drops down 5/8 inch.

5. Stationary Gripper Coil - ON

6. Movable Gripper Coil - OFF

The sequences described above are termed as one step or one cycle and the control rod moves 5/8 inch for each cycle. Each sequence can be repeated at a rate of up to 72 steps per minute and the control rods can therefore be withdrawn or inserted at a rate of up to 45 inches per minute.

Control Rod Tripping:

The holding or static mode is with the stationary gripper coil. If power to the movable gripper coil is cut off, as for tripping, the combined weight of the drive shaft and the rod cluster control assembly is sufficient to move the latches out of the shaft groove. The control rod falls by gravity into the core. The tripping occurs as the magnetic field, holding the movable gripper armature against the lift magnet collapses and the movable gripper armature is forced down by the weight acting upon the latches.

b) Part Length Control Rod Drive Mechanism

Part length control rod drive mechanisms have been eliminated.

Fuel Assembly and RCCA Mechanical Evaluation

To confirm the mechanical adequacy of the fuel assembly and full length RCCA assembly, functional test programs have been conducted on a full scale Indian Point No. 2 prototype 12 ft. canless fuel assembly and control rod. The prototype assembly was tested under simulated conditions of reactor temperature, pressure, and flow for approximately 1000 hours. The prototype mechanism accumulated 2,260,892 steps and 600 scrams. At the end of the test the CRDM was still operating satisfactorily. A correlation was developed to predict the amplitude of flow excited vibration of individual fuel rods and fuel assemblies. Inspection of the fuel assembly and drive line components did not reveal significant fretting. The wear of the absorber rods, fuel assembly guide thimbles, and upper guide tubes was minimal. The control rod free fall time against 125% of nominal flow was less than 1.5 seconds to the dashpot (10 ft. of travel). Additional tests had previously been made on a full scale San Onofre mock up version of the fuel assembly and control rods (Reference 2).

Indian Point No. 2 1/7 Scale Mockup Tests

A 1/7 scale model of the Indian Point No. 2 internals was designed and built for hydraulic and mechanical testing. The tests provided information on stresses and displacements at selected locations on the structure due to static loads, flow induced loads, and electromagnetic shaker loads. Flow distribution and pressure drop information were obtained. Results of the static tests indicated that mean strains in the upper core support plate and upper support columns are below design limits. Strains and displacements measured in the model during flow tests verified that no damaging vibration levels were present. Additional information gained from the tests were the natural frequency and damping of the thermal shield and other components in the air and water. Model response can be related to the full-scale plant for most of the expected exciting phenomena, but across the board scaling is not possible. Specifically exciting phenomena, which are strongly dependent on Reynolds number, cannot be scaled. In areas where the Reynolds number may be important, either: (1) the measured vibration amplitudes were many times lower than a level that would be damaging, or (2) full scale vibration data has been obtained.

Loading and Handling Tests

Tests simulating the loading of the prototype fuel assembly into a core location have also been successfully conducted to determine that proper provisions had been made for guidance of the fuel assembly during refueling operations.

Axial and Lateral Bending Tests

In addition, axial and lateral bending tests have been performed in order to simulate mechanical loading of the assembly during refueling operations. Although the maximum column load expected to be experienced in service is approximately 1000 lbs. the fuel assembly was successfully loaded to 2200 lb. axially with no damage resulting.

This information is also used in the design of fuel handling equipment to establish the limits for inadvertent axial loads during refueling.

3.2.2 Core Component Tests and Inspections

To ensure that all materials, components and assemblies conform to the design requirements, a release point program was established with the manufacturer. This required surveillance of all raw materials, special processes, i.e., welding, heat treating, non-destructive testing, etc., and those characteristics of parts which directly affect the assembly and alignment of the reactor internals. The surveillance was accomplished by the issuance of an Inspection Release by the Westinghouse quality control organization after conformance was verified.

A resident quality control representative performed a surveillance/audit program at the manufacturer's facility and witnessed the required tests and inspections and issued the inspection releases.

Components and materials supplied by Westinghouse to the assembly manufacturer were subjected to a similar program. Quality Control engineers developed inspection plans for all raw materials, components and assemblies. Each level of manufacturing was evaluated by a qualified inspector for conformance, e.g., witnessing the ultrasonic testing of core plate raw material. Upon completion of specified events, all documentation was audited prior to releasing the material or component for further manufacturing. All documentation and inspection releases were maintained in the quality control central records section. All materials are traceable to the mill heat number.

In conclusion a set of "as built" dimensions were taken to verify conformance to the design requirements and assure proper fitup between the reactor internals and the reactor pressure vessel.

Fuel Quality Control

The Westinghouse Electric company 'Quality Management System' (QMS), as summarized in Reference 11, has been developed in conjunction with the Nuclear Fuel Business Unit (NFBU) to serve the division in planning and monitoring its activities for the design and manufacture of nuclear fuel assemblies and associated components. The NRC-approved QMS program provides for control over all activities affecting product quality, commencing with design and development and continuing through procurement, materials handling, fabrication, testing, and inspection.

The Westinghouse QMS is fully implemented with NFBU by lower level internal procedures such as

1. Manufacturing Operating Procedures,
2. Chemical Operating Procedures, and
3. Quality Control Instructions.

Taken collectively, these procedures are fully responsive to 10 CFR 50 Appendix B as well as ISO 9001 requirements. These procedures cover all areas of manufacturing including, but not limited to, fuel system components and parts, pellets, rod inspection, non-fuel core components, fuel assemblies, and components. With respect to supplier quality control, Westinghouse reviews and approves process outlines and inspection procedures by suppliers to ensure that applicable design and specification requirements are met.

Irradiated Fuel Tests and Inspections

Should they be determined to be desirable or necessary, tests and/or inspections of irradiated fuel may be elected to be performed. These tests and/or inspections are usually performed during refueling outages, but not necessarily so. Inspections are performed to determine the status of the integrity of the fuel rod cladding, to observe any other types of mechanical fuel damage or failure, or to obtain information as to the cause of fuel damage or failure. Various inspection methods may be used to accomplish these goals. These methods include: non-intrusive visually-aided inspections, eddy current inspection, ultrasonic testing, and in-mast fuel sipping. A description of each of these activities follows below:

1. Non-Intrusive Visually-Aided Inspections

   Non-intrusive inspections of fuel, meaning that the fuel assembly is not disassembled during the inspection, for indications of fuel damage or failure, or to verify fuel assembly identification, are performed using visual aids. These aids could be binoculars or could be underwater cameras. These underwater cameras could either be fixed in position on the fuel racks, or hang from the spent fuel pool bridge crane or side of the transfer canal or spent fuel pool through use of cables or poles. No safety concerns are identified with this process provided that the fuel being inspected is handled in accordance with technical specifications and plant procedures.

A special version of this type of inspection is a "lift-and-rotate" inspection, which is performed to obtain information as to fuel failure mechanism. During a "lift-and-rotate" inspection, special tooling is used to manipulate fuel rods that reside within the fuel assembly envelope. The tooling can be used to raise, lower, and rotate each fuel rod so that a large percentage of the surface area of the fuel rod can be exposed to the outside of the fuel assembly to be observed by a camera. A fuel assembly that has been inspected using the "lift-and-rotate" inspection technique may not be reloaded into the reactor due to the inability to control lateral forces on the fuel rod being manipulated by the tooling that may cause unacceptable deformation of the spring in the lower grid strap for that fuel rod location.

2. In-Mast Fuel Sipping

When handling fuel with the manipulator crane, it may be desired to test the irradiated fuel for leaks using the in-mast full sipping system. The inspection is typically performed during core unload. When a fuel assembly is raised from the core and is in the full up position in the manipulator crane mast, the inspection of a fuel assembly may proceed. Air bubbles are pumped to the bottom of the mast such that they enter the mast and raise past the fuel assembly. The air bubbles up to above the surface of the water inside the mast, where it can be suctioned past a detector to measure its radioactivity. Should the fuel assembly contain failed fuel, radioactive fission gases that will be escaping the failed fuel rod(s) due to the chance in rod pressure corresponding to the change in surrounding water pressure with elevation will be stripped by the air bubbles as they traverse upward through the mast. A change in the measurement of radioactivity from the background level will indicate the presence of failed fuel.

An evaluation was performed to assess the effects of voiding due to the presence of air bubbles in the mast on heat transfer and criticality. No concerns were identified. Two air regulators are required in series on the supply air to the mast to provide double failure protection to prevent an excessive amount of air being supplied to the mast above the 1 gpm required for the inspection.

3. Ultrasonic Testing

Ultrasonic testing (UT) is another method used to detect the presence of fuel failures. This inspection is typically performed in the spent fuel pool. An inspection station is mounted on top of a spent fuel pool storage rack, one, which does not contain irradiated fuel. Administrative controls are in place during set up and removal of the equipment on the spent fuel pool storage rack such that impact energy limits would not be exceeded should the inspection station inadvertently be dropped. The inspection station is set up such that when a fuel assembly is lowered into the inspection station, no interaction will be encountered between the fuel and the spent fuel pool storage rack.

During UT, fuel assemblies are positioned one at a time in the UT inspection station through use of the spent fuel bridge crane. The fuel assembly being tested is lowered into the inspection station such that the lowest grid is below the level of the probe. A probe is inserted between the fuel rods to send and receive ultrasonic signals through each fuel rod. The return signal will be different depending upon the condition inside the fuel rod. If the fuel rod is not failed, the inside surface should contain helium gas. The return signal will be nearly identical to the sent signal. However, if the fuel rod is failed, water present on the inside of the fuel rod will dampen the signal, returning a signal different than what was sent. In this manner, failed fuel rods are detected. No safety concerns are identified since procedural and technical specifications requirements for fuel handling are adhered to.

Rod Cluster Control Assembly Wear Inspections

Mechanical damage has been known to occur on rod cluster control assemblies (RCCAs). Flow induced vibration of the RCCAs while in operation causes the RCCAs to come in contact with the guide support structure, potentially causing wear rings to form on the RCCAs. Also, axial cracking has been observed at the tips of the RCCAs at other nuclear facilities. Examinations are occurring on a periodic basis to assess the rate and extent of wear and cracking of the RCCAs.

Similar to UT, an inspection station is set up on a spent fuel pool rack. Administrative controls are in place during set up and removal of the equipment on the spent fuel pool storage rack such that impact energy limits would not be exceeded should the inspection station inadvertently be dropped.

RCCAs are handled using the RCCA handling tool connected to the spent fuel pool bridge crane. RCCAs are taken to the inspection station one at a time for inspection. The inspection station consists of the appropriate number of eddy current encircling coils to assess the amount of wall loss from each RCCA rodlet at any axial location. No safety concerns are identified since procedural and technical specifications requirements for fuel handling are adhered to. Evaluation of the data determines whether reuse of the RCCA is appropriate and when the next inspection should be performed.

Bottom Mounted Instrumentation Thimble Tube Wear Inspections

Mechanical wear on thimble tubes has been observed at axial locations corresponding to the lower core plate and bottom fuel nozzle, as well as in the lower internals area. This wear has been attributed to flow induced vibration. In an effort to eliminate concerns of flow-induced wear, all installed bottom-mounted instrumentation (BMI) flux thimble tubes were replaced with new thimble tubes. The replacement thimble tubes, beginning 12 feet from the bullet end, are chrome plated over a 14-foot length of the outside circumference. Chrome plating of flux thimble tubes has been determined to be an effective engineering solution to wear due to flow-induced vibration. Monitoring of the extent and rate of wear is necessary to ensure the integrity of the thimble tubes, so that they may be replaced or repositioned whenever possible, and so that the reactor core may have at least the minimum number of required instrumented locations for flux map surveillances.

Wear is observed using eddy current techniques. This inspection is performed with the reactor in Mode 5 or 6 and at atmospheric pressure. An eddy current probe is pushed into and pulled from each of the thimble tubes at the seal table to determine wall loss at each axial location.

Burnable Poison Rod Tests and Inspections

The end plug seal welds are checked for integrity by visual inspection and X-ray. The finished rods are helium leak checked.

3.2.3 References for Section 3.2

1. Daniel, R. C., et al, "Effects of High Burnup on Zircaloy-Clad Bulk UO_2, Plate Fuel Element Samples, "WAPD-263, (September, 1965).
2. Large Closed Cycle Water Reactor Research and Development Program Quarterly Progress Reports for the Period January 1963 through June 1965 (WCAP-3738, 3739, 3743, 3750, 3269-2, 3269-3, 3269-5, 3269-6, 3269-12 and 3269-13).
3. J. S. Moore, WCAP-9000 "Nuclear Design of Westinghouse PWR's with Burnable Poison Rods", March 1969.
4. WCAP-7072 "Use of Part Length Absorber Rods in Westinghouse Pressurized Water Reactors".
5. "Westinghouse Electric Company Quality Management System (QMS)", Revision 3.
6. Design Change Package, DCP 01-0125 "Core Barrel/Former Plate Bolt Replacement".
7. Slagle, W. H. (ed.), "Westinghouse Fuel Assembly Reconstitution Evaluation Methodology," WCAP-13060-P-A, July 1993.

3.3 NUCLEAR DESIGN

3.3.1 Nuclear Design and Evaluation

This section presents the nuclear characteristics of the initial core and an evaluation of the characteristics and design parameters which are significant to design objectives. The capability of the reactor to achieve these objectives while performing safely under operational modes, including both transient and steady state, is demonstrated. Power distribution limits have been updated in the current Technical Specifications which applies to cores with W 15x15 upgrade reloads. These current limits are incorporated in Section 3.5. Nuclear characteristics of the current cycles reload fuel are discussed in Section 3.5.

Nuclear Characteristics of the Design

A summary of the reactor nuclear design characteristics for the initial core is presented in Table 3.3.1-1.

Reactivity Control Aspects

Reactivity control is provided by neutron absorbing control rods and by a soluble chemical neutron absorber (boric acid) in the reactor coolant. The concentration of boric acid is varied as necessary during the life of the core to compensate for:

1. changes in reactivity which occur with changes in temperature of the reactor coolant from cold shutdown to the hot operating, zero power conditions;
2. changes in reactivity associated with changes in the fission product poisons xenon and samarium;
3. reactivity losses associated with the depletion of fissile inventory and buildup of long-lived fission product poisons (other than xenon and samarium); and
4. changes in reactivity due to burnable poison burnup.

The control rods provide reactivity control for:

1. fast shutdown;
2. reactivity changes associated with changes in the average coolant temperature above hot zero power (core average coolant temperature is increased with power level);
3. reactivity associated with any void formation;
4. reactivity changes associated with the power coefficient of reactivity.

Control to render the reactor subcritical at temperatures below the operating range is provided by a chemical neutron absorber (boron). The boron concentration during Cycle 1 refueling has been established as shown in Table 3.3.1-1, line 29. This concentration, together with the control rods, provides approximately 10 per cent shutdown margin for these operations. The concentration was also sufficient to maintain the core shutdown without any control rods during refueling. For cold shutdown, at the beginning of Cycle 1 core life, a concentration (shown in Table 3.3.1-1, line 37) was sufficient for one per cent shutdown with all but the highest worth rod inserted. The boron concentration (Table 3.3.1-1, line 29) for Cycle 1 refueling was equivalent to less than two per cent by weight boric acid (H_3BO_3) and was well within solubility limits at ambient temperature. This concentration was also maintained in the spent fuel pit since it is directly connected with the refueling canal during refueling operations.

The initial Cycle 1 full power boron concentration without equilibrium xenon and samarium was 1152 ppm. As these fission product poisons were built up, the boron concentration was reduced to 838 ppm.

This initial boron concentration was that which permitted the withdrawal of the control banks to their operational limits. The xenon-free hot, zero power shutdown (k = 0.99) with all but the highest worth rod inserted, was maintained with a boron concentration of 734 ppm. This concentration was less than the full power operating value with equilibrium xenon.

Control Rod Requirements

Neutron-absorbing control rods provide reactivity control to compensate for more rapid variations in reactivity. The rods are divided into two categories according to their function. Some rods compensate for changes in reactivity due to variations in operating conditions of the reactor such as power or temperature. These rods comprise the control group of rods. The remaining rods, which provide shutdown reactivity, are termed shutdown rods. The total shutdown worth of all the rods is also specified to provide adequate shutdown with the most reactive rod stuck out of the core.

Control rod reactivity requirements at beginning and end of Cycle 1 life are summarized in Table 3.3.1-2. The calculated worth of the control rods is shown in Table 3.3.1-3.

The difference was available for excess shutdown upon reactor trip. The control rod requirements are discussed below.

Total Power Reactivity Defect

Control rods must be available to compensate for the reactivity change incurred with a change in power level due to the Doppler effect. The magnitude of this change has been established by correlating the experimental results of numerous operating cores.

The average temperature of the reactor coolant is increased with power level in the reactor. Since this change is actually a part of the power dependent reactivity change, along with the Doppler effect and void formation, the associated reactivity change must be controlled by rods. The largest amount of reactivity that must be controlled is at the end of life when the moderator temperature coefficient has its most negative value. The moderator temperature coefficient range for Cycle 1 is given in Table 3.3.1-1, line 42, while the cumulative reactivity change is shown in the first line of Table 3.3.1-2. By the end of the fuel cycle, the non-uniform axial depletion causes a severe power peak at low power. The reactivity associated with this peak is part of the power defect.

Operational Maneuvering Band

The control group is operated at full power within a prescribed band of travel in the core to compensate for periodic changes in boron concentration, temperature, or xenon. The band has been defined as the operational maneuvering band. When the rods reach either limit of the band, a change in boron concentration must be made to compensate for any additional change in reactivity, thus keeping the control group within the maneuvering band.

Control Rod Bite

If sufficient boron is present in a chemically-shimmed core, the inherent operational control afforded by the negative moderator temperature coefficient is lessened to such a degree that the major control of transients resulting from load variations must be compensated for by control rods. The ability of the plant to accept major load variations is distinct from safety considerations, since the reactor would be tripped and the plant shut down safely if the rods could not follow the imposed load variations. In order to meet required reactivity ramp rates resulting from load changes, the control rods must be inserted a given distance into the core. The reactivity worth of this insertion has been defined as control rod bite.

The reactivity insertion rate must be sufficient to compensate for reactivity variation due to changes in power and temperature caused either by a ramp load change of five per cent per minute, or by a step load change of ten per cent. An insertion rate of $4x10^{-5}$ $\Delta\rho$ per second was determined by the transient analysis of the Cycle 1 core and plant to be adequate for the most adverse combinations of power and moderator coefficients. To obtain this minimum ramp rate one control bank of rods should remain partly inserted into the core.

Xenon Stability Control

Out-of-core instrumentation is provided to obtain necessary information concerning power distribution. This instrumentation is adequate to enable the operator to monitor and control xenon induced power oscillations. Extensive analyses, with confirmation of methods by spatial transient experiments at Haddam Neck, has shown that any induced radial or diametral xenon transients would die away naturally. A full discussion of xenon stability control can be found in Reference 2.

Excess Reactivity Insertion upon Reactor Trip

The control requirements were nominally based on providing one per cent shutdown at hot, zero power conditions with the highest worth rod stuck in its fully withdrawn position or to prevent return to criticality following a credible steam-line break, whichever was the more limiting. The condition where excess reactivity insertion is most critical is at the end of a cycle when the steam break accident is considered. The excess control available at the end of Cycle 1, hot zero power condition with the highest worth rod stuck out, allowing a 10% margin for uncertainty in control rod worth, is shown in Table 3.3.1-3.

Calculated Rod Worths

The complement of 53 full length control rods arranged in the pattern shown in Figure 3.3.1-1 meets the shutdown requirements. Table 3.3.1-3 lists the calculated worths of this rod configuration for beginning and end of the first cycle. In order to be sure of maintaining a conservative margin between calculated and required rod worths, an additional amount has been added to account for uncertainties in the control rod worth calculations. The calculated reactivity worths listed are decreased in the design by 10 per cent to account for any errors or uncertainties in the calculation. This worth is established for the condition that the highest worth rod is stuck in the fully withdrawn position in the core.

A comparison between calculated and measured rod worths in operating reactors show the calculation to be well within the allowed uncertainty of 10%.

Power Distributions

The Donald C. Cook Nuclear Plant is required to meet the Acceptance Criteria for Emergency Core Cooling Systems for Light Water Reactors as specified in 10 CFR 50.46 and Appendix K to 10 CFR 50. It is necessary to limit the core heat flux hot channel factors, F_Q, to values which would result in peak clad temperatures below 2200°F following a loss of coolant accident and also assure other ECCS related criteria are met (see Chapter 14).

The Cycle 1 peaking factor limits at full power for the plant could be met by operation using either the Power Distribution Control Procedure (PDC-II), or the Axial Power Distribution Monitoring System (APDMS), with both methods requiring limits on the amount of axial offset that is allowed. The material presented below provides information on the technical basis for operation with constant axial offset control and PDC-II that was reflected in the Technical Specifications at the time of Cycle 1 operation.

The accuracy of power distribution calculations has been confirmed through over 1000 flux maps during over 20 plant years of operation under conditions very similar to those for the plant described herein. Details of this confirmation are given in Reference (8).

Definitions

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

Power density	is the thermal power produced per unit volume of the core (kW/liter).
Linear power density or linear heat generation rate (LHGR)	is the thermal power produced per unit length of active fuel (kW/ft). Since fuel assembly geometry is standardized this is the unit of power density most commonly used. For all practical purposes it differs from kW/liter by a constant factor which includes geometry and the fraction of the total thermal power which is generated in the fuel rod.
Average linear power density	is the total thermal power produced in the fuel rods divided by the total active fuel length of all rods in the core.
Local heat flux	is the heat flux at the surface of the cladding (Btu-ft^{-2}-hr^{-1}). For nominal rod parameters this differs from linear power density by a constant factor.
Average power or rod integral power	is the length integrated linear power density in one rod (kW).
Average rod power	is the total thermal power produced in the fuel rods divided by the number of fuel rods (assuming all rods have equal length).

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

F_Q Heat Flux Hot Channel Factor, is defined as the maximum local heat flux on the surface of a fuel rod divided by the average fuel rod heat flux.

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

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

Combined statistically the net effect is a factor of 1.03 to be applied to the fuel rod surface heat flux.

F_Q^M Measured Heat Flux Hot Channel Factor. These measurements, using the incore detector system, are generally taken with core at near equilibrium conditions.

F_Q^C Measured Heat Flux Hot Channel Factor times the flux map measurement uncertainty and factor that accounts for fuel manufacturing tolerances.

F_Q^W Transient Heat Flux Hot Channel Factor. Maximum anticipated value of F_Q obtained from equilibrium value of F_Q by adjusting by a factor that accounts for the calculated worst case transient conditions.

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

Manufacturing tolerances, hot channel power distribution and surrounding channel power distributions are treated explicitly in the calculation of the DNBR described in Section 3.4.

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

F_Q = Total peaking factor or heat flux hot channel factor

$$= \frac{\text{Maximum kW/ft}^2}{\text{Average kW/ft}^2}$$

In terms of subfactors,

$$F_Q = F_Q^N \text{ x } F_Q^E$$

$$= F_{XY}^N \text{ x } F_Z^N \text{ x } F_U^N \text{ x } F_Q^E$$

Where:

F_Q^N and F_Q^E are defined above

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

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

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

In previous analyses of power peaking factors for D.C. Cook Unit 1, it was necessary to apply a penalty on calculated overpower transient F_Q values to allow for interpellet gaps caused by pellet hang-ups and pellet shrinkage due to densifications. This penalty is known as the densification spike factor. However, studies have shown (Reference 11) that this penalty can be eliminated from overpower transient calculations for the fuel type present in the D.C. Cook Unit 1 core. Furthermore, results reported in Reference (12) show that such a power spike penalty should not be included in the LOCA evaluation.

Radial Power Distributions

The power shape in horizontal sections of the core at full power is a function of the fuel and burnable poison loading patterns and the presence or absence of a single bank of full-length control rods. Thus, at any time in the cycle, a horizontal section of the core can be characterized as either unrodded or with group D control rods, and as either unpoisoned or containing burnable poison rods. These form four possible situations which, combined with burnup effects, determine the radial power shapes, which can exist in the core at full power. The effects on radial power shapes of power level, xenon, samarium, and moderator density are considered also, but these are quite small. The effect of non-uniform flow distribution is negligible. While radial power distributions in various planes of the core are often illustrated, the core radial enthalpy rise distribution as determined by the integral of power up each channel is of greater interest.

Since the position of the hot channel varies from time to time a single reference radial design power distribution is selected for DNB calculations. This reference power distribution is chosen conservatively to concentrate power in one area of the core, minimizing the benefits of flow redistribution. Assembly powers are normalized to core average power.

For the purpose of illustration, assembly power distributions from the BOL, MOL and EOL conditions in Cycle 1 are shown in Figures 3.3.1-2 through 3.3.1-4.

Fuel Rod Power Distributions

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

Axial Power Distributions

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

$$\text{Axial offset} = \frac{\phi_t - \phi_b}{\phi_t + \phi_b}$$

where ϕ_t and ϕ_b are the top and bottom detector readings.

Limiting Power Distributions

Occurrences which are expected frequently or regularly in the course of power operation, maintenance, or maneuvering of the plant are accommodated with margin between any plant parameter and the value of that parameter which would require either automatic or manual protective action. Inasmuch as these occurrences occur frequently or regularly, they must be considered from the point of view of affecting the consequences of fault conditions. In this regard, analysis of each fault condition described is generally based on a conservative set of initial conditions corresponding to the most adverse set of conditions which can occur during normal operations.

The list of steady-state and shutdown conditions, permissible deviations (such as one coolant loop out of service) and operational transients is given in Chapter 14. Implicit in the definition of normal operation is proper and timely action by the reactor operator. That is, the operator follows recommended operating procedures for maintaining appropriate power distributions and takes any necessary remedial actions when alerted to do so by the plant instrumentation. Thus, as stated above, the worst or limiting power distribution which can occur during normal operation is to be considered as the starting point for analysis of fault conditions.

Improper procedural actions or errors by the operator are assumed in the design as occurrences of moderate frequency. Some of the consequences, which might result, are discussed in Chapter 14. Therefore, the limiting power shapes which result from such events, are those power shapes which deviate from the normal operating condition at the recommended axial offset band, e.g., due to lack of proper action by the operator during a xenon transient following a change in power level brought about by control rod motion. Power shapes, which fall in this category, are used for determination of the Reactor Protection System set points so as to maintain margin to overpower the DNB limits.

The means for maintaining power distributions within the required hot channel factor limits are described in the Technical Specifications. A complete discussion of power distribution control in Westinghouse PWR's is included in Reference (2). Detailed background information on the following: design constraints on local power density in a Westinghouse PWR, the defined operating procedures and the measures taken to preclude exceeding design limits; is presented in the Westinghouse Topical Report on power distribution control and load following procedures. The following paragraphs summarize these reports and describe the calculations used to establish the upper bound on peaking factors.

The calculations used to establish the upper bound on peaking factors, F_Q and $F_{\Delta H}$, include all of the nuclear effects which influence the radial and/or axial power distributions throughout core life for various modes of operation including load follow, reduced power operation, and axial xenon transients.

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

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

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

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

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

    a. below 85% RTP - 18 steps.

    b. above 85% RTP - 12 to 18 steps dependent on F_Q^W (z) and F ΔH margins.

2. Control banks are sequenced with overlapping banks.

3. The control full-length bank insertion limits are not violated.

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

The axial power distribution procedures referred to above are part of the required operating procedures, which are followed, in normal operation. Briefly they require control of the axial offset (flux difference divided by fractional power) at all power levels within a permissible operating band of a target value corresponding to the equilibrium full power value. In the first cycle, the target value changes from about -10 to 0 percent through the life of the cycle. This minimizes xenon transient effects on the axial power distribution, since the procedures essentially keep the xenon distribution in phase with the power distribution.

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

These cases represent many possible reactor states in the life of one fuel cycle and they have been chosen as sufficiently definitive of the cycle of comparison with much more exhaustive studies performed on different, but typical, plant and fuel cycle combinations. The cases are described in detail in Reference (6) for the Westinghouse analysis, and they are considered to be necessary and sufficient to generate a local power density limit which, when increased by 5 percent for conservatism, will not be exceeded with a 95 percent confidence level. Many of the numerous amounts of points do not approach the limiting envelope; however, they are part of the time histories which lead to the hundreds of shapes which do define the envelope. They also serve as a check that the reactor studied is typical of those studied more exhaustively.

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

The calculated points were synthesized from axial calculations combined with radial factors appropriate for rodded and unrodded planes in the first cycle. In these calculations the effects on the unrodded radial peak of xenon redistribution that occurs following the withdrawal of a control bank (or banks) from a rodded region was obtained from two-dimensional X-Y calculations. A 1.03 factor to be applied on the unrodded radial peak was obtained from calculations in which xenon distribution was preconditioned by the presence of control rods and then allowed to redistribute for several hours. The calculated values have been increased by a factor of 1.05 for conservatism and a factor of 1.03 for the engineering factor F_Q^E.

For reload cores required to satisfy the Final Acceptance Criteria (10 CFR 50.46) for the Loss of Coolant Accident, the total core peaking factor (F_Q times relative power) is evaluated as a function of core height and compared to the Technical Specification limit. All of the nuclear effects which influence axial power distributions throughout the fuel cycle are included in the evaluation of the total peaking factor. Various modes of load follow and base load operation are considered. This evaluation is based on normal plant operation in compliance with the Technical Specifications.

For cores that operate within the limits of Constant Axial Offset Control (CAOC), the evaluation is initiated by determining whether the core operates within the following constraints:

1. The Technical Specification limit on the maximum height dependent F_Q was equal to or less than a value of 2.04 for ENC fuel and 2.10 for W for Cycle 8 operations, and
2. The CAOC flux difference ($\pm$ ΔI) bandwidth is less than or equal to $\pm$5% ΔI.

These procedures were detailed in the Technical Specifications and are predicted only upon excore surveillance supplemented by the normal monthly full core map requirement, and by computer based alarms on deviation and time of deviation from the allowed flux difference band.

Accident analyses for this plant are presented in Chapter 14 (Unit 1) of the Cook Nuclear Plant FSAR. The results of these analyses determined a limiting value of total peaking factor, F_Q^L, under normal operation, including load following maneuvers. This value is derived from the conditions necessary to satisfy the limiting conditions specified in the LOCA analyses of Section 14.3.1, which meet 10 CFR 50.46(a)(1) requirements. An upper bound envelope of F_Q^{ND} results from operation in accordance with constant axial offset control procedures using excore surveillance only.

The surveillance of the core hot channel factors in accordance with the above, was presented in the Cook Nuclear Plant Unit 1 Technical Specifications.

The Constant Axial Offset Control (CAOC) procedure, (Reference 7 and 10), enables Cook Nuclear Plant Unit 1 to manage core power distributions such that Technical Specification Limits on F_Q were not violated during normal operation and limits on MDNBR were not violated during steady-state, load-follow, and anticipated transients.

This procedure provides the means for predicting the maximum F_Q distribution anticipated during operation under this procedure taking into account the incore measured equilibrium power distribution. A comparison of this distribution with the Technical Specification limit curve determined whether the Technical Specification limit could be protected by the CAOC procedure. If such protection could be confirmed for a given operating cycle interval, APDMS monitoring was not necessary over this interval and the excore monitored constant axial offset limits would protect the Technical Specification F_Q limits.

The prediction of the maximum anticipated F_Q distribution, $F_Q^W(z)$, is made possible by controlling the distribution such that it does not increase by more than the factor W(z) times the equilibrium power distribution $F_Q^C(z)$. This is accomplished by maintaining the core axial offset within a specified range of values about a target value associated with the equilibrium power distribution. The value of the W(z) factor is determined from analysis of plant operation data during which the axial offset is maintained within a specified band about the equilibrium (target) axial offset. The core axial offset (AO) has been previously defined in the subsection entitled, Axial Power Distributions.

A positive axial offset signifies a power shift toward the top half of the core, while a negative axial offset signifies a power shift toward the bottom half of the core.

The basic features of the CAOC procedures are as follows:

1. An $F_Q^C(z)$ distribution was determined along with an associated axial offset, denoted as the target axial offset (AO_T), at full power, equilibrium xenon conditions. The $F_Q^C(z)$ distribution was the measured $F_Q^M(z)$ distribution multiplied by the uncertainty factors 1.05 x 1.03, where 1.05 is the measurement uncertainty and 1.03 the engineering factor.

2. The $F_Q^C(z)$ distribution is multiplied by the cycle dependent W(z) factor, to obtain the maximum anticipated $F_Q^W(z)$ which is compared to the Technical Specification limits, $F_Q^{Limit}(z)$. This limiting curve for $F_Q^{Limit}(z)$ is given by the product of $F_Q^{L/P}$ times K(z). If $F_Q^W(z)$ does not exceed the $F_Q^{Limit}(z)$, then operation under the CAOC procedures protects the F_Q Technical Specification limits. If the product $F_Q^W(z)$ exceeds the $F_Q^{Limit}(z)$ then, reactor core power must be reduced to meet the Technical Specifications limits.

3. For each axial offset target value (AO_T) a target band (AO_{TB}) was allowed.

$$AO_{TB} = \frac{\pm S\%}{P/P_o}$$

where:

P = operating reactor power (MWt)

P_o = reactor rated power (MWt)

S = target band specified in Core Operating Limits Report; typically +/-3% or +/-5%

4. Below a relative power (P/P_o) of 0.9 or 0.9 x minimum value of [F_Q^L x $K(z)/F_Q^W(z)$] (whichever is less), the axial offset is allowed to deviate from the target band for one hour out of each twenty-four consecutive hours, provided that the measured axial offset remained within a broader, but specified, axial offset band. If this requirement was violated, the core relative power should have been reduced below 0.5 of rated power where no restrictions on AO are imposed. Above a relative

power of 0.9, or 0.9 x minimum value of [F_Q^L x $K(z)/F_Q^W(z)$] (whichever is less), the measured AO must remain within the allowable target band at all times.

Reactivity Coefficients

The response of the reactor core to plant conditions or operator adjustments during normal operation, as well as the response during abnormal or accidental transients, was evaluated by means of a detailed plant simulation. In these calculations, reactivity coefficients were required to couple the response of the core neutron multiplication to the variables, which were set by conditions external to the core. Since the reactivity coefficients change during the life of the core, a range of coefficients was established to determine the response of the plant throughout life and to establish the design of the Reactor Control and Protection System.

*Moderator Temperature Coefficient**

The moderator temperature coefficient in a core controlled by chemical shim is less negative than the coefficient in an equivalent rodded core. One reason is that control rods contribute a negative increment to

the coefficient and in a chemical shim core, the rods are only partially inserted. Also, the chemical poison density is decreased with the water density upon an increase in temperature. This gives rise to a positive component of the moderator temperature coefficient due to boron being removed from the core. This is directly proportional to the amount of reactivity controlled by the dissolved poison.

In order to reduce the dissolved poison requirement for control of excess reactivity, burnable poison rods have been incorporated in the core design. The result is that changes in the coolant density will have less effect on the density of poison and the moderator temperature coefficient will be reduced.

The Westinghouse burnable poison was in the form of borated pyrex glass rods clad in stainless steel. In Cycle 1, there were 1436 of these rods in the form of clusters distributed throughout the core in vacant rod cluster control guide tubes as illustrated in Figures 3.3.1-11 and 3.3.1-12. Information regarding research, development and nuclear evaluation of the burnable poison rods

* Chapter 14 discusses operation with a positive temperature coefficient. The value currently allowed by the Technical Specifications is 0.5 x 10^{-4} $\Delta k/k/^{o}F$ at or below 70% rated thermal power and 0 x 10^{-4} $\Delta k/k/^{o}F$ at 100% rated power. The allowed value decreases linearly with power from 70% rated thermal power to 100% rated thermal power.

can be found in Reference 1. These rods initially controlled 9.0% of the installed excess reactivity and their addition resulted in a reduction of the initial hot full power boron concentration. The moderator temperature coefficient was negative at the operating coolant temperature with this boron concentration and with burnable poison rods installed.

The effect of burnup on the moderator temperature coefficient was calculated and the coefficient becomes more negative with increasing burnup. This is due to the buildup of fission products with burnup and dilution of the boric acid concentration with burnup. The reactivity loss due to equilibrium xenon is controlled by boron, and as xenon builds up, boron is taken out. With core burnup, the coefficient will become more negative as boron is removed, and because of a shift in the neutron energy spectrum due to the buildup of plutonium and fission products.

The control rods provide a negative contribution to the moderator coefficient as can be seen from Figures 3.3.1-13, 14, and 15 which are Cycle 1 initially calculated values.

Moderator Pressure Coefficient

The moderator pressure coefficient has an opposite sign to the moderator temperature coefficient. Its effect on core reactivity and stability is small because of the small magnitude of the pressure coefficient, a change of 50 psi in pressure having no more effect on reactivity than a half-degree change in moderator temperature. The calculated beginning- and end-of-life pressure coefficients for Cycle 1 are specified in Table 3.3.1-1, Line 43.

Moderator Density Coefficient

A uniform moderator density coefficient is defined as a change in the neutron multiplication* per unit change in moderator density. The range of the Cycle 1 moderator density coefficient from BOL to EOL is specified in Table 3.3.1-1, Line 44.

Doppler and Power Coefficients

The Doppler coefficient is defined as the change in neutron multiplication per degree change in fuel temperature. The coefficient is obtained by calculating neutron multiplication as a function of effective fuel temperature (Reference 3). The results from initial calculations are shown in Figure 3.3.1-16.

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* Neutron multiplication is defined as the ratio of the number of neutrons present in a reactor in each generation to that in the immediately preceding generation.

In order to know the change in reactivity with power, it is necessary to know the change in the effective fuel temperature with power, as well as the Doppler coefficient. It is very difficult to predict the effective temperature of the fuel using a conventional heat transfer model because of uncertainties in predicting the behavior of the fuel pellets. Therefore, an empirical approach was taken to calculate the power coefficient, based on operating experience of existing Westinghouse fueled cores. Figure 3.3.1-17 shows the power coefficient as a function of power obtained by this method. The results presented do not include any moderator coefficient even though the moderator temperature changes with power level.

Nuclear Evaluation

The basis for confidence in the procedures and design methods comes from the comparison of these methods with many experimental results. These experiments include criticals performed at the Westinghouse Reactor Evaluation Center (WREC) and other facilities, and also measured data from operating power reactors. A summary of the results and discussion of the agreement between calculated and measured values is given in other Safety Analysis Reports such as the FSAR for Indian Point Unit 2, Docket No. 50-247, Section 3.2.1, and the PSAR for Cook Nuclear Plant, Docket No. 50-315-316, Section 3.2.1.

Extensive analyses on the threshold to xenon instabilities as a function of variation in core parameters (power coefficient, etc.) have been reported in Reference 4.

Finally, verification of design analysis during the startup physics tests is described in Section 3.3.2.

3.3.2 Physics Tests

First Core Tests to Confirm Reactor Core Characteristics

A detailed series of startup physics tests were performed from zero power up to and including 100% power. As part of these tests, a series of core power distribution measurements were made over the entire range of operation in terms of RCCA configuration and power level by means of the incore movable detector system. In addition, rod worth, boron end-point, and reactivity coefficient measurements were made.

Within relevant acceptance criteria, these test results show good agreement with design predictions (Reference 1). To detect and eliminate possible errors in the calculations of the initial reactivity of the core and the reactivity depletion rate, the predicted relationship between fuel burnup and the boron concentration was normalized to accurately reflect actual core conditions. When full power was initially reached, and with the control groups in the desired positions, the boron concentration was measured and the predicted curve was adjusted to this point. As power operation continued, the measured boron concentration was compared with the predicted concentration and the slope of the predicted curve relating burnup and reactivity was corrected as necessary. This normalization was completed after about 10 percent of the total core burnup has occurred. Thereafter, actual boron concentration was compared with the predicted concentration, and the reactivity prediction of the core was continuously evaluated. No reactivity anomaly greater than one percent was observed.

In addition, periodic full-core flux maps were taken, using the incore detector system, to monitor power distribution, heat flux hot channel factors, enthalpy hot channel factors, quadrant power tilt ratios and axial flux differences. These measurements were utilized to ensure compliance with technical specifications.

Reload Physics Tests

Reload physics testing is performed at the beginning of each reload cycle to verify that the physics models used to predict the core's behavior (power distributions, reactivity parameters, and kinetics parameters) are accurate and reflect the actual reload core characteristics.

3.3.3 Anticipated Transients without Scram

In the Code of Federal Regulations, 10 CFR 50.62(c)(1) requires that each pressurized water reactor have equipment, from sensor output to final actuation device, that is diverse from the reactor trip system, to automatically initiate the auxiliary feedwater system and initiate a turbine trip under conditions indicative of an anticipated transient without scram (ATWS). Such a system has been installed at Cook Nuclear Plant, having been designed in accordance with Reference 1. This system is called "ATWS Mitigating System Actuation Circuitry" (AMSAC). This equipment will protect against reactor coolant system overpressurization in the event that a loss of normal feedwater or a loss of load transient is not accompanied by a reactor trip after having reached the reactor trip setpoint.

The generic ATWS analysis utilized, among other items, a Westinghouse Model 51 steam generator. A review of the impact of replacing the Model 51 steam generators with Babcock & Wilcox (BWI) Model 51R replacement steam generators indicated the AMSAC System was unaffected by the replacement.

3.3.4 Criticality of Fuel Assemblies

Information on criticality of the fuel assemblies outside of the reactor is presented in Section 3.3 of the Unit 2 FSAR and in Section 9.7.

3.3.5 References for Section 3.3

3.3.5.1 References for Section 3.3.1

1. Wood, P. M., Bassler, E. A., et al, "Use of Burnable Poison Rods in Westinghouse Pressurized Water Reactors", WCAP-7413 (October 1967).

2. Moore, J. S., "Power Distribution Control in Westinghouse Pressurized Water Reactors", WCAP-7811, December 1971.

3. Barry, R. F., "The Revised LEOPARD Code - A Spectrum Depending Non-Spatial Depletion Program", WCAP-2759, March 1965.

4. Poncelet, C. G., and Christie, A. M., "Xenon-Induced Spatial Instabilities in Large Pressurized Water Reactors", WCAP-3680-20 (1968).

5. McFarlane, A. F., "Core Power Capability in Westinghouse PWR's" WCAP-7267-L, October 1969.

6. Morita, T., et. al. "Power Distribution Control and Load Follow Procedures", WCAP-8385, September 1974.

7. "Exxon Nuclear Power Distribution Control for Pressurized Water Reactors Phase 2" XN-NF-77-57, January 1978.

8. Langford, F. L. and Nath, R. J., "Evaluation of Nuclear Hot Channel Factor Uncertainties", WCAP-7038-L, April, 1969 (Proprietary) and WCAP-7810, December 1971 (Non-Proprietary).

9. Letter from R. F. Hering to H. R. Denton dated April 7, 1982, AEP:NRC:0665.

10. Miller, R. W., et al, "Relaxation of Constant Axial Offset Control F_Q Surveillance Tech Spec.", WCAP-10216-P, Rev 1a, Feb. 1994.

11. Kersting, P.J. et al., “Assessment of Clad Flattening and Densification Power Spike Factor Elimination in Westinghouse Nuclear Fuel,” WCAP-13589-A, March 1995 (Proprietary) and WCAP-14297-A, March 1995 (Non-Proprietary).

12. Hellman, J. M. and Yang, J. W., “Effects of Fuel Densification Power Spikes on Clad Thermal Transients,” WCAP-8359, July 1974.

3.3.5.2 References for Section 3.3.2

1. Nelson, J. F., et al, "Summary Report of the Startup Nuclear Test Results for Donald C. Cook Unit 1, Cycle 1", WCAP-8688, December 1975.

3.3.5.3 References for Section 3.3.3

1. Adler, M. R., "AMSAC Generic Design Package," WCAP-10858, June 1985.
2. Framatome Technologies Report No. 77-5002104-01 "Replacement Steam Generator Report for American Electric Power D.C. Cook Unit 1 - Section 4.6.4; Anticipated Transients Without SCRAM"

3.4 UNIT 1 - THERMAL AND HYDRAULIC DESIGN

This section describes the thermal and hydraulic design of the Unit 1 core with Westinghouse (W) fuel. The thermal and hydraulic design for cores containing 15x15 upgrade fuel is discussed in Section 3.5.3.

3.4.1 Thermal and Hydraulic Evaluation for the Initial Core

Thermal and Hydraulic Characteristics of the Design

Thermal Data

Central Temperature of the Hot Pellet

The temperature distribution in the pellet is mainly a function of the uranium dioxide thermal conductivity and the local power density. The surface temperature of the pellet is affected by the cladding temperature and the thermal conductance of the gap between the pellet and the cladding.

The occurrence of nucleate boiling maintains the maximum cladding surface temperature below about 657°F at nominal system pressure. The contact conductance between the fuel pellet and cladding is a function of the contact pressure and the composition of the gas in the gap(1)(2) and may be calculated by the following equation:

$$h = 0.6P + \frac{k}{f\left(14.4\,X\,10^{-6}\right)}$$

where:

h is conductance in Btu/hr-ft^{2}-°F

P is contact pressure in psi

k is the thermal conductivity of the gas mixture in the rod

f is the correction factor for the accommodation coefficient

The thermal-hydraulic design assures that the temperature of the center of the hottest fuel pellet is below the melting point of the UO_2. (Melting point of 5080°F (Reference 7) unirradiated and reducing by 58°F per 10,000 MWD/MTU.) The temperature distribution within the fuel pellet is predominantly a function of the local power density and the UO_2 thermal conductivity. The pellet surface temperature is governed by the cladding temperature and the thermal conductance of the fuel pellet-cladding gap.

The thermal conductivity of uranium dioxide was evaluated from data reported by Howard, et. al. (Reference 21); Lucks, et. al. (Reference 22); Daniel, et. al. (Reference 23); Feith (Reference 24); Vogt, et. al. (Reference 25); Nishijima, et. al. (Reference 26); Wheeler, et. al. (Reference 27); Godfrey, et al (Reference 3); Stora, et. al. (Reference 28); Bush (Reference 29); Asamoto, et. al. (Reference 30); Kruger (Reference 31); and Gyllander (Reference 32). An examination of the UO_2 thermal conductivity data, Figure 3.4.1-1 shows that at temperatures between 0ºC and 1600ºC there is little variation in the data, while above 1600ºC the scatter increases considerably.

At the higher temperatures, thermal conductivity is best obtained utilizing the integral conductivity to melt, which can be determined with more certainty. From an examination of the data, it has been concluded that the best estimate for the value of $2800°C_{kdT}$ is 93 watts/cm. This conclusion is based on the integral values reported by Gyllander (Reference 32); Lyons, et. al. (Reference 33); Coplin, et. al. (Reference 34); Duncan (Reference 5); Bain (Reference 35); and Stora (Reference 36).

The design curve for the thermal conductivity is shown in Figure 3.4.1-1. The section of the curve at temperatures between 0ºC and 1300ºC is in excellent agreement with the recommendation of the IAEA panel[37]. The section of the curve above 1300ºC is normalized to an integral value of 93 watts/cm (References 5, and 32-36) from 0 to 2800ºC.

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

$$k = \frac{1}{11.8 + 0.238T} + 8.775X10^{-13}T^3$$

with k in watts/cm-ºC and T in ºC.

Radial Power Distribution in UO_2 Fuel Rods

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

This information on radial power distributions in UO_2 fuel rods is determined with the neutron transport theory code, LASER. The LASER code has been validated by comparing the code predictions on radial burnup and isotopic distributions with measured radial microdrill data[38,39]. Using LASER predicted radial power distributions, "radial power depression factors" are determined. The "radial power depression factor", f, enters into the determination of the centerline temperature, T_c, relative to the surface temperature, T_s, of a pellet with a uniform density distribution by the expression

$$\int_{T_S}^{T_C} k(T)dt = \frac{q'f}{4\pi}$$

where k(T) is the UO_2 thermal conductivity in watts/cm-°C and q' is the linear power in watts/cm.

Westinghouse Experience with High Power Fuel Rods

Westinghouse experience with non-pressurized fuel rods operating at high power ratings has been summarized in Appendix A, of the Indian Point No. 2 Preliminary Safety Analysis Report (Docket 50-247) and in Appendix-Section IX of the Preliminary Safeguards Report for the Saxton Reactor Operating at 35 MWt (Docket 50-146). These reports present considerable statistical evidence of successful operation of high performance Zircaloy clad fuel rods in CVTR (1368 rods) and Shippingport Core I Blanket (94,920 rods). Since the date of these reports, a significant amount of additional information has been developed relating to the integrity of free standing Zircaloy clad oxide fuel rods at high power ratings. In addition, a comprehensive experimental program has been initiated to extend the operating experience to higher power and to higher exposures for many of these fuel rods. This information is summarized in Figure 3.4.1-2.

The figure shows that thirty Saxton Plutonium Project non-pressurized fuel rods have operated at a design peak power level of up to 18.5 kW/ft to a peak exposure of approximately 30,000 MWD/MTM [Megawatt days per metric ton of metal (U + Pu)]. No failures have occurred with this fuel. In the Saxton overpower test, two selected fuel rods from the Saxton Plutonium Project assemblies were removed after peak exposures of 18,000 MWD/MTM and inserted in a subassembly for short time irradiation at a design rating of 25 kW/ft. Results of this program indicate satisfactory performance of the fuel in every respect.

In the above tests (performed on non-pressurized rods) the strain fatigue experienced by the cladding is more severe than expected to occur for pressurized rods which would be placed under identical operating conditions.

Internally pressurized fuel rods have been under investigation[9] at Westinghouse for a number of years. These investigations include out-of-pile and in-pile experimental programs and analytical studies. Fuel rods internally pressurized with various gases have been irradiated in the Saxton reactor. Tests results show that initial pressurization is effective in substantially reducing the rate of cladding-creep on the UO_2 fuel. The Saxton test results confirm the results of analyses, which predict fuel-cladding mechanical interaction early in life for non-pressurized fuel rods and delayed interaction for initially pressurized fuel rods.

To verify the substantial design margin, which exists in the fuel rods with regard to excessive internal pressures in a fuel rod, several highly pressurized Zircaloy-clad fuel rods were irradiated for several months in the Saxton reactor, then removed for examination. At an internal pressure of approximately 3500 psia (as compared to the design value of 2250 psia), the fuel operated satisfactorily for the period of the test without any indication of failure.

Finally, satisfactory performance of pressurized fuel rods has been demonstrated for long term irradiation periods in the PWR electric power plants that have been operating during the past decade.

Heat Flux Ratio and Data Correlation

Departure from Nucleate Boiling, (DNB), is predicted for a combination of hydrodynamic and heat transfer phenomena and is affected by the local and upstream conditions including the flux distribution.

In reactor design, the heat flux associated with DNB and the location of DNB are both important. The magnitude of the local fuel rod temperature after DNB depends upon the axial location where DNB occurs. The W-3 DNB correlation (Reference 8), which has been utilized in this design, incorporates both local and system parameters in predicting the local DNB heat flux. This correlation includes the non-uniform flux effect, and the upstream effect, which includes inlet enthalpy and path length. The local DNB heat flux ratio (defined as the ratio of the DNB heat flux to the local heat flux) is indicative of the contingency available in the local heat flux without reaching DNB.

Objective of the W-3 DNB Correlation

The W-3 DNB correlation [8] has been developed to predict the DNB flux and the location of DNB equally well for uniform and an axially non-uniform heat flux distribution.

The sources of the data used in developing this correlation are:

WAPD-188	(1958)	CU-TR-No. 1 (NW-208)	(1964)
ASME Paper 62-WA-297	(1962)	CISE-R-90	(1964)
CISE-R-63	(1962)	DP-895	(1964)
ANL-6675	(1962)	AEEW-R-356	(1964)
GEAP-3766	(1962)	BAW-3238-7	(1965)
AEEW-R-213 and 309	(1963)	AE-RTL-778	(1965)
CISE-R-74	(1963)	AEEW-355	(1965)
CU-MPR-XIII	(1963)	EUR-2490.e	(1965)

The comparison of the measured to predicted DNB flux of this correlation is given in Figure 3.4.1-3. The local flux DNB ratio versus the probability of not reaching DNB is plotted in Figure 3.4.1-4. This plot indicates that with a DNBR of 1.3 the probability of not reaching DNB is 95% at a 95% confidence level.

Rod bundle data without mixing vanes agree very well with the predicted DNB flux as shown in Figure 3.4.1-5, and rod bundle data with mixing vanes (Figure 3.4.1-6) show on the average an 8% higher value of DNB heat flux than predicted by the W-3 DNB correlation.

It should be emphasized that the inlet subcooling effect of the W-3 correlation was obtained from both uniform and non-uniform data. The existence of an inlet subcooling effect has been demonstrated to be real and hence the actual subcooling should be used in the calculations. The W-3 correlation was developed from tests with flow in tubes and rectangular channels. Good agreement is obtained when the correlation is applied to test data for rod bundles.

Local Non-Uniform DNB Flux

The W-3 correlation gives the equivalent uniform DNB heat flux, $q''_{DNB,EU'}$, for a given set of system and local conditions. The heat distribution upstream of the DNB point affects the value of the DNB flux. This influence is accounted for by the F-factor.[8] The non-uniform DNB heat flux, $q''_{DNB,N}$, is given by

$$q''_{DNB,N} = \frac{q''_{DNB,EU}}{F} \qquad (1)$$

Definition of DNB Ratio (DNBR)

The DNB heat flux ratio is defined as

$$DNBR = \frac{q''_{DNB,N}}{q''_{loc}} = \frac{q''_{DNB,EU}}{(F)(q''_{loc})} \qquad (2)$$

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

The F-factor may be considered as a hot spot factor, applicable to DNB, due to the axial heat flux distribution. An alternate, although improper, DNB ratio could be defined as $\frac{q''_{DNB,EU}}{q''_{loc}}$ instead of $\frac{q''_{DNB,EU}}{(F)(q''_{loc})}$

Since the F-factor at the minimum DNBR location is generally greater than unity, this alternate DNBR would be greater than the proper DNBR as defined by equation (2). Because this alternate DNBR does not consider the effects of the non-uniform flux distribution, it does not give the correct physical meaning to DNB and is therefore not used in the evaluation of DNB ratios.

Procedure for Using W-3 Correlation

In predicting the local DNB flux in a non-uniform heat flux channel, the following two steps are required:

1. The uniform DNB heat flux, $q''_{DNB,EU}$, is computed with the W-3 correlations using the specified local reactor conditions.
2. This equivalent uniform heat flux is converted into corresponding non-uniform DNB heat flux, $q''_{DNB,N}$, for the non-uniform flux distribution in the reactor. This is accomplished by dividing the uniform DNB flux by the F-factor.[8] Since F-factor is generally greater than unity $q''_{DNB,N}$ will be smaller than $q''_{DNB,EU}$.

To calculate the DNBR of a reactor channel, the values of $\frac{q''_{DNB,N}}{q''_{loc}}$ along the channel are evaluated and the minimum value is selected as the minimum DNBR incurred in that channel.

The W-3 correlation depends on both local and inlet enthalpies of the actual system fluid, and the upstream conditions are accommodated by the F-factor. Hence, the correlation provides a realistic evaluation of the safety margin on heat flux.

Hot Channel Factors

The total hot channel factors for heat flux and enthalpy rise are defined as the maximum-to-core average ratios of these quantities. The heat flux factors consider the local maximum at a point (the "hot spot" or location with maximum linear power density), and the enthalpy rise factors involve the maximum integrated value along a channel (the "hot channel").

Definition of Engineering Hot Channel Factor:

Each of the total hot channel factors is the product of a nuclear hot channel factor describing the neutron flux distribution and an engineering hot channel factor to allow for variations from design conditions. The engineering hot channel factors account for the effects of flow conditions and fabrication tolerances and are made up of subfactors accounting for the influence of the variations of fuel pellet diameter, density and enrichment; fuel rod diameter; pitch and bowing; inlet flow distribution; flow redistribution; and flow mixing.

The enthalpy rise engineering hot channel factors are evaluated using the THINC code [10]. These factors which are obtained by the THINC analysis will vary with the operating conditions. For this plant (Donald C. Cook Nuclear Plant, Unit 1) the engineering hot channel factors are 1.03 for F_q^E and an average value of 1.01 for $F_{\Delta H}^E$ The subfactors used in obtaining these values are described in the following paragraphs.

Heat Flux Engineering Subfactor, F_q^E

This subfactor, used to evaluate the maximum heat flux, is determined by statistically combining the tolerances for the fuel diameter, density, enrichment and the fuel rod diameter, pitch and bowing and has a value of 1.03. Measured manufacturing data for the first three Yankee cores, the SELNI core and Indian Point Core B show this factor is conservative in comparison to the value obtained for the probability limit of three standard deviations. Thus, it is expected that a statistical sampling of the fuel assemblies of this plant will also show this subfactor is conservative. This factor was decreased from a value of 1.04 (PSAR) to 1.03 (FSAR) as a result of more accurate measurements of fuel enrichment.

Enthalpy Rise Engineering Subfactor, $F^{E}_{\Delta H}$

Pellet Diameter, Density, Enrichment and Fuel Rod Diameter, Pitch and Bowing:

Based on the applicable tolerances and consistent with the probability limit of three standard deviations for the measured Yankee, SELNI, SENA, SCE, Connecticut Yankee and Indian Point data, a value of 1.08 was selected for this subfactor.

Inlet Flow Maldistribution:

Studies performed on 1/7 scale hydraulic reactor models indicate that a conservative design basis is to consider a 5% reduction in the flow to the hot fuel assembly under isothermal conditions. This inlet flow reduction in the THINC analysis results in an increase of 1% in the hot channel enthalpy rise.

Flow Redistribution:

The flow redistribution accounts for the reduction in flow in the hot channel resulting from the high flow resistance in the channel due to the local or bulk boiling. A nominal value for this channel subfactor when evaluated by the THINC code is 1.03, but in practice during the thermal and hydraulic analysis, the actual value for each case is calculated individually.

Flow Mixing:

Mixing vanes have been incorporated into the spacer grid design. These vanes induce flow mixing between the various flow channels in a fuel assembly and also between adjacent assemblies. This mixing reduces the enthalpy rise in the hot channel resulting from local power peaking or unfavorable mechanical tolerances.

In the THINC analysis, the benefit of coolant mixing in all the subchannels in the hot assembly is considered and a mixing factor of approximately 0.90 is used to evaluate the enthalpy rise to the point of minimum DNB ratio.

The above subfactors are combined to obtain the total engineering hot channel factor for an enthalpy rise of 1.01. The reduction in this subfactor at nominal operating conditions from a value of 1.075 (PSAR) was the result of the adaption of the THINC code (multi-subchannel analyses) as a thermal and hydraulic design method. Table 3.4.1-2 is a tabulation of the design engineering hot channel factors.

Operational Limits:

The above subfactors are incorporated in THINC steady-state and transient analyses to yield operating limits for the maximum measured value of the enthalpy rise hot channel factor, $F\Delta^N_H$. For Cook Nuclear Plant Unit 1, the technical specification limit (for Cycle 7) is:

$$F\Delta^N_H. = 1.51\ [(1 + 0.2(1\text{-}P)]^* \qquad (2a)$$

where, P is the ratio of operating power to rated power. The engineering subfactor $F\Delta^E_H$ is incorporated into the limiting value of 1.51*, and

$$F^N_{\Delta H} = 1.04 F^{N'}_H \qquad (2b)$$

where, $F^{N'}_{\Delta H}$ is the measured nuclear enthalpy rise peaking factor, and the factor of 1.04 accounts for measurement uncertainty.

The heat flux engineering subfactor of 1.03 is included in the maximum measured value of the heat flux hot channel factor,

$$F^N_Q = 1.03 \text{ x } 1.05\ F^N_Q \qquad (2c)$$

where, F^N_Q is the measured nuclear hot channel factor and the factor of 1.05 accounts for measurement uncertainties. For Cycle 8 operations, the technical specifications require that F^N_Q not exceed the limits defined in Section 3.2.2 of the technical specifications.

Pressure Drop and Hydraulic Forces

The total loss across the reactor vessel, including the inlet and outlet nozzles, and the pressure drop across the core are listed in Table 3.4.1-1. These values include a 10% uncertainty factor.

Thermal and Hydraulic Design Parameters

The thermal and hydraulic design parameters are given in Table 3.4.1-1.

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* For subsequent cycles, the FDHN limiting values for Westinghouse and ENC fuel at rated power have been changed to the values stated in the footnotes to Table 3.3.1-1.

Thermal and Hydraulic Evaluation

W-3 Equivalent Uniform Flux DNB Correlation

The equivalent uniform DNB flux q"DNB,EU is calculated from the W-3 equivalent uniform flux DNB correlation as follows:

$$\frac{q''\,\mathrm{DNB,\,EU}}{10^6} = \left[(2.022 - 0.0004302\,p) + (0.1722 - 0.0000984\,p)e^{(18.177 - 0.004129p)X}\right]$$

$$x\left[1.037 + \frac{G}{10^6}\left(0.1484 - 1.596X + 0.1729X|X|\right)\right]x\left[1.157 - 0.869\right]$$

$$x\left[0.2664 + 0.8357e^{-3.151De}\right]x\left[0.8258 + 0.000794\left(H_{sat} - H_{in}\right)\right] \qquad (3)$$

The heat flux is in Btu/hr-ft^2 and the units of the parameters are as listed below. The ranges of parameters of the data used in developing this correlation are:

System pressure, p = 1000 to 2300 psia

Mass velocity, G = 1.0 x 10^6 to 5.0 x 10^6 lb/hr-ft^2

Equivalent diameter, D_e = 0.2 to 0.7 inches

Quality, X = - 0.15 to + 0.15

Inlet enthalpy, $H_{in} \geq 400$ Btu/lb

Length, L = 10 to 144 inches

$$\frac{\text{Heated perimeter}}{\text{Wetted perimeter}} = 0.88 \text{ to } 1.00$$

Geometries - circular tube, rectangular channel and rod bundles

Flux = Uniform and equivalent uniform flux converted from non-uniform data by using F-factor of Reference (8).

Local Non-Uniform DNB Flux

The local non-uniform $q''_{DNB,N}$ is calculated as follows:

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

where

$$F = \frac{C}{q''\text{local at } \ell_{DNB} \text{ x } \left(1 - e^{-C\ell_{DNB}}\right)} \int_0^{\ell_{DNB}} q''(z)\, e^{-C(\ell_{DNB} - z)} dz \tag{5}$$

ℓ_{DNB} = distance from the inception of local boiling to the point of DNB.

z = distance from the inception of local boiling, measured in the direction of flow.

The empirical constant, C, as presented in Reference (8) has been updated through the use of more recent non-uniform DNB data. However, the revised expression does not significantly influence [less than one percent deviation from that of Reference (8)] the value of the F-factor and the DNBR. It does provide a better prediction of the location of DNB. The new expression is

$$C = 0.15 \frac{\left(1 - X_{DNB}\right)^{4.31}}{\left(G/10^6\right)^{0.478}} \text{ inch}^{-1} \tag{6}$$

where

G = mass velocity lb/hr-ft^2

X_{DNB} = quality of the coolant at the location where DNB flux is calculated.

In determining the F-factor, the value of q''_{local} at ℓ_{DNB} in equation (5) was measured as $z=\ell_{DNB}$ the location where the DNB flux is calculated. For a uniform flux, F becomes unity so that $q''_{DNB,N}$ reduces to $q''_{DNB,EU}$ as expected. The comparisons of predictions by using W-3 correlations and the non-uniform DNB data obtained by B&W (Reference 11), AEEW (Reference 12) (Reference 13) and Fiat are given in Figures 3.4.1-7 and 3.4.1-8. The criterion for determining the predicted location of DNB is to evaluate the ratio of the predicted DNB flux to the local heat flux along the length of the channel. The location of the minimum DNB ratio is considered to be location of DNB.

Application of the W-3 Correlation in Design

During steady state operation at the nominal design conditions, the DNB ratios are determined. Under other operating conditions, particularly overpower transients, more limiting conditions develop than those existing during steady state operation. The DNB correlation is sensitive to several parameters. In addition, thermal flux generated under transient conditions is also sensitive to many parameters. Therefore, for each case studied, a conservative combination of the significant parameters is used as an initial condition. These parameters include:

a. Reactor Coolant System pressure

b. Reactor Coolant System temperature

c. Reactor power (determined from secondary plant calorimetrics)

d. Core power distribution (hot channel factors)

For transient accident conditions where the power level, system pressure and core temperature may increase, the DNBR is limited to a minimum value of 1.30. The Reactor Control and Protection System is designed to prevent any credible combination of conditions from occurring which would result in a lower DNB ratio.

DNB Evaluation

A preliminary evaluation is made to predict the statistical number of fuel rods in the core that might reach DNB, both under normal operating conditions and under assumed overpower conditions. For this calculation, a convolution procedure is utilized in which the product of the number of fuel rods experiencing a given DNB ratio and the probability of reaching that DNB ratio is summed over the entire core.

Two cases were investigated using this method; one in which the nominal conditions of Table 3.4.1-1 were used and a second in which the coolant parameters were adjusted to give a minimum DNB ratio of 1.30 at 112% power. Less than 0.1 rod may experience DNB when operating at nominal conditions and less than eleven rods (less than 0.03%) may experience DNB for the arbitrary case mentioned above.

Table 3.4.1-3 summarizes the results of a sensitivity study to show the effect of major parameters on the statistical number of fuel rods which may experience DNB, using the design axial power distribution and the design and best estimate radial power distributions as shown in Figure 3.4.1-9.

Effects of DNB on Neighboring Rods

Westinghouse has never observed DNB to occur in a group of neighboring rods in a rod bundle as a result of DNB in one rod in the bundle.

DNB with Physical Burnout

Westinghouse (Reference 19) has conducted DNB tests in a 25-rod bundle where physical burnout occurred with one rod. After this occurrence, the 25 rod test section was used for several days to obtain more DNB data from the other rods in the bundle. The burnout and deformation of the rod did not affect the performance of neighboring rods in the test section during the burnout or the validity of the subsequent DNB data points as predicted by the W-3 correlation. No occurrences of flow instability or other abnormal operation were observed.

DNB with Return to Nucleate Boiling

Additional DNB tests have been conducted by Westinghouse (Reference 20) in 19 and 21 rod bundles. In these tests, DNB without physical burnout was experienced more than once on single rods in the bundles for short periods of time. Each time, a reduction in power of approximately 10% was sufficient to re-establish nucleate boiling on the surface of the rod. During these and subsequent tests, no adverse effects were observed on this rod or any other rod in the bundle as a consequence of operating in DNB.

Hydrodynamic and Flow Power Coupled Instability

The interaction of hydrodynamic and spatial effects have been considered and it is concluded that a large margin exists between the design conditions and those for which an instability is possible.

It has been known for some time that heated channels in parallel can lead to flow instability. If substantial boiling takes place, periodic flow instabilities have been observed and, as long ago as 1938, Ledinegg (Reference 14) proposed a stability criterion on the basis of which the concept of inlet orificing has been developed to stabilize flow. Other work (Reference 15-17) has demonstrated that periodic instabilities are possible which violate the Ledinegg criterion.

In normal flow channels with little or no boiling, the type of instability proposed by Ledinegg is not possible since it results primarily from the large changes in water density along the channel due to boiling. Moreover, the periodic instabilities examined by Quandt (Reference 15&16) and Meyer (Reference 17) are not exhibited in non-boiling channels of the type found in PWR cores.

3.4.2 Thermal and Hydraulic Tests and Inspections

General hydraulic tests on models have been used to confirm the design flow distributions and pressure drops (Reference 1&2). Fuel assemblies and control rod drive mechanisms were also tested in this manner. Appropriate on-site measurements were made to confirm the design flow rates.

Vessel and internals inspections were also reviewed to confirm such thermal and hydraulic design values as bypass flow.

3.4.3 References for Section 3.4

1. R. A. Dean, "Thermal Contact Conductance Between UO_2 and Zircaloy-2," CVNA-127, May 1962.
2. M. Ross and Stoute, R. D., "Heat Transfer Coefficient Between UO_2 and Zircaloy-2," AECL-1552, June 1962.
3. T. G. Godfrey, et. al., "Thermal Conductivity of Uranium Dioxide and Armco Iron by an Improved Radial Heat Flow Technique," ORNL-3556, June 1964.
4. J. A. L. Robertson, et. al., "Temperature Distribution of UO_2 Fuel Elements," Journal of Nuclear Materials 7, No. 3, 1962. 255-262.
5. R. N. Duncan, "Rabbitt Irradiation of UO_2", CVNA-142, June 1962.
6. G. R. Horn and J. A. Christensen, "Identification of the Molten Zone In Irradiated UO_2," ANS Winter Meeting Transactions, 1963, p. 348.
7. J. A. Christensen, R. J. Allio and A. Biancheria, "Melting Point of Irradiated Uranium Dioxide," WCAP-6065, February 1965.
8. L. S. Tong, "Prediction of Departure from Nucleate Boiling for an Axially Non-Uniform Heat Flux Distribution," Journal of Nuclear Energy, Vol. 21, pp. 241-248 (1967).
9. H. M. Ferrari, et al, "Use of Internally Pressurized Fuel Rods in Westinghouse Pressurized Water Reactors", WCAP-9002, Westinghouse Proprietary.
10. Chelemer, H., Weisman, J., Tong, L.S., "Subchannel Thermal Analysis of Rod Bundle Cores," WCAP-7015, January, 1967.
11. D. F. Judd, et al, "Non-Uniform Heat Generation Experimental Program," BAW-3238-7 (1965).
12. D. H. Lee, J. D. Obertelli, "An Experimental Investigation of Forced Convection Burnout in High Pressure Water, Part II, Preliminary Results for Round Tubes with

Non-Uniform Axial Heat Flux Distribution," AEEW-R-309, Winfrith, England (1963).

13. D. H. Lee, "An Experimental Investigation of Forced Convection Burnout in High Pressure Water, Part IV, Large Diameter Tubes at About 1600 psi", AEEW-R-479, Winfrith, England (1966).

14. Ledinegg, M., "Die Warme," 61 (48), (1938), 891-8.

15. Quandt, E. R., "Analysis of Parallel Channel Transient Response and Flow Oscillations," WAPD-AD-TH-489, (1959).

16. Quandt, E. R., "Analysis and Measurement of Flow Oscillations" Chemical Engineering Progress Symposium Series, Vol. 57, 32, (1961) III.

17. Meyer, J. E., Rose, R. P., Journal of Heat Transfer, Vol. 85, 1 (1963) 1.

18. Tong, L. S., et at., "HYDNA Digital Computer Program for Hydrodynamic Transient", CVNA-77, 1961.

19. J. Weisman, A. H. Wenzel, L. S. Tong, D. Fitzsimmons, W. Thorne, and J. Batch, "Experimental Determination of the Departure from Nucleate Boiling in Large Rod Bundles at High Pressure", AIChE, Preprint 29, 9th National Heat Transfer Conference, 1967, Seattle, Washington.

20. L. S. Tong, H. Chelemer, J. E. Casterline, and B. Matzner, "Critical Heat Flux (DNB) in Square and Triangular Array Rod Bundles", JSME, Semi-International Symposium, Paper #256, 1967, Tokyo, Japan.

21. V. C. Howard and T. G. Gulvin, "Thermal Conductivity Determinations on Uranium Dioxide by a Radial Flow Method", UKAEA 1G Report 51 (RD/C), 1960.

22. F. Lucks and H. W. Deem, "Progress Reports for June 1960, BMI-1448; December 1960, BMI-1489, and May 1961, BMI-1448.

23. J. L. Daniel, J. Matolich, Jr., and H. W. Deem, "Thermal Conductivity of UO_2", HW-69945, September 1962.

24. D. Feith, "Thermal Conductivity of UO_2 by Radial Heat Flow Method", TM-63-9-5, (1963).

25. J. Vogt, L. Grandell, and U. Runfors, "Determination of the Thermal Conductivity of Unirradiated Uranium Dioxide", AB Atomenergi Report RMB-537 (1964), Quoted by IAEA Report on Thermal Conductivity of Uranium Dioxide.

26. T. Nishijima, T. Kawada, and A. Ishihata, "Thermal Conductivity of Sintered UO_2 and A1203 at High Temperatures", J. American Ceramic Society, 48, 31 (1965).

27. M. J. Wheeler and J. B. Ainscough, "Thermal Diffusivity and Thermal Conductivity of Sintered Uranium Dioxide", Proc. of 7th Conference on Thermal Conductivity, Gaithersburg, Maryland, November 1967.

28. J. P. Stora, et. al., CEA-R-2586 (1964).

29. J. Bush, "Apparatus for Measuring Thermal Conductivity to 2500°C", Westinghouse R&D Report 64-1P6-401-R3, February 1965.

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

31. O. L. Kruger, "Heat Transport Properties of Uranium and Plutonium Dioxide", Paper presented at the Fall Meeting of Nuclear Division of the American Ceramic Society, September 1968, Pittsburgh. Also Private Communications.

32. J. A. Gyllander, In-Pile Determination of the Thermal Conductivity of UO_2 in the Range 500-2500 Degrees Centigrade, AE-411 (January 1971).

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

34. H. Coplin, et. al., "The Thermal Conductivity of UO_2 by Direct In-reactor Measurement", GEAP-5100-6, March 1968.

35. S. Bain, "The Heat Rating Required to Produce Center Melting in Various UO_2 Fuels", ASTM Special Technical Publication, No. 306, p. 30.

36. J. P. Stora, "In-Reactor Measurements of the Integrated Thermal Conductivity of UO_2 – Effect of Porosity", Trans. ANS, 13, p. 137. June 1970.

37. "Thermal Conductivity of Uranium Dioxide", IAEA Technical Reports Series, No. 59, 1965.

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

39. R. J. Nodvick, "Saxton Core II Fuel Performance Evaluation Part II", WCAP-3385-56 Part II, July 1970.

3.4.3.1 References for Section 3.4.2

1. G. Hetsroni, "Hydraulic Tests of the San Onofre Reactor Model", WCAP-3269-8 (1964).

2. G. Hetsroni, "Studies of the Connecticut-Yankee Hydraulic Model," WCAP-2761 (1965).

3.5 WESTINGHOUSE RELOAD FUEL

Starting with Cycle 21 operation the Cook Nuclear Plant Unit 1 has been refueled with Westinghouse (W) fuel assemblies of the 15x15 Upgrade design. This chapter evaluates the mechanical, nuclear, and thermal hydraulic design of the Upgrade fuel. Unit 1 Cycle 22 will be principally used as an example of Westinghouse 15x15 Upgrade Reload Fuel for current cycles. Information for other fuel designs will be used when a significant design change is introduced.

The W Upgrade fuel assemblies utilize guide thimble tubes, instrument tubes, mixing vane grids and Intermediate Flow Mixer (IFM) grids fabricated with ZIRLO®. ZIRLO® is used in place of its predecessor, Zircaloy-4, for its improved corrosion resistance. Beginning with Cycle 25, ZIRLO® fuel rod cladding material is replaced by Optimized ZIRLO™.

As mentioned above, the 15x15 Upgrade fuel employs IFM grids. IFM grids are considered non-structural upper assembly grids, which contain mixing vanes similar to structural grids. Specifically, the IFM grids are located between the top three mixing vane grid spans. Based on a proven design, the 15x15 IFM grids have less than one third the mass of the current 15x15 structural grids. These IFM grids have virtually no effect on neutron economy.

Two key elements of the Upgrade fuel's structural mid grid design are the 1-spring and a balanced or symmetrical vane pattern. The 1-spring is designed to increase contact area with the fuel rod, while the symmetric vane pattern reduces fuel assembly vibration. Both of these aspects have a positive impact on fuel rod fretting margin as described in Section 2.2.2.3 of Reference 10.

Another feature of the Upgrade fuel is the guide thimble's tube-in-tube (TNT) dashpot design. This thimble design adds lateral stiffness to the assembly which decreases the possibility of Incomplete Rod Insertion (IRI). Further details are presented in Section 3.5.1.2.1, as well as Section 2.2.3 of Reference 10.

The Cook Nuclear Plant Unit 1 is licensed for a maximum power level of 3304 MWt. The Thermal Hydraulic Design and Nuclear Design summarized in this chapter and the accident analyses in Chapter 14 were performed at this power level.

Finally, the W Upgrade fuel is fabricated with debris mitigation features carried over from the Performance + fuel design. These features are described in detail in Section 3.5.1.5. All analyses were performed utilizing W standard methods, which are described in the W Reload Safety Evaluation Methodology Topical (Reference 2). The approved Westinghouse Revised Thermal

Design Procedure (RTDP) is used in the DNB analyses of W fuel. The W WRB-1 correlation is used in the 15 Upgrade DNB analyses.

3.5.1 Fuel Mechanical Design

Each W Upgrade assembly consists of 204 fuel rods, 20 guide thimble tubes, and 1 instrumentation thimble tube arranged within a supporting structure. The instrumentation thimble is located in the center position and provides a channel for insertion of an incore neutron detector, if the fuel assembly is located in an instrumented core position. The guide thimbles provide channels for insertion of either a rod cluster control assembly, a neutron source assembly, a WABA assembly, or a thimble plug assembly, depending on the position of the particular fuel assembly in the core. The fuel rod pitch is maintained by two lnconel end grids and five ZIRLO® intermediate grids. The ZIRLO® guide tubes are mechanically attached to the top and bottom nozzles. The guide tubes, nozzles and grids form the structural skeleton of the fuel bundle. The fuel rods are loaded into the fuel assembly structure so that there is clearance between the fuel rod ends and the top and bottom nozzle. Figure 3.5.1-1 shows an Upgrade assembly with IFMs and protective grid fuel length schematic view, and Table 3.5.1-1 shows Upgrade fuel design values.

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

The top nozzle functions as the upper structural element of the fuel assembly in addition to providing a partial protective housing for the rod cluster control assembly or other components. It consists of an adapter plate, enclosure, top plate, and pads. The nozzle assembly comprises holddown springs, screws or pins, and clamps mounted on the top plate. The springs and spring screws are made of lnconel-718, whereas other components are made of Type 304 stainless steel.

The adapter plate is provided with round penetrations and semicircular ended slots to permit the flow of coolant upward through the top nozzle. Other round holes are provided to accept inserts which are mechanically attached to the adapter plate and swaged to the thimble tubes as shown in Figure 3.5.1-4. The ligaments in the plate cover the tops of the fuel rods and prevent their upward ejection from the fuel assembly. The enclosure is a box-like structure, which sets the distance between the adapter plate and the top plate. The top plate has a large square hole in the center to permit access for the control rods and the control rod spiders. Holddown springs are mounted on the top plate and are retained by spring screws and clamps or pins and an integral nozzle pad located at two diagonally opposite corners. On the other two corners integral pads are positioned which contain alignment holes for locating the upper end of the fuel assembly.

The guide thimble to top nozzle attachment is shown in Figure 3.5.1-4. The stainless steel top nozzle inserts are mechanically connected to the top nozzle adapter plate by means of a preformed bulge near the top of the insert. The insert engages a mating groove in the wall of the adapter plate/thimble tube through-hole. The insert has equally spaced axial slots, which allow the insert to deflect inwardly at the elevation of the bulge thus permitting the installation or removal of the nozzle. The insert bulge is positively held in the adapter plate mating groove by placing a lock tube, with a uniform inside diameter identical to that of the thimble tube, into the insert.

3.5.1.1 Mechanical Compatibility of Fuel Assemblies

Design Basis

The 15x15 Upgrade fuel shall be dimensionally compatible with core components and fuel handling equipment.

Evaluation

The 15x15 Upgrade fuel is designed to be compatible with existing fuel handling equipment. The Upgrade compatibility with other core components is shown, in Section 3.5.1.4, to be acceptable.

Fuel Assembly Grid Load Analysis

Forcing functions for the reactor internals model are based on postulated LOCA and seismic conditions. The hydraulic forces and loop mechanical loads resulting from a postulated LOCA pipe rupture are prescribed at appropriate locations of the Reactor Pressure Vessel (RPV) model. For the seismic analysis, the plant-specific design acceleration spectra are specified based upon the plant site characteristics. For the current analysis, the synthesized seismic time histories are calculated from the D.C. Cook Unit 1 plant specific acceleration response spectra envelope. These spectra are for the containment buildings at the 612.62 feet elevation and use the appropriate Design Basis damping. Both the LOCA and seismic time histories are applied to the Reactor Pressure Vessel system model. The core plate motions from the dynamic analysis of this model are obtained and are then input to the Reactor Core Model

The Reactor Core Model includes four individual fuel assembly array models with varying row lengths and inter-assembly grid impact elements. The number of fuel assemblies in the array models for the D.C. Cook Unit 1 are 7, 11, 13 and 15, which represent the number of fuel assemblies in each of the core planar arrays. The peak grid loads for each LOCA and seismic transient are the maximum impact load obtained from four different models (rows) in the X and Z directions, i.e. parallel to the reactor vessel horizontal cardinal axes. The seismic and most limiting case of LOCA analyses were performed for every array of fuel assemblies.

The limiting LOCA and seismic grid impact loads for homogeneous 15 Upgrade assembly cores are summarized in Table 3.5.1-3. The maximum grid loads, obtained from two seismic accidents (Safe Shutdown Earthquake (SSE) and Operational Basis Earthquake (OBE)) and LOCA loading analyses (from auxiliary line breaks (accumulator line and pressurizer line)), were combined as required using the square root of the sum of the values squared (SRSS) method. The results of the seismic and LOCA analyses of the maximum impact forces for the 15x15 structural grids are compared to allowable grid distortion loads. These allowable grid loads are experimentally established as the 95 percent confidence level on the mean from the distribution of grid distortion data at normal plant operating temperature. Acceptability of the fuel (grid) performance for RCCA control rod insertion is verified by demonstrating that no grid deformation occurs in assemblies directly beneath control rod locations. For Unit 1, no fuel assembly grid distortion was calculated and thus control rod insertion will not be impeded by the fuel for either the limiting Leak Before Break (LBB) criteria break locations or the design basis cold leg breaks. The 15x15 upgrade fuel is structurally acceptable for the D.C. Cook Unit 1 reactor. Refer to Section 3.5.1.6.3.2.1 for further details on grid impact loads as well as thimble tube and fuel rod stresses.

3.5.1.2 Fuel Assembly Structure

The fuel assembly structure consists of a bottom nozzle, top nozzle with holddown springs, twenty (20) guide thimble tubes, center instrumentation thimble tube, and eleven grids (five ZIRLO mixing vane grids, three ZIRLO™ IFMs, two end lnconel and one lnconel protective grid) as shown in Figure 3.5.1-1.

The design bases of the W assembly structure are presented in Reference 10. For the lnconel and ZIRLO™ grids, lateral loads resulting from a seismic or LOCA event will not cause an unacceptably high plastic deformation. Each fuel assembly's geometry will be maintained such that the fuel remains in an array amenable to cooling.

3.5.1.2.1 Guide and Instrumentation Thimbles

Description

The 15x15 Upgrade guide thimbles are structural members, which also provide channels for the neutron absorber rods, burnable poison rods, neutron source, or thimble plug assemblies. Each

15x15 Upgrade guide thimble is fabricated from ZIRLO™ and incorporates a tube-in-tube (TNT) dashpot design. Whereas a standard guide thimble/dashpot design consists of a single piece of tubing having two different diameters to provide a dashpot action near the end of the control rod travel during normal trip operation. The 15x15 upgrade tube-in-tube design utilizes a separate dashpot tube assembly that is inserted into the guide thimble assembly pulled to a press fit over the thimble end plug and bulged into place (Figure 3.5.1-5). As the dashpot tube in this design can provide additional lateral support in that bottom thimble span, it provides additional resistance to lateral deformation and incomplete rod insertions as a result of this design modification.

The 15x15 Upgrade tube-in-tube guide thimble and dashpot tubes are each terminated with end plugs welded prior to skeleton assembly. The end plug is provided with a small flow port to avoid fluid stagnation in the dashpot volume during normal operation. Once inserted into the guide thimble assembly the dashpot assembly end plug is pulled onto a raised boss on the guide thimble end plug producing an interference fit. This is accomplished by drawing it down from the bottom of the guide thimble using a tooling screw. Once in place, the dashpot is further secured using a retaining bulge. The 15x15 Upgrade tube-in-tube design is illustrated in Figure 3.5.1-5.

The top end of the guide thimble is fastened to a tubular insert by three bulges. The insert fits into the top nozzle adapter plate. The lower end of the guide thimble is fitted with an end plug, which is then fastened into the bottom nozzle by a locking cup thimble screw (See Section 3.5.1.2.3).

Each grid is fastened to the guide thimble assemblies to create an integrated structure. The fastening technique depicted in Figures 3.5.1-2 and 3.5.1-3 is used for all grids in a fuel assembly, except for the Protective grid. An expanding tool is inserted into the inner diameter of the ZIRLO™ thimble tube at the elevation of ZIRLO™ sleeves that have been welded into the inner five ZIRLO™ grid assemblies. The four lobed tool forces the thimble and sleeve outward to a predetermined diameter, thus joining the two components. To accommodate the tube-in-tube design, the protective grid is fastened to the assembly using 4 spacers instead of being welded to inserts. Beginning with Cycle 25, the protective grid is fastened using 8 spacers instead of 4.

The top grid to thimble attachment is shown in Figure 3.5.1-4. The stainless steel sleeves are brazed into the lnconel grid assembly. The ZIRLO® guide thimbles are fastened to the long sleeves by expanding the two members, as shown by Figure 3.5.1-4. The bottom grid to sleeve and sleeve to thimble attachments are similar to the top grid attachments. The central instrumentation thimble of each Upgrade assembly is constrained by seating in counterbores in each nozzle. This tube is a constant diameter and guides the incore neutron detectors. This thimble is expanded at the top and mid-grids in the same manner as the previously discussed expansion of the guide thimbles to the grids.

Evaluation

The Upgrade guide tube thimble ID provides an adequate nominal diametral clearance for the control rods as well as other components. The control rod scram time to the dashpot is equal to or less than 2.4 seconds. This rod drop time was determined from conservative analytical calculations. The 2.4 second scram time was used in all the accident analyses. Section 3.5.1.1 shows that guide thimble mechanical integrity is maintained during a seismic and/or LOCA event(s).

There is sufficient diametral clearance for the instrumentation thimble to traverse the Upgrade instrumentation tube.

3.5.1.2.2 Top Nozzle and Holddown Springs

Description

The 15 Upgrade top nozzle is a machined and welded structure approximately 8.4 inches square by 3.5 inches high. The top nozzle assembly is the uppermost structural member of the fuel assembly. The top nozzle forms a plenum, where coolant received from the fuel assembly is mixed and directed to flow holes in the upper core plate. Four fuel assembly holddown springs are mounted to the top of the nozzle and fastened in place by bolts and clamps located at two diagonally opposite corners. Except for the screws and springs, which are lnconel, the top nozzle assembly is made from 304 stainless steel.

The 15 Upgrade fuel design utilizes a composite (cast) top nozzle. Compared to prior nozzle designs, the cast nozzle reduces the number of component parts required to fabricate and assemble a top nozzle assembly. Part count is reduced from twelve components to five components. The design includes a single casting that replaces the machined top plate/enclosure/pad weldment. The adapter plate, thimble hole and flow hole patterns remain unchanged from prior designs.

Cycle 27 implements the Westinghouse Integral Nozzle (WIN) top nozzle design, which is a direct replacement for the reconstitutable top nozzle (RTN) design. The WIN design incorporates design and manufacturing improvements to eliminate the Alloy 718 spring screw for attachment of the holddown springs. The springs are assembled into the nozzle pad and pinned in place. The WIN design provides a wedged rather than a clamped (bolted) joint for transfer of the fuel assembly holddown forces into the top nozzle structure. The flow plate, thermal characteristics, and method of attachment of the top nozzle are all unchanged from the RTN top nozzle design.

Evaluation

The Upgrade fuel top nozzles have a reconstitutable feature (Figure 3.5.1-4). In the reconstitutable top nozzle design, a stainless steel nozzle insert is mechanically connected to the top nozzle adapter plate by means of a preformed circumferential bulge near the top of the insert. The insert engages a mating groove in the wall of the adapter plate thimble tube through hole. The insert has four equally spaced axial slots that allow the insert to deflect inwardly at the elevation of the bulge, thus permitting the installation or removal of the nozzle. The insert bulge is positively held in the adapter plate mating groove by placing a lock tube with a uniform ID identical to that of the thimble tube into the insert.

To remove the top nozzle, a tool is first inserted through the lock tube and expanded radially to engage the bottom edge of the tube. An axial force is then exerted on the tool, which overrides the local lock tube deformations and withdraws the lock tube from the insert. After the lock tubes have been withdrawn, the nozzle is removed by raising it off the upper slotted ends of the nozzle inserts, which deflect inwardly under the axial lift load. With the top nozzle removed, direct access is provided for fuel rod examination or replacement. Reconstitution is completed by remounting the nozzle and inserting new lock tubes.

3.5.1.2.3 Bottom Nozzle

Description

The 15 Upgrade bottom nozzle is a machined and welded structure approximately 8.4 inches square by 2.7 inches in height. The bottom nozzle is made of 304 stainless steel, consisting of a top plate, containing flow holes, to which four (4) "legs" are welded; one at each corner. The bottom nozzle is the bottom structural member of the fuel assembly. The top plate portion of the bottom nozzle is designed to prevent the fuel rods from passing through, as well as to provide for coolant flow to be distributed toward the fuel assembly.

As part of its structural function, the guide thimble assemblies are attached to the bottom nozzle top plate, while the four corner legs rest on the lower core plate and support the entire fuel assembly. Two (2) of the bottom nozzle legs, located diagonally opposite, contain holes, which receive the fuel alignment pins that are mounted on the core plate. Additionally, a skirt plate is employed on all four sides to improve the structural integrity.

The 15 Upgrade bottom nozzle design has a reconstitutable feature, as shown in Figure 3.5.1-6, which allows it to be easily removed. A locking cup is used to lock the thimble screw of a guide thimble tube in place. The reconstitutable nozzle design facilitates remote removal of the bottom nozzle and relocking of thimble screws as the bottom nozzle is reattached.

Cycle 25 implements the Westinghouse modified Debris Filter Bottom Nozzle (mDFBN), which Westinghouse has developed for 15x15 fuel and is designed to have a loss coefficient that is the same, independent of supplier. The mDFBN has eliminated the side skirt communication flow holes as a means of improving the debris mitigation performance of the bottom nozzle. This nozzle has been extensively evaluated and analyzed, and it was demonstrated that it meets all of the applicable mechanical design criteria. In addition, specific testing was performed to demonstrate that there is no adverse effect on the thermal hydraulic performance of the mDFBN either with respect to the pressure drop or with respect to the DNB.

Evaluation

The bottom nozzle provides adequate distribution of reactor coolant flow to the entrance of the flow channels between the fuel rods. It accomplishes this without an unacceptable loss of pressure. The bottom nozzle also provides a filtering action that minimizes particles of debris that would cause fuel clad failure from entering the fuel flow channel.

3.5.1.2.4 Grids

Description

Two types of grid assemblies are used in each fuel assembly. Both types consist of individual slotted straps interlocked in an "egg-crate" arrangement. The straps contain springs, dimples, and mixing vanes. The mid grid is located in five (5) places per fuel assembly. These grids contain ZIRLO™ straps which are permanently joined by welding at their points of intersection. Their internal straps include mixing vanes, which project into the coolant stream and promote mixing of the coolant. The assembly also contains a top and bottom grid that are both made of Alloy-718 material. The material for these grids is chosen because of its corrosion resistance and high strength. Joining of the individual straps is achieved by brazing at the points of intersection. The top and bottom grids do not contain mixing vanes on their internal straps. The outside straps on all grids contain mixing vanes which, in addition to their mixing function, aid in guiding the grids and fuel assemblies past projecting surfaces during handling or during loading and unloading of the core. The individual grid cells at each fuel rod location provide six-point contact with the rod; four dimples and two springs.

In addition to the above, two other grid types are used in the core. These are the protective grid and the Intermediate Flow Mixer (IFM).

The protective grid is an lnconel grid which is used in the bottom span of the fuel assembly. The purpose of the protective grid is to provide an additional entrance barrier to debris entering the fuel assembly, especially when used in conjunction with the debris-resistant bottom nozzle.

Cycle 25 implements the Westinghouse Robust Protective Grid (RPG) which was developed as a result of observed failures in the field as noted in Post Irradiation Exams (PIE) performed at several different plants. It was determined that observed failures were the result of two primary issues:

1. Fatigue failure within the protective grid itself at the top of the end strap and
2. Stress Corrosion Cracking (SCC) primarily within the rod support dimples.

The RPG implements design changes such as increasing the maximum nominal height of the grid, increasing the ligament length and radii of the ligament cutouts, and the use of four additional spacers for a total of 8 spacers to help strengthen the grid. The nominal height of the grid was increased to allow "V-notch" window cutouts to be added to help minimize flow-induced vibration caused by vortex shedding at the trailing edge of the inner grid straps. These design changes incorporated into the RPG design help address the issues of fatigue failures and failures due to SCC. It was demonstrated that the above changes do not impact the thermal hydraulic performance of the RPG as there is no change to the pressure loss coefficient. In addition, the RPG retains the original protective grid function as a debris mitigation feature.

The IFMs are considered non-structural upper assembly grids which contain mixing vanes similar to the ZIRLO™ structural grids. The purpose of the IFM grids is to improve the fluid mixing which will give DNB margin for future DNB analyses.

The attachment of the five ZIRLO™ inner-grid and two lnconel end-grid assemblies to the guide thimble tubes is described in Section 3.5.1.2.1.

Evaluation

The fuel rods, as shown in Figure 3.5.1-1, are supported at intervals along their length by grid assemblies which maintain the lateral spacing between the rods. Each fuel rod is supported within each grid by the combination of support dimples and springs. The magnitude of the grid restraining force on the fuel rod is set high enough to minimize possible fretting, without overstressing the cladding at the points of contact between the grids and fuel rods. The grid assemblies also allow axial thermal expansion of the fuel rods without imposing restraint sufficient to develop buckling or distortion of the fuel rods.

Elevation of the grids was established to ensure axial match-up during operation. Impact tests have been performed at 600°F to obtain the dynamic strength data to verify that the ZIRLO™ grid strength at reactor operating conditions is acceptable. The ability of the grids to withstand seismic and LOCA impact loads is shown in Section 3.5.1.1.

3.5.1.3 Fuel Rods

The fuel rod consists of uranium dioxide ceramic pellets contained in a cold worked Zircaloy-4/ZIRLO® or partially annealed Optimized ZIRLO™ tubing which is plugged and seal welded at the ends to encapsulate the fuel. Beginning with Cycle 25, the fuel rod tubing is Optimized ZIRLO™. The fuel pellets are right circular cylinders consisting of slightly enriched uranium dioxide powder which has been compacted by cold pressing and then sintered to the required density. The ends of each pellet are dished slightly to allow greater axial expansion at the center of the pellets.

Void volume and clearances are provided within the rods to accommodate fission gases released from the fuel, differential thermal expansion between the cladding and the fuel, and fuel density changes during irradiation, thus avoiding overstressing of the cladding or seal welds. Shifting of the fuel within the cladding during handling or shipping prior to core loading is prevented by a stainless steel helical spring which bears on top of the fuel. At assembly, the pellets are stacked in the cladding to the required fuel height, the spring is then inserted into the top end of the fuel tube, and the end plugs are pressed into the ends of the tube and welded. All fuel rods are internally pressurized with helium during the welding process in order to minimize compressive cladding stresses and prevent cladding flattening due to coolant operating pressures. Nominal fuel rod parameters are given in Table 3.5.1-1.

A limited number of fuel rods may be replaced with substitutions of zirconium alloy, zircaloy-4, ZIRLO™, or stainless steel filler rods, in accordance with the NRC approved methodology in Reference 11.

Integral Fuel Burnable Absorber (IFBA)

The IFBA coating on the fuel pellets provides partial control of the excess reactivity available during the beginning of the fuel cycle. In doing so, the burnable absorber prevents the moderator temperature coefficient from violating safety limits at normal operating conditions. The burnable absorber performs this function by reducing the requirement for soluble poison in the moderator at the beginning of the fuel cycle as described previously. For purposes of illustration, a typical IFBA pattern in the core together with the number of IFBA rods per assembly are shown in Figure

3.5.2-1. The IFBA coating on the fuel is part length and can vary from cycle to cycle. The burnable absorber is part-length to allow a better (flatter) axial power distribution in the core. The ZrB2 coating on the fuel pellets is depleted with burnup, but at a sufficiently slow rate so that the resulting critical concentration of soluble boron is such that the moderator temperature coefficient remains within safety limits at all times for power operating conditions.

Axial Blankets

The fuel rods used in the 15 Upgrade fuel for Cook Nuclear Plant Unit 1 contain axial blankets.

The axial blankets are a nominal 6 inches of un-enriched or slightly enriched fuel pellets at each end of the fuel rod pellet stack. Axial blankets reduce neutron leakage and improve fuel utilization. The axial blankets utilize chamfered pellets which are physically different (length) than the enriched pellets to help prevent accidental mixing during manufacturing.

Axial blankets can consist of solid or annular pellets. Annular pellets provide additional void volume to provide increased rod internal pressure margin.

Design Bases

The fuel design bases and criteria for W 15x15 Upgrade fuel are discussed in Reference 13 and 14. The integrity of the fuel rods is ensured by designing to prevent excessive fuel temperatures, excessive internal rod gas pressures due to fission gas releases, and excessive cladding stresses and strains. This is achieved by designing the fuel rods so that the conservative design bases in the following subsections are satisfied during Condition I and II events over the fuel lifetime. For each design basis, the performance of the limiting fuel rod must not exceed the limits specified by the design basis. These design bases and criteria are applicable to both standard ZIRLO® and Optimized ZIRLO™ cladding (References 19 and 20) and are summarized below:

a. The cladding stresses under Condition I and II events are less than the ZIRLO™ 0.2% offset yield stress, with due consideration of temperature and irradiation effects, as discussed in References 5 and 14. While the cladding has some capability for accommodating plastic strain, the yield stress has been accepted as a conservative design basis.

b. Cladding Tensile Strain -The total tensile creep strain due to uniform clad creep and uniform cylindrical fuel pellet expansion associated with fuel swelling and thermal expansion is less than 1% from the un-irradiated condition (Reference 13). The elastic tensile strain during a transient is less than 1% from the pre-transient value. This limit is consistent with proven practice.

c. Strain Fatigue- The fatigue life usage factor is less than 1.0 (Reference 13). That is, for a given strain range, the number of strain fatigue cycles are less than those required for failure, considering a minimum safety factor of 2 on the stress amplitude or a minimum safety factor of 20 on the number of cycles, whichever is more conservative.

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

e. The rod internal gas pressure shall remain below the value, which causes the fuel cladding diametral gap to increase due to outward cladding creep during steady-state operation (Reference 5). Rod pressure is also limited such that extensive DNB propagation shall not occur during normal operation and accident events (Reference 5 and 14).

f. Cladding collapse shall be precluded during the fuel rod design lifetime. The models described in Reference 6 are used for this evaluation.

g. During modes of operation associated with Condition I and Condition II events, there is at least a 95 percent probability that the peak kW/ft fuel rods will not exceed the U02 melting temperature. The melting temperature of U02 is taken at 5080°F (Reference 4), un-irradiated and decreasing 58°F per 10,000 MWD/MTU. By precluding U02 melting, the fuel geometry is preserved and possible adverse effects of molten U02 on the cladding are eliminated. To preclude center melting, and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700°F has been selected as the overpower limit.

h. Design values for the properties of materials used for the fuel rod design and performance are given in Reference 4.

i. The peak fuel rod average burnup is cycle-specific. The fuel rod average burnup limit is 62,000 MWD/MTU (Reference 16), provided the evaluation of the fuel design performance is performed with PAD 4.0 (Reference17).

j. Manufacturing tolerances on the pre-stated parameters are considered in the design evaluation. The design also considers effects such as fuel density changes, fission gas release, clad creep, and other physical properties, which vary with burn up.

k. Extensive irradiation testing and fuel surveillance operational experience program are conducted to verify the adequacy of the fuel performance and design bases, such as the program discussed in Reference 13 and 15. Fuel surveillance and testing results, as they become available, are used to improve fuel rod design and manufacturing processes and assure that the design bases and safety criteria are satisfied.

Evaluation

The detailed Upgrade fuel rod design establishes such parameters as pellet size and density, cladding pellet diametral gap, gas plenum size, and helium pre-pressurization level. The design also considers effects such as fuel density changes, fission gas release, cladding creep, and other physical properties which vary with burn up. The integrity of the fuel rods is ensured by designing to prevent excessive fuel temperatures, excessive internal rod gas pressures due to fission gas releases, and excessive cladding stresses and strains. This is achieved by designing the fuel rods to satisfy the conservative design bases in the following subsections during Condition I and Condition II events over the fuel lifetime. For each design basis, the performance of the limiting fuel rod must not exceed the limits specified. NRC-approved fuel rod design models (References 7, 8, 9, and 17) are used to assure that design bases are satisfied and to predict fuel operating characteristics. Additional details which show that the design bases are satisfied are given in Reference 10. Also applicable are the fuel rod evaluations given in Section 3.2.1.3.1 of the Cook Nuclear Plant Unit 2 UFSAR. The impact of rod bow on DNBR penalties is discussed in Section 3.5.3.

The wear of fuel rod cladding is dependent on both the support provided by the grids and the flow environment to which it is subjected. Tests have been conducted on the 15 Upgrade fuel to investigate the integrity and wear performance. The assemblies were tested for 500 hours with an average bundle velocity of 17.96 ft/s. The results predicted a worst case linearly projected time to critical wear volume of 1937 days (Reference 12).

3.5.1.4 Core Components

The core components consist of the rod cluster control assemblies (RCCAs), the primary and secondary source assemblies and may contain the thimble plug assemblies. The design of the control rod assemblies in the Cook Nuclear Plant Unit 1 core has remained essentially unchanged. Enhanced performance rod cluster control assemblies (EP-RCCAs), which use silver-indium-cadmium (Ag-In-Cd), are utilized with the standard RCCAs. These have a thin chrome electroplate applied over the length of absorber rodlet cladding in contact with the reactor internal guides to provide increased resistance to cladding wear. In addition, the absorber diameter is reduced slightly at the lower extremity of the rodlets in order to accommodate absorber swelling and minimize cladding interaction. The absorber rod cladding material is a very high purity 10% cold worked type 304 stainless steel tubing.

The RCCA scram time is 2.4 seconds which was used in all accident analyses.

All thimble plug assemblies have been removed from the core, but may be reinserted for use in future cycles.

The Upgrade assemblies, their thimble plugging devices, and source assemblies are compatible with existing handling tools.

Wet Annular Burnable Absorber (WABA)

The Wet Annular Burnable Absorber (WABA) rod design is sometimes used in the Cook Nuclear Plant Unit 1 reload cores. The materials, mechanical, thermal hydraulic, and nuclear design evaluations of the WABA rods are presented in a topical report (Reference 3). This report has received NRC generic approval and approval for Cook Nuclear Plant Unit 1 application of WABAs.

The WABA design has annular aluminum oxide - boron carbide (AI203 - B4C) absorber pellets contained within two concentric Zircaloy tubes with water flowing through the center tube as well as around the outer tube. The WABA design provides significantly enhanced nuclear characteristics, when compared with the W borosilicate absorber rod design. Fuel cycle benefits result from the reduced parasitic neutron absorption of Zircaloy compared to stainless steel tubes, increased water fraction in the burnable absorber cell, and a reduced boron penalty at the end of each cycle.

Figures 3.5.1-7 and 3.5.1-8 show the design of a WABA rod, and Table 3.5.1-2 and Figure 3.5.1-8 present a comparison between the WABA rod and a W borosilicate glass absorber rod.

The WABA rods inserted into each fuel assembly are attached at their top ends to a holddown assembly and retaining plate in the same manner as burnable absorber rods previously used in Cook Nuclear Plant Unit 1 reload cores.

Based on the materials and design evaluations in Reference 3, it is concluded that the wet annular burnable absorber rod satisfies all performance and design requirements for 18,000 effective-full-power-hours irradiated life.

3.5.1.5 PERFORMANCE + Debris Mitigation Features

The following PERFORMANCE + fuel features are included in the 15 Upgrade fuel design.

1. Protective fuel rod oxide coating
2. Debris mitigating bottom end plug
3. Protective grid
4. Variable pitch plenum spring
5. Debris filter bottom nozzle

Pre-Oxidized Fuel Rod Cladding

The ZIRLO®/Optimized ZIRLO™ fuel rod cladding is pre-oxidized on the bottom 7" (beginning with the bottom end plug) for debris fretting resistance and fuel rod reliability. This fuel feature consists of a typically 3 to 6 micron Zirconium Oxide coating which is thermally grown on the cladding as part of the fuel rod manufacturing process. The coating is applied to the fuel rod at a location below the bottom lnconel structural grid and as such provides for an increase in the resistance to debris damage in this region.

Debris Mitigating Bottom End Plug

A 0.81" long, debris-mitigating fuel rod bottom end plug is used with the protective grid, and the rods have been positioned close (0.065" gap) to the bottom nozzle at the beginning of life (BOL). The active fuel stack length remains at 144".

Protective Grid (PG)

A protective grid has been added at the bottom of the assembly to provide an additional debris barrier thereby improving fuel reliability. The protective grid will also provide additional fretting resistance by supporting the bottom of the fuel rod. This feature is designed to enhance the debris mitigation performance of the fuel and consists of an additional "thin" lnconel grid positioned directly above the bottom nozzle.

Rods are positioned close to the bottom and are modified, as discussed below, with a slightly longer bottom end plug. This grid provides added protection against debris induced fretting by trapping debris below this grid where it can wear against the solid end plug. In addition, the grid provides improved resistance to grid-rod fretting by means of the additional support at the bottom of the rod.

Variable Pitch Plenum Spring

Because of the long fuel rod end plugs and resulting decrease in internal rod void volume, a PERFORMANCE+ variable pitch (VP) plenum spring was designed for fuel rods utilizing the protective grid package. The VP spring has longer coil lengths in the middle section of the spring. The VP spring continues to meet all design and manufacturing criteria while regaining some of the plenum volume lost to the longer end plugs.

Debris Filter Bottom Nozzle

The PERFORMANCE+ package for the 15x15 fuel assembly design included a Debris Filter Bottom Nozzle (DFBN). The DFBN was designed to inhibit debris from entering the active fuel region of the core and thereby improved fuel performance by minimizing debris related fuel failures. The flow hole pattern for the DFBN aligns with the protective grid straps to achieve the debris resistance capability. Mechanical testing was performed to show that design criteria are maintained under Condition I, II, Ill, and IV structural loads, as well as shipping and handling loads and abnormal handling (single leg) load conditions. Beginning in Cycle 25, the modified Debris Filter Bottom Nozzle (mDFBN) was introduced (see Section 3.5.1.2.3 for details).

3.5.1.6 Intermediate Flow Mixer Grids

All fresh fuel assemblies for Cook Unit 1 are fabricated with Intermediate Flow Mixer (IFM) grids. IFM grids are considered non-structural upper assembly grids which contain mixing vanes similar to structural grids. Specifically, the IFM grids are located in the top three mixing vane grid spans. Based on a proven design, the 15x15 IFM grids have less than one third the mass of the current 15x15 structural grids. These IFM grids will have virtually no effect on neutron economy.

EVALUATIONS

3.5.1.6.1 Structural Evaluation

Design changes associated with the 15x15 upgrade design do not significantly influence the fuel assembly structural characteristics that were determined by prior mechanical testing. The functional requirements and design criteria are evaluated in Reference 10. It is shown that the 15x15 upgrade fuel exhibits expected structural behavior and projected performance and will meet design requirements throughout the fuel's life.

3.5.1.6.2 Reactor Vessel and Internals Evaluation

The following aspects of the reactor internals system were reviewed and found acceptable:

- RCCA drop time

- Core bypass flow
- LOCA core plate motions

The RCCA drop time Technical Specification limit of 2.4 seconds remains applicable.

The design core bypass flow value of either 6.6 percent of the total vessel flow with thimble plugging devices installed or 8.6 percent of the total vessel flow with thimble plugging devices removed is maintained with the Upgrade fuel.

The core plate motions are analyzed and discussed in Reference 22. The resulting vertical impact loads and the lateral deflections are within acceptable limits. Based on this result, it is concluded that the 15x15 upgrade fuel assembly design is structurally acceptable for use in D. C. Cook Unit 1.

3.5.1.6.3 Loss of Coolant Accident Evaluation

3.5.1.6.3.1 Small Break LOCA

The Small Break LOCA analysis assumes upgrade fuel as indicated in Table 3-1 of Reference 18.

3.5.1.6.3.2 LOCA Related Issues

3.5.1.6.3.2.1 LOCA Forces

Grid impact forces of the 15x15 Upgrade fuel assemblies, considering the effects of upflow conversion, acceptable baffle bolting pattern analysis, and updated stiffness effects relative to NSAL-11-2, resulting from seismic (DBE) and LOCA core plate motions were evaluated in Reference 22. Grid impact forces of the 15x15 Upgrade fuel assemblies resulting from OBE seismic events were evaluated in Reference 10. The results show that the all impact forces resulting from all load cases are well below the grid impact force allowable limits. The summary of the maximum grid impact forces are listed in Table 3.5.1-3.

Based on the vertical impact load and the maximum lateral fuel assembly deflection, the thimble tube and fuel rod stresses were calculated. The results indicated that all the stresses are below the allowable limits. The summary results are listed in Table 3.5.1-3a.

3.5.1.6.3.2.2 Post- LOCA (Hot Leg Switchover and Subcriticalityl

The primary factors affecting the calculations for Hot Leg Switchover (HLSO) and subcriticality are power level (HLSO only), RCS, RWST and accumulator water volumes, and boron concentrations. The use of the 15x15 upgrade fuel does not result in changes to these parameters. The evaluation in Reference 10 demonstrates that the 15x15 upgrade fuel has no impact on the post-LOCA calculations and all pertinent 10 CFR 50.46 criteria continue to be met.

3.5.1.6.4 Transient (non-LOCA) Evaluation

The following potential impacts were evaluated and the design criteria were met (Reference 10):

- Control rod drop time
- Core thermal limits I axial offset limits
- Core bypass flow fraction
- Core pressure drop (ΔP)

3.5.1.7 References for Section 3.5 and 3.5.1

1. Barsic, J.A., Garner, D.C., Lee, Y.C., Neubert, K.B., Yu, C., "Control Rod Insertion Following a Cold Leg LB LOCA D.C. Cook Units 1 and 2," WCAP-15245-P, May 28, 1999.
2. Bordelon, F. M., et. al., "Westinghouse Reload Safety Evaluation Methodology," WCAP-9273 (Non-Prop.), March 1978.
3. Skaritka, J., et. al., "Westinghouse Wet Annular Burnable Absorber Evaluation Report," WCAP-10021-P-A, Revision 1 (Proprietary), October 1983. Contains NRC SER dated August 9, 1983 and W responses to NRC questions.
4. Beaumont, M. D., lorii, J. A, (Ed.), Properties of Fuel and Core Component Materials," WCAP-9719, Revision 1 (Proprietary) and WCAP-9224, July, 1978; Appendix B, AL20 3-B4C Pellets, issued October 1980; Revised pages issued September 1982.
5. D. H. Risher, Ed., "Safety Analysis for the Revised Fuel Rod Internal Pressure Design Basis," WCAP-8963 (Proprietary), November 1976.
6. George, R. A., et. al., "Revised Clad Flattening Model," WCAP-8377 (Proprietary) and WCAP-8381 (Non-Proprietary), July 1974.
7. J. V. Miller, Ed., "Improved Analytical Models Used in Westinghouse Fuel Rod Design Computations," WCAP-8720 (Proprietary) and WCAP-8785 (Non-Proprietary) dated October 1976.
8. W. J. Leech, "Improved Analytical Models Used in Westinghouse Fuel Rod Design Computations- Application for Transient Analyses," WCAP-8720, Addendum 1 (Proprietary) dated September 1979.

9. Weiner, R. A, et. al., "Improved Fuel Performance Models for Westinghouse Fuel Rod Design and Safety Evaluations," WCAP-11873-A, August 1988.

10. "D.C. Cook Unit 1 15x15 Upgrade Fuel Project Engineering Report," WCAP-16586-P, September 2006.

11. Slagle, W. H. (ed.), "Westinghouse Fuel Assembly Reconstitution Evaluation Methodology," WCAP-13060-P-A, July 1993.

12. McRae O., Sparrow, J., Smith, G., "Revision 1 to 15x15 Upgrade Fuel Assembly-Design Closeout Package", MD2-04-25, May 2004.

13. Davidson, S. L., (Ed.) et al, “Extended Burnup Evaluation of Westinghouse Fuel,” WCAP-10125-P-A, December, 1985.

14. Davidson, S. L., et.al. (Ed.), WCAP-12488-A, "Westinghouse Fuel Criterion Evaluation Process," October, 1994.

15. Eggleston, F. T., "Safety-Related Research and Development for Westinghouse Pressurized Water Reactors, Program Summaries," WCAP-8768, Latest Revision.

16. Letter from J.D. Peralta (USNRC) to B.F.Maurer (Westinghouse), "Approval for Increase in Licensing Burnup Limit to 62,000 MWD/MTU (TAG No. MD1486)," May 25, 2006.

17. Slagle, W. H., (Ed.) et. al., WCAP-15063-P-A, Revision 1, "Westinghouse Improved Performance Analysis and Design Model (PAD 4. 0)," July 2000.

18. "D.C. Cook Unit 1 Small Break Loss-of-Coolant Accident Reanalysis Engineering Report," WCAP-17563-P, September 2012.

19. Shah, H. H., "Optimized ZIRLO™ WCAP-12610-P-A & CENPD-404-P-A Addendum 1-A," July, 2006.

20. Tam, P.S., "Environmental Assessment and Finding of No Significant Impact Related to a Proposed Exemption to 10 CFR 50.46 and a Proposed Amendment to Extend the Fuel Burn up Limit," August 11, 2011.

21. Szweda, N. S., “Determination of Acceptable Long-Term Replacement Baffle-Former Bolting Patterns for D. C. Cook Unit 1,” WCAP-18260-P Rev. 1 January 2019.

22. Good, B.F., "Reactor Internals Upflow Conversion Program Engineering Report For Donald C. Cook Nuclear Generating Station Unit 1," WCAP-18346-P, Rev. 0, November 2018.

3.5.2 Nuclear Design

The nuclear design of cores with W 15x15 upgrade fuel is accomplished by using the standard calculational methods as described in the W Reload Safety Evaluation Methodology. In addition to Westinghouse's standard methods, the Westinghouse Advanced Nodal Code (ANC) (Reference 6) was introduced in Cycle 11 to perform core neutronics analyses and the PHOENIX-P code (Reference 3) was introduced in Cycle 12 to calculate lattice physics constants.

Each reload core design is evaluated to assure that design and safety limits for the fuel are satisfied according to the W reload safety evaluation methodology. For the evaluation of the worst-case $F_Q(Z)$ envelope, axial power shapes are synthesized with the limiting F_{xy} values chosen over three overlapping burn up windows during the cycle.

In order to accommodate potential increases in future feed assembly enrichments, criticality analyses of the fuel storage areas were performed to cover W 15x15 upgrade fuel up to and including 4.95 wt.% U-235 for the new fuel storage vault and the spent fuel pool. These analyses provide specific requirements needed to satisfy all current safety criteria applicable to fuel storage (Reference 2, Reference 8).

3.5.2.1 Computerized Methods, Codes and Cross Section Data

Three principal computer codes have been used in the nuclear design of reactor cores with W 15x15 upgrade fuel; these are PHOENIX-P (two-dimensional), APOLLO (one-dimensional), and ANC (two-dimensional and three-dimensional). Descriptions and uses for these codes follow.

PHOENIX-P (Reference 3) is a two-dimensional multi-group transport theory code used to calculate lattice physics constants. Microscopic cross section data are based on a 70-energy group structure that has been derived from ENDF/B-VI files (Reference 4). It provides the capability for cell lattice modeling on an assembly level. In the core design, PHOENIX-P is used to provide homogenized, two-group cross-sections for nodal calculations and feedback models. It is also used in a special geometry to generate appropriately weighted constants for the baffle/reflector regions.

APOLLO, an advanced version of PANDA (Reference 5), is a two-group, one-dimensional diffusion-depletion code. APOLLO utilizes the burnup dependent radially averaged macroscopic cross sections of the corresponding 3-D model. The APOLLO model is used as an axial model. APOLLO is utilized to determine axial power and burnup distributions, differential rod worths, and control rod operational limits (insertion limits, return to power limits, etc.).

ANC (Reference 6) is an advanced nodal code that is used in two-dimensional and three-dimensional calculations. ANC calculations include power and burnup distributions, critical boron concentrations, reactivity coefficients, control rod worths, and various safety analysis calculations. ANC is used to validate one-dimensional results from APOLLO and to provide information about radial (X-Y) peaking factors as a function of axial position. ANC also has the capability of calculating discrete pin powers and pin burnups from the nodal information.

Additional support codes are used for special calculations such as determining fuel temperatures.

3.5.2.2 Neutronic Design of Cook Nuclear Plant Unit 1 Reactor Core

3.5.2.2.1 Analytical Input

The neutronics design methods utilized to calculate the data presented herein are consistent with those described previously with primary reliance upon the ANC code.

For each cycle, the burn up history of each of the fuel assemblies retained from previous cycles for further energy production is calculated by a three-dimensional model which is utilized to simulate operation of the core for previous cycles.

As an example, Cycle 22 core calculations used assembly exposures calculated from the Nominal Cycle 21 burn up of 18,351 MWD/MTU.

3.5.2.2.2 Design Bases

For each cycle, the nuclear design bases are very similar to those for the example Cycle 22 core as follows:

1. At core full power, 3,304 MWt (not including pump heat), nuclear peaking factors of 2.15 and 1.55 for F_Q^T and $F_{\Delta H}^N$ respectively, will not be exceeded. In addition, at any relative power level P ($0.0 \le P \le 1.0$), F_Q^T and $F_{\Delta H}^N$ shall not exceed the bases of the plant control and protection system.
2. The moderator temperature coefficient at operating conditions greater than 70% power level is a ramp function limited to +5.0 pcm/°F at 70% power and 0.0 pcm/°F

at 100% power. Below 70% power level, the moderator temperature coefficient shall be less than +5.0 pcm/°F.

3. With the most reactive control rod stuck out of the core, the remaining control rods shall be able to shut the reactor down by a sufficient reactivity to reduce the consequences of any credible accident to acceptable levels.

4. The effects of all accident situations in Cycle 22 will be acceptable and compatible with the safety bases of the Final Safety Analysis Report (FSAR), as specified in Reference 7.

5. The fuel loading specified shall be capable of generating approximately 19,430 MWD/MTU at normal full power operating conditions during Cycle 22.

3.5.2.2.3 Design Description and Results

Each cycle's reactor core consists of 193 W 15x15 upgrade assemblies, each having a 15x15 fuel rod array. A description of the W 15x15 upgrade assemblies is given in Section 3.5.1.

As an example, the Cycle 22 loading pattern is given in Figure 3.5.2-1, which shows the assembly IDs and corresponding region numbers and enrichments. The core consists of 48 fresh W 15x15 upgrade assemblies with a nominal enrichment of 3.800 w/o U-235, 36 fresh W 15x15 upgrade assemblies with a nominal enrichment of 4.200 w/o U-235, 25 fresh W 15x15 upgrade assemblies with a nominal enrichment of 1.500 w/o U-235, 1 fresh W 15x15 upgrade assembly with a nominal enrichment of 1.600 w/o U-235, and 83 once-burnt W 15x15 upgrade assemblies. A low leakage loading pattern was developed, which results in the scatter-loading of the fresh W 15x15 upgrade assemblies throughout the core. The Cycle 22 loading pattern contains 6,608 new IFBA rods in 76 fresh W 15x15 upgrade assemblies to control power peaking and MTC. Pertinent fuel assembly parameters for the Cycle 22 fuel are given in Tables 3.5.1-1 and 3.5.2-1.

Physics Characteristics

The neutronics characteristics of a reactor core with W 15x15 upgrade fuel are presented in Table 3.5.2-2. These reactivity coefficients are bounded by the coefficients used in the safety analysis. For an example cycle length, Cycle 22 was projected to be 19,430 MWD/MTU at a core power of 3,304 MWt with ~ 16 ppm soluble boron remaining.

Power Distribution Considerations

Figure 3.5.2-2 shows the K(Z) function (fuel height limit for normalized $F_Q(Z)$). Each cycle's core loading satisfies the envelope shown in Figure 3.5.2-2.

Control Rod Reactivity Requirements

The Cook Nuclear Plant Unit 1 Technical Specifications require a minimum shutdown margin of greater than or equal to 1.3% Δk/k in operational Modes 1, 2, 3 and 4 and greater than or equal to 1.0% Δk/k in operational Mode 5 at BOC and EOC. As an example, detailed calculations of shutdown margins for Cycle 22 are presented in Table 3.5.2-3. The Cycle 22 analysis indicates excess shutdown margin of 2,792 pcm at BOC and 2,137 pcm at EOC.

Insertion limits are specified for the control rod groups and are given in the Core Operating Limits Report, as described in Technical Specification 6.9.1.11. The control rod shutdown requirements allow for a HFP D-Bank insertion to rod insertion limits. Table 3.5.2-3 gives the shutdown requirements for the example of Cycle22.

Moderator Temperature Coefficient

Core loadings must satisfy the Technical Specifications requirements that the moderator temperature coefficient be less than or equal to +5 pcm / °F below 70% of rated thermal power and less than or equal to a linear ramp between +5 pcm / °F at 70% power and 0 pcm / °F at 100% power.

3.5.2.3 References for Section 3.5.2

1. Davidson, S. L. (Ed), et al., "Westinghouse Reload Safety Evaluation Methodology," WCAP-9272-P-A, July 1985.
2. Alexich, M. P. to Murley, T. E., "Proposed Units 1 and 2 License Conditions and Technical Specifications Changes for Unit 2 Cycle 8 Spent Fuel Pool and New Fuel Storage Vault," AEP:NRC:1071F, December 8, 1989.
3. Nguyen, T. Q., et. al., "Qualification of the PHOENIX-P/ANC Nuclear Design System for Pressurized Water Reactor Cores, WCAP-11596-P-A, June 1988.
4. McLane, V., et al, "ENDF/B-VI Summary Documentation.", BNL-NCS-17541, December 1996.
5. R. F. Barry, C. C. Emery, and T. D. Knight, "The PANDA Code," WCAP-7048, (April 1967).
6. Liu, Y. S., et al., "ANC - A Westinghouse Advanced Nodal Computer Code," WCAP-10966-NP-A, (September 1986).

7. Donald C. Cook Nuclear Plant Unit 1 Cycle 22, Final Reload Evaluation Report, Rev.2 (January 2009)

8. Letter from J. F. Stang , NRC, to R. P. Powers, I&M, “Issuance of Amendment 239 and 220 Re: Fuel Rod ZIRLO Cladding and Integral Fuel Burnable Absorber Requirements (TAC Nos. MA 7041 and MA 7042),” dated January 6, 2000.

3.5.3 Thermal and Hydraulic Design for Cores Containing 15x15 Upgrade Fuel

Introduction

This section describes the thermal and hydraulic design of Cook Nuclear Plant Unit 1 core with Westinghouse 15x15 Upgrade fuel.

The thermal hydraulic design of the core supports 3304 MWt core power with a 575.4°F vessel average temperature. The analyses employ the Revised Thermal Design Procedure (Reference 1) (RTDP) and VIPRE-01 (References 19 and 20) computer code. The WRB-1 (Reference 4) DNB correlation was used in the Westinghouse analyses of the 15x15 Upgrade fuel.

Summary

The design method employed to meet the DNB design basis is the RTDP (Reference 1). Uncertainties in plant operating parameters, nuclear and thermal parameters, fuel fabrication parameters, computer codes and DNB correlation uncertainties are considered statistically, such that there is at least a 95 percent probability at a 95 percent confidence level that the minimum DNBR will not occur on the most limiting fuel rod during normal operation and operational transients and during transient conditions arising from faults of moderate frequency (Condition I and II events as defined in ANSI N18.2). These uncertainties are used to determine the DNBR Design Limits which must be met in plant safety analyses. Since the parameter uncertainties are considered in determining the DNBR Design Limit value, the plant safety analyses are performed using values of input parameters without uncertainties. In addition, the DNBR Design Limit values are increased to values designated as the Safety Analysis Limit DNBRs. The plant allowance (DNB margin) available between the Safety Analysis Limit DNBR values and the Design Limit DNBR values is not required to meet the design basis, but is used to offset DNB penalties.

In this application, the WRB-1 DNB correlation (Reference 4) is employed in the thermal hydraulic design of the Westinghouse 15x15 upgrade (with IFMs) fuel. Due to an improvement in the accuracy of the critical heat flux prediction with the WRB-1 correlation compared to previous DNB correlations, a correlation limit DNBR of 1.17 is applicable.

The table below shows the relationships which exist between the correlation limit DNBR, Design Limit DNBR, and the Safety Analysis Limit DNBR values used for this design, using the Westinghouse Revised Thermal Design Procedure (RTDP) (Reference 1). These limits can be found in Reference 9.

	Typical	Thimble
Correlation Limit	1.17	1.17
Design Limit	1.21	1.21
Safety Analyses Limit	1.55	1.55

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

For events where conditions fall outside the range of applicability of the WRB-1 correlation, the W-3 (References 5, 6) correlation is used.

The available DNB margin between the DNBR Design Limits and Safety Analysis Limits is more than sufficient to cover the maximum rod bow penalty at full flow conditions.

3.5.3.1 Design Bases (Upgrade)

The overall objective of the thermal and hydraulic design of the reactor core is to provide adequate heat transfer compatible with the heat generation distribution in the core and with the heat removal capability of the reactor coolant system (or the emergency core cooling system when applicable). The thermal and hydraulic design assures that the following performance and safety criteria requirements are met:

1. Fuel damage (defined as penetration of the fission product barrier, i.e., the fuel rod cladding) is not expected during normal operation and operational transients (Condition I) or any transient conditions arising from faults of moderate frequency

(Condition II). It is not possible, however, to preclude a very small number of rods damaged. These will be within the capability of the plant cleanup system and are consistent with the plant design bases.

2. The reactor can be brought to a safe state following a Condition III event with only a small fraction of fuel rods damaged (see above definition - Item 1) although sufficient fuel damage might occur to preclude resumption of operation without considerable outage time.

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

In order to satisfy the above requirements the following design bases have been established for the thermal and hydraulic design of the reactor core:

Departure from Nucleate Boiling Design Basis (Upgrade)

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

Fuel Temperature Design Bases (Upgrade)

During modes of operation associated with Condition I and Condition II events, the maximum fuel temperature for at least 95 percent of the peak kW/ft fuel rods will not exceed the UO_2 melting temperature with 95 percent confidence. Melting temperature of UO_2 is taken at 5080ºF unirradiated and decreasing by 58ºF per 10,000 MWD/MTU (See Section 3.5.1.3.g). To preclude center melting and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700ºF has been selected as the overpower limit. This provides sufficient margin for uncertainties in the thermal evaluation.

Fuel rod thermal evaluations are performed at rated power, maximum overpower and during transients at various burnups. These analyses assure that this design basis as well as the fuel integrity design bases given in Section 3.5.1.3 are met. They also provide input for the evaluation of Condition III and IV faults given in Chapter 14.

Core Flow Design Bases (Upgrade)

A minimum of 91.4 percent of the thermal flow rate will pass through the fuel rod region of the core and be effective for fuel rod cooling. Coolant flow through the thimble tubes as well as the leakage from the core barrel-baffle region into the core are not considered effective for heat removal.

Core cooling evaluations are based on the thermal flow rate (minimum flow) entering the reactor vessel. A maximum of 8.6 percent of this value is allotted as bypass flow. This includes RCC guide thimble cooling flow, head cooling flow, cavity flow, baffle leakage, and leakage to the vessel outlet nozzle.

The RTDP design of Cook Nuclear Plant Unit 1 supports a minimum measured flow of 362,900 gpm.

Hydrodynamic Stability Design Bases (Upgrade)

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

Other Considerations (Upgrade)

The above design bases together with the fuel cladding and fuel assembly design bases given in Section 3.5.1 are sufficiently comprehensive so additional limits are not required.

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

Meeting the fuel cladding integrity criteria covers possible effects of cladding temperature limitations. As noted in Section 3.5.1.3, the fuel rod conditions change with time. A single cladding temperature limit for Condition I or Condition II events is not appropriate since of necessity it would be conservative. An appropriate cladding temperature limit is applied in each case to the following Condition IV events: loss of coolant accident (Sections 14.3.1, 14.3.2, and 14.3.9), control rod ejection accident (Section 14.2.8), and locked rotor accident (Section 14.1.6).

3.5.3.2 Fuel and Cladding Temperatures (Upgrade)

Consistent with the thermal-hydraulic design bases described in the previous section, the following discussion pertains mainly to fuel pellet temperature evaluation. A discussion of fuel cladding integrity is presented in Section 3.5.1.3.

The thermal-hydraulic design assures that the maximum fuel temperature is below the melting point of UO_2. To preclude center melting and as a basis for overpower protection system setpoints, a calculated centerline fuel temperature of 4700°F has been selected as the overpower limit. This provides sufficient margin for uncertainties in the thermal evaluation. The temperature distribution within the fuel pellet is predominantly a function of the local power density and the UO_2 thermal conductivity. However, the computation of radial fuel temperature distributions combines crud, oxide, cladding, gap and pellet conductances. The factors which influence these conductances, such as gap size (or contact pressure), internal gas pressure, gas composition, pellet density, and radial power distribution within the pellet, etc., have been combined into a semi-empirical thermal model which is employed when generating fuel temperatures for use in non-LOCA safety analyses (References 7 and 8). Fuel temperature related input from PAD 4.0, Reference 18, was used for LOCA analyses.

As described in Section 3.5.1.3, fuel rod thermal evaluations (fuel centerline, average, and surface temperatures) are determined throughout the fuel rod lifetime with consideration of time dependent densification. To determine the maximum fuel temperatures, various burnup rods, including the highest burnup rod, are analyzed over the rod linear power range of interest.

3.5.3.3 Calculational Methods (Upgrade)

Three calculation methods are employed: (1) the use of the VIPRE-01 computer code, (2) the Revised Thermal Hydraulic Design Procedure (RTDP), (Standard Thermal Design Procedure (STDP) where RTDP is not applicable) and (3) use of the WRB-1 DNB Correlation for treatment of the 15x15 Upgrade fuel. (W-3 DNB Correlation where WRB-1 is not applicable).

The minimum DNBRs for the rated power, design overpower and anticipated transient conditions are given in Table 3.5.3-1. The core average DNBR is not a safety related item as it is not directly related to the minimum DNBR in the core, which occurs at some elevation in the limiting flow channel. Similarly, the DNBR at the hot spot is not directly safety related. The minimum DNBR in the limiting flow channel will be downstream of the peak heat flux location (hot spot) due to the increased downstream enthalpy rise.

DNBRs are calculated by using the correlation and definitions described in the following sections. The VIPRE-01 computer code is used to determine the flow distribution in the core and the local conditions in the hot channel for use in the DNB correlation.

3.5.3.3.1 Critical Heat Flux Ratio or Departure from Nucleate Boiling Ratio and Mixing Technology (Upgrade)

The minimum DNBRs for the rated power, design overpower and anticipated transient conditions are given in Table 3.5.3-1. The minimum DNBR in the limiting flow channel is usually downstream of the peak heat flux location (hotspot) due to the increased downstream enthalpy rise. DNBRs are calculated using the correlation and definitions described below.

The WRB-1 (Reference 4) correlation was developed based exclusively on the large band of mixing vane grid rod bundle CHF data (over 1100 points) that Westinghouse has collected. The WRB-1 correlation, based on local fluid conditions, represents the bundle data with better accuracy over a wide range of variables than the previous correlation used in design. This correlation accounts directly for both typical and thimble cold wall cell effects, uniform and non-uniform heat flux profiles, and variations in rod heated length and in grid spacing.

The applicable range of variables is:

Pressure	:	$1440 \leq P \leq 2490$ psia
Local Mass Velocity	:	$0.9 \leq G_{loc} / 10^6 \leq 3.7$ lb/ft^2-hr
Local Quality	:	$-0.2 \leq x_{loc} \leq 0.3$[1]
Heated Length, Inlet to CHF Location	:	$L_h \leq 14$ feet
Grid Spacing	:	$13 \leq g_{sp} \leq 32$ inches
Equivalent Hydraulic Diameter	:	$0.37 \leq d_e \leq 0.60$ inches
Equivalent Heated Hydraulic Diameter	:	$0.46 \leq d_h \leq 0.58$ inches

Figure 3.5.3-1 shows measured critical heat flux plotted against predicted critical heat flux using the WRB-1 correlation.

Definition of Departure from Nucleate Boiling

The DNBR as applied to this design for both typical and thimble cold wall cells is:

[1] A penalty is applied when local quality is ≥0.25 per Westinghouse Letter AEP-15-59 (Reference 24).

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

Where:

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

and $q''_{DNB,\ EU}$ is the equivalent uniform critical heat flux as predicted by the WRB-1 Correlation (Reference 4); q''_{loc} is the actual local heat flux.

F is the flux shape factor to account for non-uniform axial heat flux distributions (Reference 12).

Mixing Technology (Upgrade)

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

$$TDC = \frac{w'}{\rho Va}$$

Where:

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

ρ= fluid density, lb/ft^3.

V= fluid velocity, ft/sec.

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

The application of the TDC in the VIPRE-01 analysis for determining the overall mixing effect or heat exchange rate is presented in Reference 19.

TDC is determined by comparing the computer code predictions with the measured subchannel exit temperatures. Data for 20 and 26 inch axial grid spacing have been evaluated by plotting thermal diffusion coefficient versus the Reynolds number (Figure 3.5.3-2 plots results for 26 inch grid spacing). TDC is found to be independent of Reynolds number, mass velocity, pressure and quality over the ranges tested. The two phase data (local, subcooled boiling) fell within scatter of the single phase data. The effect of two-phase flow on the value of TDC has been demonstrated by Cadek (Reference 13), Rowe and Angle (References 14, 15), and Gonzalez-Santalo and Griffith (Reference 16). In the subcooled boiling region the values of TDC were indistinguishable from the single phase values. In the quality region, Rowe and Angle (References 14,15) show that in the case with rod spacing similar to that in PWR reactor core geometry, the value of TDC increased with quality to a point and then decreases, but never below the single phase value. Gonzalez-Santalo and Griffith showed that the mixing coefficient increased as the void fraction increased. The data from these tests indicate an appropriate value of TDC for the current design of 0.038 (Reference 13).

3.5.3.3.2 VIPRE-01 and RTDP (Upgrade)

The VIPRE-01 computer program was used to perform the thermal/hydraulic calculations. The VIPRE-01 code is used to calculate coolant density, mass velocity, enthalpy, void fractions, static pressure, and DNBR distributions along flow channels within a reactor core under all expected operating conditions. References 19 and 20 contain details of the VIPRE-01 computer program, including models and correlations used.

The design method employed to meet the DNB design basis is the Revised Thermal Design Procedure (Reference 1). Uncertainties in plant operating parameters, nuclear and thermal parameters, fuel fabrication parameters, computer codes and DNB correlation uncertainties are considered statistically, such that there is at least a 95 percent probability at a 95 percent confidence level that the minimum DNBR will not occur on the most limiting fuel rod during normal operation and operational transients and during transient conditions arising from faults of moderate frequency (Condition I and II events as defined in ANSI N182). These uncertainties are used to determine the DNBR Design Limits which must be met in plant safety analyses. Since the parameter uncertainties are considered in determining the DNBR Design Limit value, the plant safety analyses are performed using values of input parameters without uncertainties.

In addition, for the Unit 1 plant, the limit DNBR values are increased to values designated as the Safety Analysis Limit DNBRs. The plant allowance available between the Safety Analysis Limit DNBR values and the Design Limit DNBR values is not required to meet the design basis. This allowance will be used to offset DNB penalties.

For this design, the WRB-1 correlation is used for the Westinghouse 15x15 Upgrade fuel with a correlation limit of 1.17 (both Typical and Thimble cells), a Design Limit of 1.21 for Typical cells and 1.21 for Thimble cells, and Safety Analysis Limit of 1.55 for Typical cells and 1.55 for Thimble cells.

3.5.3.4 Rod Bow (Upgrade)

The phenomenon of fuel rod bowing, as described in Reference 21, must be accounted for in the DNBR safety analysis of Condition I and Condition II events for each plant application.

The maximum rod bow DNBR penalty accounted for in the design safety analysis is based on an assembly average burn up of 24,000 MWD/MTU. At burn ups greater than 24,000 MWD/MTU, credit was taken for the effect of $F_{\Delta H}$ burn down, due to the decrease in fissionable isotopes and the buildup of fission product inventory, and no additional rod bow penalty is required (Reference 22).

In the upper spans of the 15x15 upgrade fuel assemblies, additional restraint is provided with the intermediate flow mixer grids such that the grid-to-grid spacing in those spans with IFM grids is shorter. Using the NRC approved scaling factor results in predicted channel closure of less than 50-percent closure in the IFM spans. Therefore, no rod bow DNBR penalty is required in the IFM spans.

Sufficient margin between the Safety Analysis Limit DNBR and the Design Limit DNBR is maintained to accommodate this penalty.

3.5.3.5 Filler Rods

A limited number of fuel rods may be replaced with substitutions of zirconium alloy, zircaloy-4, ZIRLO™, or stainless steel filler rods, in accordance with the NRC-approved methodology in Reference 23.

3.5.3.6 References for Section 3.5.3 (Upgrade)

1. Friedland, A.J., and Ray, S. "Revised Thermal Design Procedure," WCAP-11397-P-A, April 1989.
2. Chelemer, H., et. al., "THINC IV - An Improved Program for Thermal Hydraulic Analysis of Rod Bundle Cores," WCAP-7956-A, February 1989.
3. Hochreiter, L. E., et. al., "Applications of THINC IV Program to PWR Design," WCAP-8054-P-A, February 1989.
4. Motley, F. E., et. al., "New Westinghouse Correlation WRB-1 for Predicting Critical Heat Flux in Rod Bundles with Mixing Vane Grids," WCAP-8762-P-A, July 1984.

5. Tong, L. S., "Critical Heat Fluxes in Rod Bundles, Two Phase Flow and Heat Transfer in Rod Bundles," Annual Winter Meeting ASME, November 1969, pp. 31-46.

6. Tong, L. S., "Boiling Crisis and Critical Heat Flux,...," AEC Office of Information Services, TID-25887, 1972.

7. Leech, W. J., et. al., (Ed.), "Revised PAD Code Thermal Safety Model," Westinghouse Report WCAP-8720-Addenda 2, October 1982.

8. Weiner, R. A., et al., "Improved Fuel Performance Models for Westinghouse Fuel Rod Design and Safety Evaluation," WCAP-11873-A, August 1988.

9. "D.C. Cook Unit 1 15x15 Upgrade Fuel Project Engineering Report," WCAP-16586-P, September 2006.

10. Tong, L. S., "Boiling Heat Transfer and Two-Phase Flow," New York: John Wiley and Sons, 1965.

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

12. Tong, L. S., "Prediction of Departure from Nucleate Boiling for an Axially Non-Uniform Heat Flux Distribution," J. Nuclear Energy, 21, 241-248, 1967.

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

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

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

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

17. Friedland, A.J., and Ray, S. "Improved THINC IV Modeling for PWR Core Design," WCAP-12330-P-A, September 1991.

18. Slagle, W. H., (Ed.) et. al., WCAP-15063-P-A, Revision 1, “Westinghouse Improved Performance Analysis and Design Model (PAD 4.0),” July 2000.

19. Y.Sung, P. Schueren, A. Meliksetian, "VIPRE-01 Modeling and Qualification for Pressurized Water Reactor Non-LOCA Thermal-Hydraulic Safety Analysis," WCAP-14565-P-A, October 1999.

20. C.W. Stewert, et al., "VIPRE-01: A Thermal-Hydraulic Code for Reactor Cores," Volume 1-3 (Revision 3, August 1989), Volume 4 (April 1987), NP-2511-CCMA, Electric Power Research Institute.

21. Skaritka, J., ed, "Fuel Rod Bow Evaluation," WCAP-8691, Revision 1, July 1979.

22. Letter From C. Berlinger (NRC) to E. P. Rahe Jr. (W), Subject: "Request for Reduction in Fuel Assembly Burnup Limit for Calculation of Maximum Rod Bow Penalty," June 18, 1986.

23. Slagle, W. H. (ed.), “Westinghouse Fuel Assembly Reconstitution Evaluation Methodology,” WCAP-13060-P-A, July 1993.

24. AEP-15-59, "American Electric Power, Donald C. Cook Units 1 & 2, Westinghouse Resolution Plan and Technical Basis for NSAL-14-5, "Lower Than Expected Critical Heat Flux Results Obtained During DNB Testing," December 1, 2015.