15.4 CONDITION IV - LIMITING FAULTS

Condition IV occurrences are faults which are not expected to take place, but are postulated because their consequences would include the potential or the release of significant amounts of radioactive material. They are the most drastic occurrences which must be designed against and represent limiting design cases. Condition IV faults are not to cause a fission product release to the environment resulting in an undue risk to public health and safety in excess of 10CFR50.67 limits. A single Condition IV Fault is not to cause a consequential loss of required functions of systems needed to cope with the fault including those of the Emergency Core Cooling System (ECCS) and the containment. The following faults have been classified in this category:

1. Major rupture of pipes containing reactor coolant up to and including double-ended rupture of the largest pipe in the Reactor Coolant System (RCS) loss-of-coolant accident (LOCA)

2. Major secondary system pipe ruptures

3. Major rupture of a main feedwater line

4. Steam generator tube rupture

5. Single reactor coolant pump locked rotor

6. Fuel handling accident

7. Rupture of a control rod drive mechanism (CRDM) housing (rod cluster control assembly ejection).

8. Mass and energy releases to containment following a steam line rupture

The time sequence of events during applicable Condition IV events is shown in Table 15.4-1.

15.4.1 MAJOR REACTOR COOLANT SYSTEM PIPE RUPTURES (LOSS-OF-COOLANT ACCIDENT)

15.4.1.1 Identification of Causes and Frequency Classification

A Loss-of-Coolant Accident (LOCA) is the result of a pipe rupture of the Reactor Coolant System (RCS) pressure boundary. A major pipe break, also called a large break, is defined as a rupture having a total cross-sectional area equal to or greater than 1.0 ft^2. This event is considered a limiting fault, ANS Condition IV, in that it is not expected to occur during the lifetime of the plant but is postulated as a conservative design basis.

Acceptance criteria for the LOCA are described in 10 CFR 50, Paragraph 46 (Reference 1). These are as follows:

1. The calculated maximum fuel element cladding temperature shall not exceed 2200°F.
2. The calculated total oxidation of the cladding shall nowhere exceed 0.17 times the total cladding thickness before oxidation.
3. The calculated total amount of hydrogen generated from the chemical reaction of the cladding with water or steam shall not exceed 0.01 times the hypothetical amount that would be generated if all the metal in the cladding cylinders surrounding the fuel, excluding the cladding surrounding the plenum volume, were to react.
4. Calculated changes in core geometry shall be such that the core remains amenable to cooling.
5. After any successful initial operation of the Emergency Core Cooling System (ECCS), the calculated core temperature shall be maintained at an acceptably low value and decay heat shall be removed for the extended period of time required by the long lived radioactivity remaining in the core.

These criteria were established to provide significant margin in ECCS performance following a LOCA.

15.4.1.2 Sequence of Events and Systems Operations

Should a major break occur, depressurization of the RCS results in a pressure decrease in the pressurizer. The reactor trip signal subsequently occurs when the pressurizer low pressure trip setpoint is reached. A Safety Injection Actuation Signal (SIAS) is generated when the appropriate setpoint is reached. These actions will limit the consequences of the accident in two ways:

1. Reactor trip and borated water injection attenuate void formation by rapidly reducing power to a residual level corresponding to fission product decay heat. However, the LOCA results presented in this section have taken no credit for the boron content of the injection water and no credit for insertion of the control rods to shut down the reactor. It should be noted that subcriticality calculations, which are not presented in this section, do take credit for the boron concentration of the injected water and insertion of the control rods. These subcriticality calculations pertain to long-term core cooling during post-LOCA conditions. With the core subcritical at all times post-LOCA, no further challenge to the fuel cladding limits beyond the results documented in this section are expected.
2. Injection of borated water provides for heat transfer from the core and prevents excessive clad temperature.

Before the break occurs, the reactor is in an equilibrium condition at full power and load (i.e., the heat generated in the core is being removed via the secondary system). During blowdown, heat from fission product decay, hot internals, and the vessel continues to be transferred to the reactor coolant. At the beginning of the blowdown phase, the entire RCS contains subcooled liquid which transfers heat from the core by forced convection with some fully developed nucleate boiling. Thereafter, core heat transfer is based on local conditions with transition boiling and forced convection to steam as the major heat transfer mechanisms.

The heat transfer between the RCS and the secondary system may be in either direction depending on the relative temperatures. In the case of continued heat addition to the secondary side, secondary system pressure increases and the main steam code safety valves may actuate to limit the pressure. Feedwater to the secondary side is automatically provided by the Auxiliary Feedwater System (AFS) to aid in the reduction of RCS pressure. The SIAS isolates the steam generators from normal feedwater flow and initiates emergency flow from the AFS. The large break LOCA analysis models a turbine trip, turbine isolation, main feedwater isolation, and a Loss of Offsite Power (LOOP) all to occur at the time of reactor trip.

When the RCS depressurizes to a conservative, minimum setpoint value, the accumulators begin to inject borated water into the reactor coolant loops. Since a LOOP is assumed, the reactor coolant pumps are assumed to trip at the beginning of the accident. The effects of pump coastdown are included in the blowdown analysis.

There are three phases to a large break LOCA. The first, the blowdown phase, starts with the break and ends when the RCS pressure reaches that of the containment. Prior to or at the end of the blowdown, various mechanisms preventing the injection of ECCS into the RCS become ineffective. As such, injected flow begins to reach the reactor vessel lower plenum. At this time (referred to as the end of bypass), the reactor vessel lower plenum begins to

refill. This is the second phase of the transient. This phase starts when the lower plenum of the reactor vessel starts to receive continuous flow from the downcomer and ends when the liquid level reaches the bottom of the active core. (This point in time is referred to as bottom of core recovery.)

Reflood is the third and final phase of the transient. It begins with the bottom of core recovery and ends when the reactor vessel has been filled with water and core temperature is no longer rising.

Halfway through blowdown continuing into the reflood phase, the safety injection accumulator tanks rapidly discharge borated cooling water into the RCS contributing to the recovery of the downcomer. The downcomer water elevation head provides the driving force required for the reflooding of the reactor core. The Residual Heat Removal (RHR), Intermediate Head Safety Injection, and High Head Safety Injection pumps aid the filling of the downcomer and subsequently supply water to maintain a full downcomer and complete the reflooding process.

Continued operation of the ECCS pumps supplies water during long-term cooling. Core temperatures are reduced to long-term steady state levels associated with dissipation of residual heat generation. After the water level of the Refueling Water Storage Tanks (RWST) reaches a minimum allowable value, coolant for long-term cooling of the core is obtained by switching from the injection mode to the cold leg recirculation mode of operation in which spilled borated water is drawn from the now full recirculation sump by the RHR pumps, cooled in the RHR heat exchanger, and returned to the RCS cold legs. The design is such that the containment sump becomes full prior to low level in the RWST. The Containment Spray System continues to operate to further reduce containment pressure. Approximately 14 hours (Unit 1) and 6.5 hours (Unit 2) after initiation of the LOCA, the RHR and Intermediate Head Safety Injection pumps are realigned to supply water to the RCS hot legs in order to control the boric acid concentration in the reactor vessel.

15.4.1.3 Thermal Analysis

15.4.1.3.1 Westinghouse Performance Criteria for ECCS

The reactor is designed to withstand thermal effects caused by a LOCA including the double-ended severance of the largest RCS pipe. The reactor core, internals, and the ECCS are designed so that the reactor can be safely shut down following a LOCA and the essential heat transfer geometry of the core can be preserved.

Even with the most severe, single active failure, the ECCS s designed to meet acceptance criteria described in Reference 1.

15.4.1.3.2 Large Break LOCA Evaluation Model

The analysis of a large break LOCA transient is divided into three phases: blowdown, refill, and reflood. For each phase, three distinct transients are analyzed: the thermal-hydraulic transient in the RCS, the pressure and temperature transient within the containment, and the fuel and clad temperature transient of the hottest fuel rod in the core. Based on these considerations, a system of interrelated computer codes was developed for the analysis of the large break LOCA.

The analysis was performed using the 1981 Evaluation Model with BASH methodology and computer codes described in References 2, 3, 4, 5, 6, 7, 8, 10, 11, 12, 13, 19, Item 2.1, "Fuel Rod Model Revisions", Item 3.2, "Large Break LOCA Burst and Blockage Assumption", and Item 3.3, "Steam Generator Flow Area" of Reference 20, and the "Structural Metal Heat Modeling" and "Spacer Grid Heat Transfer Error in BART" items of Reference 21. These documents describe the major phenomena modeled, the interface between the computer codes, and the features of the codes which ensure compliance with the requirements defined in Appendix K to 10 CFR 50 (Reference 1). Item 3.2, "Large Break LOCA Burst and Blockage Assumption," of Reference 20 must be reviewed on a case by case basis since a coding change was not incorporated in to the model. The cases performed for fuel with Integral Fuel Burnable Absorbers (IFBA) fall into the category where the results would be affected by this issue. However, detailed sensitivity study work and investigation into the transient results concluded that no peak cladding temperature penalty needed to be assessed for this issue on the limiting IFBA transients due to different mechanisms involved in determining the peak cladding temperature for IFBA fuel.

The SATAN-VI, WREFLOOD, and COCO codes which are used in the LOCA analysis, are described in detail in References 3, 5, and 6. The BASH code is described in Reference 4 and the LOCBART code is described in References 4, 8, 12, and 13. These codes are used to assess the core heat transfer characteristics and to determine if the core remains amenable to cooling throughout the blowdown, refill, and reflood phases of the LOCA. The SATAN-VI computer code analyzes the thermal-hydraulic transient in the RCS during blowdown. The WREFLOOD and BASH computer codes are used to calculate this transient during the refill and reflood phases of the accident. The COCO computer code is used to calculate

the containment pressure transient during all three phases of the LOCA analysis. Similarly, the LOCBART computer code is used to compute the core fluid and heat transfer conditions and the fuel cladding thermal transient of the hot assembly, including the hot rod, during the three phases.

SATAN-VI is used to calculate the RCS pressure, enthalpy, density, mass, and energy flow rates in the RCS, and energy transfer between the primary and steam generator secondary systems as a function of time during the blowdown phase of the LOCA. SATAN-VI also calculates accumulator water mass, internal pressure, pipe break mass, and internal energy flow rates assumed to be vented to the containment during blowdown. During blowdown, no credit is taken for rod insertion or boron content of the injection water. Instead, the core shuts down due to void formation. At the end of the blowdown phase, data are transferred to the WREFLOOD code, and mass and energy release rates (during blowdown) are transferred to the COCO code for the determination of the containment pressure response during this first phase of the LOCA. Additional SATAN-VI output data from the end of blowdown, including the pressure and core power decay transient, are input to the LOCBART code.

With input from the SATAN-VI code, WREFLOOD uses a system, thermal-hydraulic model to determine the coolant pressure, temperature, and mixture level height during the refill phase of the LOCA. WREFLOOD also calculates mass and energy flow addition to the containment throughout the break. Since the mass flow rate to the containment depends upon the core flooding rate and the local core pressure (ultimately dependent upon the containment back pressure), the WREFLOOD and COCO codes are interactively linked. In this way, COCO utilizes mass and energy releases calculated by WREFLOOD during the entire reflood transient. BASH is an integral part of the ECCS evaluation model which provides a realistic, thermal-hydraulic simulation of the reactor core and RCS during the reflood phase of a LOCA. Instantaneous values of accumulator conditions at the time of completion of lower plenum refill are provided to BASH from WREFLOOD. BASH has been substituted for WREFLOOD in calculating transient values of core inlet flow and enthalpy for the detailed fuel rod model, LOCBART. A more detailed description of the BASH code is contained in Reference 4. The version of the BASH code used for the analysis has been modified to create a plot tape in the standard plotting code format and to correct a problem with a library compatibility which previously prevented code restarts. There are no effects on the calculated results from these changes.

The COCO code is a mathematical model of the containment. It uses mass and energy releases to the containment provided by SATAN and WREFLOOD. COCO is described in detail in Reference 6.

The LOCBART code is a computer program that evaluates fuel, cladding, and coolant temperatures during a LOCA. This code includes the clad swelling and rupture model of NUREG-0630 as supplemented by Reference 13. A more complete description than is presented here can be found in References 4, 8, 12, and 13. In the LOCBART detailed fuel rod model, for the calculation of local heat transfer coefficients, the code employs rigorous mechanistic models to generate heat transfer coefficients appropriate to the actual flow and heat transfer regimes experienced by the fuel rods.

Modifications (Reference 11) were made to the LOCBART code to represent the ZIRLO™ cladding properties. An evaluation was performed, as described in Reference 11, to ensure that the effect of the ZIRLO™ clad fuel did not result in a more severe hydraulic transient. The conclusions of this study indicated that only the clad heat-up portion of the transient is significantly affected by the ZIRLO™ clad related changes. Consequently, based on Reference 11, the ZIRLO™ cladding was only modeled in the LOCBART computer code for this analysis.

Additional modifications have been made to the LOCBART code version used to perform the analysis. Miscellaneous minor LOCBART error corrections have been made to the pellet/clad contact and clad thinning models. These errors were deemed to have negligible effect on the transient for the analysis performed here. Various discretionary changes to input/output format and inclusion of code diagnostics are also contained in the LOCBART version used. These changes do not affect the results.

15.4.1.4 Large Break LOCA Input Parameters and Initial Conditions

The bases used to select the numerical values that are input parameters to the analysis have been conservatively determined from extensive sensitivity studies (References 15, 16, and 17). In addition, the requirements of 10 CFR 50 Appendix K regarding specific model features were met by selecting models which provide a significant overall conservatism in the analysis.

The ECCS large break LOCA analysis assumes minimum safeguards to determine safety injection flow. This minimizes the amount of flow to the RCS by assuming maximum line resistances, degraded ECCS pump performance, and the loss of one RHR pump as the limiting single failure. As an additional conservatism, the analysis assumed the loss of one Intermediate Head Safety Injection pump and one High Head Safety Injection pump. A maximum safety injection delay of 32 seconds after occurrence of the SIAS (on Containment

Pressure - High or Pressurizer Pressure - Low SIAS) was assumed. This accounts for signal initiation, diesel generator startup and emergency power bus loading consistent with the assumed LOOP and coincident reactor trip as well as the delay for aligning the valves and bringing the pumps up to speed. This conservatively bounds Technical Specification requirements.

As identified in Reference 9, it may be more limiting in the current Appendix K ECCS evaluation models to assume maximum possible ECCS flow delivery for some Westinghouse four loop, non-upper head injection, non-burst node limited plants. Therefore, a maximum safeguards case is included in this large-break LOCA analysis. This case was performed to provide further assurance that the minimum safeguards assumption continues to result in the limiting peak cladding temperature. The maximum safeguards assumption includes minimum injection line resistance, enhanced ECCS pump performance, and no single failure, and thus, results in the highest amount of flow delivered to the RCS.

The large break LOCA analysis assumed operation at 102[(1)] percent of 3411 MWt, a maximum peaking factor, and a maximum hot channel enthalpy rise factor. The assumption of use of 102[(1)] percent of 3411 MWt is made in accordance with Section A of Appendix K to 10 CFR 50 (Reference 1). A chopped cosine core power distribution was assumed.

The assumption that the upper head fluid temperature was equal to the RCS hot leg fluid temperature was made since this is expected to be the upper bound temperature of the fluid in this region. The effect of the upper head temperature on ECCS performance is discussed in References 14 and 15.

The initial conditions with the containment prior to accident initiation and the containment heat sink data used in the large break LOCA analysis are given in Table 15.4-3. The containment back pressure is calculated using the methods assumptions described in Reference 2, Appendix A. The containment initial conditions of 90°F and 14.7 psia are representatively low values anticipated during normal full power operation. The condensing heat transfer coefficient used for heat transfer to the containment wall structures for the limiting break is given on Figure 15.4-1. The mass and energy releases used in the containment back pressure calculation for the limiting break are presented on Figures 15.4-2 and -3. In addition, Tables 15.4-4 and -5 present mass and energy releases for the limiting case from the RCS to containment, and from the accumulator and injection pumps to the containment.

---

(1) An evaluation was performed to document a 1.4 percent power uprate achieved through a calorimetric uncertainty reduction. The BASH LBLOCA Analysis was performed assuming a power level of 3411 MWt and calorimetric uncertainty of 2 percent. The evaluation accounts for a reduction in calorimetric uncertainty to approximately 0.6 percent, allowing a 1.4 percent power uprate.

The analysis was performed considering a full core of either VANTAGE5H (without Intermediate Flow Mixing Vane grids [IFMs]) or VANTAGE+ fuel. The VANTAGE5H fuel consists of Zirc-4 cladding and Zirc-4 low pressure drop grids. The VANTAGE+ features analyzed were ZIRLO™ cladding, ZIRLO™ IFMs, and ZIRLO™ low pressure drop (LPD) mid-grids. The inclusion of IFMs in these analyses supports but does not require their use in future reloads. An additional feature, the Integral Fuel Burnable Absorber (IFBA), was also considered for VANTAGE5H and VANTAGE+ fuel. Core loss coefficients characteristic of VANTAGE+ (with IFMs) fuel, which bound VANTAGE5H (without IFMs) fuel, were assumed in the thermal hydraulic blowdown portion of the analysis (SATAN-VI).

15.4.1.5 Large Break LOCA Analysis Results

Based on the results of the generic LOCA sensitivity studies (References 16 and 17), the limiting large break was found to be the double-ended cold leg guillotine (DECLG). Therefore, only the DECLG break is considered in the large break ECCS performance analysis.

Calculations were performed for a range of Moody break discharge coefficients (C_D) assuming minimum safeguards safety injection (SI), maximum vessel average temperature (high T_{AVG}), and VANTAGE+ fuel. These calculations determined that the C_D=0.4 Moody break discharge coefficient is limiting. An analysis based on maximum safeguards SI was then performed for the limiting discharge coefficient at the maximum vessel average temperature which confirmed minimum safeguards SI assumptions are limiting. An analysis at the minimum vessel average temperature (low T_{AVG}) was also performed for the limiting discharge coefficient with minimum safeguards SI which confirmed the maximum vessel average temperature assumption is limiting. Additional LOCBART sensitivities to determine the effects of VANTAGE5H fuel and IFBA coated fuel pellets are more limiting than pellets without IFBA coating. Table 15.4-2 presents the peak clad temperature (PCT) and hot spot metal-water reaction for the analyses performed. The time sequence of events during the large break is shown in Table 15.4-1.

The effect of the break discharge coefficient of the large break LOCA transient is discussed in References 16 and 17. A best estimate value of the Moody discharge coefficient is about 0.6 and varying the discharge coefficient from a maximum of 1.0 to a minimum of 0.4 covers all uncertainties associated with the prediction of the break flow in the case of a guillotine-type severance of a cold leg pipe. The position to limit the break discharge coefficient to that range has been reviewed and approved by the NRC (Reference 16). Therefore, analyzing a LOCA for break discharge coefficients less than 0.4 is inconsistent with experimental data and the established procedure for a 10 CFR 50 Appendix K evaluation of ECCS performance.

For the results discussed below, the hot spot is defined to be the location of maximum PCT. This location is given in Table 15.4-2. Figures 15.4-4 through -47 and Figures 15.4-55 through -58 present the transient behavior of the principal parameters. Figures 15.4-4 through -47 and -55 contain results for VANTAGE+ (with IFMs) fuel. Figures 15.4-56 through -58 contain cladding heat-up sensitivities for VANTAGE5H (without IFMs) fuel, VANTAGE5H (without IFMs, with IFBA) fuel, and VANTAGE+ (with IFMs and IFBA) fuel, respectively.

The SATAN code was not rerun for the maximum safeguards SI case because this assumption would have a negligible impact on the blowdown transient (Reference 9). This means that although Figures 15.4-2, -3, -13A, -19A, -28A, -37A, and -46A all assume minimum safeguards for a C_D=0.4, the results would not change if maximum safeguards were assumed.

1. Figures 15.4-4 through -12 show the fluid quality, the mass velocity and the heat transfer coefficient (as calculated by the LOCBART code) at the hot spot (location of maximum clad temperature) and burst location, on the hottest fuel rod (hot rod) for the [C_D=0.4, minimum SI, high T_{AVG}], [C_D =0.4 maximum SI, high T_{AVG}], [C_D=0.4, minimum SI, low T_{AVG}], [C_D=0.6, minimum SI, high T_{AVG}], and [C_D=0.8, minimum SI, high T_{AVG}] cases.
2. Figures 15.4-13 through 15.4-21 show the core pressure, the flow rate out of the break (the sum of both ends for the guillotine break) and the core pressure drop (from the lower plenum at the core inlet, to the upper plenum at the core outlet).
3. Figures 15.4-22 through 15.4-30 show the clad average temperature transient at the hot spot and the burst location, the fluid temperature (also for the hot spot and burst location), and the core flow rate (top and bottom).
4. Figures 15.4-31 through 15.4-36 show the core reflood transient parameters (water level and flooding rate).
5. Figures 15.4-37 through 15.4-42 show the accumulator injected flow and the pumped ECCS flow. The accumulator flow is the flow injection to one of the intact cold legs.
6. Figures 15.4-43 through 15.4-45 show the containment pressure transient.
7. Figures 15.4-46 through -47 and -55 show the core power transient.
8. Figures 15.4-56 through 15.4-58 show the clad average temperature transient at the hot spot and the burst location for VANTAGE5H (with IFMs and IFBA) fuel, VANTAGE5H (without IFMs, with IFBA) fuel, and VANTAGE+ (with IFMs and IFBA) fuel, respectively.

The maximum clad temperature calculated for a large break LOCA with a full core of VANTAGE5H (without IFMs, with IFBA) fuel is 2020°F. The maximum clad temperature calculated for a large break LOCA with full core of VANTAGE+ (with IFMs and IFBA) fuel is 1978°F. The addition of a bounding 50°F transition core penalty to the VANTAGE+ fuel results to account for flow redistribution due to fuel assembly hydraulic resistance mismatch between the VANTAGE5H and VANTAGE+ assemblies (see discussion in Section 15.4.1.6), yields a transition core PCT of 2028°F. Additional penalties may be added onto the analytical result to compensate for other effects. For instance, there is a penalty associated with the use of VANTAGE+ assemblies that do not have IFMs. If such assemblies are used in the core, this and other appropriate penalties are added onto the calculated PCT. To satisfy the criteria limit in 10 CFR 50.46, this sum must be less than 2200°F. the current value satisfies this limit.

The maximum local metal-water reaction is 6.3 percent which is well below the embrittlement limit of 17 percent as required by 10 CFR 50.46. The total core metal-water reaction is less than 1 percent for all breaks, and the clad temperature transient is terminated at a time when the core geometry is still amenable to cooling. As a result, the core temperature will continue to drop and the ability to remove decay heat generated in the fuel for an extended period of time will be provided.

15.4.1.6 Large Break LOCA Transition Core Effects

The large break LOCA analysis was performed assuming a full core of either 17 x 17 VANTAGE5H fuel (without IFMs) or VANTAGE+ fuel (with IFMs). As stated earlier, these analyses bound cores that contain 1) IFM assemblies only, 2) non-IFM assemblies only, and 3) a combination of IFM and non-IFM assemblies (otherwise known as a transition core). When assessing the effect of transition cores on the large break LOCA analysis, it must be determined whether the transition core can have a greater calculated PCT than a complete core of either the 17 x 17 VANTAGE5H (without IFMs) design or the 17 x 17 VANTAGE+ design. For a given peaking factor, the only mechanism available to cause a transition core to have a greater calculated PCT than a full core of either fuel is the possibility of flow redistribution due to fuel assembly hydraulic resistance mismatch. This hydraulic resistance mismatch may exist only for transition cores and is the only unique difference between a complete core of either fuel type and the transition core.

The transition from VANTAGE5H (without IFMs) fuel to VANTAGE+ fuel (with IFMs) is similar to a transition from STANDARD (without IFMs) fuel to VANTAGE+ (with IFMs) fuel. This is because the addition of the IFM grids creates hydraulic mismatch between the assemblies. Westinghouse transition core designs have been generically analyzed. The increase in hydraulic resistance for the VANTAGE+ with IFMs assembly was shown to produce a reduction in reflood steam flow rate for the fuel at mixing vane grid elevations for transition core configurations. The various fuel assembly specific transition core analyses performed resulted in PCT increases up to 50°F for core axial elevations that bound the location of the PCT (Reference 18). Therefore, the maximum PCT penalty possible for VANTAGE+ with IFMs fuel residing in a transition core with VANTAGE5H (without IFMs) fuel is 50°F since the increased hydraulic resistance for the VANTAGE+ assemblies with the IFMs produces a reduction in flow rate for the assemblies with the IFMs and not for the VANTAGE5H (without IFMs) assembly. Thus, the increase in hydraulic resistance for the VANTAGE+ assembly with the IFMs with result in a maximum PCT penalty of 50°F. Once a full core VANTAGE+ fuel (with IFMs) is achieved, the penalty would no longer be necessary.

15.4.1.7 Large Break LOCA Analysis Conclusions

Analyses presented in this section confirm that the High Head, Intermediate Head, and RHR Safety Injection pumps of the ECCS, together with the accumulators, provide sufficient core flooding to keep the calculated PCT below the 10 CFR 50.46 requirement. Hence, adequate protection is provided by the ECCS against a large break LOCA and acceptance criteria (10 CFR 50.46) is satisfied.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

THE INFORMATION ON THIS PAGE HAS BEEN DELETED.

15.4.1.8 Environmental Consequences of a Loss-of-Coolant Accident

15.4.1.8.1 Sources

Initial Release Fractions

In the design basis loss-of-coolant accident (LOCA), containment airborne activities are derived from two sources: The RCS and the core. The RCS inventory, which is assumed to be instantaneously released and to be homogeneously mixed in the containment atmosphere, is assumed to be at technical specification levels with a pre-existing iodine concentration and noble gases at 1 percent Failed Fuel. Before the containment vacuum relief line can be isolated, a limited release of containment atmosphere to the environment is calculated based on containment pressurization and penetration size. The release is isolated (Section 9.4.4.3.2), prior to the time of the postulated initiation of fuel damage. After the onset of fuel damage, 40 percent of the equilibrium radioactive halogen inventory and 100 percent of the equilibrium noble gas inventory developed from the maximum full power operation of the core is considered to be released to the containment atmosphere. Of the radioiodine released from the Reactor Coolant System (RCS) to the containment in the postulated accident, 95 percent of the iodine released is assumed to be cesium iodide (CsI), 4.85 percent elemental iodine, and 0.15 percent organic iodine. The airborne activity is assumed to be initially homogeneously mixed inside the containment atmosphere.

Independently of the assumptions used in determining the airborne radioactive inventory, 40 percent of equilibrium radioactive halogen inventory is assumed to be released to the containment sump. The material is available for release to the environment by leaks from systems circulating sump fluids outside containment.

The basic core and coolant inventories, accident parameters, and meteorological data used in the analyses are consistent with the guidance provided in Regulatory Guide 1.183 and are identified in Table 15.4-5B.

Organic Iodine Inventory

It is assumed that 0.15 percent of the radioiodine released from the Reactor Coolant System to the containment as a result of a LOCA is organic iodine based on Regulatory Guide 1.183 (Ref. 80).

15.4.1.8.2 Method of Analysis

The accident source term used in the LOCA design basis radiological analyses was replaced with an alternative source term (AST) pursuant to Section 50.67 of Title 10 of the Code of Federal Regulations (10 CFR 50.67), "Accident Source Term". The guidance provided in Regulatory Guide 1.183, "Alternative Radiological Source Terms for Evaluating Design Basis Accidents at Nuclear Power Reactors", July 2002, was used. To evaluate the ability to meet 10CFR50.67 limits, offsite and control room total effective dose equivalents (TEDE) are calculated. Results are presented for offsite exposure at the exclusion area boundary (EAB) and at the outer boundary of the low population zone (LPZ).

Basic Activity Transport Model

RADTRAD (Version 3.02 and 3.03)

RADTRAD (Version 3.02 and 3.03) (References 71, 78 and 82) is a qualified analytical computational code that was developed for the NRC to estimate transport and removal of radionuclides and dose at selected receptors.

The RADTRAD (Version 3.02 and 3.03) code can be used to estimate the containment release using either default source terms, and assumptions, or a user-specified table.

In addition, the code can account for the reduction in the quantity of radioactive material due to containment sprays, natural deposition, filters, and other natural and engineered safety features. The RADTRAD (Version 3.02 and 3.03) code uses a combination of tables and/or numerical models of source term reduction phenomena to determine the time dependent dose at user specified locations for a given accident scenario. The code also provided the inventory, decay chain, and dose conversion factors needed for the dose calculation. The RADTRAD (Version 3.02 and 3.03) code can be used for occupational radiation exposure assessments, typically in the control room, for site boundary dose estimates, and for dose attenuation estimates due to facility or accident releases.

Model for Offsite Doses

RADTRAD (Version 3.02 and 3.03) uses atmospheric relative concentrations for offsite locations and breathing rates that are provided by the user. Values of the atmospheric relative concentrations for the exclusion area boundary (EAB) and low population zone (LPZ) distances are given in Table 2.3-21. The breathing rates are given in Table 15.4-9.

THIS PAGE INTENTIONALLY LEFT BLANK

THIS PAGE INTENTIONALLY LEFT BLANK

THIS PAGE INTENTIONALLY LEFT BLANK

15.4.1.8.3 Effectiveness of Safeguards for Limiting Activity Release

The reactor containment serves as an activity leakage limiting boundary. The containment is steel lined and designed to withstand internal pressure in excess of that resulting from the design basis LOCA. All weld seams and penetrations are designed with a double barrier to inhibit leakage. The Containment Isolation System (CIS), Section 6.2.4, provides a minimum of two barriers in piping penetrating the containment. The containment is designed to leak at a rate of less than 0.1 percent per day at design pressure.

The effectiveness of the Spray System for removal of iodine from the containment atmosphere is evaluated in detail in Section 6.2.3. If there is a large excess of chemical reagent to react with the iodine and convert it to a non-volatile form with little or no tendency to return to the gas phase, the elemental iodine removal rate by spray can be expressed by:

$$\frac{dA}{dt} = -\lambda_s A$$

where:

A = inventory of elemental iodine which is available for leakage at any time, t

λ_s = elemental iodine removal coefficient

Integration of equation (1) gives:

$$A = A_o e^{-\lambda_s t}$$

As discussed in Section 6.2.3, an elemental iodine removal coefficient of 29 hr^{-1} is expected for one of the two spray pumps operating.

The spray removal rate is conservatively limited to a maximum value of 20 hr^{-1} in the sprayed region.

Particulate iodine is removed both by injection and recirculation phase sprays and by gravitational settling. Values for spray removal were conservatively calculated and are given in Table 6.2-9. Gravitational settling is calculated by RADTRAD 3.02 and 3.03 (Refs. 71, 78 and 82).

Mixing of the containment air space is provided by assuming ventilation flow from 2 of the 5 fan, coolers which remove air from the sprayed region and discharge flow in both sprayed and unsprayed areas. The assumed mixing rate is given in Table 15.4-5B.

Sprays are assumed to remove elemental and particulate iodine during the injection and recirculation phases of the accident. Transition from injection to recirculation spray is assumed to begin at 48 minutes (single train operational) and complete at 58 minutes. No removal by sprays is assumed for the organic iodine form at any time. Credit for spray removal is limited to a maximum DF of 100 for elemental iodine. At a DF of 50 the particulate (aerosol) iodine removal coefficient is reduced by a factor of 10 and then is reduced to zero at 4.0 hours (see Table 6.2-9). A DF of 100 is achieved for elemental iodine approximately 2.1 hours and a DF of 50 is achieved for the particulates approximately 3.0 hours after accident initiation.

The sump pH is maintained greater that 7.0 long term and iodine re-evolution is not considered credible.

15.4.1.8.4 Sources Analyzed

Containment Pressure-Vacuum Relief Valve Release

The RCS inventory, which is assumed to be instantaneously released and to be homogeneously mixed in the containment atmosphere, is assumed to be at the technical specification equilibrium iodine concentration limit and noble gases at 1% failed fuel. Before the containment vacuum relief line can be isolated, a limited release of containment atmosphere to the environment is calculated based on containment pressurization and penetration size. The release is isolated (Section 9.4.4.3.2), prior to the time of the postulated initiation of fuel damage. Activity released from the core is then added to the RCS source term and then released as part of containment leakage.

Containment Leakage

Credit for safeguards systems operation is taken by reducing the assumed containment leakage rate after the first day.

It is assumed that the release terminates after 30 days consistent with Regulatory Guide 1.183 guidelines. The containment is assumed to have a leak rate of 0.1 percent per day for the first 24 hours and 0.05 percent per day for the remainder of the thirty-day period. The breathing rates used are given in Table 15.4-9. The χ/Q values are given in Table 2.3-21. The initial inventories available for release to the containment are the same as those discussed in Section 15.4.1.3.1.

All containment leakage is assumed to be released to the environment unfiltered via the Plant Vent.

THIS PAGE INTENTIONALLY LEFT BLANK

Recirculation Leakage

Subsequent to the emptying of the refueling water storage tank (RWST) during the initial phase of safety injection and containment spray, water from the containment sump is recirculated by the residual heat removal (RHR) pumps and cooled via the residual heat exchangers and then returned to the RCS. Because the LOCA may cause the sump water to contain radioactivity, the potential offsite exposures due to operation of these external recirculation paths have been evaluated.

The maximum leakage to the Auxiliary Building from the components and joints of the ECCS components during recirculation is controlled by procedure and limited to 0.45 gpm. A factor of two is applied to the maximum estimated leakage for conservatism. Sump water temperature is calculated to be less than 262°F at the initiation of recirculation. Sump water is cooled by the RHR heat exchangers during recirculation. Therefore, only a small fraction of the leakage has the potential to flash to vapor. An initial iodine flashing fraction of 5.06% is applied for one hour after the postulated LOCA and then 3.79% is applied until the sump temperature is well below 212°F (170°F) at approximately 17 hours. From this time throughout the 30-day duration of the accident a flashing fraction of 2% is used. No credit is taken for ESF leakage mixing within the room or tank volumes. Additionally, no credit is taken for plate-out of elemental iodine on the building surfaces or ECCS room coolers. Vaporized liquid is entrained within the Auxiliary Building Ventilation System. The vapor will be discharged to the environment through the Plant Vent and it is assumed that the vapor is released unfiltered.

It is conservatively assumed that during recirculation some ECCS liquid could back-leak into the Refueling Water Storage Tank (RWST) and be released through the tank vent. Although all ECCS recirculation piping paths to the RWST have double-valve isolation during the recirculation phase, it is assumed that the largest line has a valve that fails to close and the second valve leaks 0.5 gallons per minute. It is assumed that this leakage would migrate to the RWST, become diluted by the tank water and air volume, and be released from the tank vent to contribute to offsite and control room doses. These dose contributions are small and the offsite dose and the dose to the control room operators have been assessed, and are within GDC-19 limits.

15.4.1.8.5 Results

The radiological consequences from a postulated LOCA are analyzed in accordance with the guidance provided in Regulatory Guide 1.183 for use of an alternative source term (AST). The analysis for the dose contribution of the cloud resulting from containment leakage and from ESF leakage are based on the parameters listed in Table 15.4-5B.

The environmental releases resulting from the LOCA, including the ESF leakage, is used in conjunction with the atmospheric dispersion values give in Table 2.3-21 to calculate the offsite doses using the methodology described.

The total doses at the exclusion area boundary (EAB) and the low population zone (LPZ), presented in Table 15.4-5C, from the LOCA are within 10 CFR 50.67 limits and Regulatory Guide 1.183 guidelines.

THIS PAGE INTENTIONALLY BLANK

THIS PAGE INTENTIONALLY BLANK

15.4.1.9 Control Room Analyses During a Loss-of-Coolant Accident

Two cases are considered applicable for control room habitability during a Loss-of-Coolant Accident.

CASE #1; Two-Train Alignment:

The expected case is to have the control room in its normal operational mode, for ventilation purposes, at the beginning of an accident. Unfiltered air at 1320 cfm maximum from the intake with the higher ¬/Q is directed by the Control Area Air Conditioning System (CAACS) into the control room pressure envelope. The normal (CAACS) intake dampers close and the Control Room Emergency Air Conditioning System (CREACS) air supply dampers are opened, upon receipt of the control room isolation signal from the Safety Injection (SI) system. This is to allow airflow from the less contaminated air intake, which is automatically pre-selected from the Unit not generating the SI signal, to pressurize the control room envelope. The CAACS air supply to the control room envelope is shutdown and CREACS supply/recirculation fans are started. The normal intake damper closure time is expected to be no more than 20 seconds. The Loss of Offsite Power (LOOP) is assumed to occur at the time the dampers are fully closed and the power to the CREACS fans is lost. At the LOOP, the Diesel Generators start and become fully functional in 13 seconds and power is delivered to the CREACS fans. The CREACS fans are expected to take 15 seconds to gain full speed and to fully pressurize the control room. Since the two-train alignment has double the filtration performance of the single-train alignment, it is not the limiting condition with respect to control room dose consequences and therefore is not analyzed.

CASE #2; Single-Train Alignment:

Only one train is assumed in this analysis since it is the most limiting allowable system configuration as described in UFSAR Section 9.4.1.2. The time allowed in this mode is limited by an action statement as described in T/S 3/4.7.6, and represents a detailed analysis of a condition that is beyond the required design and licensing basis of the system. Only one Unit's CREACS is assumed available with a maximum filtered make up flow rate of 2,100 cfm. The

normal intake damper closure time is assumed to be no more than one minute. The Loss Of Offsite Power (LOOP) is assumed to occur and the power to the CREACS fans is lost. At the LOOP, the Diesel Generators start and become fully functional in 13 seconds and power is delivered to the CREACS fans. The CREACS fans are assumed to take one minute to gain full speed and to fully pressurize the control room. The minimum filtered recirculation flow rate is 5,100 cfm (with one fan operating) and the total unfiltered in-leakage assumed is 275 cfm, which includes ingress/egress and ductwork leakage.

The single train alignment is also considered for a condition, in which the intake is aligned, for up to 15 minutes, to the LOCA unit, prior to switching to the intake on the non-LOCA unit.

The time dependent flow rates are summarized as follows:

T_d = Total delay time from the issuance of the control room isolation signal until control room is fully pressurized, 60 seconds

F_u = Unfiltered intake/inleakage flow rate
= 1320 cfm $t \leq T_d$
= 275 cfm $t > T_d$

F_f = Filtered emergency makeup flow rate
= 0 cfm $t \leq T_d$
= 2100 cfm $t > T_d$

F_r = Filtered recirculation flow rate

= 0 cfm $t \leq T_d$

= 5000 cfm $t > T_d$

The filtration efficiency for emergency makeup flow and recirculation flow are:

Elemental iodine	-	95%
Organic iodine	-	95%
Particulate	-	99%

The control room is modeled as a single region. Isotopic concentrations in areas outside the control room envelope are assumed to be comparable to the isotopic concentrations at the control room intake locations.

Two additional dose components were added to the operator dose: shine from the overhead plume and shine from the activity on the control room filters.

Computer Codes RADTRAD (Version 3.02 and 3.03) and MicroShield (Version 5.05) (Reference 79) were used to determine the dose within the Salem CR from attenuated plume shine. The dose inside a Control Room from a semi-infinite cloud was calculated and attenuation factors for 2 feet of standard concrete as a function of energy as well as the atmospheric dispersion factors associated with a point at the center of the CR were applied to the unshielded dose rate per energy group.

Similarly, Computer Codes RADTRAD (Version 3.02 and 3.03) and MicroShield (Version 5.05) were used to determine the dose within the Salem CR from attenuated shine from the activity deposited on the HEPA and charcoal filters.

15.4.1.9.1 Results

The radiological consequences from a postulated LOCA are analyzed in accordance with the guidance provided in Regulatory Guide 1.183. The analysis for the dose contribution of the cloud resulting from containment leakage and from ESF leakage are based on the parameters listed in Table 15.4-5B.

The environmental releases resulting from the LOCA, including the ESF leakage, is used in conjunction with the atmospheric dispersion values given in Table 15.4-5D to calculate the control room doses using the methodology described.

The potential dose to control room operators is due to inleakage into the control room of the external cloud and direct doses from immersion in the external cloud and radiation sources near the control room. The operating procedures for the control room ventilation systems are described in Section 9.4. The accident analyses consider a conservative post-accident ventilation rate in the control room to evaluate the committed effective dose equivalent (CEDE) from inhalation. Control room walls, floor and roof provide the necessary shielding to protect personnel from the external cloud due to containment building and ESF leakage as well as sources such as the control room filters. Parameters required to calculate the total effective dose equivalent (TEDE), which is the sum of the CEDE from inhalation and the deep dose equivalent (DDE) from external exposure, for persons located in the control room are provided in Table 15.4-5B.

The TEDE dose to the Salem control room operators due to a LOCA at the Salem plant is presented in Table 15.4-5C and is the bounding dose calculated for single-train operation of the Control Room Emergency Air Conditioning System (CREACS). This result is below the limit set in 10 CFR 50.67 of 5 REM TEDE. The control room doses as a consequence of the LOCA are the controlling doses for control room habitability.

15.4.2 Major Secondary System Pipe Rupture

15.4.2.1 Identification of Causes and Accident Description

The steam release arising from a rupture of a main steam pipe would result in an initial increase in steam flow which decreases during the accident as the steam pressure falls. The energy removal from the RCS causes a reduction of coolant temperature and pressure. In the presence of a negative moderator temperature coefficient, the cooldown results in a reduction of core shutdown margin. If the most reactive RCCA is assumed stuck in its fully withdrawn position after reactor trip, there is an increased possibility that the core will become critical and return to power. A return to power following a steam pipe rupture is a potential concern mainly because of the high power peaking factors which exist assuming the most reactive RCCA to be stuck in its fully withdrawn position. The core is ultimately shutdown by the boric acid injection delivered by the SIS.

The analysis of a main steam pipe rupture is performed to demonstrate that the following criteria are satisfied:

1. Assuming a stuck RCCA with or without offsite power, and assuming a single failure in the engineered safeguards there is no consequential damage to the primary system and the core remains in place and intact.

2. Energy release to containment from the worst steam pipe break does not cause failure of the containment structure.

Although DNB and possible clad perforation following a steam pipe rupture are not necessarily unacceptable, the following analysis,

in fact, shows that the DNBR limit is not violated for any rupture assuming the most reactive RCCA stuck in its fully withdrawn position.

The following safety functions provide the necessary protection against a steam pipe rupture:

1. Safety Injection System actuation from any of the following:

   a. Two-out-of-three channels of low pressurizer pressure.

   b. High differential pressure signals between steam lines.

   c. High steam line flow in two main steam lines (one-out-of-two per line) in coincidence with either low-low RCS average temperature or low steam line pressure in any two lines.

   d. Two-out-of-three high containment pressure.

2. The overpower reactor trips (neutron flux and ΔT) and the reactor trip occurring in conjunction with receipt of the safety injection signal.

3. Redundant isolation of the main feedwater lines: Sustained high feedwater flow would cause additional cooldown. Therefore, in addition to the normal control action which will close the main feedwater valves, a safety injection signal will rapidly close all feedwater control valves, trip the main feedwater pumps, and close the feedwater pump discharge valves.

4. Trip of the fast acting main steam isolation valves (MSIVs) (assumed to isolate within 12 seconds including instrumentation delays) on:

a. High steam flow in two main steam lines in coincidence with low steam line pressure in any two lines.

b. High-high containment pressure.

For breaks downstream of the MSIVs, closure of all valves would completely terminate the blowdown. For any break, in any location, no more than one steam generator would blow down even if one of the isolation valves fails to close. Steam line breaks in Mode 3 that require steam line isolation will result in a steam line isolation signal and have sufficient steam pressure to close the MSIVs within the time assumed in the safety analysis. Any steam line break in Mode 3 too small to generate a steam line isolation signal does not require steam line isolation for core protection and is not limiting with respect to DNBR. A description of steam line isolation is included in Section 10.

Steam flow is measured by monitoring dynamic head in nozzles inside the steam pipes. The nozzles, which are of considerably smaller diameter than the main steam pipe, are located inside the containment near the steam generators.

15.4.2.2 Method of Analysis

The analysis of the steam pipe rupture has been performed to determine the following:

1. The core heat flux and RCS temperature and pressure resulting from the cooldown following the steam line break. The LOFTRAN (27) code has been used.

2. The thermal and hydraulic behavior of the core following a steam line break. A detailed thermal and hydraulic digital-computer code, THINC, has been used to determine the minimum DNBR reached for the core conditions computed in Item (1) above.

The following conditions were assumed to exist at the time of a main steam line break accident.

1. End-of-life (EOL) shutdown margin at no load, equilibrium xenon conditions, and the most reactive RCCA stuck in its fully withdrawn position: Operation of the control rod banks during core burnup is restricted in such a way that addition of positive reactivity in a steam line break accident will not lead to a more adverse condition than the case analyzed.

2. The negative moderator coefficient corresponding to the EOL rodded core with the most reactive rod in the fully withdrawn position: The variation of the coefficient with temperature and pressure has been included. The k_{eff} versus temperature corresponding to the negative moderator temperature coefficient used is shown on Figure 15.4-50 (Unit 1) and Figure 15.4-48 (Unit 2). The effect of power generation in the core on overall reactivity is shown on Figure 15.4-49.

   The core properties associated with the sector nearest the affected steam generator and those associated with the remaining sector were conservatively combined to obtain average core properties for reactivity feedback calculations. Further, it was conservatively assumed that the core power distribution was uniform. These two conditions cause under-prediction of the reactivity feedback in the high power region near the stuck rod. To verify the conservatism of this method, the reactivity as well as the power distribution were checked. These core analyses considered the Doppler reactivity from the high fuel temperature near the stuck RCCA, moderator feedback from the high water enthalpy near the stuck RCCA, power redistribution and non-uniform core inlet temperature effects. For cases in which steam generation occurs in the high flux regions of the core, the effect of void formation was also included. It was determined that the reactivity employed in the kinetics analysis was always

larger than the reactivity calculated for all cases. These results verified conservatism; i.e., underprediction of negative reactivity feedback from power generation.

3. Minimum SIS capability for the injection of borated flow into the RCS is assumed in the analysis. Due to single failure considerations, injection flow is assumed to be delivered by only a single charging pump. The modeling of the SIS in LOFTRAN is described in Reference 27.

   A conservatively bounding total time delay is modeled in the analysis to account for the delay between the time that the ESF actuation setpoint is reached and the time that SIS flow is capable of being pumped from the RWST into the RCS cold leg header. The total time delay assumed in the analysis is 22 seconds. This 22 second assumption was selected to conservatively bound the sum of the following time delay components:

   a. Instrumentation, logic and signal transport time delay associated with generation and transport of the SI signal, and

   b. The following actions which occur in parallel:
      1. SIS suction valve alignment (opening of RWST valves followed by closure of VCT valves), and
      2. High Head SI/Charging Pump starting and attaining full speed.

In addition, the analysis conservatively assumes that the SIS lines between the RWST and the RCS initially contain unborated water. After the appropriate total time delay described above, the analysis takes into account the purging of this unborated water prior to crediting the injection of borated flow from the RWST into the RCS.

4. Two cases are considered. Both model a complete severance of the main steam line at the outlet of the steam generator with the plant initially at no load conditions. One case models full RCS flow and the other case models loss of RCS flow due to loss of offsite power early in the transient. Note that a loss of offsite power at anytime during the transient results in a loss of forced RCS cooling and subsequently a less severe reactivity transient. As such, the case without offsite power available is not discussed further in this section.

5. Power peaking factors corresponding to one stuck RCCA and non-uniform core inlet coolant temperatures are determined at end of core life. The coldest core inlet temperatures are assumed to occur in the sector with the stuck rod. The power peaking factors account for the effect of the local void in the region of the stuck control assembly during the return to power phase following the steam line break. This void, in conjunction with the large negative moderator coefficient, partially offsets the effect of the stuck assembly. The power peaking factors depend upon the core power, temperature, pressure, and flow, and, thus, are different for each case studied.

Both of the cases above assume initial hot standby conditions at time zero since this represents the most pessimistic initial condition. Should the reactor be just critical or operating at power at the time of a steam line break, the reactor will be tripped by the normal overpower protection system when power level reaches a trip point. Following a trip at power, the RCS contains more stored energy than at no load, the average coolant temperature is higher than at no load, and there is appreciable energy stored in the fuel. Thus, the additional stored energy is removed via the cooldown caused by the steam line break, before the no load conditions of RCS temperature and shutdown margin assumed in the analyses are reached. After the additional stored energy has been removed, the cooldown and reactivity insertions proceed in the same manner as in the analysis, which assumes no load condition at time zero.

However, since the initial steam generator water inventory is greatest at no load, the magnitude and duration of the RCS cooldown are less for steam line breaks occurring at power.

With respect to steam line breaks initiated from temperatures lower than 547°F in Mode 3, plant-specific evaluations (Reference 77) have been performed that demonstrate that either (1) an automatic MSIV closure signal is generated and the MSIVs achieve fast closure, (2) an automatic MSIV signal is not generated but the MSIVs are fast closed after a ten-minute operator action, or (3) an MSIV closure signal is received via an automatic signal or an operator action, but the MSIVs do not have adequate steam for fast closure. In all cases, fast MSIV closure [cases (1) and (2) above] and no MSIV closure [case (3) above], the transients result in much less limiting minimum DNBR values than the cases initiated from hot standby conditions (T_{AVG} of 547°F and shutdown margin of 1.3%Δk).

6. In computing the steam flow during a steam line break, the Moody Curve (25) for fL/D = 0 is used.

7. Perfect moisture separation in the steam generator is assumed. The assumption leads to conservative results since, in fact, considerable water would be discharged. Water carryover would reduce the magnitude of the

temperature decrease in the core and the pressure increase in the containment.

15.4.2.3 Results

The results presented are a conservative indication of the events which would occur assuming a steam line rupture, since it is postulated that all of the conditions described above occur simultaneously. Additional cases were considered for the Unit 2 Replacement Steam Generators initiated at Hot Full Power conditions. A spectrum of break sizes ranging from 1.4 ft^2 break to a 0.1 ft^2 break was considered. Protection was provided by the Low Steam Pressure and Over Power Delta-T trip functions. The most limiting break size is a 0.92 ft^2 break. In all of the cases initiated at Hot Full Power conditions, adequate and timely protection is generated from the Reactor Protection System such that DNB design basis is met and linear heat generation rate limits are not exceeded.

15.4.2.4 Core Power and Reactor Coolant System Transient

Figures 15.4-51A through 15.4-51C show the RCS transient and core heat flux following a main steam line rupture (complete severance of a pipe) at the exit of the steam generator with the reactor at no load conditions. The break is assumed equivalent to the flow area through the flow restrictor in the steam generator outlet nozzle.

Offsite power is assumed available such that full reactor coolant flow exists. The transient shown assumes an uncontrolled steam release from only one steam generator. Should the core be critical at near zero power when the rupture occurs, the initiation of safety injection by high differential pressure between any steam line and the remaining steam lines, or by high steam flow signals in coincidence with either low-low RCS temperature or low steam line pressure will trip the reactor. Steam release from more than one steam generator will be prevented by automatic trip of the fast action isolation valves in the steam line by the high steam flow signals in coincidence with either low-low RCS temperature or low steam line pressure.

The steam flow on Figure 15.4-51B represents loop steam flow for the faulted loop and one unfaulted loop. All steam generators were assumed to discharge through the break until steam line isolation had occurred.

As shown on Table 15.4-1, the core attains criticality with the RCCAs inserted (with the design shutdown assuming one stuck assembly) before boron solution at 2300 ppm enters the RCS from the SIS. The delay time consists of the time to receive and actuate the safety injection signal and the time to completely open valve trains in the safety injection lines. The safety injection pumps are then ready to deliver flow. At this stage, a further delay time is incurred before 2300 ppm boron solution can be injected to the RCS due to low concentration solution being purged from the safety injection lines. A peak core power well below the nominal full power value is attained.

The calculation assumes the boric acid is mixed with and diluted by the water flowing in the RCS prior to entering the reactor core. The concentration after mixing depends upon the relative flow rates in the RCS and in the SIS. The variation of mass flow rate in the RCS due to water density changes is included in the calculation as is the variation of flow rate from the SIS and the accumulator due to changes in the RCS pressure.

The SIS flow calculation includes the line losses in the system as well as the pump head curve. The accumulators would provide an additional source of borated water if the RCS pressure decreased below 592.2 psia. The core boron concentration is shown on Figure 15.4-51C.

The sequence of events is shown in Table 15.4-1.

The figures presented for this event are taken from explicit calculations performed for Unit 1. Explicit analysis results for Unit 2 are similar in nature to those presented here, and the conclusions presented below apply to both sets of analyses.

15.4.2.5 Margin to Critical Heat Flux

A DNB analysis was performed for the case most critical to DNB. It was found that all cases had a minimum DNBR greater than the design DNBR limit.

15.4.2.6 Offsite Doses

The analysis is performed using Alternate Source Term (AST) methodology, the total effective dose equivalent (TEDE) dose criteria, and plant-specific design information including data applicable to the steam generator (SG) replacement at Salem Unit 2. The dose consequences are analyzed individually for each unit using maximum primary-to-secondary leakage for two cases of iodine concentrations in the primary coolant, resulting from:

1) A pre-accident iodine spike and
2) An accident-initiated concurrent iodine spike.

The pre-accident iodine spike concentrations are assumed to result from a reactor transient which raises the primary coolant concentrations to the maximum values identified in the Technical Specifications and is based on the pre-accident iodine spike activity level of 60 μCi/g of dose-equivalent I-131 with the initial primary coolant noble gas activity based on 1% fuel defects.

The activities leaked to the secondary system via a primary-to-secondary leak of 1 gpm are mixed with the existing activities in the steam generators (initial iodine activity is the Technical Specification limit of 0.1 μCi/g of dose equivalent I-131) and are released to the environment via a steam release.

Offsite power is assumed lost and the main steam condensers are not available for heat removal via a steam dump. Steam is released directly to the environment through the steam generator safety relief valves from the generators isolated from the steam line break. Noble gases from the leaked reactor coolant are released directly to the environment with no retention in the steam generators (SGs). Iodine activity is released from the SGs to the environment in proportion to the steam release rate and the partition coefficient. The iodine partition coefficient during the steaming process is conservatively assumed to be 1.0 for the unaffected steam generators and 1.0 for the affected generator. Thirty-two hours after the accident, the Residual Heat Removal System is assumed to start operation to cool down the plant and no steam is released to the environment after this time.

The accident-initiated or concurrent iodine spike is modeled by assuming that the iodine release rates from the fuel rods into the primary coolant exceed 500 times the equilibrium release rates for a period of eight hours.

Other assumptions, parameters, mass transfer rates, and initial activity inventories used in the analysis are listed in Table 15.4-7 with the consequences listed in Table 15.4-7A.

The radiological consequences for the postulated main steam line break accident assuming either a pre-accident iodine spike or a concurrent iodine spike are less than the guideline values described in Regulatory Guide 1.183 and the limits described in 10 CFR 50.67. The values are listed in Table 15.4-7A.

THIS PAGE INTENTIONALLY BLANK

15.4.3 MAJOR RUPTURE OF A MAIN FEEDWATER LINE

15.4.3.1 Accident Description

A major feedwater line rupture is defined as a break in a feedwater pipe large enough to prevent the addition of sufficient feedwater to the steam generators to maintain shell-side fluid inventory in the steam generators. If the break is postulated in a feedline between the check valve and the steam generator, fluid from the steam generator may also be discharged through the break. Further, a break in this location could preclude the subsequent addition of auxiliary feedwater (AFW) to the affected steam generator. (A break upstream of the feedline check valve would affect the Nuclear Steam Supply System (NSSS) only as a loss of feedwater. (This case is covered by the evaluation in Section 15.2.8).

Depending upon the size of the break and the plant operating conditions at the time of the break, the break could cause either a Reactor Coolant System (RCS) cooldown (by excessive energy discharge through the break) or a RCS heatup. Potential RCS cooldown resulting from a secondary pipe rupture is evaluated in the section, "Major Rupture of a Main Steam Line." Therefore, only the RCS heatup effects are considered for a feedline rupture analysis. A main feedwater line rupture is classified as an ANS Condition IV event.

A feedline rupture reduces the ability to remove heat from the RCS generated by the core. First, feedwater to the steam generators is reduced. Also, inventory in the steam generators may be discharged through the break and would then not be available for decay heat removal. Finally, the break may be large enough to prevent the addition of any main feedwater after the trip.

An AFW System is provided to assure that adequate feedwater is supplied to the steam generators for decay heat removal. Reactor trip and AFW assure that no overpressurization of the RCS or Main Steam System occur (equivalent to 110% of their respective design pressures) and that sufficient liquid in the RCS will be maintained. No bulk boiling should occur in the primary coolant system following a feedline rupture prior to the time that the heat removal capability of the intact steam generators, being fed AFW, exceeds the NSSS heat generation.

The severity of the feedwater line rupture transient depends on a number of parameters including break size, initial reactor power, and credit for various control and safety systems. A number of cases have been analyzed. Results of these analyses show that the most limiting feedwater line ruptures are the double-ended rupture of the largest feedwater line at full power, with and without offsite power available.

The following provides the necessary protection against a main feedwater rupture:

1. A reactor trip on any of the following conditions:
   a. High pressurizer pressure,
   b. Overtemperature delta-T,
   c. Low-low steam generator water level in any steam generator,
   d. Safety injection signals from any of the following:
      1. High steam flow coincident with low steam line pressure
      2. High containment pressure
      3. High steam line differential pressure
      4. Low pressurizer pressure
      5. High steam flow coincident with low-low T_{avg}

      (Refer to Chapter 7 for a description of the actuation system.)

2. An AFW System that starts on a low-low steam generator water level signal to provide an assured source of feedwater to the steam generators for decay heat removal. (Refer to Chapter 10 for a description of the AFW System.)

3. Main steam line isolation from any of the following signals:
   a. High-high containment pressure
   b. High steam flow coincident with low-low T_{avg}
   c. High steam flow coincident with low steam line pressure
   d. Operator action

15.4.3.2 Method of Analysis

A detailed analysis using the LOFTRAN code is performed to determine the plant transient following a feedline rupture. The code describes the plant thermal kinetics, RCS including natural circulation, pressurizer, steam generators and feedwater system, and computes pertinent variables including core average temperature, RCS pressure and pressurizer water volume.

The event is analyzed with and without offsite power available for a double ended rupture of the largest feedwater pipe initiated from full power conditions. Major assumptions made in the analysis are as follows:

1. The plant is initially operating at the NSSS power rating plus uncertainties.

2. Initial reactor coolant average temperature is 5°F above the nominal value, and the initial pressurizer pressure is 50 psi below its nominal value.

3. The pressurizer power operated relief valves (PORVs) are assumed to operate to minimize the RCS pressure and corresponding saturation temperature. Initial pressurizer level is nominal plus 5% span.

4. Initial water mass in the faulted steam generator is nominal plus 5% span.

5. Main feed to all steam generators is assumed to stop at the time the break occurs (all main feedwater spills out through the break).

6. Saturated liquid discharge (i.e., no steam) is initially assumed from the affected steam generator through the feedline rupture. Saturated steam discharge is released after an appropriate level has been reached. This assumption minimizes energy removal from the NSSS during blowdown.

7. A double-ended break is assumed. This maximizes the blowdown discharge rate, which minimizes the steam generator inventory and maximizes the resultant heatup of the reactor coolant.

8. No credit is taken for heat energy deposited in RCS metal during the RCS heatup.

9. No credit is taken for charging or letdown.

10. Following the trip of the reactor coolant pumps for the feedline rupture case without offsite power, there is a flow coastdown until the flow in the loops reaches natural circulation flow conditions.

11. Steam generator heat transfer area is assumed to decrease as the shell-side liquid inventory decreases.

12. Conservative core residual heat generation is assumed based upon long-term operation at the initial power level preceding the trip.

13. The AFW System is assumed to be initiated 85 seconds after the trip. The most limiting single failure for this event is the loss of the motor-driven AFW pump not feeding the faulted steam generator. It takes approximately eleven minutes for the feedlines to become purged and deliver the relatively cold AFW to the unaffected steam generator that receives the most AFW. AFW flow to the faulted steam generators is assumed to be manually isolated ten minutes after the reactor trip.

14. For the most limiting feedwater line break size, steamline isolation would normally be initiated in a matter of seconds from the Containment Pressure High-High, or High Steam Flow/Low Steam Pressure or Low T_{avg} signals. Although the time to isolation would increase with decreasing break size, the severity of the accident would decrease due to the reduced break flow. In order to conservatively bound all potential feedline break sizes, the analysis evaluates the largest potential break but does not credit main steam isolation.

15. Initial water mass in the faulted steam generator is maximized and the initial water mass in the intact steam generators is minimized.

16. Cases with both minimum (beginning-of-life) and maximum (end-of-life) reactivity conditions were assumed. The most limiting results were from the case assuming minimum reactivity feedback coefficients.

17. No credit is taken for potential protection logic signals on high pressurizer pressure, OTΔT or high pressurizer level to mitigate the consequences of the accident.

18. A maximum steam generator tube plugging level of 10% was modeled.

No reactor control systems are assumed to function except the pressurizer power operated relief valves. The only engineered safety features assumed to function are the AFW and Safety Injection Systems.

15.4.3.3 Results

Calculated plant parameters following a major feedwater pipe rupture for the limiting case, where offsite power is available, are presented in Figures 15.4-60A through 15.4-60D. Results for the case without offsite power are presented in Figures 15.4-60E through 15.4-60H. The calculated sequence of events for these cases are listed in Table 15.4-1. The results show that pressures in the RCS and main steam system remain below 110% of their respective design pressures and that the RCS hot legs remain subcooled.

Feedline Rupture with Offsite Power Available

Reactor Coolant System pressure, temperature, and pressurizer water volume initially decrease due to the increased secondary side heat removal as steam from the three unfaulted steam generators flows to the depressurizing, faulted steam generator. The secondary side inventory reduction then leads to a primary system heatup, so RCS pressure, temperature, and pressurizer water volume all increase. Approximately eleven minutes after reactor trip, the steam generators repressurize slightly with the addition of relatively cold AFW. The heat removal capability of the secondary side becomes sufficient to remove the core decay heat plus pump heat from the RCS at approximately 5000 seconds. The results show that the core remains covered at all times and that no hot leg saturation occurs.

The pressurizer water volume increases slightly in response to the heatup, but the steam bubble in the pressurizer is maintained throughout the transient. Pressurizer filling is not predicted in either case (with or without offsite power). Therefore, no water relief through the pressurizer relief or safety valves occurs.

Feedline Rupture without Offsite Power

The system response following a feedwater line rupture without offsite power available is similar to the case with offsite power available. However, due to the loss of offsite power (assumed to occur at the time of reactor trip), the reactor coolant pumps coast down. This results in a reduction in RCS heat generation equal to the amount produced by pump operation but also results in reduced heat transfer due to the loss of forced flow. The penalty associated with the reduced heat transfer is easily offset by the reduction in heat generation due to the tripped RCPs. Hence, this case is less limiting than the case where offsite power is available. The results show that the core remains covered at all times and that no hot leg saturation occurs.

The results presented for this event are taken from explicit calculations performed for Unit 2. Explicit analysis results for the Unit 1 replacement steam generators are similar in nature to those presented here, and the conclusions presented below apply to both sets of analyses.

15.4.3.4 Conclusion

Results of the analysis show that for the postulated feedline rupture, the assumed AFW System capacity is adequate to remove decay heat, to prevent overpressurization of the RCS and Main Steam System, and to prevent hot leg saturation.

The analysis of the FLB (Feedwater Line Break) event that is documented in this section of the UFSAR has been evaluated by Westinghouse with respect to an issue associated with the method used to calculate the steam generator secondary-side mass at the time the LOW-LOW SG water level setpoint is reached for Salem Unit 2. The results of this evaluation demonstrate that all applicable safety analysis criteria continue to be satisfied and the conclusions of the UFSAR remain valid. The trip mass issue does not impact the Salem Unit 1 analyses.

15.4.4 Steam Generator Tube Rupture

15.4.4.1 General

The accident examined is the complete severance of a single steam generator tube. The accident is assumed to take place at power with the reactor coolant contaminated with fission products corresponding to continuous operation with a limited amount of defective fuel rods. The accident leads to an increase in contamination of the secondary system due to leakage of radioactive coolant from the RCS. In the event of a coincident loss of offsite power or failure of the condenser dump system, discharge of activity to the atmosphere takes place via the steam generator safety and/or power-operated relief valves.

In view of the fact that the steam generator tube material is Inconel 600 (Unit 1) and Inconel 690 (Unit 2) and is a highly ductile material, it is considered that the assumption of a complete severance is somewhat conservative. The more probable mode of tube failure would be one or more minor leaks of undetermined origin. Activity in the Steam and Power Conversion System is subject to continual surveillance and an accumulation of minor leaks which exceed total primary-to-secondary leakage through the steam generators, as defined in the Technical Specifications, is not permitted during the unit operation.

The main objective of the operator is to determine that a steam generator tube rupture has occurred, and to identify and isolate the faulty steam generator on a restricted time scale in order to minimize contamination of the secondary system and ensure termination of radioactive release to the atmosphere from the faulty unit. The recovery procedure can be carried out on a time

scale which ensures that break flow to the secondary system is terminated before water level in the affected steam generator rises into the main steam pipe. Sufficient indications and controls are provided to enable the operator to carry out these functions satisfactorily.

Consideration of the indications provided at the control board, together with the magnitude of the break flow, leads to the conclusion that the isolation procedure can be completed within the time requirements set forth in this analysis.

15.4.4.2 Description of Accident

Assuming normal operation of the various plant control systems the following sequence of events is initiated by a tube rupture:

1. Pressurizer low pressure and low level alarms are actuated and, prior to plant trip, charging pump flow increases in an attempt to maintain pressurizer level. On the secondary side there is a steam flow/feedwater flow mismatch before trip as feedwater flow to the affected steam generator is reduced due to the additional break flow which is now being supplied to that unit.

2. Continued loss of reactor coolant inventory leads to falling pressure and level in the pressurizer until a reactor trip signal is generated by low pressurizer pressure or overtemperature ΔT. Resultant plant cooldown following reactor trip leads to a rapid decrease in pressurizer level, and the safety injection signal, initiated by low pressurizer pressure, follows soon after the reactor trip. The safety injection signal automatically terminates normal feedwater supply and initiates auxiliary feedwater addition on low steam generator level.

3. The steam generator blowdown liquid monitor and the condenser offgas radiation monitor will alarm, indicating a sharp increase in radioactivity in the secondary system. The steam generator blowdown liquid monitor will automatically terminate steam generator blowdown.

4. The reactor trip automatically trips the turbine and if offsite power is available the steam dump valves open permitting steam dump to the condenser. In the event of a coincident loss of offsite power, the steam dump valves would automatically close to protect the condenser. The steam generator pressure would rapidly increase resulting in steam discharge to the atmosphere through the steam generator safety and/or power-operated relief valves.

5. The following sequence of operator actions is initiated to terminate steam release from the faulted steam generator and primary to secondary leakage:

   a. Identification of the faulted steam generator (A primary indication of a steam generator tube rupture event is steam generator water level increasing in an uncontrolled manner.)

   b. Isolation of the faulted steam generator

   c. Cooldown of the RCS using the non-faulted steam generator to assure 20°F subcooling at the faulted steam generator pressure

   d. Controlled depressurization of the RCS to the faulted steam generator pressure

   e. Subsequent termination of safety injection flow

Sufficient indications and controls are provided at the control board to enable the operator to complete these functions satisfactorily within the time requirements set forth in this analysis.

15.4.4.3 Method of Analysis

In estimating the mass transfer from the RCS through the broken tube, the following assumptions are made:

1. Reactor trip occurs automatically as a result of low pressurizer pressure.

2. Following the initiation of the safety injection signal, all centrifugal charging pumps are actuated and continue to deliver flow until the rupture flow has been terminated. Pump flow is secured procedurally.

3. After reactor trip the break flow reaches equilibrium at the point where incoming safety injection flow is balanced by outgoing break flow.

4. The steam generators are controlled at the safety valve setting rather than the power-operated relief valve setting. Mass and energy balance calculations are performed to determine primary to secondary mass release and to determine amount of steam vented from each of the steam generators.

5. The delivered safety injection flow rates consider maximum performance from the centrifugal charging pumps and safety injection pumps. The contribution from the RHR pumps is not included since RCS pressure will remain their shutoff head during a steam generator tube rupture accident transient.

15.4.4.4 Results

The previous assumptions lead to conservative upper limit estimates for the total amount of reactor coolant transferred to the secondary side of the faulty steam generator as a result of a tube rupture accident.

An evaluation (Reference 72) with respect to the operator action time assumption for isolation of the faulted steam generator has been applied to this analysis. The current licensed method used to calculate the mass released from the faulted steam generator, as has been used for this event analysis, has been shown to be conservative with respect to mass released over an assumed 30-minute operator action time. The amount of mass released, as predicted by the current licensed method, from the faulted steam generator over the 30-minute assumed operator action time is much larger than expected mass release if the transient was to be modeled explicitly. An explicit modeling method was used to evaluate the equivalent amount of operator action time that would be available that yields an equivalent mass release to that calculated by using a 30-minute operator action time with the current licensed method. This time was found to be 55 minutes. Since the operator is able to isolate the faulted steam generator within 50 minutes from event initiation, the amount of mass released is not expected to exceed that calculated using a 30-minute isolation time with the current licensed method. Therefore, the 30-minute assumption used in the current licensed analysis for the time to isolate the faulted steam generator is conservative since it results in a bounding mass release calculation.

15.4.4.5 Environmental Consequences of a Tube Rupture

These analyses incorporate one percent defective fuel clad, and steam generator leakage prior to the release for a time sufficient to establish equilibrium-specific activity levels in the secondary system.
The analysis is performed using Alternate Source Term (AST) methodology, the total effective dose equivalent (TEDE) dose criteria, and plant-specific design information including data applicable to the steam generator (SG) replacement at Salem Unit 2. The dose consequences are analyzed individually for each unit using maximum primary-to-secondary leakage for two cases of iodine concentrations in the primary coolant, resulting from:

1) A pre-accident iodine spike
2) An accident-initiated concurrent iodine spike

The pre-accident iodine spike concentrations are assumed to result from a reactor transient which raises the primary coolant concentrations to the maximum values identified in the Technical Specifications.

The initial primary coolant iodine activity is based on the pre-accident iodine spike activity level of 60 μCi/g of dose equivalent I-131 with the initial primary coolant noble gas activity based on 1% fuel defects. The activities leaked to the secondary system via a primary-to-secondary leak are mixed with the existing activities in the steam generators (initial iodine activity is the Technical Specification limit of 0.1 μCi/g of dose equivalent I-131) and are released to the environment via a steam release.

Offsite power is assumed lost and the main steam condensers are not available for heat removal via a steam dump. Steam is released directly to the environment through the steam generator safety relief valves for the intact steam generators. Noble gases from the leaked reactor coolant are released directly to the environment with no retention in the Steam Generators (SGs). Iodine activity is released from the SGs to the environment in proportion to the steam release rate and the partition coefficient. The iodine partition coefficient during the steaming process is conservatively assumed to be 0.1. Thirty-two hours after the accident, the Residual Heat Removal System is assumed to start operation to cool down the plant and no steam is released to the environment after this time from the intact steam generators. The faulted steam generator is assumed to be isolated within an acceptable operator action time.

The accident-initiated or concurrent iodine spike is modeled assuming that the iodine release rates from the fuel rods into the primary coolant are 335 times the equilibrium release rates for a period of eight hours.

Other assumptions, parameters, mass transfer rates, and initial activity inventories used in the analysis are listed in Table 15.4-7B with the consequences listed in Table 15.4-7C.

15.4.4.6 Conclusions

A steam generator tube rupture will cause no subsequent damage to the RCS or the reactor core. An orderly recovery from the accident can be completed even assuming simultaneous loss of offsite power. Offsite dose consequences are calculated based on conservative estimates of reactor coolant

transferred to the secondary side of the affected steam generator following the steam generator tube rupture accident are less than the guideline values described in Regulatory Guide 1.183 and the limits described in 10 CFR 50.67. The values are listed in Table 15.4-7C.

15.4.5 Single Reactor Coolant Pump Locked Rotor and Reactor Coolant Pump Shaft Break

15.4.5.1 Identification of Causes and Accident Description

The events postulated are an instantaneous seizure of a reactor coolant pump rotor and a reactor coolant pump shaft break. Following either event, flow through the affected reactor coolant loop is rapidly reduced, resulting in the initiation of a reactor trip on a low flow signal and subsequent turbine trip.

Following initiation of reactor trip, heat stored in the fuel rods continues to be transferred to the coolant, causing the coolant to expand. At the same time, heat transfer to the shell side of the steam generator in the faulted loop is reduced. This reduction in primary heat removal capability is initially caused by the decrease in primary coolant flow, which reduces the tube side film coefficient. Following turbine trip, primary heat removal is further impaired as the shell side temperature in all steam generators increases. Rapid expansion of the coolant in the reactor core, caused by flow reduction and degraded primary-to-secondary heat removal, results in an insurge into the pressurizer and an RCS pressure increase.

The insurge into the pressurizer sequentially compresses the steam volume, actuates the Automatic Spray System, opens the power-operated relief valves, and opens the pressurizer safety valves. The power-operated relief valves are designed for reliable operation and would be expected to function properly during the accident. However, for conservatism, their pressure-reducing effect, as well as the pressure-reducing effect of the spray, is not included in the analysis.

The consequences of a reactor coolant pump shaft break are similar

to those that follow a locked rotor event. With a broken shaft, the impeller is free to spin, as opposed to its being fixed in position during the locked rotor event. Therefore, the initial rate of reduction in core flow is greater during a locked rotor event than in a pump shaft break event because the fixed shaft causes greater resistance than a free spinning impeller early in the transient, when flow through the affected loop is in the positive direction. As the transient continues, the flow direction through the affected loop is reversed. If the impeller is able to spin free, the flow to the core will be less than that available with a fixed shaft during periods of reverse flow in the affected loop. Because the peak pressure, clad temperature, and maximum number of fuel rods in DNB occur very early in the transient, before periods of any appreciable reverse flow, the reduction in core flow during the period of forward flow in the affected loop dominates the severity of the results. Therefore, the bounding results for the locked rotor transients also are applicable to the reactor coolant pump shaft break.

The locked rotor accident is an ANS Condition IV event and, as such, is analyzed to demonstrate that the peak RCS pressure reached during the transient is less than that which would cause stresses to exceed the faulted condition stress limits and compromise the integrity of the primary coolant system. In addition, it must be demonstrated that the core will remain intact, with no loss of core cooling capability, and that radioactive releases do not exceed acceptable levels.

15.4.5.2 Method of Analysis

Two digital computer codes are used to analyze this transient. The LOFTRAN code calculates the resulting loop and core flow transients following the event, the time of reactor trip based on loop flow transients, and the nuclear power following reactor trip and determines the peak pressure. Thermal behavior of the fuel located at the core hot spot is investigated with the FACTRAN code, using the core flow and nuclear power calculated by LOFTRAN. The FACTRAN

code includes the use of a film boiling heat transfer coefficient.

The case of all loops operating and one locked rotor is analyzed as follows. At the beginning of the postulated event, i.e., when the shaft in one of the reactor coolant pumps is assumed to seize, the plant is assumed to be in operation under the most adverse steady-state operating conditions, with respect to the margin to DNB. These conditions include maximum steady-state power level (including 2-percent uncertainty), thermal design flow, minimum steady-state pressure, and maximum steady-state coolant average temperature.

There is no postulated single failure which will increase the severity of the consequences following this event.

When the peak pressure is evaluated, the initial pressure is conservatively estimated to be 50 psi above the nominal pressure of 2250 psia to allow for errors in the pressurizer pressure measurement and control channels. This is done to obtain the highest possible rise in coolant pressure during the transient. The pressurizer pressure and peak RCS pressure responses for the case analyzed are shown on Figures 15.4-68 and 15.4-70.

Evaluation of Pressure Transient

After pump seizure, the neutron flux is rapidly reduced by control rod insertion. Rod motion begins 1 second after flow in the affected loop reaches 87 percent of nominal flow. Offsite power is assumed to be lost immediately at reactor trip, resulting in a coastdown of the other three reactor coolant pumps. No credit is taken for the pressure reducing effect of the pressurizer relief valves, pressurizer spray, or steam dump.

Although these operations are expected to occur and would result in a lower RCS peak pressure, an additional degree of conservatism is provided by ignoring their effects.

The pressurizer safety valves are assumed to initially open at 2575 psia and achieve rated flow at 2650 psia. This analysis assumed an initial pressurizer pressure of 2300 psia.

Evaluation of DNB in the Core During the Accident

For this accident, DNB is assumed to occur in the core; therefore, an evaluation of the consequences with respect to fuel rod thermal transients is performed. Two DNB-related analyses are performed. The first incorporates the assumption of rods going into DNB as a conservative initial condition to determine the clad temperature and zirconium water reaction. This analysis assumed an initial pressurizer pressure of 2200 psia. Result obtained from the analysis of this hot-spot condition represent the upper limit with respect to clad temperature and zirconium water reaction. In this analysis, the rod power at the hot spot is assumed to be 3.0 times the average rod power (i.e., F_Q = 3.0) at the initial core power level.

The second analysis is performed to determine what percentage of rods, if any, is expected to be in DNB during the transient. Analyses to determine this percentage for the locked rotor and shaft break accidents use three digital computer codes. In addition to the LOFTRAN and FACTRAN codes, the THINC code is used to calculate DNBR during the transient, based on flow calculated by LOFTRAN and heat flux calculated by FACTRAN. Consistent with RTDP (Reference 76), initial reactor power, RCS pressure, and RCS temperature are assumed to be at their nominal values.

Film Boiling Coefficient

The film boiling coefficient is calculated in the FACTRAN code using the Bishop-Sandburg-Tong film boiling correlation. The fluid properties are evaluated at film temperatures (average between wall and bulk temperatures). The program calculates the film coefficient at every time step, based upon the actual heat transfer conditions at the time. Neutron flux, system pressure, bulk density, and mass

flow rate as a function of time are used as program inputs.

For this analysis, the initial values of pressure and bulk density are used throughout the transient, since they are the most conservative with respect to clad temperature response. For conservatism, DNB was assumed to start at the beginning of the accident.

Fuel Clad Gap Coefficient

The magnitude and time dependence of the heat transfer coefficient between fuel and clad (gap coefficient) have a pronounced influence on thermal results. The larger the value of the gap coefficient, the more heat is transferred between pellet and clad. Based on investigations on the effect of the gap coefficient upon the maximum clad temperature during the transient, the gap coefficient was assumed to increase from a steady-state value consistent with the initial fuel to 10,000 BTU/hr-ft-°F at the initiation of the transient. Thus, the large amount of energy stored in the fuel is released to be clad at the initiation of the transient because of the small gap coefficient initially assumed.

Zirconium-Steam Reaction

The zirconium-steam reaction can become significant above clad temperatures of 1800°F. In order to take this phenomenon into account, the Baker-Just parabolic rate equation shown below is used to define the rate of the zirconium-steam reaction.

$$d(w^2)/dt = 33.3 \times 10^6 \times \exp - [(45{,}000)/1.986T]$$

where: w = amount reacted, mg/cm^2

t = time, sec

T = temperature, °K

The reaction heat is 1510 cal/gm.

15.4.5.3 Locked Rotor Results

The locked rotor/shaft break analysis is performed to demonstrate that the peak RCS pressure reached during the transient is less than that which would cause the stresses to exceed the faulted condition stress limits. In addition, it must be shown that a coolable core geometry is maintained and that the radioactive release is within acceptable levels.

To demonstrate that the above conditions are met following a locked rotor/shaft break event, the following criteria are used:

1. RCS maximum pressure ≤ 110-percent design (2750 psia) (110-percent design pressure < faulted condition stress limit).

2. Peak clad temperature ≤ 2700°F.

3. Maximum zirconium-water reaction ≤ 16 percent.

4. Offsite radiological release within 10CFR50.67 limits and Regulatory Guide 1.183 guidance.

The transient response of the reactor coolant system during the locked rotor/shaft break incidents analyzed is shown on Figures 15.4-68 through 15.4-70. The peak RCS pressure occurs at the pump outlet. The pump outlet pressure transient is shown on Figure 15.4-70. The clad temperature transient calculated is shown on Figure 15.4-69. The maximum RCS pressure, maximum clad temperature, amount of zirconium-water reaction, and percent of fuel in DNB are listed in Table 15.4-6. The calculated sequence of events is shown in Table 15.4-1.

The results of the locked rotor/shaft break analysis demonstrate that the peak pressure reached is less than that which would cause the faulted condition stress limits to be exceeded. In addition, it was determined that the peak clad surface temperature calculated for the hot spot is less than 2700°F, and the maximum number of fuel rods which undergo DNB will not exceed 5 percent of the total fuel rods.

An analysis of the radiological dose consequences of this event, using five percent of the fuel rods in the core experienced clad failure, the Alternate Source Term (AST) methodology, the total effective dose equivalent (TEDE) dose criteria, and plant-specific design information including data applicable to the steam generator (SG) replacement at Salem Unit 2. The dose consequences are analyzed individually for each unit using maximum primary-to-secondary leakage. Other assumptions, parameters, mass transfer rates, and initial activity inventories used in the locked rotor analysis demonstrated that the maximum dose that the general public could receive would be less than 10 CFR 50.67 limits and Regulatory Guide 1.183 guidelines. The values are listed in Table 15.4-6B.

15.4.5.4 Conclusions

1. The integrity of the primary coolant system is not endangered since the peak RCS pressure reached during any of the transients is less than that which would cause stresses to exceed the faulted condition stress limits.

2. The core will remain in place and intact with no loss of core cooling capability since the peak clad surface temperature calculated for the hot spot during the worst transient remains considerably less than 2700°F (the temperature at which clad embrittlement may be expected) and the amount of zirconium-steam reacted is small.

3. The maximum dose that the general public could receive from this event would be within 10 CFR 50.67 limits and Regulatory Guide 1.183 guidelines, since less than five percent of the fuel rods were calculated to have a DNB ratio less than the design limit.

15.4.6 Fuel Handling Accident

15.4.6.1 Identification of Causes and Accident Description

The accident is defined as dropping of a spent fuel assembly onto the spent fuel pit floor in the fuel handling building or inside containment resulting in the rupture of the cladding of all the fuel rods in the assembly despite many administrative controls and physical limitations imposed on fuel handling operations. All refueling operations are conducted in accordance with prescribed procedures under direct surveillance of a supervisor.

15.4.6.2 Analysis of Effects and Consequences

In 2002, the accident source term used in the fuel handling accident design basis radiological analyses was replaced with an alternative source term (AST) pursuant to 10CFR50.67, "Accident Source Term", and the potential radiological consequences were reevaluated. The guidance provided in Regulatory Guide 1.183, Alternative Radiological Source Terms for Evaluating Design Basis Accidents at Nuclear Power Reactors, July 2002, was used in the re-evaluation.

Method of Analysis

Evaluation of this accident in the fuel handling building was based on the following data and assumptions:

1. The reactor was assumed to have been operating at 3600 Mw thermal power prior to shutdown.

2. Deleted.

3. The failed fuel decay time considered prior to accident was 24 hours.

4. The fuel handling accident was assumed to result in the release of the gaseous fission products contained in the fuel/cladding gaps of all the 264 fuel rods in a peak-power fuel assembly (radial peaking factor of 1.70).

5. The fractions of the core inventory assumed to be in the gaps for the various radionuclides are given in Table 3 of Regulatory Guide 1.183. The release fractions from the table were used in conjunction with the fission product inventory calculated with the maximum core radial peaking factor.

6. Core fission product inventories (curies) were estimated using the ORIGEN Code.

7. The chemical form of radioiodine released from the fuel to the spent fuel pool was assumed to be 95% cesium iodide (CsI), 4.85% elemental iodine, and 0.15% organic iodide. The CsI released from the fuel was assumed to completely dissociate in the pool water. Because of the low pH of the pool water, the iodine was assumed to re-evolve as elemental iodine. This was assumed to occur instantaneously.

8. The decontamination factors for the elemental and inorganic iodine species were assumed to be 500 and 1, respectively, giving an overall effective decontamination factor of 200 (i.e., 99.5% of the total iodine released from the damaged rods is retained by the water). This difference in decontamination factors for elemental (99.85%) and organic iodine (0.15%) species results in the iodine above the water being composed of 57% elemental and 43% organic species. The pool decontamination factor for noble gases is 1.

9. The radioactive material that escapes was assumed to be released to the environment over a two-hour period and no filtration was credited for reduction in the amount of radioactive material released to the environment.

10. The atmospheric diffusion factors used were 1.30×10^{-4} s/m^3 at the exclusion area boundary (EAB) and 1.86×10^{-5} s/m^3 for the low population zone (LPZ) and breathing rate used was 3.5×10^{-4} m^3/s.

11. The values of average energy per disintegration, and dose conversion factors used are listed in Table 15.4-5A.

The following additional information relates to an evaluation of a fuel handling accident inside containment:

1. An instantaneous puff release of noble gases and radioiodine from the gap and plenum of failed fuel rods is assumed.

2. All airborne activity reaching the containment atmosphere is assumed to exhaust to the environment within 2 hours without filtration.

3. Offsite doses are computed using the RADTRAD (Version 3.02 and 3.03) computer code (References 71, 78 and 82).

15.4.6.3 Conclusions

15.4.6.3.1 Radiation Doses

The doses at the EAB and LPZ from the specified fuel handling accidents are listed below. The doses are based on the release of all gaseous fission product activity in the gaps of all 264 fuel rods in the highest-power assembly.

For a fuel handling accident in the fuel handling building:

1.28 rem TEDE (EAB)

0.18 rem TEDE (LPZ)

For a fuel handling accident inside containment:

1.26 rem TEDE (EAB)

0.18 rem TEDE (LPZ)

These potential doses are within Regulatory Guide 1.183 guidelines.

15.4.7 Rupture of a Control Rod Drive Mechanism Housing (Rod Cluster Control Assembly Ejection)

15.4.7.1 Identification of Causes and Accident Description

This accident is defined as the mechanical failure of a control rod mechanism pressure housing resulting in the ejection of a RCCA and drive shaft. The consequence of this mechanical failure is a rapid reactivity insertion together with an adverse core power distribution, possibly leading to localized fuel rod damage.

Design Precautions and Protection

Certain features of the Salem Plants are intended to preclude the possibility of a rod ejection accident, or to limit the consequences if the accident were to occur. These include a sound, conservative mechanical design of the rod housings, together with a thorough quality control (testing) program during assembly, and a nuclear design which lessens the potential ejection worth of RCCAs and minimizes the number of assemblies inserted at power.

Mechanical Design

The mechanical design is discussed in Section 3.2. Mechanical design and quality control procedures intended to preclude the possibility of a RCCA drive mechanism housing failure sufficient to allow a RCCA to be rapidly ejected from the core are listed below:

1. Each full-length CRDM housing is completely assembled and shop tested in accordance with the requirements of the applicable code of construction.

2. During original plant construction, the mechanism housings were individually hydrotested as they were attached to the head adapters in the reactor vessel head, and checked during the hydrotest of the completed RCS. For the replacement RVCH installed in Unit 2, the welds, joining the CRDM housings to the head adapters are tested by performing a leak test at normal operating pressure/normal operating temperature in accordance with applicable ASME Code Section XI requirements.

3. Stress levels in the mechanism are not affected by anticipated system transients at power, or by the thermal movement of the coolant loops. Moments induced by the design earthquake can be accepted within the allowable primary working stress range specified by the ASME Code, Section III, for Class 1 components.

4. The latch mechanism housing and rod travel housing are each a single length of forged Type-304 stainless steel.

This material exhibits excellent notch toughness at all temperatures which will be encountered.

A significant margin of strength in the elastic range together with the large energy absorption capability in the plastic range gives additional assurance that gross failure of the housing will not occur. The joints between the latch mechanism housing and head adapter, and between the latch mechanism housing and rod travel housing, are threaded joints reinforced by canopy-type rod welds. Administrative regulations require periodic inspections of these (and other) welds.

Nuclear Design

Even if a rupture of a RCCA drive mechanism housing is postulated, the operation of a plant utilizing chemical shim is such that the severity of an ejected RCCA is inherently limited. In general, the reactor is operated with the RCCAs inserted only far enough to permit load follow. Reactivity changes caused by core depletion and xenon transients are compensated by boron changes. Further, the location and grouping of control rod banks are selected during the nuclear design to lessen the severity of a RCCA ejection accident. Therefore, should a RCCA be ejected from its normal position during high power operation, only a minor reactivity excursion, at worst, could be expected to occur.

However, it may be occasionally desirable to operate with larger than normal insertions. For this reason, a rod insertion limit is defined as a function of power level. Operation with the rod cluster control assemblies above this limit guarantees adequate shutdown capability and acceptable power distribution. The position of all RCCAs is continuously indicated in the Control Room. An alarm will occur if a bank of RCCAs approaches its insertion limit or if one assembly deviates from its bank. There are low and low-low level insertion monitors with visual and audio signals. Operating instructions require boration at low level alarm and emergency boration at the low-low alarm.

Reactor Protection

The reactor protection in the event of a rod ejection accident has been described in Reference 29. The protection for this accident is provided by the power range high neutron flux trip (high and low setting) and high rate of neutron flux increase trip. These protection functions are described in detail in Section 7.

Effects on Adjacent Housings

Disregarding the remote possibility of the occurrence of a RCCA mechanism housing failure, investigations have shown that failure of a housing due to either longitudinal or circumferential cracking is not expected to cause damage to adjacent housings leading to increased severity of the initial accident.

Limiting Criteria

Due to the extremely low probability of a RCCA ejection accident, limited fuel damage is considered an acceptable consequence.

Comprehensive studies of the threshold of fuel failure and of the threshold of significant conversion of the fuel thermal energy to mechanic energy, have been carried out as part of the SPERT project by the Idaho Nuclear Corporation (30). Extensive tests of UO_2 zirconium clad fuel rods representative of those in pressurized water reactor-type cores have demonstrated failure thresholds in the range of 240 to 257 cal/gm. However, other rods of a slightly different design have exhibited failures as low as 225 cal/gm. These results differ significantly from the TREAT (31) results, which indicated a failure threshold of 280 cal/gm. Limited results have indicated that this threshold decreases by 10 percent with fuel burnup. The clad failure mechanism appears to be melting for zero burnup rods and brittle fracture for irradiated rods. Also important is the conversion ratio of thermal to mechanical energy. This ratio becomes marginally detectable above 300 cal/gm for unirradiated rods and 200 cal/gm

for irradiated rods; catastrophic failure, (large fuel dispersal, large pressure rise) even for irradiated rods, did not occur below 300 cal/gm.

In view of the above experimental results, conservative criteria are applied to ensure that there is little or no possibility of fuel dispersal in the coolant, gross lattice distortion, or severe shock waves. These criteria are:

1. Average fuel pellet enthalpy at the hot spot below 225 cal/gm for unirradiated fuel and 200 cal/gm for irradiated fuel.

2. Peak reactor coolant pressure less than that which would cause stresses to exceed the faulted condition stress limits.

3. Fuel clad damage will be limited to less than 10 percent of the fuel volume at the hot spot even if the average fuel pellet enthalpy is below the limits of Criterion 1 above.

(This text has been deleted)

15.4.7.2 Analysis of Effects and Consequences

Method of Analysis

The analysis of the RCCA ejection accident is performed in two stages, first an average core nuclear power transient calculation and then a hot spot heat transfer calculation. The average core calculation is performed using spatial neutron kinetics methods to determine the average power generation with time including the various total core feedback effects, i.e., Doppler reactivity and moderator reactivity. Enthalpy and temperature transients in the hot spot are then determined by multiplying the average core energy generation by the hot-channel factor and performing a fuel rod transient heat transfer calculation. The power distribution calculated without feedback is pessimistically assumed to persist throughout the transient.

A detailed discussion of the method of analysis can be found in Reference 32.

Average Core Analysis

The spatial kinetics computer code, TWINKLE (33), is used for the average core transient analysis. This code uses cross sections generated by LEOPARD (34) to solve the two group neutron diffusion theory kinetic equations in one, two or three spatial dimensions (rectangular coordinates) for six delayed neutron groups and up to 2000 spatial points. The computer code includes a detailed multi-region, transient fuel-clad-coolant heat transfer model for calculating pointwise Doppler and moderator feedback effects.

In this analysis, the code is used as a one-dimensional axial kinetics code since it allows a more realistic representation of the spatial effects of axial moderator feedback and RCCA movement and the elimination of axial feedback weighting factors. However, since the radial dimension is missing, it is still necessary to employ very conservative methods (described below) of calculating the ejected rod worth and hot-channel factor. Further description of TWINKLE appears in Section 15.1.9.7.

Hot Spot Analysis

The average core energy addition, calculated as described above, is multiplied by the appropriate hot-channel factors, and the hot spot analysis is performed using the detailed fuel and clad transient heat transfer computer code, FACTRAN (28). This computer code calculates the transient temperature distribution in a cross section of a metal clad UO_2 fuel rod, and the heat flux at the surface of the rod, using as input the nuclear power versus time and local coolant conditions. The zirconium-water reaction is explicitly represented, and all material properties are represented as functions of temperature. A parabolic radial power generation is used within the fuel rod.

FACTRAN uses the Dittus-Boelter or Jens-Lottes correlation to determine the film transfer before DNB, and the Bishop-Sandburg-Tong correlation (35) to determine the film

boiling coefficient after DNB. The DNB heat flux is not calculated; instead the code is forced into DNB by specifying a conservative DNB heat flux. The gap heat transfer coefficient can be calculated by the code; however, it is adjusted in order to force the full power steady state temperature distribution to agree with that predicted by design fuel heat transfer codes presently used by Westinghouse.

For full power cases, the design initial hot-channel factor (F_Q) is input to the code. The hot-channel factor during the transient is assumed to increase from the steady state design value to the maximum transient value in 0.1 second, and remain at the maximum for the duration of the transient. This is conservative, since detailed spatial kinetics models show that the hot-channel factor decreases shortly after the nuclear power peak due to power flattening caused by preferential feedback in the hot-channel (32). Further description of FACTRAN appears in Section 15.1.9.1.

System Overpressure Analysis

Because safety limits for fuel damage specified earlier are not exceeded, there is little likelihood of fuel dispersal into the coolant. The pressure surge may therefore be calculated on the basis of conventional heat transfer from the fuel and prompt heat generation in the coolant.

The pressure surge is calculated by first performing the fuel heat transfer calculation to determine the average and hot spot heat flux versus time. Using this heat flux data, a THINC calculation is conducted to determine the volume surge. Finally, the volume surge is simulated in a plant transient computer code. This code calculates the pressure transient taking into account fluid transport in the system, heat transfer to the steam generators, and the action of the pressurizer spray and pressure relief valves. No credit is taken for the possible pressure reduction caused by the assumed failure of the control rod pressure housing.

Calculation of Basic Parameters

Input parameters for the analysis are conservatively selected on the basis of calculated values for this type of core. The more important parameters are discussed below. Table 15.4-12 presents the parameters used in this analysis.

Ejected Rod Worths and Hot-Channel Factors

The values for ejected rod worths and hot-channel factors are calculated using a synthesis of one-dimensional and two dimensional calculations. Standard nuclear design codes are used in the analysis. No credit is taken for the flux flattening effects of reactivity feedback. The calculation is performed for the maximum allowed bank insertion at a given power level, as determined by the rod insertion at a given power level, as determined by the rod insertion limits. Adverse Xenon distributions and part length rod positions are considered in the calculation.

The total transient hot channel factor F_Q, is then obtained by combining the axial and radial factors.

Appropriate margins are added to the results to allow for calculational uncertainties, including an allowance for nuclear power peaking due to fuel densification.

Reactivity Feedback Weighing Factors

The largest temperature rises, and hence the largest reactivity feedbacks occur in channels where the power is higher than average. Since the weight of a region is dependent on flux, these regions have high weights. This means that the reactivity feedback is larger than that indicated by a simple single channel analysis. Physics calculations were carried out for temperature changes with a flat temperature distribution, and with a large number of axial and radial temperature distributions. Reactivity

changes were compared and effective weighting factors determined. These weighting factors take the form of multipliers which when applied to single channel feedbacks correct them to effective whole core feedbacks for the appropriate flux shape. In this analysis, since a one-dimensional (axial) spatial kinetics method is employed, axial weighting is not used. In addition, no weighting is applied to the moderator feedback. A conservative radial weighting factor is applied to the transient fuel temperature to obtain an effective fuel temperature as a function of time accounting for the missing spatial dimension. These weighting factors were shown to be constructive compared to three dimensional analysis.

Moderator and Doppler Coefficient

The critical boron concentrations at the beginning-of-life (BOL) and EOL were adjusted in the nuclear code in order to obtain moderator density coefficient curves which are conservative compared to actual design conditions for the plant. As discussed above, no weighting factor is applied to these results.

The Doppler reactivity defect is determined as a function of power level using the one-dimensional steady state computer code with a Doppler weighting factor of 1.0. The resulting curve is conservative compared to design predictions for this plant. The Doppler weighting factor should be larger than 1.0 (approximately 1.3), just to make the present calculation agree with design predictions before ejection. This weighting factor used in the analysis is presented in Table 15.4-12.

Delayed Neutron Fraction

Calculations of the effective delayed neutron fraction (β_{eff}) typically yield values of 0.70 percent at BOL and 0.50 percent at EOL for the first cycle. The accident is sensitive to β if the ejected rod worth is nearly equal to or greater than β as in zero power transients. In order to allow for future fuel cycles,

pessimistic estimates for β of 0.48 percent at beginning of cycle and 0.40 percent at end of cycle were used in the analysis.

Trip Reactivity Insertion

The trip reactivity insertion is assumed to be 4 percent from hot full power and 2 percent from hot zero power, including the effect of one stuck rod in each case. The analyses assume that the start of rod motion occurs 0.5 second after the high neutron flux trip point is reached. The analyses also assume a total rod insertion time of 2.7 seconds, from the start of rod motion to the entrance of the dashpot. This conservative insertion rate includes over a 1 second delay from when the trip setpoint is reached until significant shutdown reactivity is inserted into the core. This conservatism is particularly important for accidents occurring during hot full power. Reactivity insertion versus time assumptions are discussed in Section 15.1.5.

15.4.7.3 Results

The values of the parameters used in the Rod Cluster Control Assembly Ejection Accident analysis, as well as the results of the analysis, are presented in Tables 15.4-1 and 15.4-12 and discussed below. The parameters and results for the radiological consequence calculations are presented in Tables 15.4-12A and 15.4-12B and are also discussed below.

Beginning of Cycle, Full Power

Control bank D was assumed to be inserted to its insertion limit. The worst ejected rod worth and hot channel factor were conservatively assumed to be 0.20 percent Δk and 7.4, respectively. The peak hot spot fuel pellet enthalpy remained below 200 cal/g. The peak hot spot fuel centerline temperature reached melting at 4900°F; however, melting was restricted to less than ten percent of the pellet.

Beginning of Cycle, Zero Power

For this condition, control bank D was assumed to be fully inserted and C was at its insertion limit. Assuming the worst ejected rod worth of 0.77 percent Δk and a hot channel factor of 14.2 resulted in the peak fuel pellet enthalpy below 200 cal/g. The peak pellet centerline temperature remained below the melting temperature of 4900°F.

End of Cycle, Full Power

Control bank D was assumed to be inserted to its insertion limit. The worst ejected rod worth and hot channel factor were conservatively assumed to be 0.21 percent Δk and 8.2, respectively. The resulting peak hot spot fuel pellet enthalpy remained below 200 cal/g. The peak hot spot fuel centerline temperature reached melting at 4800°F; however, melting was restricted to less than ten percent of the pellet.

End of Cycle, Zero Power

For this condition, control bank D was assumed to be fully inserted and C was at its insertion limit. Assuming the worst ejected rod worth of 0.90 percent Δk and a hot channel factor of 20.5 resulted in the peak pellet enthalpy below 200 cal/g. The peak pellet centerline temperature remained below the melting temperature of 4800°F.

A summary of the cases presented above is given in Table 15.4-12. The nuclear power and hot spot fuel and clad temperature transients for the worst cases are presented on Figures 15.4-76 through 15.4-78B.

Fission Produce Release

It is assumed that fission products are released from the gaps of all rods having a DNB ratio of less than the design limit. In all cases

THIS PAGE INTENTIONALLY BLANK

considered, less than 10 percent of the rods entered DNB based on a detailed three-dimensional THINC analysis. Although limited fuel melting at the hot spot was predicted for the full power cases, in practice melting is not expected since the analysis conservatively assumed that the hot spots before and after ejection were coincident.

Pressure Surge

A detailed calculation of the pressure surge for an ejection worth 1 dollar at BOL, hot full power, indicates that the peak pressure does not exceed that which would cause stress to exceed the faulted condition stress limits (32). Since the severity of the present analysis does not exceed this "worst case" analysis, the accident for this plant will not result in an excessive pressure rise of further damage to the RCS.

Lattice Deformations

A large temperature gradient will exist in the region of the hot spot. Since the fuel rods are free to move in the vertical direction, differential expansion between separate rods cannot produce distortion. However, the temperature gradients across the individual rods may produce a force tending to bow the midpoint of the rods toward the hot spot. Physics calculations indicate that the net result of this would be a negative reactivity insertion. In practice, no significant bowing is anticipated, since the structural rigidity of the core is more than sufficient to withstand the forces produced. Boiling in the hot spot region would produce a net flow away from that region. However, the heat from the fuel is released to the water relatively slowly, and it is considered inconceivable that cross flow will be sufficient to produce significant lattice forces. Even if massive and rapid boiling, sufficient to distort the lattice, is hypothetically postulated, the large void fraction in the hot spot region would produce a reduction in the total core moderator to fuel ratio, and a large reduction in this ratio at the hot spot. The net effect would therefore be a negative feedback. It can be concluded that no conceivable mechanism exists for a net positive feedback resulting from lattice deformation. In fact, a small negative feedback may result. The effect is conservatively ignored in the analyses.

Radiological Consequences of a RCCA Ejection Accident

The Rod Cluster Control Assembly (RCCA) Ejection Accident, or Rod Ejection Accident, is analyzed using the Alternate Source Term (AST) methodology, the total effective dose equivalent (TEDE) dose criteria, and plant-specific design information including data applicable to the steam generator (SG) replacement at Salem Unit 2. The dose consequences are analyzed individually for each unit using maximum primary-to-secondary leakage. Assumptions, parameters, mass transfer rates, and initial activity inventories used in the analysis are listed in Table 15.4-12A. Two release paths are analyzed. In the first, 100% of the activity released from the fuel is assumed to be released instantaneously and homogeneously throughout the containment atmosphere and available for release to the environment via containment leakage. In the second, 100% of the activity released from the fuel is assumed to be completely dissolved in the primary coolant (PC). This PC activity is assumed to leak into the secondary via steam generator (SG) tube leakage, and to be available for release to the environment by secondary system steaming rates through the SG relief valves (atmospheric steam dumps) or the SG safety valves.

Core and RCS Release: The following activity is assumed to be instantaneously and homogeneously distributed in the containment following a RCCA:

1. 10% of the core iodine and 10% of the core noble gases in the fuel gap of 10% clad damaged fuel,

2. 25% of the core iodine and 100% of the core noble gases in the 0.25% melted fuel, and

3. 100% of the iodine and noble gasses initially present (i.e., pre-rod ejection accident) in the reactor coolant system (RCS).

Containment Release: During the first 24 hours the containment is assumed to leak at its maximum technical specification leak rate of 0.10 volume percent per day and at 50% of this leak rate for the remaining duration of the accident. No credit is taken for a reduction in the amount of radioactive material available for leakage from the containment due to natural deposition and containment spray.

Secondary System Releases: To maximize the calculated secondary system release doses, it is assumed that offsite power is lost so that the main steam condensers are not available. Due to the rod ejection accident, the reactor is shutdown and the plant begins discharging secondary coolant via the steam generator (SG) relief valves. During the release period, reactor coolant is assumed to leak into the SGs at the maximum primary-to-secondary leakage rate. 100% of the activity released from the fuel is assumed to be completely dissolved in the primary coolant (PC) and available for release to the SGs via primary-to- secondary (P-T-S) leakage. The reactor coolant noble gasses that enter the SGs via P-T-S leakage are released directly to the environment without reduction and mitigation. It is conservatively assumed that the SG tubes are uncovered (i.e., not submerged in the SGs liquid), and consequently iodine introduced into the SGs via the P-T-S leakage is assumed to be directly released to the environment in proportion to the steam release rate, with no credit taken for iodine partitioning in the SG liquid.

Release Consequences: The two release paths are combined to determine the dose consequences. The EAB, LPZ, and CR radiological doses from the RCCA Ejection Accident release demonstrates that the maximum dose that the general public could receive would be less than the 10 CFR 50.67 limits and Regulatory Guide 1.183 guidelines and are listed in Table 15.4-12B.

15.4.7.4 Conclusions

Even on a pessimistic basis, the analyses indicate that the described fuel limits are not exceeded. It is concluded that there is no danger of sudden fuel dispersal into the coolant. Since the peak pressure does not exceed that which would cause stresses to exceed the faulted condition stress limits, it is concluded that there is no danger of further consequential damage to the primary circuit. The analyses have demonstrated that upper limit in fission product release as a result of a number of fuel rods entering DNB amounts to 10 percent.

15.4.8 Containment Pressure Analysis

The containment pressure response to a spectrum of RCS and steam line breaks have been analyzed. The containment response to minor reactor coolant leakage and the loss of normal containment cooling have also been evaluated. Finally, a subcompartment analysis is provided to permit evaluation of the blowdown loads on the structure.

15.4.8.1 Reactor Coolant System Breaks

15.4.8.1.1 Method of Analysis

Calculation of containment pressure and temperature transients is accomplished by use of the digital computer code, COCO. The analytical model is restricted to the containment volume and structure.

THIS PAGE INTENTIONALLY BLANK

For analytical rigor and convenience, the containment air-steam-water mixture is separated into two systems. The first system consists of the air-steam phase, while the second is the water phase. Sufficient relationships to describe the transient are provided by the equations of conservation of mass and energy as applied to each system, together with appropriate boundary conditions. As thermodynamic equations of state and conditions may vary during the transient, the equations have been derived for all possib1e cases of superheated or saturated steam, and subcooled or saturated water. Switching between states is handled automatically by the code. The following are the major assumptions made in the analysis:

1. At the break point, the discharge flow separates into steam and water phases. The saturated water phase is at the total containment pressure, while the steam phase is at the partial pressure of the steam in the containment.

2. Homogeneous mixing is assumed. The steam-air mixture and the water phase each have uniform properties. More specifically, thermal equilibrium between the air and steam is assumed. This does not imply thermal equilibrium between the steam-air mixture and the water phase.

3. Air is taken as an ideal gas, while compressed water and steam tables are employed for water and steam thermodynamic properties.

During the transient, there is energy transfer from the steam-air and water systems to the internal structures and equipment within the shell.

Provision is made in the computer analysis for the effects of several engineered safeguards, including internal spray, fan coolers, and recirculation of sump water. The heat removal from containment steam-air phase by internal spray is determined by allowing the spray water temperature to rise to the steam-air temperature.

15.4.8.1.2 Mass and Energy Releases from the Reactor Coolant System

The uncontrolled release of pressurized high-temperature reactor coolant, termed a loss-of-coolant accident (LOCA), will result in release of steam and water into the containment. This, in turn, will result in increases in the local subcompartment pressures, and an increase in the global containment pressure and temperature. Therefore, there are typically both long- and short-term issues reviewed relative to a postulated LOCA that must be considered for a complete containment integrity analysis.

The long-term LOCA mass and energy releases are analyzed to approximately 10^7 seconds and are utilized as input to the containment integrity analysis. This demonstrates the acceptability of the containment safeguards systems to mitigate the consequences of a hypothetical large-break LOCA. The containment safeguards systems must be capable of limiting the peak containment pressure to less than the design pressure and to limit the temperature excursion to less than the acceptance limits. Westinghouse generated Salem Unit 1 and Unit 2 specific LOCA mass and energy releases for containment design using the flexible multi-nodal model (hereafter referred to as "the March 1979 model") described in Reference 73. The Nuclear Regulatory Commission (NRC) review and approval letter is included with Reference 73.

The mass and energy release rates described in this section form the basis of further computations to evaluate the containment following the postulated accident. Discussed in this section are the long-term LOCA mass and energy releases for the hypothetical double-ended pump suction (DEPS) rupture with minimum safeguards, the DEPS break with maximum safeguards, and the blowdown portion of the double-ended hot leg (DEHL) rupture break. These three break cases are performed in order to bound the containment design basis.

Input Parameters and Assumptions

The mass and energy release analysis is sensitive to the assumed characteristics of various plant systems, in addition to other key modeling assumptions. Where appropriate, bounding inputs are utilized and instrumentation uncertainties are included. For example, the RCS operating temperatures are chosen to bound the highest average coolant temperature range of all operating cases and a temperature uncertainty allowance of (+5.0°F) is then added.

Nominal parameters are used in certain instances. For example, the RCS pressure in this analysis is based on a nominal value of 2250 psia plus an uncertainty allowance (+50.0 psi). All input parameters are chosen consistent with accepted analysis methodology.

Some of the most critical items are the RCS initial conditions, core decay heat, safety injection flow, and primary and secondary metal mass and steam generator heat release modeling. Tables 15.4-13 and 15.4-14 present key data assumed in the analyses.

The core rated power of 3479.75 MWt adjusted for calorimetric error (i.e., 100.6% of 3459 MWt) was used in the analysis. As previously noted, the use of RCS operating temperatures to bound the highest average coolant temperature range were used as bounding analysis conditions. The use of higher temperatures is conservative because the initial fluid energy is based on coolant temperatures that are at the maximum levels attained in steady-state operation. Additionally, an allowance to account for instrument error and deadband is reflected in the initial RCS temperatures. The selection of 2250 psia as the limiting pressure is considered to affect the blowdown phase results only, since this represents the initial pressure of the RCS. The RCS rapidly depressurizes from this value until the point at which it equilibrates with containment pressure.

The rate at which the RCS blows down is initially more severe at the higher RCS pressure. Additionally the RCS has a higher fluid density at the higher pressure (assuming a constant temperature) and subsequently has a higher RCS mass available for releases. Thus, 2250 psia plus uncertainty was selected for the initial pressure as the limiting case for the long-term mass and energy release calculations.

The selection of the fuel design features for the long-term mass and energy release calculation is based on the need to conservatively maximize the energy stored in the fuel at the beginning of the postulated accident (i.e., to maximize the core stored energy). The core stored energy that was selected to bound the Westinghouse 17 x 17 RFA fuel product that will be used at Salem Unit 1 and Unit 2 was 4.23 full power seconds (FPS). The margins in the core stored energy include +15 percent in order to address the thermal fuel model and associated manufacturing uncertainties and the time in the fuel cycle for maximum fuel densification. Thus, the analysis very conservatively accounts for the stored energy in the core.

Margin in RCS volume of 3 percent (which is composed of 1.6-percent allowance for thermal expansion and 1.4-percent allowance for uncertainty) was modeled.

A uniform steam generator tube plugging level of 0 percent was modeled. This assumption maximizes the reactor coolant volume and fluid release by virtue of consideration of the RCS fluid in all steam generator tubes. During the post-blowdown period, the steam generators are active heat sources since significant energy remains in the secondary metal and secondary mass that has the potential to be transferred to the primary side. The 0-percent tube plugging assumption maximizes the heat transfer area and, therefore, the transfer of secondary heat across the steam generator tubes. Additionally, this assumption reduces the reactor coolant loop resistance, which reduces the ΔP upstream of the break for the pump suction breaks and increases break flow. Thus, the analysis conservatively accounts for the level of steam generator tube plugging.

The secondary- to primary-heat transfer is maximized by assuming conservative heat transfer coefficients. This conservative energy transfer is ensured by maximizing the initial internal energy of the inventory in the steam generator secondary side. This internal energy is based on full-power operation plus uncertainties.

Regarding safety injection flow, the mass and energy release calculation considered configurations, component failures, and offsite power assumptions to conservatively bound respective alignments. The cases include a minimum safeguards assumption (1 charging/high-head safety injection (HHSI) pump, 1 intermediate-head safety injection (IHSI) pump and 1 RHR/low-head safety injection (LHSI) pump) and a maximum safeguards assumption (2 charging/high-head safety injection (HHSI) pump, 2 intermediate-head safety injection (IHSI) pump and 2 RHR/low-head safety injection (LHSI) pump) (see Table 15.4-14). In addition, the containment backpressure is assumed to be equal to the containment design pressure. This assumption was shown in Reference 73 to be conservative for the generation of LOCA mass and energy releases.

In summary, the following assumptions were employed to ensure that the mass and energy releases are conservatively calculated, thereby maximizing energy release to containment:

1. Maximum expected operating temperature of the RCS (100-percent full-power conditions)

2. Allowance for RCS temperature uncertainty (+5.0°F)

3. Margin in RCS volume of 3 percent (which is composed of 1.6-percent allowance for thermal expansion and 1.4-percent allowance for uncertainty)

4. Core rated power of 3459 MWt

5. Allowance for calorimetric error (+0.6 percent of power)

6. Conservative heat transfer coefficients (i.e., steam generator primary/secondary heat transfer and RCS metal heat transfer)

7. Allowance in core stored energy for effect of fuel densification

8. A margin in core stored energy (+15 percent to account for manufacturing tolerances)

9. An allowance for RCS initial pressure uncertainty (+50 psi)

10. A maximum containment backpressure equal to design pressure (47.0 psig)

11. Steam generator tube plugging leveling (0-percent uniform)

    - Maximizes reactor coolant volume and fluid release
    - Maximizes heat transfer area across the steam generator tubes
    - Reduces coolant loop resistance, which reduces the ΔP upstream of the break for the pump suction breaks and increases break flow

Thus, based on the previously discussed conditions and assumptions, an analysis to bound both Salem Unit 1 and Salem Unit 2 was made for the release of mass and energy from the RCS in the event of a large break LOCA at 3479.75 MWt.

Description of Analyses

The evaluation model used for the long-term LOCA mass and energy release calculations is the March 1979 model described in Reference 73. The containment system receives mass and energy releases following a postulated rupture in the RCS.

These releases continue over a time period, which, for the LOCA mass and energy analysis, is typically divided into four phases:

1. Blowdown - the period of time from accident initiation (when the reactor is at steady-state operation) to the time that the RCS and containment reach an equilibrium state.

2. Refill - the period of time when the lower plenum is being filled by accumulator and emergency core cooling system (ECCS) water. At the end of blowdown, a large amount of water remains in the cold legs, downcomer, and lower plenum. To conservatively consider the refill period for the purpose of containment mass and energy releases, it is assumed that this water is instantaneously transferred to the lower plenum along with sufficient accumulator water to completely fill the lower plenum. This allows an uninterrupted release of mass and energy to containment. Thus, the refill period is conservatively neglected in the mass and energy release calculation.

3. Reflood - begins when the water from the lower plenum enters the core and ends when the core is completely quenched.

4. Post-reflood (Froth) - describes the period following the reflood phase. For the pump suction break, a two-phase mixture exits the core, passes through the hot legs, and is superheated in the steam generators prior to exiting the break as steam. After the broken loop steam generator cools, the break flow becomes two phase.

Computer Codes

The Reference 73 mass and energy release evaluation model is comprised of mass and energy release versions of the following codes: SATAN VI, WREFLOOD, FROTH, and EPITOME. These codes were used to calculate the long-term LOCA mass and energy releases to bound both Salem Unit 1 and Salem Unit 2.

SATAN VI calculates blowdown, the first portion of the thermal-hydraulic transient following break initiation, including pressure, enthalpy, density, mass and energy flow rates, and energy transfer between primary and secondary systems as a function of time.

The WREFLOOD code addresses the portion of the LOCA transient where the core reflooding phase occurs after the primary coolant system has depressurized (blowdown) due to the loss of water through the break and when water supplied by the ECCS refills the reactor vessel and provides cooling to the core. The most important feature of WREFLOOD is the steam/water mixing model.

FROTH models the post-reflood portion of the transient. The FROTH code is used for the steam generator heat addition calculation from the broken and intact loop steam generators.

EPITOME continues the FROTH post-reflood portion of the transient from the time at which the secondary equilibrates to containment design pressure to the end of the transient. It also compiles a summary of data on the entire transient, including formal instantaneous mass and energy release tables and mass and energy balance tables with data at critical times.

Break Size and Location

Generic studies have been performed and documented in Reference 73 with respect to the effect of postulated break size on the LOCA mass and energy releases. The double-ended guillotine break has been found to be limiting due to larger mass flow rates during the blowdown phase of the transient. During the reflood and froth phases, the break size has little effect on the releases.

Three distinct locations in the RCS loop can be postulated for a pipe rupture for mass and energy release purposes:

- Hot leg (between vessel and steam generator)
- Cold leg (between pump and vessel)
- Pump suction (between steam generator and pump)

The break locations analyzed for this program are the DEPS rupture (10.48 ft^2) and the DEHL rupture (9.174 ft^2). Break mass and energy releases have been calculated for the blowdown, reflood, and post-reflood phases of the LOCA for the DEPS cases. For the DEHL case, the releases were calculated only for the blowdown. The following information provides a discussion on the three possible break locations and why the DEPS break is limiting for the long term.

The DEHL rupture has been shown in previous studies to result in the highest blowdown mass and energy release rates. Although the core flooding rate would be the highest for this break location, the amount of energy released from the steam generator secondary is minimal because the majority of the fluid that exits the core vents directly to containment bypassing the steam generators. As a result, the reflood mass and energy releases are reduced significantly as compared to either the pump suction or cold leg break locations where the core exit mixture must pass through the steam generators before venting through the break. For the hot leg break, generic studies have confirmed that there is no reflood peak (i.e., from the end of the blowdown period the containment pressure would continually decrease). Therefore, with respect to long term heat removal the hot leg break is not limiting and no further evaluation is necessary.

The cold leg break location has also been found in previous studies to be much less limiting in terms of the overall containment energy releases. The cold leg blowdown is faster than that of the pump suction break, and more mass is released into the containment. However, the core heat transfer is greatly reduced, and this results in a considerably lower energy release into containment. Studies have determined that the blowdown transient for the cold leg is, in general, less limiting than that for the pump suction break. During reflood, the flooding rate is greatly reduced and the energy release rate into the containment is reduced. Therefore, the cold leg break is bounded by other breaks and no further evaluation is necessary.

The pump suction break combines the effects of the relatively high core flooding rate, as in the hot leg break, and the addition of the stored energy in the steam generators. As a result, the pump suction break yields the highest energy flow rates during the post-blowdown period by including all of the available energy of the RCS in calculating the releases to containment. Thus, only the DEPS case is analyzed for the current design basis.

Application of Single-Failure Criterion

An analysis of the effects of the single-failure criterion has been performed on the mass and energy release rates for each break analyzed. An inherent assumption in the generation of the mass and energy release is that offsite power is lost coincident with the pipe rupture. This results in the actuation of the emergency diesel generators. Operation of the diesel generators delays the operation of the safety injection system that is required to mitigate the transient. This is not an issue for the blowdown period, which is limited by the DEHL break.

Two cases have been analyzed to assess the effects of a single failure. The first case assumes minimum safeguards safety injection (SI) flow based on the postulated single failure of an entire train of safeguards equipment. Typically, this is synonymous with the failure of an emergency diesel generator to start. However, the Salem plants have a three diesel generator system, so the loss of one diesel would be less limiting than the loss of one complete train of safeguards equipment. The loss of one train of safety injection pumps results in only one HHSI pump, one IHSI pump and one LHSI pump available for accident mitigation. The containment heat removal equipment that is assumed to operate under the postulated failure of the safeguards equipment train is discussed in Section 15.4.1.3. The other case assumes maximum safeguards based on no postulated failures that would impact the amount of ECCS flow (i.e., the single failure is assumed to be the failure of a containment spray pump to operate). The analysis of the cases described provides confidence that the effect of credible single failures is bounded.

Acceptance Criteria for LOCA M&E Analyses

A large break loss-of-coolant accident is classified as an American Nuclear Society (ANS) Condition IV event, an infrequent fault. To satisfy the NRC acceptance criteria presented in the Standard Review Plan, Section 6.2.1.3, the relevant requirements are the following:

- 10 CFR 50, Appendix A
- 10 CFR 50, Appendix K, paragraph I.A

To meet these requirements, the following must be addressed:

- Break size and location
- Calculation of each phase of the accident
- Sources of energy

The description of the individual sources of mass and energy and the modeling of each phase of the transient with the March 1979 model (Reference 73) are provided in the following sections. The break size and location were discussed above.

Sources of Mass and Energy

The sources of mass considered in the LOCA mass and energy release are the RCS, accumulators, and pumped safety injection.

The following energy sources are considered in the LOCA mass and energy release analysis:

- Reactor coolant system water
- Accumulator water (all four inject)
- Pumped safety injection water
- Decay heat
- Core-stored energy
- Reactor coolant system metal (includes steam generator tubes)
- Steam generator metal (includes transition cone, shell, wrapper, and other internals)
- Steam generator secondary energy (includes fluid mass and steam mass)
- Secondary transfer of energy (feedwater into and steam out of the steam generator secondary)

The mass and energy inventories are presented at the following times, as appropriate:

- Time zero (initial conditions)
- End of blowdown time
- End of refill time
- End of reflood time
- Time of broken loop steam generator equilibration to pressure setpoint
- Time of intact loop steam generator equilibration to pressure setpoint
- Time of full depressurization (3600 seconds)

In the mass and energy release data presented, no Zirc-water reaction heat was considered because the cladding temperature does not rise high enough for the rate of the Zirc-water reaction heat to proceed.

Blowdown Mass and Energy Release Data

The SATAN-VI code is used for computing the blowdown transient. The code utilizes the control volume (element or nodal) approach with the capability for modeling a large variety of plant specific thermal fluid system configurations. The fluid properties are considered uniform and thermodynamic equilibrium is assumed in each element. A point kinetics model is used with weighted feedback effects. The major feedback effects include moderator density, moderator temperature, and Doppler broadening.

A critical flow calculation for subcooled (modified Zaloudek), two-phase (Moody), or superheated break flow is incorporated into the analysis. The methodology for the use of this model is described in Reference 73. A comparison of these two critical flow correlations is shown in Section III-1 of Reference 36.

Table 15.4-15A presents the calculated mass and energy release for the blowdown phase of the DEHL break for Salem Unit 2. For the hot leg break mass and energy release tables, break path 1 refers to the mass and energy exiting from the reactor vessel side of the break; break path 2 refers to the mass and energy exiting from the steam generator side of the break.

Table 15.4-15B presents the calculated mass and energy releases for the blowdown phase of the DEPS break. For the pump suction breaks, break path 1 in the mass and energy release tables refers to the mass and energy exiting from the steam generator side of the break. Break path 2 refers to the mass and energy exiting from the pump side of the break.

Reflood Mass and Energy Release Data

The WREFLOOD code is used for computing the reflood transient. The WREFLOOD code consists of two basic hydraulic models — one for the contents of the reactor vessel and one for the coolant loops. The two models are coupled through the interchange of the boundary conditions applied at the vessel outlet nozzles and at the top of the downcomer. Additional transient phenomena such as pumped safety injection and accumulators, reactor coolant pump performance, and steam generator release are included as auxiliary equations that interact with the basic models as required. The WREFLOOD code permits the capability to calculate variations during the core reflooding transient of basic parameters such as core flooding rate, core and downcomer water levels, fluid thermodynamic conditions (pressure, enthalpy, density) throughout the primary system, and mass flow rates through the primary system. The code permits hydraulic modeling of the two flow paths available for discharging steam and entrained water from the core to the break, i.e., the path through the broken loop and the path through the unbroken loops.

A complete thermal equilibrium mixing condition for the steam and ECCS injection water during the reflood phase has been assumed for each loop receiving ECCS water. The complete mixing process, however, is made up of two distinct physical processes.

The first is a two-phase interaction with condensation of steam by cold ECCS water. The second is a single-phase mixing of condensate and ECCS water. Since the steam release is the most important influence to the containment pressure transient, the steam condensation part of the mixing process is the only part that need be considered. (Any spillage directly heats only the sump and not the atmosphere.)

The most applicable steam/water mixing test data have been reviewed for validation of the containment integrity reflood steam/water mixing model. This data was generated in 1/3-scale tests (Reference 74), which are the largest scale data available and thus most clearly simulates the flow regimes and gravitational effects that would occur in a pressurized water reactor (PWR). These tests were designed specifically to study the steam/water interaction for PWR reflood conditions.

A group of 1/3-scale steam/water mixing tests discussed in Reference 74 corresponds directly to containment integrity reflood conditions. The injection flow rates for this group cover all phases and mixing conditions calculated during the reflood transient. The data from these tests were reviewed and discussed in detail in Reference 73. For all of these tests, the data clearly indicate the occurrence of very effective mixing with rapid steam condensation. The mixing model used in the containment integrity reflood calculation is, therefore, wholly supported by the 1/3-scale steam/water mixing data.

Additionally, the following justification is also noted. The post-blowdown limiting break for the containment integrity peak pressure analysis is the pump suction double-ended rupture break. For this break, there are two flow paths available in the RCS by which mass and energy may be released to containment. One is through the outlet of the steam generator, the other via reverse flow through the reactor coolant pump. Steam that is not condensed by ECCS injection in the intact RCS loops passes around the downcomer and through the broken loop cold leg and pump in venting to containment. This steam also encounters ECCS injection water as it passes through the broken loop cold leg, complete mixing occurs and a portion of it is condensed. It is this portion of steam that is condensed that is taken credit for in this analysis. This assumption is justified based upon the postulated break location, and the actual physical presence of the ECCS injection nozzle.

Table 15.4-16 and Table 15.4-17 present the calculated mass and energy releases for the reflood phase of the double-ended pump suction rupture with minimum safeguards and maximum safeguards, respectively.

The transient responses of the principal parameters during reflood are given in Table 15.4-23 and Table 15.4-25 for the DEPS cases.

Post-Reflood Mass and Energy Release Data

The FROTH code (Reference 36) is used for computing the post-reflood transient. The FROTH code calculates the heat release rates resulting from a two-phase mixture present in the steam generator tubes. The mass and energy releases that occur during this phase are typically superheated due to the depressurization and equilibration of the broken loop and intact loop steam generators. During this phase of the transient, the RCS has equilibrated with the containment pressure. However, the steam generators contain a secondary inventory at an enthalpy that is much higher than the primary side. Therefore, there is a significant amount of reverse heat transfer that occurs. Steam is produced in the core due to core decay heat. For a pump suction break, a two-phase fluid exits the core, flows through the hot legs, and becomes superheated as it passes through the steam generator. Once the broken loop cools, the break flow becomes two phase. During the FROTH calculation, ECCS injection is addressed for both the injection phase and the recirculation phase. The FROTH code calculation stops when the secondary side equilibrates to the saturation temperature (T_{sat}) at the containment design pressure, after this point the EPITOME code completes the steam generator depressurization.

The methodology for the use of this model is described in Reference 73. The mass and energy release rates are calculated by FROTH and EPITOME until the time of containment depressurization. After containment depressurization (14.7 psia), the mass and energy release available to containment is generated directly from core boil-off/decay heat.

Table 15.4-18 and Table 15.4-19 present the two-phase post-reflood mass and energy release data for the minimum and maximum safeguards pump suction double-ended break cases.

Decay Heat Model

ANS Standard 5.1 (Reference 75) was used in the LOCA mass and energy release model for Salem Units 1 and 2 for the determination of decay heat energy. This standard was balloted by the Nuclear Power Plant Standards Committee (NUPPSCO) in October 1978 and subsequently approved. The official standard (Reference 75) was issued in August 1979.

Significant assumptions in the generation of the decay heat curve for use in the LOCA mass and energy releases analysis include the following:

1. The decay heat sources considered are fission product decay and heavy element decay of U-239 and Np-239.
2. The decay heat power from fissioning isotopes other than U-235 is assumed to be identical to that of U-235.
3. The fission rate is constant over the operating history of maximum power level.
4. The factor accounting for neutron capture in fission products has been taken from Equation 11 of Reference 75 up to 10,000 seconds and from Table 10 of Reference 75 beyond 10,000 seconds.
5. The fuel has been assumed to be at full power for 10^8 seconds.
6. The number of atoms of U-239 produced per second has been assumed to be equal to 70% of the fission rate.
7. The total recoverable energy associated with one fission has been assumed to be 200 MeV/fission.
8. Two sigma uncertainty (two times the standard deviation) has been applied to the fission product decay.

Based upon NRC staff review, (Safety Evaluation Report of the March 1979 evaluation model [Reference 73]), use of the ANS Standard 5.1 (Reference 75) decay heat model was approved for the calculation of mass and energy releases to the containment following a LOCA.

Steam Generator Equilibration and Depressurization

Steam generator equilibration and depressurization is the process by which secondary-side energy is removed from the steam generators in stages. The FROTH computer code calculates the heat removal from the secondary mass until the secondary temperature is the saturation temperature (T_{sat}) at the containment design pressure. After the FROTH calculations, the EPITOME code continues the FROTH calculation for steam generator cooldown removing steam generator secondary energy at different rates (i.e., first- and second-stage rates). The first-stage rate is applied until the steam generator reaches T_{sat} at the user specified intermediate equilibration pressure, when the secondary pressure is assumed to reach the actual containment pressure. Then the second-stage rate is used until the final depressurization, when the secondary reaches the reference temperature of T_{sat} at 14.7 psia, or 212°F. The heat removal of the broken loop and intact loop steam generators are calculated separately.

During the FROTH calculations, steam generator heat removal rates are calculated using the secondary-side temperature, primary-side temperature and a secondary-side heat transfer coefficient determined using a modified McAdam's correlation. Steam generator energy is removed during the FROTH transient until the secondary-side temperature reaches saturation temperature at the containment design pressure. The constant heat removal rate used during the first heat removal stage is based on the final heat removal rate calculated by FROTH. The steam generator energy available to be released during the first stage interval is determined by calculating the difference in secondary energy available at the containment design pressure and that at the (lower) user-specified intermediate equilibration pressure, assuming saturated conditions. This energy is then divided by the first-stage energy removal rate, resulting in an intermediate equilibration time.

At this time, the rate of energy release drops substantially to the second-stage rate. The second-stage rate is determined as the fraction of the difference in secondary energy available between the intermediate equilibration and final depressurization at 212°F, and the time difference from the time of the intermediate equilibration to the user-specified time of the final depressurization at 212°F. With current methodology, all of the secondary energy remaining after the intermediate equilibration is conservatively assumed to be released by imposing a mandatory cooldown and subsequent depressurization down to atmospheric pressure at 3600 seconds, i.e., 14.7 psia and 212°F.

The sources of mass and energy that are considered in the LOCA mass and energy release analysis are given in Table 15.4-28 and Table 15.4-29 for the DEPS minimum safeguards case, Table 15.4-30 and Table 15.4-31 for the DEPS maximum safeguards case, and Table 15.4-32 and Table 15.4-33 for the DEHL case. The sources of mass are the RCS, accumulators, and pumped safety injection. The energy sources are the following:

- Reactor coolant system water
- Accumulator water (all four inject)
- Pumped safety injection water
- Decay heat
- Core-stored energy
- Reactor coolant system metal (includes steam generator tubes)
- Steam generator metal (includes transition cone, shell, wrapper, and other internals)
- Steam generator secondary energy (includes fluid mass and steam mass)
- Secondary transfer of energy (feedwater into and steam out of the steam generator secondary)

The analysis used the following energy reference points:

- Available energy: 212°F; 14.7 psia [energy available that could be released]
- Total energy content: 32°F; 14.7 psia [total internal energy of the RCS]

The mass and energy inventories are presented at the following times, as appropriate:

- Time zero (initial conditions)
- End of blowdown time
- End of refill time
- End of reflood time
- Time of broken loop steam generator equilibration to pressure setpoint
- Time of intact loop steam generator equilibration to pressure setpoint
- Time of full depressurization (3600 seconds)

The energy release from the metal-water reaction rate is considered as part of the WCAP-10325-P-A (Reference 73) methodology. Based on the way that the energy in the fuel is conservatively released to the vessel fluid, the fuel cladding temperature does not increase to the point where the metal-water reaction is significant. This is in contrast to the 10 CFR 50.46 analyses, which are based to calculate high fuel rod cladding temperatures and, therefore, a insignificant metal-water reaction. For the LOCA mass and energy release calculation, the energy created by the metal-water reaction value is small and is not explicitly provided in the energy balance tables. The energy that is determined is part of the mass and energy releases and is therefore already included in the overall mass and energy releases for the Salem units.

15.4.8.1.3 Heat Sinks

Energy is absorbed from the containment atmosphere during the transient by heat sinks in the containment. Heat sinks include the containment structure, fan coolers and sprays.

Containment Structures

Provision is made in the containment pressure transient analysis for heat transfer through, and heat storage in, both interior and exterior walls.

The structural heat sink model includes a thermal resistance between the steel and concrete layers. The interface resistance is represented by a conservatively low heat transfer coefficient between the steel and concrete of 10 Btu/hr-°F-ft^2. If an incredible postulation of a 0 Btu/hr-°F-ft^2 heat transfer coefficient between the steel and concrete was made, it has been shown for a similar four-loop plant that the peak pressure of the design basis case would rise only 0.1 psi.

The different layers of each heat sink structure are subdivided into thin sublayers. The sublayer thickness is related to the conductivity and thickness of the layer. There are four types of layers: paint topcoat, primer paint, steel and concrete. The paint topcoat is 5 mils thick and is modeled with five interior layers and two surface layers. The primer paint is 3 mils thick and is represented with three interior nodes and two surface nodes[1].

[1] VTD 900986 evaluates as-built coatings thickness data for impact to containment heat sink performance.

The steel layers are from one-eighth inch to one inch thick. The number of sublayers varies from three interior sublayers and two surface sublayers for the thickest steel layers. The concrete is modeled as slabs of either 1 or 1 1/2 feet in thickness. The number of sublayers used in the concrete model varies from 19 interior nodes and 2 surface nodes to 29 interior nodes and 2 surface nodes. It should be noted that in any layer each surface sublayer is one-half the thickness of an interior sublayer. A conservation of energy equation expressed in finite difference form accounts for transient conduction into and out of the node and temperature rise of the node. The use of the large number of sublayers assures that the temperature profile through the containment structural heat sink can be accurately represented and the energy distribution through the structures accurately known. Table 15.4-20 is a summary of the containment structural heat sinks used in the analysis. The structural heat sinks listed as "Containment Floor" and "Reactor Cavity" in the table are assumed to be in contact with sump water. The other structural heat sinks are assumed to be in contact with the containment atmosphere.

The model used for heat transfer to the structure from the sump assumes a constant film heat transfer coefficient of 200 Btu/hr-°F-ft^2. The energy transfer is then calculated as the product of the heat transfer area times the difference in temperature between the sump and the structure multiplied by 200 Btu/hr-°F-ft^2.

A detailed listing of structural heat sinks is provided in Table 15.4-21.

The uncertainty in these heat sinks is considered as follows:

Containment Liner - 5 percent
Other Miscellaneous items - 7 1/2 percent
Miscellaneous Mechanical Items - 10 percent

These uncertainty factors are based on an evaluation of unavoidable variations resulting from material thickness and fabrication tolerances applied by manufacturers and fabricators.

The heat transfer coefficient between the containment atmosphere and exposed heat sinks is calculated by the code based primarily on the work of Tagami (44).

From this work it was determined that the value of the heat transfer coefficient increases parabolically to peak value at the end of blowdown and then decreased exponentially to a stagnant heat transfer coefficient which is a function of steam to air weight ratio.

It should be noted that this method is different than that presented in the Preliminary Facility Description and Safety Analysis Report. In that report the heat transfer coefficients were based on the work of Koflat (45). The revised method of calculation results in decreased heat transfer to the containment structure during blowdown.

Tagami presents a plot of the maximum value of h as a function of "coolant energy transfer speed," defined as:

$$\frac{\text{total coolant energy transferred into containment}}{\text{(containment vessel volume) (time interval to peak pressure)}}$$

From this, the maximum of h for steel is calculated:

$$h_{max} = 75 \frac{(E)^{0.60}}{(t_p V)} \qquad (1)$$

where:

h_{max} = maximum value of h (Btu/hr-°F-ft^2)

t_p = time from start of accident to end of blowdown

V = containment volume (ft^3)

E = Coolant energy discharged (Btu)

The parabolic increase to the peak value is given by:

$$h_s = h_{max}\sqrt{\frac{t}{t_p}} \qquad \text{for } 0 \leqq t \leqq t_p \qquad (2)$$

where:

h_s = heat transfer coefficient for steel (Btu/hr-°F-ft^2)

t = time from start of accident (sec)

The exponential decrease of the heat transfer coefficient is given by:

$$h_s = h_{stag} + (h_{max} - h_{stag})e^{-.05(t-tp)} \qquad \text{for } t > t_p \qquad (3)$$

where:

$h_{stag} = 2 + 50\chi$ for $0 \leqq \chi \leqq 1.4$

h_{stag} = h for stagnant conditions (Btu/hr-°F-ft^2)

χ = steam to air weight ratio in containment

For concrete, the heat transfer coefficient is taken as 40 percent of the value calculated for steel.

Containment Fan Coolers

The ability of the containment fan coolers to function properly in an accident environment is assured by periodic inspection and cleaning, in accordance with Salem commitments to NRC Generic Letter 89-13. This is aided by periodic full flow testing, SW system chlorination, and a biofouling trending program that ensures differential pressures across the CFCUs are maintained within acceptable limits.

Containment fan cooler unit thermal performance is calculated using a computer based heat transfer model, which has been benchmarked against a prototype version of the Salem containment fan cooler units. With an assumed design basis fouling factor of 0.0015, containment fan cooler unit design basis performance is given below:

Containment Accident Temperature	265.9°F
Containment Accident Pressure	58.2 psia
Containment Relative Humidity	100%
Service Water Flow	1250 gpm
Service Water Temperature	93°F
Cooler Air Flow Rate	40,000 cfm
Fouling Factor	0.0035
Heat Duty or Capacity	44.0×10^6 Btu/hr
LOCA/MSLB Nominal Capacity for 1 CFCU	37.6×10^6 Btu/hr
LOCA/MSLB Analysis Assumption for 3 CFCUs[1]	112.8×10^6 Btu/hr

[1] The LOCA/MSLB accident analyses of Section 15 determined a minimum number of three fan-cooler units, along with other containment heat sinks, are needed to maintain containment integrity. This correlates to a cumulative heat transfer rate of at least 112.8×10^6 Btu/hr.

The fan cooler heat removal rate as a function of steam saturation temperature, provided in Table 15.4-34, is applicable for LOCA and steam line rupture events.

Containment Spray

When a spray drop enters the hot saturated steam-air environment, the vapor pressure of the water at its surface is much less than the partial pressure of the steam in the atmosphere. Hence, there will be diffusion of steam to the drop surface and condensation on the drop. This mass flow will carry energy to the drop. Simultaneously the temperature difference between the atmosphere and the drop will cause a heat flow to the drop. Both of these mechanisms will cause the drop temperature and vapor pressure to rise. The vapor pressure of the drop will eventually become equal to the partial pressure of the steam and the condensation will cease. The temperature of the drop will be essentially equal to the temperature of the steam-air mixture.

The terminal velocity of the drop can be calculated using the formula given by Weinberg (53) where the drag coefficient C_D is a function of the Reynolds number:

$$V^2 = \frac{4 D_g (\rho - \rho_m)}{3 C_D \rho_m} \quad (1)$$

For the 700 micron drop size expected from the nozzles, the terminal velocity is less than 7 feet per second. For a 1000 micron drop, the velocity would be less than 10 feet per second. The Nusselt number for heat transfer, Nu, and the Nusselt number for mass transfer, Nu' (Sherwood Nubmer), can be calculated from the empirical relations given by Ranz and Marshall (54).

$$Nu = 2 + 0.6 (Re)^{1/2} (Pr)^{1/3} \quad (2)$$

$$Nu' = 2 + 0.6 (Re)^{1/2} (Sc)^{1/3} \quad (3)$$

The Prandtl number and the Schmidt number for the conditions assumed are approximately 0.7 and 0.6, respectively. Both of these are sufficiently independent of pressure, temperature and composition to be assumed constant under containment conditions (49,55). The coefficients of heat transfer (h_C) and mass transfer (k_G) are calculated from Nu and Nu', respectively. The equations describing the temperature rise of a falling drop are:

$$\frac{d}{dt} (Mu) = mh_g + q \quad (4)$$

$$\frac{d}{dt} (M) = m \quad (5)$$

*Nomenclature used is given at the end of this discussion.

where:

$$q = h_C A (T_S - T) \quad (6)$$

$$m = k_G A (P_S - P_V) \quad (7)$$

These equations can be integrated numerically to find the internal energy and mass of the drop as a function of time as it falls through the atmosphere. Analysis shows that the liquid drop temperature rises to the steam-air mixture temperature in less than 0.5 second, which occurs before the drop has fallen 5 feet. These results demonstrate that the spray will be 100 percent effective in removing heat from the atmosphere.

Nomenclature for Equations 1 through 7 Above

A = Area

C_D = Drag coefficient

D = Droplet diameter

g = Acceleration of gravity

h_C = Coefficient of heat transfer

h_S = Steam enthalpy

k_G = Coefficient of mass transfer

M = Droplet mass

m = Diffusion rate

Nu = Nusselt number for heat transfer

Nu' = Nusselt number for mass transfer

P_s = Steam partial pressure

P_v = Droplet vapor pressure

Pr = Prandtl number

q = Heat flow rate

Re = Reynolds number

Sc = Schmidt number

T = Droplet temperature

T_s = Steam temperature

t = Time

u = Droplet external energy

V = Velocity

ρ = Droplet density

ρ_m = Steam-air mixture density

15.4.8.1.4 LOCA Containment Pressure and Temperature Response Results

The containment pressure was originally calculated for a spectrum of break sizes including the largest cold leg and hot leg breaks (reactor inlet and reactor outlet) and a range of pump suction breaks from 3.0 square feet up to the largest. The break locations analyzed for Salem Unit 1 and Salem Unit 2 are the double-ended pump suction guillotine break (10.48 ft^2) and the double-ended hot leg guillotine break (9.12 ft^2). Pump suction break mass and energy releases have been calculated for the blowdown, reflood, and post-reflood phases of the LOCA and the hot leg break mass and energy releases have been calculated for only the blowdown phase.

The double ended hot leg guillotine has been shown in previous studies to result in the highest blowdown mass and energy release rates. Although the core flooding rate would be highest for this break location, the amount of energy released from the steam generator secondary is minimal because the majority of the fluid which exits the core bypasses the steam generator in venting to containment. As a result, the reflood mass and energy releases are reduced significantly as compared to either the pump suction or cold leg break locations where the core exit mixture must pass through the steam generators before venting through the break. For the hot break, there is no reflood peak as determined by generic studies. Therefore, the reflood (and subsequently, post-reflood) releases are not calculated for a hot leg break. The cold leg break location has also been found in previous studies to be much less limiting in terms of the overall containment peak pressure. The cold leg blowdown is faster than that of the pump suction break, and more mass is released into the containment. However, the core heat transfer is greatly reduced, and this results in considerably lower energy release into containment. Studies have determined that the blowdown transient is less limiting than the pump suction break. During the reflood, the flooding rate is greatly reduced and the energy release rate into the containment is reduced. Therefore, the cold leg break was not included in the current containment analysis.

The pump suction break combines the effects of the relatively high core flooding rate, as in the hot leg break, and the addition of the stored energy in the steam generators. As a result, the pump suction break yields the highest energy flow rates during the post-blowdown period by including all of the available energy of the Reactor Coolant System in calculating the releases to containment.

An analysis of the effects of the single failure criteria has been performed on the mass and energy release rates for the double ended pump suction break. An inherent assumption in the generation of mass and energy releases is that offsite power is lost. This results in the actuation of the emergency diesel generators, required to power the safety injection system. This is not an issue for the blowdown period, which is limited by the double ended hot leg break.

The loss of an emergency diesel generator results in the loss of one pumped safety injection train (minimizing safety injection flow) and the containment safeguards (one spray pump and two fan coolers will fail to operate) on that diesel. The analysis further considers the safety injection pump head curves to be degraded by 5%.

The containment response analysis parameters, including those for the containment fan coolers and spray pumps, are presented in Table 15.4-26 and Table 15.4-34.

The DEPS minimum safeguards pressure and temperature results are shown in Figures 15.4-86 and 15.4-87. The DEPS maximum safeguards containment pressure and temperature are shown in Figure 15.4-88 and Figure 15.4-89. The DEHL case containment pressure and temperature are provided in Figure 15.4-90 and Figure 15.4-91.

The primary-side volume, secondary-side volume, primary-side metal properties and secondary-side metal properties of the Model F steam generator differ from those of the AREVA Model 61/19T. Table 15.4-22 provides a comparison of the peak containment pressure and temperature for the DEPS minimum safeguards case and the DEHL case for Salem Unit 1 with the Westinghouse Model F steam generator and Salem Unit 2 with the Areva Model 61/19T steam generator. The pertinent parameters for the containment integrity analyses for LOCA as well as the main steamline break cases presented in Section 15.4.8.2 are presented in Table 15.4-24. The containment spray pump performance that was modeled is presented in Table 15.4-26. The containment fan cooler performance that was modeled is presented in Table 15.4-34. Each of the Unit 1 LOCA cases resulted in less limiting pressure and temperature values than the corresponding cases for Salem Unit 2. Therefore, the transient results and conclusions presented in this section remain bounding for both Salem Unit 1 and Unit 2.

15.4.8.2 MASS AND ENERGY RELEASES TO CONTAINMENT FOLLOWING A STEAMLINE RUPTURE

15.4.8.2.1 Accident Description

A steamline rupture results in an increased steam flow from one or more steam generators. The increased steam flow causes an increase in the heat extraction rate from the Reactor Coolant System, resulting in a reduced primary coolant temperature and pressure. The core power will increase due to negative moderator temperature and Doppler fuel temperature reactivity coefficients, assuming no intervention of control, protection, or engineered safety features. The rate of the power increase level that matches the steam flow is greatest when the moderator reactivity coefficients are the most negative, which corresponds to end-of-life conditions. The mass and energy release to containment following a steamline rupture is considered a Condition IV event.

Steamline ruptures occurring inside a reactor containment structure may result in significant releases of high energy fluid to the containment environment that could possibly result in high containment temperatures and pressures. High containment temperatures and pressures may result in failure of equipment that is not qualified to perform its function in an adverse environment. This environment could degrade the effectiveness of the protection system in mitigating the consequences of the steamline rupture. Thus, it is necessary to demonstrate that the conditions that can exist inside the containment during a steamline rupture do not violate the existing environmental qualification envelopes. In addition, the containment structure is designed to withstand limited internal pressure. To ensure containment integrity, the analyses must also demonstrate that the containment design pressure is not exceeded.

The safety features that provide the necessary protection to limit the mass and energy releases to containment are reactor trip, safety injection, feedline isolation, and steamline isolation. Reactor trip may be provided during a steamline break from OPΔT, safety injection (from any source), low pressurizer pressure, or high containment pressure. A safety injection signal (which will also isolate main feed water) can be generated on any of the following functions.

a. Low Steamline Pressure Coincident with High Steamline Flow
b. Low-Low T_{avg} Coincident with High Steamline Flow
c. High Steamline Differential Pressure
d. Low Pressurizer Pressure
e. High Containment Pressure

Steamline isolation can be generated on any of the following functions.

a. Low Steamline Pressure Coincident with High Steamline Flow
b. Low-Low T_{avg} Coincident with High Steamline Flow
c. High-high Containment Pressure

15.4.8.2.2 Method of Analysis

The steamline break analysis performed utilized the Westinghouse containment model developed for the IEEE Standard 323-1971 Equipment Qualification Program. These models and their justification (experimental and analytical) are detailed in References 56 through 60. Some major points of the model are as follows:

a. The saturation temperature corresponding to the partial pressure of the containment vapor is used in the calculation of condensing heat transfer to the passive heat sinks and the heat removal by containment fan coolers.
b. The Westinghouse containment model utilizes the analytical approaches described in References 6 and 60 to calculate the condensate removal from the condensate film. Justification of this model is provided in References 6, 56, 59, and 60. (For large breaks, 100% revaporization of the condensate is used, and a calculated fractional revaporization due to convective heat flux is used for small breaks.)
c. The small steamline break containment analyses utilized the stagnant Tagami correlation, and the large steamline break analyses utilized the blowdown Tagami correlation with an exponential decay to the stagnant Tagami correlation. The details of these models are given in Reference 38. Justification of the use of heat transfer coefficients has been provided in References 58, 59, and 61.

A complete analysis of main steamline breaks inside containment has been performed using the LOFTRAN code and the Westinghouse containment computer code, COCO[6]. All blowdown calculations with the LOFTRAN code assumed the reactor coolant pumps were running (i.e., offsite power available), because this increases the primary to secondary heat transfer and therefore maintains higher blowdown flow rates (Reference 63, Section 3.1.7).

Single Failure Assumptions

Several failures can be postulated which would impair the performance of various steamline break protection systems and therefore would change the net energy releases from a ruptured line. Four different single failures were considered for each break condition resulting in a limiting transient. These were:

a. failure of a main feed regulating valve,
b. failure of a main steam isolation valve,
c. failure of the auxiliary feed water (AFW) runout protection equipment, and
d. failure of a containment safeguards train.

Details about each of the single failures and their major assumptions follow.

Feed Water Flow

There are two valves in each main feedline that serve to isolate main feed water flow following a steamline break. One is the main feed water regulator valve, which receives dual, separate train trip signals from the Plant Protection System on any safety injection signal and closes within 10 seconds (including instrument delays). The second is the feed water isolation valve that also receives dual, separate train trip signals from the reactor protection system following a safety injection signal. This valve closes within 32 seconds (including instrument delays). Additionally, the main feed water pumps receive dual, separate train trips from the protection system following a steamline break. Thus, the worst failure in this system is a failure of the main feed water regulator valve to close. This failure results in an additional 22 seconds during which feed water from the Condensate Feed System may be added to the faulted steam generator. Also, since the feed water isolation valve is upstream of the regulator valve, failure of the regulator valve results in additional feedline volume that is not isolated from the faulted steam generator. Thus, water in this portion of the lines can flash and enter the faulted steam generator.

The feed water regulating valves (main and bypass) and main feed water isolation valves, which are relied upon to terminate main feed flow to the steam generators, are exempt from seismic requirements (thus classified as Seismic Category 3). However, each valve has safety-related performance requirements, and as such receives dual, independent, safety grade, trip close signals from the protection system following a steamline rupture event. The feed water regulating valves are air-operated, fail close design, whereas the feed water isolation valves are motor operated. Since the assumed pipe break occurs inside containment in a Seismic Category I pipe, the steamline rupture is not assumed to be initiated by a seismic event. There is no requirement to assume a coincident seismic event with the hypothetical pipe rupture. Thus a seismic classification for the main feed water regulating and isolation valves is not necessary to ensure closure following a steamline break inside containment. Also, since the feed water isolation valves are only credited in the event of a single failure of the regulating valves to close, additional failure of these valves does not need to be considered.

Feed water flow to the faulted steam generator from the Main Feed Water System is calculated using the hydraulic resistances of the system piping, head/flow curves for the main feed water pumps, and the steam generator pressure decay as calculated by the LOFTRAN code. In the calculations performed to match these systems' variables, a variety of assumptions is made to maximize the calculated flows. These include:

a. No credit is taken for extra pressure drop in the feedlines due to flashing of water.
b. Feed water regulator valves in the intact loops do not change position prior to a trip signal.
c. All feed water pumps are running at maximum speed.

Calculation of feed water flashing is performed by the LOFTRAN code as described in Reference 27, Section 4.1.5. For the Salem units, conservative maximum un-isolatable volumes (water available to flash) are considered for both the case without a main feed water regulator valve failure and the case with a feed water regulator valve failure.

Main Steam Isolation

Since all main steam isolation valves have closing times of no more than 12 seconds after receipt of signal (including the instrument delays), failure of one of these valves affects only the volume of the main steam and turbine steam piping which cannot be isolated from the pipe rupture.

Steam contained in the un-isolatable portions of the steamlines and turbine plant was considered in the containment analyses in two ways. For the large double-ended ruptures (DERs), steam in the un-isolatable steamlines is released to containment as part of the reverse flow.

When considering split ruptures, steam in the steamlines is included in the analysis by adding the total mass in the lines to the initial mass of steam in the faulted steam generator. This is necessary because, unlike DERs, the total break area of a split is unchanged by steamline isolation; only the source of the blowdown effluent is changed. Thus, steam flow from the piping in the intact loops is indistinguishable from steam leaving the faulted steam generator. However, by adding the water mass in the piping to the faulted steam generator mass and by having dry steam blowdowns, the steamline inventory is included in the total blowdown.

With respect to steam line breaks initiated from temperatures lower than 547°F in Mode 3, plant-specific evaluations (Reference 77) have been performed that demonstrate that either (1) an automatic MSIV closure signal is generated and the MSIVs achieve fast closure, (2) an automatic MSIV signal is not generated but the MSIVs are fast closed after a ten-minute operator action, or (3) an MSIV closure signal is received via an automatic signal or an operator action, but the MSIVs do not have adequate steam for fast closure. In all cases, fast MSIV closure [cases (1) and (2) above] and no MSIV closure [case (3) above], the transients result in much less limiting peak containment pressures than the cases initiated from hot zero power through hot full power.

Auxiliary Feed Water Flow

The AFW System is actuated shortly after the occurrence of a steamline break. The mass addition to the faulted steam generator from the AFW System was conservatively determined by using the following assumptions.

a. The entire AFW System is assumed to be actuated early: For hot, zero power cases, AFW is assumed on as an initial condition. For other initial power levels, AFW is actuated at the time the actuation setpoint is reached and instantaneously pumping at a conservatively high capacity dependent upon the specific configuration.
b. AFW flows are conservatively determined based upon a fluids model for the AFW system that includes the AFW pump flow/head curves, component and line resistances, control valve modeling (runout protection failure), and steam generator pressures.
c. Separate AFW flow input values are used for the faulted and non-faulted steam generators since the steam generator pressures are potentially different. Flow to the faulted steam generator is assumed to exist until realignment of the system is complete.
d. The failure of the AFW runout control equipment is considered as one of the four single failures. For this failure, one of the four AF21 control valves downstream of the two AFW motor pumps fails in a fully open position.

The AFW System is manually realigned by the operator 10 minutes into the transient. Therefore, the analysis assumes a conservatively high AFW flow to the depressurizing faulted steam generator for a full 10 minutes. In the event a postulated main steamline break occurs, AFW to the faulted steam generator must be terminated manually. Present design criteria allow 10 minutes for the operator to recognize the postulated event and perform the necessary actions. However, it is anticipated the operator would terminate AFW flow to the faulted steam generator in much less time due to the amount of Class 1E indication provided to monitor plant conditions.

A single failure of the AFW isolation valve to close was not considered since the failure would not occur until the operator attempted to close the valve after ten minutes. At that time, the operator can simply trip the respective AFW pump as an alternative.

Heat Sinks

The worst effect of a containment safeguards failure is the loss of a vital bus; this reduces containment spray flow and the containment fan cooler heat removal. In all analyses, the times assumed for initiation of containment sprays and fan coolers are 85 and 100 seconds, respectively, following the appropriate initiating trip signal. These times are based on the assumption of offsite power available, and the delays are consistent with Tech Spec limits. The delay time for spray delivery includes the time required for the spray pumps to reach full speed and the time required to fill the spray headers and piping.

The saturation temperature corresponding to the partial pressure of the vapor in the containment is conservatively assumed for the temperature in the calculation of condensing heat transfer to the passive heat sinks. This temperature is also conservatively assumed for the calculation of heat removal by the containment fan coolers. The fan cooler heat removal rate as a function of containment temperature is presented on Figure 15.4-96.

Other major assumptions included in this analysis are shown below.

a. A shut down margin of 1.3% Δk
b. Minimum steam generator tube plugging
c. Maximum T_{avg}
d. Moderator density coefficient to end-of-cycle conditions
e. The 1979 ANS Decay Heat Model
f. Containment Spray Setpoint of 17 psig
g. Containment Fan Cooler Setpoint of 5.5 psig

h. For cases assuming a Containment Safeguards train failure:

1 Containment Spray Pump

2 Containment Fan Coolers

For cases not assuming a Containment Safeguards train failure:

2 Containment Spray Pumps

3 Containment Fan Coolers

i. Containment Design Pressure of 47 psig

j. Containment Volume of 2,620,000 ft^3

k. Initial Containment Pressure of 0.3 psig

l. Initial Air Partial Pressure of 14.7 psia

m. Initial Containment Temperature of 120°F

n. Service Water Temperature of 93°F

15.4.8.2.3 Results

Historically, a total of 80 different blowdowns were evaluated, grouped into five break type categories:

a. 4.6 ft^2 full DER with entrainment

b. 1.4 ft^2 full DER with entrainment

c. Small DER with an area just large enough to cause entrainment

d. Small DER with an area just small enough to avoid entrainment

e. Largest split rupture that will not result in entrainment and will not generate a Steam Line Isolation signal from the primary plant protection system equipment

For each break type category, four power levels were evaluated - 0, 30, 69 and 100.6% of nominal. At each of these power levels, the four single failure assumptions listed earlier were assessed to ensure a limiting transient.

Evaluation at four power levels under four single failure assumptions yields 16 cases for each break type category, or 80 cases total.

For break type categories c through e, a different break size was required at each of the four power levels to meet the specified entrainment and steam line isolation criteria. For these categories, reactor trip, feedline isolation and steam line isolation are generated by high containment pressure signals.

Since the Model F steam generators in Unit 1 and the AREVA Model 61/19T steam generators in Unit 2 have integral flow restrictors, the 4.6 ft^2 DER cases are no longer applicable. Unit specific analyses have been done for a selection of limiting cases. Results and trends are similar for both units, but Unit 2 has a higher peak containment pressure and temperatures.

The mass and energy releases for the most limiting cases along with the resulting containment pressures and temperatures are plotted as follows:

a. Highest Containment Pressure (1.4 ft^2 DER, 30% power, containment safeguards train failure): Figures 15.4-97 and -98.

b. Highest Containment Temperature (0.88 ft^2split break, 30% power, MSIV failure): Figures 15.4-99 and -100.

15.4.8.2.4 Conclusions

The results provided in the steam line break analysis demonstrate sufficient margin available below the containment design pressure and equipment qualification temperature. Similarly, the containment temperature response demonstrates sufficient margin below the required equipment qualification temperature as described in Reference 67.

15.4.8.3 Subcompartment Pressure Analysis

Reference 64 presents the containment subcompartment pressure analysis using an 18-node containment model and the latest version of the TMD computer code.

15.4.8.4 Miscellaneous Analysis

15.4.8.4.1 Minor Reactor Coolant Leakage

The High Containment Pressure signal actuates engineered safety features. Since the setpoint for this signal is 4 psig, the maximum containment pressure caused by leakage is restricted to this value. The containment response to such leakage would be a gradual pressure and temperature rise which would reach a pressure peak of slightly less than 4 pounds gauge. At this point, energy removal due to structural heat sinks and operating fan coolers would match the energy addition due to the leakage and other sources.

Since the containment atmosphere for this case would consist of saturated steam and air, the maximum containment temperature is established by the maximum steam partial pressure. In order to determine the maximum steam partial pressure for this case, an initial containment atmosphere of saturated steam and air at 120°F should be assumed. This assumption results in a partial steam pressure of 1.69 psi before consideration of leakage. In addition, it is conservative to assume that the entire differential (2 psi) between the initial pressure and the setpoint is due to an increase in steam pressure. Since some of the increase in pressure will be due to added air pressure, this will give a conservatively high steam pressure. Finally, a 0.3 psi margin is added to allow for the possibility of an initial low containment pressure. The maximum steam partial pressure is thus 1.69 + 2.00 + 0.30 = 3.99 psia. The temperature which corresponds to this pressure is 153°F. This is far below the containment design temperature.

This analysis is based on the assumption that the containment pressure signal is the one signal which will actuate the safeguards. In reality there are other signals which could cause the initiation of safeguards.

15.4.8.4.2 Loss of Normal Containment Cooling

A loss of offsite power incident is not sufficient to cause a loss of normal containment cooling. The normal containment cooling can only be interrupted by a coincident loss of offsite power and plant trip.

In the event of a loss of normal containment cooling due to the loss of offsite power and plant trip, a small temperature rise will occur in the containment until the high fan cooler inlet temperature signal setpoint is reached. When this signal is received, the operator will actuate the fan coolers and stop the temperature rise. The setpoint is sufficiently low that the components are not endangered.

15.4.9 References for Section 15.4

1. "Acceptance Criteria for Emergency Core Cooling Systems for Light Water Cooled Nuclear Power Reactors," 10CFR50.46 and Appendix K of 10CFR50. Federal Register, Volume 39, No. 3, January 4, 1974, as amended at 51 FR 40311 [November 9, 1986], 53 FR 36005 [September 16, 1988], 57 FR 39353 [August 31, 1992], and 57 FR 61786 [December 29, 1992].

2. Bordelon, F. M.; Massie, H. W.; and Zordan, T. A., "Westinghouse ECCS Evaluation Model - Summary," WCAP-8339, July 1974.

3. Bordelon, F. M. et al, "SATAN-VI Program: Comprehensive Space-Time Dependent Analysis of Loss of Coolant," WCAP-8302, June 1974 (Proprietary) and WCAP-8306, June 1974 (Nonproprietary).

4. Kabadi, J. N., et al., "The 1981 Version of the Westinghouse ECCS Evaluation Model Using the BASH Code", WCAP-10266-P-A, Revision 2 (Proprietary), WCAP-11524-NP-A, Revision 2 (Nonproprietary), March 1987; including Addendum 1-A, "Power Shape Sensitivity Studies", December 1987, and Addendum 2-A, "BASH Methodology Improvements and Reliability Enhancements", May 1988.

5. Kelly R. D. et al, "Calculational Model for Core Reflooding after a Loss-of-Coolant Accident (WREFLOOD Code)," WCAP-8170, June 1974 (Proprietary) and WCAP-8171, June 1974 (Nonproprietary).

6. Bordelon, F. M. and Murphy, E. T., "Containment Pressure Analysis Code (COCO)," WCAP-8327, June 1974 (Proprietary) and WCAP-8326, June 1974 (Nonproprietary).

7. Bordelon, F. M. et al, "Westinghouse ECCS Evaluation Model - Supplementary Information," WCAP-8471-P-A, April 1975 (Proprietary) and WCAP-8472-A, April 1975 (Nonproprietary).

8. Bordelon, F. M., et al., "LOCTA-IV Program: Loss-of-Coolant Transient Analysis", WCAP-8301 (Proprietary), WCAP-8305 (Nonproprietary), June 1974.

9. Rahe, E.P. (Westinghouse) letter to Tedesco, R.L. (USNRC), NS-EPR-2538, December 1981.

10. WCAP-9220-P-A and WCAP-9221-A, Rev. 1, "Westinghouse ECCS Evaluation Model - 1981 Version," February 1982.

11. Davidson, S. L., Nuhfer, D. L., "VANTAGE+Fuel Assembly Reference Core Report", WCAP-12610 (Proprietary), June 1990; including Kachmar, M.P., et al., Appendix F, "LOCA NOTRUMP Evaluation Model: ZIRLO Modification", and Appendix G, "Accident Evaluations LOCA Plant Specific," December 1990. NRC Safety Evaluation Reports contained in letter from Thadani, A. C. (NRC) to Tritch. S. R. (Westinghouse), "Acceptance for Referencing of Topical Report WCAP-12610 'VANTAGE+ Fuel Assembly Reference Core Report' (TAC No. 77258)," July 1, 1991 and letter from Thadani, A. C. (NRC) to Tritch, S. R. (Westinghouse), "Acceptance for Referencing of Licensing Topical Reports, WCAP 12610, Appendices F, 'LOCA NOTRUMP Evaluation Model: ZIRLO Modification,' and G, 'LOCA Plant Specific Accident Evaluations," (TAC No. 77258)." Davidson, S. L. and Ryan, T. L., "VANTAGE+ Fuel Assembly Reference Core Report," WCAP-12610-P-A, April 1995.

12. Shimeck, D. J., "Spacer Grid Heat Transfer Effects During Reflood, "WCAP-10484-P Addendum 1 (Proprietary); December 1992. (Transmittal to NRC Document Control Desk: "Notification of Changes to the Westinghouse Large Break LOCA ECCS Evaluation Model, "Letter No. ET-NRC-92-3787, December 22, 1992.)

13. Letter from Liparulo, N. J. (Westinghouse) to NRC Document Control Desk, "Extension of NUREG-0630 Fuel Rod Burst Strain and Assembly Blockage Models to High Fuel Rod Burst Temperatures," Letter No. ET-NRC-92-2746, September 16, 1992.

14. Letter from C. Eicheldinger of Westinghouse Electric Corporation to V. Stello of the Nuclear Regulatory Commission. Letter No. NS-CE-1163, dated August 13, 1976.

15. Beck, N. S. and Kemper, R. M., "Westinghouse ECCS Four-Loop Plant (17 x 17) Sensitivity Studies," WCAP-8865, October 1976.

16. Salvatori, R., "Westinghouse ECCS - Plant Sensitivity Studies," WCAP-8340, July 1974 (Proprietary) and WCAP-8356, July 1974 (Nonproprietary).

17. Johnson, W. J.; Massie, H. W.; and Thompson, C. M., "Westinghouse ECCS - Four-Loop Plant (17 x 17) Sensitivity Studies," WCAP-8565, July 1975 (Proprietary) and WCAP-8566, July 1975 (Nonproprietary).

18. Davidson, S. L., et al., "VANTAGE5H Fuel Assembly", WCAP-10444-P-A, Addendum 2-A (Proprietary), April 1988.

19. Letter from Johnson, W. J. (Westinghouse) to Murley (NRC), "10 CFR 50.46 Annual Notification for 1989 of Modification in the Westinghouse ECCS Evaluation Models," Letter No. NS-NRC-89-3463, October 5, 1989.

20. Letter from Steinmetz, J. N. (Westinghouse) to Nichols, J. A. (PSE&G), "Public Service Electric and Gas Company Salem Units No. 1 and 2 ECCS Evaluation Model Changes," Letter No. PSE-91-122, June 20, 1991.

21. Letter from Gasperini, J. R. (Westinghouse), to Nichols, J. A. (PSE&G), "Public Service Electric and Gas Company Salem Units No. 1 abd 2 10 CFR 50.46 Notification and Reporting Information", Letter No. PSE-93-201, February 2, 1993.

22. Deleted.

23. Deleted.

24. Geets, J. M., "MARVEL - A Digital Computer Code for Transient Analysis of a Multiloop PWR System," WCAP-7909, June 1972.

25. Moody, F. S., "Transactions of the ASME Journal of Heat Transfer," Figure 3, page 134, February 1965.

26. Bordelon, F. M., "Calculation of Flow Coastdown After Loss of Reactor Coolant Pump (PHOENIX Code)," WCAP-7973, September 1972.

27. Burnett, T. W. T.; et. al., "LOFTRAN Code Description," WCAP-7907-P-A (Proprietary), WCAP-7907-A (Non-Proprietary), April 1984.

28. Hargrove, H.G., "FACTRAN, A Fortran IV Code for Thermal Transients in a UO_2 Fuel Rod," WCAP-7908, December 1989.

29. Burnett, T. W. T., "Reactor Protection System Diversity in Westinghouse Pressurized Water Reactors," WCAP-7306, April 1969.

30. Taxelius, T. G. (Ed), "Annual Report - Spert Project, October 1968, September 1969," Idaho Nuclear Corporation IN-1370, June 1970.

31. Liimataninen, R. C. and Testa, F. J., "Studies in TREAT of Zircaloy-2-Clad, UO_2-Core Simulated Fuel Elements," ANL-7225, January-June 1966, p. 177, November 1966.

32. Risher, D. H., Jr., "An Evaluation of the Rod Ejection Accident in Westinghouse Pressurized Water Reactors Using Spatial Kinetics Methods," WCAP-7588, Revision 1-A, January 1975.

33. Risher, D. H., Jr. and Barry, R. F., "TWINKLE - A Multi-Dimensional Neutron Kinetics Computer Code," WCAP-7979-P-A, January 1975 (Proprietary) and WCAP-8028-A, January 1975 (Nonproprietary).

34. Barry, R. F., "LEOPARD - A Spectrum Dependent Nonspatial Depletion Code for the IBM-7094," WCAP-3269-26, September 1963.

35. Bishop, A. A.; Sanberg, R. O.; and Tong, L. S., "Forced Convection Heat Transfer at High Pressure After the Critical Heat Flux," ASME 65-HT-31, August 1965.

36. "Westinghouse Mass and Energy Release Datas for Containment Design," WCAP-8264 (Proprietary) and WCAP-8312 (Nonproprietary).

37. Dittus, F. W., and Boelter, L. M. K., University of California (Berkeley), Publishers, Publs, Eng., 2 433 (1930).

38. Jens, W. H., and Lottes, P. A., "Analysis of Heat Transfer, Burnout, Pressure Drop, and Density Data for High Pressure Water," USAEC Report ANL-4627, 1951.

39. Macbech, R. V., "Burnout Analysis, Pt. 2, The Basis Burnout Curve," U. K. Report AEEW-R 167, Winfrith, 1963. Also Pt. 3, "The Low-Velocity Burnout Regimes," AEEW-R 222, 1963; Pt. 4, "Application of Local Conditions Hypothesis to World Data for Uniformly Heated Round Tubes and Rectangular Channels," AEEW-R 267, 1963.

40. Dougall, R. S. and Rohsenow, W. M., "Film Boiling on the Inside of Vertical Tubes with Upward Flow of Fluid at Low Quantities," MIT Report 9079-26.

41. EcEligot, D. M.; Ormond, L. W.; and Perkins, Jr., H. C., "Internal Low Reynolds - Number Turbulent and Transitional Gas Flow with Heat Transfer," Journal of Heat Transfer, 88, 239-245, May 1966.

42. W. H. McAdams, "Heat Transmission," McGraw-Hill, 3rd edition, 1954, p. 172.

43. Cunningham, V. P. and Yeh, H. C., "Experiments and Void Correlation for PWR Small-Break LOCA Condition," Transactions of American Nuclear Society, Vol. 17, November 1973, pp. 369-370.

44. Tagami, Takaski, "Interim Report on Safety Assessments and Facilities Establishment Project in Japan for Period Ending June 1965 (No. 1)."

45. Kolflat, A. and Chittenden, W. A., "A New Approach to the Design of Containment Shells for Atomic Power Plants," Proc. of Amer. Power Conf., 1957 p. 651-9.

46. McAdams, W. H., "Heat Transmission," 3rd Edition, McGraw-Hill Book Co., Inc., New York, 1954.

47. Standards of Tubular Exchanger Manufacturers Association

48. Eckert, E. R. G., and Drake, P. M. J., "Heat and Mass Transfer," McGraw-Hill Book Co., Inc., New York, 1959.

49. Eckert, E. and Gross, J., "Introduction to Heat and Mass Transfer", McGraw-Hill, 1963.

50. Kern, D. Q., "Process Heat Transfer," McGraw-Hill Book Co., Inc., New York, 1950.

51. Chilton, T. H., and Colburn, A. P., "Mass Transfer (Absorption) Coefficients Prediction from Data on Heat Transfer and Fluid Friction," Imd. Eng. Chem., 26, 1934, pp. 1183-87.

52. Boettinger, W. L., "Topical Report - Reactor Containment Fan Cooler Cooling Test Coil," WCAP-7336-L, July 1969.

53. S. Weinberg, Proc. Inst. Mech. Engr., 164, pp. 240-258, 1952.

54. Ranz, W. and Marshall, W., Chem. Engr., Prog. 48, 3, pp. 141-146 and 48, 4, pp. 173-180, 1952.

55. Perry, J., "Chemical Engineers Handbook" 3rd Ed. McGraw-Hill, 1950.

56. Letter to Mr. D. B. Vassallo, Chief, Light Water Reactor Projects Branch 6, USNRC, from Mr. C. Eicheldinger, Manager, Nuclear Safety, Westinghouse Electric Corporation. Letter No. NS-CE-992, March 17, 1976.

57. Letter to Mr. D. B. Vassallo, Chief, Light Water Reactor Projects Branch 6, USNRC, from Mr. C. Eicheldinger, Manager, Nuclear Safety, Westinghouse Electric Corporation. Letter No. NS-CE-692, July 10, 1975.

58. Letter to Mr. D. B. Vassallo, Chief, Light Water Reactor Projects Branch 6, USNRC, from Mr. C. Eicheldinger, Manager, Nuclear Safety, Westinghouse Electric Corporation. Letter No. NS-CE-1021, April 7, 1976.

59. Letter to Mr. J. F. Stolz, Chief, Light Water Reactor Projects Branch 6, USNRC, from Mr. C. Eicheldinger, Manager, Nuclear Safety, Westinghouse Electric Corporation. Letter No. NS-CE-1883, August 27, 1976.

60. Hsieh, T. et al, "Environmental Qualification Instrument Transmitter Temperature Transient Analysis," WCAP-8936, February 1977 (Proprietary) and WCAP-8937, February 1977 (Nonproprietary).

61. Letter to John F. Stolz, Chief, Light Water Reactor Projects Branch 6, USNRC, from C. Eicheldinger, Manager, Nuclear Safety Westinghouse Electric Corporation. Letter No. NS-CE-1453, June 14, 1977.

62. Deleted.

63. Land, R. E., "Mass and Energy Releases Following a Steam Line Rupture," WCAP-8822, September 1976 (Proprietary) and WCAP-8860, September 1976 (Nonproprietary).

64. "Evaluation of the Reactor Coolant System Considering Subcompartment Pressurization Following a LOCA for Salem Units 1 and 2," transmitted by PSE&G letter, R. L. Mottle to O. D. Parr, dated March 6, 1979.

65. Liu, Y. S., et al., "ANC: A Westinghouse Advanced Nodal Computer Code," WCAP-10965-P-A, September 1986.

66. DELETED

67. "Salem Generating Station Environmental Design Criteria," S-C-ZZ-SDC-1419.

68. "Passive/Evolutionary Regulatory Consequence Code (PERC2)," Version 00, Level 01, Stone & Webster Engineering Corporation Computer Code, September 1996 (Proprietary).

69. Letter from Moore, L.H. (Stone & Webster), to Duffy, J. (PSE&G), Letter No. PS-97-002, January 2, 1997.

70. Letter from Moore, L.H. (Stone & Webster), to Duffy, J. (PSE&G), Letter No. PS-96-1496, December 19, 1996.

71. "RADTRAD: A Simplified Model for Radionuclide Transport and Removal and Dose Estimation", NUREG/CR-6604, USNRC, April 1998.

72. Letter from Huckabee, J. (Westinghouse), to Rosenfeld, E.S. (PSE&G) "Evaluation of SGTR analysis using assumed 30 minute operator action time," Letter No. PSE-96-579, March 7, 1996.

73. "Westinghouse LOCA Mass and Energy Release Model for Containment Design – March 1979 Version," WCAP-10325-P-A, May 1983 (Proprietary), WCAP-10326-A (Nonproprietary).

74. EPRI 294-2, "Mixing of Emergency Core Cooling Water with Steam; 1/3-Scale Test and Summary," (WCAP-8423), Final report, June 1975.

75. ANSI/ANS-5.1 1979, "American National Standard for Decay Heat Power in Light Water Reactors," August 1979.

76. Friedland, A.J. and Ray, S., "Revised Thermal Design Procedure," WCAP-11397, April 1989.

77. Letter from Osborne, M. P. (Westinghouse), to Ross, T. K. (PSEG Nuclear), "Evaluation of MSIV Operability in Mode 3", letter number PSE-00-075, dated 10/27/00.

78. "RADTRAD: A Simplified Model for Radionuclide Transport and Removal and Dose Estimation", NUREG/CR-6604 Supplement 1, USNRC, June 8, 1999.

79. "MicroShield Version 5 User's Manual", Grove Engineering, February 1998.

80. Regulatory Guide 1.183, "Alternative Radiological Source Terms for Evaluating Design Basis Accidents at Nuclear Power Reactors", July 2000.

81. Scherder, W. J. (Ed.) and McHugh, C. J. (Ed.), "Reactor Core Response to Excessive Secondary Steam Releases," WCAP-9226-P-A, Revision 1, 1998.

82. "RADTRAD: A Simplified Model for RADionuclide Transport and Removal and Dose Estimation," W.C. Arcieri (ITSC), NUREG/CR-6604 Supplement 2 (October 2002).