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Date: 10/5/00 9:29AM
Subject: revised appendices and adiabatic heatup times attached

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The times calculated are in hours for an adiabatic heatup from 30 C to 900 C with no oxidation heat source. Adiabatic Heatups are based on a Peaking Factor of 1.1 for PWRs and 1.2 for BWRs. The decay heats at these burnup values are interpolations or extrapolations of the decay heat from NUREG/CR-5625. The thermal mass of the BWR fuel is modeled as 9x9 fuel assemblies and the associated fuel rack structure. The mass per assembly is 170 kg UO2, 97.5 kg Zirconium and 42.4 kg stainless steel. The thermal mass PWR fuel is modeled 17x17 fuel assemblies and the associated rack structure. The mass per assembly is 470 kg UO2, 101 kg Zirconium, and 68.6 kg stainless steel. Temperature dependent values of the specific heat are used for steel, zircaloy, and UO2.

Adiabatic Heatup Time at 1 Year

Burnup	PWR	BWR
50	7.1	11.9
60	6.0	9.9

Adiabatic Heatup Time at 2 Years

Burnup	PWR	BWR
50	13.0	20.8
60	10.8	17.4

Adiabatic Heatup Time at 5 Years

Burnup	PWR	BWR
50	30.1	46.6
60	26.9	38.8

Adiabatic Heatup Time at 10 Years

Burnup	PWR	BWR
50	49.4	67.6
60	41.2	56.4

Decay Time in Years for a 10 Hour Adiabatic Heatup Time

Burnup	PWR	BWR
50	1.8	1.0
60	2.0	1.1

Decay Time in Years for a 24 Hour Adiabatic Heatup Time

Burnup	PWR	BWR
50	4.3	2.8
60	5.0	3.5

Spent Fuel Pool Heatup and Boiloff Time in hours to 3 feet Above Active Fuel. Fuel Burnup is 62.5 Gwd/MTU with a 2 year cycle time. The decay heat at this value of burnup is an extrapolation of the decay heat from NUREG/CR-5625. The BWR pool holds 4200 9x9 fuel assemblies. The pool surface area is 105.7 square meters. The PWR pool holds 965 17x17 fuel assemblies. The pool surface area is 61.3 square meters. The pools have a water depth of 11.54 meters and are assumed to be at an initial temperature of 30 C. An estimated volume fraction of 0.5 of water in the racks and assemblies was used. Errors in this value can impact the heatup time portion of the heatup and boiloff calculation. The specific heat of water was assumed to be constant at 4200 J/kg for the heatup calculation. Temperature dependent properties were used for steel, zircaloy, and UO2. The enthalpy change due to vaporization used in the boiloff calculation is 2257 KJ/kg.

Decay Time	PWR		BWR
60 days		100	145
1 year	195	253	
2 year	272	337	
5 year	400	459	
10 year		476	532

Appendix 1.A Thermal Hydraulics Analysis of Spent Fuel Pool Heatup

1. Spent Fuel Heatup Analyses

Spent fuel heatup analyses model the decay power and configuration of the fuel to characterize the thermal hydraulic phenomena that will occur in the SFP and the building following a postulated loss of water accident. This appendix reviews the existing studies on spent fuel heatup and zirconium oxidation, the temperature criteria used in the analyses, and how it applies to decommissioned plants. Since mechanistic accident progression and fission product release calculations were not performed, a temperature criterion to evaluate the potential for a significant fission product release. To determine the timing of a significant fission product release the heatup time from 30 *C to 900 *C is used. To prevent a significant fission product release the long term steady state temperature of the fuel rods must remain below 600 *C if the time after shutdown is less than 5 years. The long term steady state temperature of the fuel rods must remain below 800 *C if the time after shutdown is greater than 5 years. The technical basis of the temperature criteria for spent fuel pool fission product releases are discussed in Appendix 1.B.

1.1 Spent Fuel Pool Heatup and Boiloff

The PRA modeling required a calculation of the time in hours for the pool water level to be reduced to 3 feet Above Active Fuel if pool heat removal and makeup capability is lost. The fuel burnup is assumed to be 62.5 Gwd/MTU with a 2 year cycle time. The decay heat at this value of burnup is an extrapolation of the decay heat tables from NUREG/CR-5625. The BWR pool was assumed to hold 4200 9x9 fuel assemblies. The BWR pool surface area is 105.7 square meters. The PWR pool was assumed to hold 965 17x17 fuel assemblies. The PWR pool surface area is 61.3 square meters. The pools have a water depth of 11.54 meters and are assumed to be at an initial temperature of 30 C. An estimated water volume fraction of 0.5 of water in the racks and assemblies was used in the calculations. Errors in this value can only impact the heatup time portion of the heatup and boiloff calculation. The specific heat of water was assumed to be constant at 4200 J/kg for the heatup calculation. Temperature dependent properties were used for steel, zircaloy, and UO2. The enthalpy change due to vaporization used in the boiloff calculation is 2257 KJ/kg.

Table 1 Boiloff time in hours to 3 feet above active fuel.

Decay Time	**Boiloff Time (hours)**	
	PWR	**BWR**
60 days	100	145
1 year	195	253
2 year	272	337
5 year	400	459
10 year	476	532

1.2 Evaluation of Previous Spent Fuel Heatup Analyses

In the 1980's, severe accidents in operating reactor SFPs were evaluated to assess the significance of the results of some laboratory studies on the possibility of self-sustaining

zirconium oxidation and fire propagation between assemblies in an air-cooled environment, and also to assess the impact of the increase in the use of high density spent fuel storage racks on severe accidents in spent fuel pools. This issue was identified as Generic Safety Issue (GI) 82. Sandia National Laboratory (SNL) and Brookhaven National Laboratory (BNL) used the SFUEL and SFUEL1W computer codes to calculate spent fuel heatup in these studies. While decommissioned plants were not addressed in the study, many of the insights gained from these studies are applicable to decommissioned plants.

1.2.1 SFUEL Series Based Analyses

Extensive work on the phenomena of zirconium oxidation in air for a SFP configuration was performed by SNL and BNL in support of GSI 82. SNL investigated the heatup of spent fuel, the potential for self-sustaining zirconium oxidation, and the propagation to adjacent assemblies [Ref. 1, 10]. SNL used SFUEL and SFUEL1W computer codes to analyze the thermal-hydraulic phenomena, assuming complete drainage of the SFP water. In NUREG/CR-4982 [Ref. 5], BNL extended the SNL studies on the phenomenology of zirconium-air oxidation and its propagation in spent fuel assemblies. The SFUEL series of codes includes all modes of heat transfer, including radiation.

In NUREG/CR-0649, SNL concluded that decay heat and configuration are important parameters. SNL found that key configuration variables are the baseplate hole size, downcomer width, and the availability of open spaces for airflow. They also found that building ventilation is an important configuration variable.

The general conclusions and the phenomena described in the studies assist in assessing issues for decommissioned plants. However, the calculated critical decay time and heatup time values do not represent current plant operational and storage practices.

1.3 New Spent Fuel Heatup Analyses

New analyses were performed for this study since previous analyses are not representative of current plant operation and spent fuel pool storage practices. Theses analyses had three objectives:

1. Determine the heatup time and critical decay time of a given pool configuration.

2. Study the global flow pattern to determine the applicability of approximations.

3. Determine the impact of detailed pool loading assumptions.

The decay heat used in these analyses were calculated using the method described in NUREG/CR-5625 (Ref. 13). The decay heat for values of burnup higher than contained in the tables of NUREG/CR-5625 were computed by linear extrapolation in burnup of the tabular values to the higher values of burnup.

1.3.1 TRAC-M Calculations

NRR performed calculations using a version of TRAC-M that was modified to include wall drag correlations and oxidation models that are applicable to the spent fuel pool heatup problem. These calculations were performed to estimate the time available after draining the pool before

a large fission product release occurs and to provide an estimate of the critical decay time when a large release is no longer possible. The pool is divided into 10 flow channels. The downcomer flow area around the periphery of the pool is one channel. The last core offloaded is represented by 3 flow channels with each representing 1/3 of a core. The three previous 1/3 core offloads are modeled separately. The other three channels each model a full core for a PWR and slightly more than a full core for a BWR. Only an average fuel rod is modeled for each channel. The model does not allow heat transfer between different powered flow channels. The effect of heat transfer between the heated channels and the downcomer was examined.

1.3.2 FLUENT Calculations

RES performed FLUENT calculations of a loss of water spent fuel pool accident that are documented in NUREG-1726. FLUENT is a three dimensional CFD (computational fluid dynamics) computer code. FLUENT is used to model the entire spent fuel build and pool, including the fuel and the fuel racks. A porous media approach is used to model the fuel and the racks. A detailed description of the FLUENT spent fuel pool modeling and the results of the FLUENT calculations are in NUREG-1726. The FLUENT calculations do not include radiation heat transfer. The FLUENT calculations provided information that enabled a comparison between the global flow pattern and approximations made in other calculations in order to determine the applicability of the approximations.

1.3.3 TEMPEST-COBRA Calculations

PNNL has performed calculations of a loss of water spent fuel pool accident with a methodology that uses the TEMPEST CFD code and the COBRA-SFS spent fuel cask analysis code. TEMPEST is a PNNL developed three dimensional CFD code that uses a cartesian grid. COBRA-SFS is a single phase computer code that was developed to model spent fuel casks. It has the capability to model fuel racks and fuel bundles down to the single fuel pin and subchannel level of detail including thermal radiation transport. The TEMPEST code is used to model the build and spent fuel pool down to the top of the fuel racks. The code calculates the average temperature immediately above the top of the spent fuel racks using a uniform average power as a heat source at the top of the fuel racks. TEMPEST does not model the flow through the fuel rack and bundles. The average temperature from the TEMPEST calculations in input as a boundary condition for the downcomer inlet conditions for the COBRA-SFS calculations. The COBRA-SFS calculations calculate the thermal response of the fuel and racks based on the power and the TEMPEST generated boundary condition. The COBRA-SFS calculations allow detailed modeling of the fuel racks and allow an assessment of the impact of fuel checkerboarding and other fuel loading strategies on peak fuel temperatures.

1.4 Heatup Calculation Uncertainties and Sensitivities

The phenomenology needed to model spent fuel heatup is dependent on the chosen cladding temperature success criterion and the assumed accident scenario. Many assumptions and modeling deficiencies exist in the current calculations. The staff reviewed the models to assess the impact of those modeling assumptions. Some of these uncertainties for the SFUEL series codes are further discussed in NUREG/CR-4982. For cases of flow mixing, decay heat, bundle flow resistance and other severe accident phenomena, additional information is provided here.

Calculations performed to date assume that the building, fuel, and rack geometry remain intact.

This would not be a valid assumption if a seismic event or a cask drop damaged some of the fuel racks or the building. Rack integrity may not be a good assumption after the onset of significant zirconium oxidation due to fuel failure criteria issues discussed in Appendix 1.B. The building may also be hot enough to ignite other materials. Assuming that the racks remain intact is the most optimistic assumption that can be made about the rack geometry. Any damage to the racks or the building could significantly reduce the coolability of the fuel.

Previous SFUEL, SFUEL1W, and SHARP calculations, used in the resolution of GI 82 and decommissioning studies, used a perfect ventilation assumption. With the perfect ventilation assumption an unlimited amount of fresh, ambient-temperature air is available. This assumption would be valid if the building failed early in the event or if large portions of the walls and ceilings were open and no flow blockages occured in the spent fuel pool. If the building does not fail, the spent fuel building ventilation flow rate would dictate the airflow available. Mixing between the rising hot air and the descending cooler air in the spent fuel pool is not modeled in the codes.

The spent fuel building ventilation flow rate is important in determining the overall building energy balance. Airflow through the building is an important heat removal mechanism. Most of the air would recirculate in the building and the air drawn under the racks would be higher than ambient temperature and, therefore, less heat removal would occur. Airflow also provides a source of oxygen for zirconium oxidation. Sensitivity studies have shown that heatup rates increase with decreasing ventilation flow, but that very low ventilation rates limit the rate of oxidation. Other oxidation reactions (fires) that occur in the building will also deplete available oxygen in the building. Zirconium-Nitrogen reaction modeling is not included in the SFUEL code and may have an impact on zero and low ventilation cases. GSI 82 studies concluded that the perfect ventilation assumption was more conservative than no ventilation because the oxidation reaction became oxygen starved with no ventilation. Since these studies did not consider the failure modes of the building under high temperature scenarios or the reaction of nitrogen with zirconium at high temperatures they should not be considered as reliable information. Intermediate ventilation rate results were not studied and give longer critical decay times than the perfect ventilation case.

The dominant heat removal mechanism is buoyancy-driven natural circulation. The calculated airflow and peak temperatures are very sensitive to the flow resistance in the storage racks, fuel bundles and downcomer. The downcomer flow resistance is determined by the spacing between the fuel racks and the wall of the SFP. The storage rack resistance is primarily determined by the orifice size at the bottom entrance to the fuel bundle. Smaller inlet orifices have higher flow resistance. As shown by SFUEL and SHARP calculations, changes in the rack-wall spacing and the orifice size over the range of designs can shift critical decay times by more than a year. The fuel bundle flow resistance is determined by the rod spacing, the grid spacers, intermediate flow mixers and the upper and lower tie plates. NUREG/CR-4982 concluded that the largest source of uncertainty was due to the natural circulation flow rates.

The downcomer and bundle inlet air temperatures and mass flow rates are important in determining the peak cladding temperature. The extent of flow mixing will determine the air temperatures at the downcomer and bundle inlet. The SFUEL and SHARP calculations assume a well-mixed building air space. The downcomer inlet temperature is set equal to the bulk building temperature. This assumption neglects the mixing that occurs between the hot air rising from the bundles and the cooler air descending down the SFP wall. FLUENT calculations [Ref 12] performed by RES indicate that fully 3-dimensional calculations are needed to

accurately predict the mixing and flow fields because unrealistic flow topologies in 2-dimensional approximations may overstate the mixing. The assumption of perfect mixing in the building can underestimate the temperature of the air reaching the fuel racks by as much as 20 *C. The calculations also indicate that the quasi-steady state assumptions for conditions above the fuel rack may not be appropriate. Time varying temperature fluctuations on the order of ± 20*C have been observed in 3D FLUENT calculations.

The FLUENT calculations are the only calculations that have simulated the global flow pattern. The calculations have shown several interesting results that can be used to assess the approximations and simplifying assumptions made in the other calculations. The FLUENT calculations indicate that the fluid temperature under the fuel racks is close to the building exit temperature which is the average building temperature in the SFUEL and SHARP calculations. The downcomer inlet temperature used in the TEMPEST-COBRA calculations is higher than this value. Almost half of the downcomer is in upflow in the FLUENT calculations. All other calculations assume that the downcomer is entirely in downflow.

Radiation heat transfer is important in spent fuel pool heatup calculations. Radiation heat transfer can affect both the onset of a zirconium fire and the propagation of a fire. Both the SFP loading pattern and the geometry of the fuel racks can affect the radiation heat transfer between adjacent bundles. Simple gray body calculations show that at clad temperatures of 800 *C, a temperature difference of 100 *C between adjacent bundles would cause the radiation heat flux to exceed the critical decay power of 6 kW/MTU. Therefore, the temperature difference that could be maintained between adjacent bundles is highly constrained by the low decay heat levels. SFUEL calculations performed by SNL and BNL included radiation heat transfer, but the radiation heat transfer was underpredicted since the spent fuel placement is two-dimensional and the hottest elements are in the middle of the pool with cooler elements placed progressively toward the pool walls. Heat transfer between hotter and cooler assemblies has the potential to be significantly higher if the fuel bundles were intermixed in a realistic loading pattern. PNNL COBRA calculations have shown that not including radiation heat transfer will increase cladding temperature approximately 50 *C at a base temperature of approximatley 500 *C under one actual plant fuel loading.

At temperatures below 800 *C, the SFP heat source is dominated by the spent fuel decay heat if breakaway oxidation does not occur. SNL and BNL found that, for high-density PWR racks, that 6 kW/MTU was the critical decay heat level for a zirconium fire to occur in configurations resembling current fuel storage practices. At the fuel burnups used in the calculations, this critical decay heat level was reached after two years. Decay heat calculations in NUREG/CR-5625 [Ref. 13] were performed to be the basis for calculating fuel assembly decay heat inputs for dry cask storage analyses. These decay heat calculations are consistent with the decay heat used in SFUEL calculations. Extrapolation of the decay heat calculations from NUREG/CR-5625 to current burnups indicate that approximately 3 years will be needed to reach a decay heat of 6 kW/MTU. The extrapolation has been confirmed to provide a reasonable decay heat approximation by performing ORIGEN calculations that extend to higher burnup. The critical decay heat may actually be less than 3 kW/MTU when in-bundle peaking effects, higher density rack configurations and actual building ventilation flows are taken into account.

The oxidation of zirconium alloys in air can be a significant source of heat in spent fuel pool heatup calculations at temperatures above 500-600 *C. There is little data for zirconium alloy oxidation in air in the range of temperatures and timescales of interest in spent fuel pool heatup

calculations. There are large variations in the data and between correlations of the data in the temperature range of interest. The available data is discussed in appendix 1.B.

It should be noted that none of the analysis codes have all of the required models or enough experimental assessment to be considered as properly qualified to analyze the spent fuel pool heatup problem. Proper qualification would require a substantial research effort involving experiments, code development and analysis.

1.5 Estimated Heatup Time of Uncovered Spent Fuel

The staff recognized that the decay time necessary to ensure that air cooling was adequate to remain below the temperature of self-sustaining zirconium oxidation was a conservative criteria for the reduction in emergency preparedness criteria. Using the fact that the decay heat of the fuel is reducing with time, credit could be given, if quantified, for the increasing length of time for the accident to progress after all water is lost from the SFP. The staff sought to quantify the decay time since final shutdown such that the heatup time of the fuel after uncovery was adequate for effective protective measures using local emergency response. The heatup time was defined as the amount of time to heat the fuel from 30 *C to 800 *C.

The heatup time of the fuel depends on the amount of decay heat in the fuel, and the amount of heat removal available for the fuel. The amount of decay heat is dependent on the burnup. The amount of heat removal is dependent on several variables, as discussed above, that are difficult to represent generically without making a number of assumptions that may be difficult to confirm on a plant and event specific basis.

For the calculations, the staff used a decay heat per assembly and divided it equally among the pins. It assumed a 9X9 assembly for the BWRs and a 17x17 assembly for the PWRs. Decay heats were computed using an extrapolation of the decay power tables in NUREG/CR-5625 [Ref. 8]. The decay heat in NUREG/CR-5625 is based on ORIGEN calculations. The tables for the decay heat extend to burnups of 50 GWD/MTU for PWRs and 45 GWD/MTU for BWRs. The staff recognizes that the decay heat is only valid for values up to the maximum values in the tables, but staff ORIGEN calculations of the decay power, with respect to burnup for values in the table, indicate that extrapolation provides a reasonable and slightly conservative estimate of the decay heat for burnup values beyond the limits of the tables. Current peak bundle average burnups are approximately 50 GWD/MTU for BWRs and 55 GWD/MTU for PWRs. The BWR decay heat was calculated using a specific power of 26.2 MW/MTU. The PWR decay heat was calculated using a specific power of 37.5 MW/MTU. Both the PWR and BWR decay heats were calculated for a burnup of 60 GWD/MTU and include an uncertainty factor of 6 percent.

The staff used a specially modified version of TRAC-M to estimate the heatup time. TRAC-M was used because it is robust, has flexible modeling capabilities and runs fast enough to perform sensitivity studies. Modifications were made to the wall drag, the wall heat transfer, and the oxidation models so that they would be applicable to the spent fuel pool heatup problem. The transfer of heat between high powered bundles and low powered bundles was not modeled and only the fuel and fuel rack heat structures were modeled so the heatup time estimates should be conservative if the rack geometry is intact after the pool draining.

The staff examined the effect of zirconium oxidation models on spent fuel pool heatup. Several parabolic rate equations were used in heatup calculations. The oxidation rate equations that

were used do not model the effect of breakaway oxidation. The heatup times are sensitive to the oxidation model used in the heatup calculations.

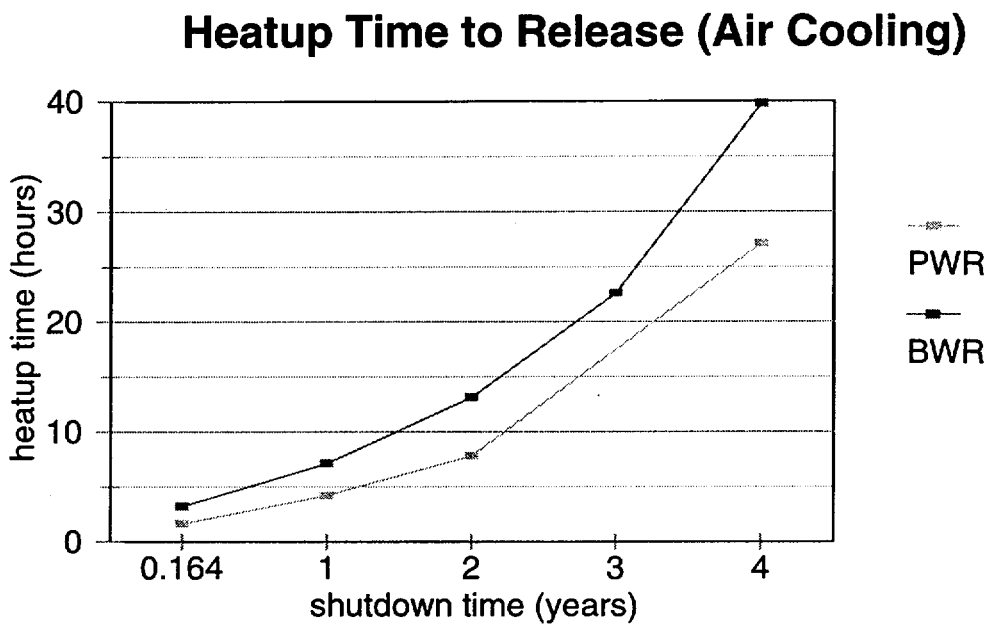

Figure 1. Heatup time from 30 C to 800 C The staff has also considered a scenario with a rapid partial draindown to a level at or below the top of active fuel with a slow boiloff of water after the draindown. This could occur if a large breach occurred in the liner at or below the top of active fuel. Section 5.1 of NUREG/CR-0649 analyzes the partial draindown problem. For the worst case draindown and a lower bound approximation for heat transfer to the water and the building the heatup time is less than the heatup time for the corresponding air cooled case. Calculations, assuming an instant draindown of the pool and air-cooling, show a heatup time to fission product release of 4 hours

at 1 year after shutdown for a PWR with 60 Gwd/MTU fuel burnup. The worst case partial draindown could also release fission products in 4 hours at 1 year after shutdown. At 5 years after shutdown the release of fission products may occur approximately 24 hours after the accident. The results are calculated the oxidation the parabolic rate equation defined in equation 1. This oxidation model is leads to the fastest heatup times of the oxidation models that were examined. The results are shown in figure 1. Adiabatic heatup calculations were also performed that include the thermal mass of the spent fuel pool racks but do not include the oxidation heat source. Figure 2 show a comparison of the air cooled calculations to an adiabatic heatup calculation for a PWR at a burnup of 60 Gwd/MTU. Figure 2 shows that the air cooled heatup times are shorter than the adiabatic heatup times for times up to two years after shutdown. This is because the air cooling heatup rate is close to the adiabatic heatup rate and the oxidation heat source becomes a significant contributor to the total power at

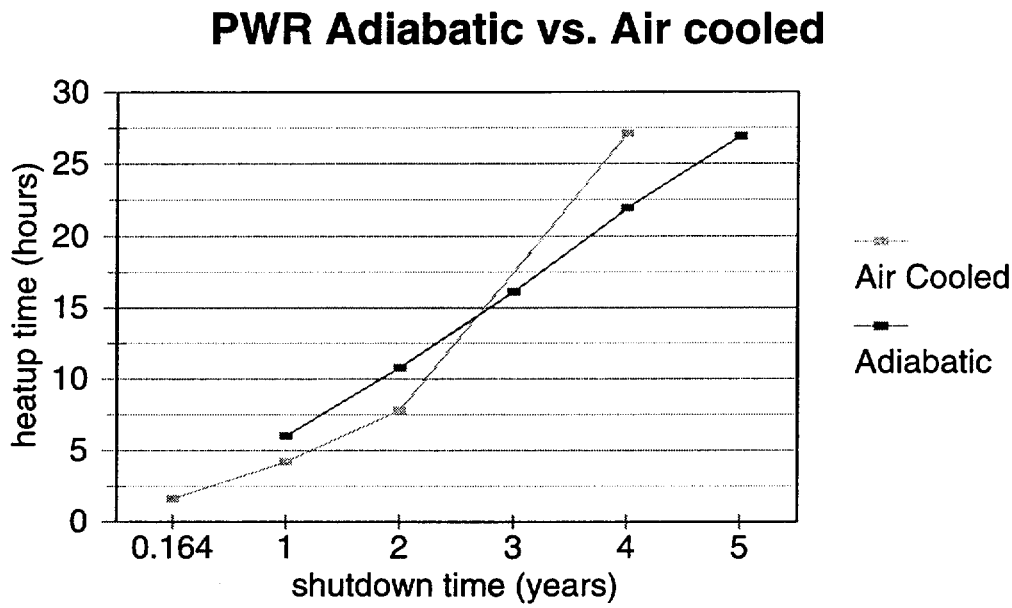

Figure 2 PWR heatup times for air cooling and adiabatic heatup temperatures of approximately 600 *C. An adiabatic heatup calculation that included the oxidation heat source would have heatup times shorter than the air cooled heatup times. Sensitivity calculations were performed that varied the oxidation correlation, the rack flow resistance, the rack heat transfer, and the building flow rates. Even though significant variations in heatup times were observed in these calculations, the decision making process would not be affected.

1.6 Critical Decay Times to Reach Sufficient Air Cooling

Calculations performed using the SFUEL code in support of GI-82 have determined a critical specific decay heat of 6 kW/MTU is needed for the onset of runaway zirconium oxidation. The 6 kW/MTU estimate calculated using SFUEL in a high-density storage rack configuration is reasonable and is based on the best calculations to date. However, this estimate is based on perfect ventilation conditions in the building and lower density rack configurations than exist today. The SFUEL calculations also did not consider the possibility of releasing ruthenium and cesium even if the a zirconium fire does not occur.

For high burnup PWR and BWR fuel, the staff estimates it will take approximately 3 years to reach the critical decay heat level cited in NUREG/CR-4982. Other assumptions, such as imperfect ventilation, could extend the critical decay time for the onset of a zirconium fire by 1 to 2 years. FLUENT calculations show that the critical decay heat is less than 3 kW/MTU for the pool and building configuration that was studied at a building ventilation rate of 2 building volumes per hour. The staff estimates it will take approximately 5 years to reach a decay heat less than 3 kW/MTU for current BWR plant fuel and approximately 7 years for current PWR fuel burnups. TRAC-M calculations using the parabolic oxidation rate correlation recommended by RES in Appendix 1.B give a critical decay time of approximately 5 years for both the BWR and PWR cases at approximately two building volumes per hour. Sensitivity calculations show that the critical decay time will be extended by reducing the building ventilation flow rate or decreasing the size of the downcomer flow area. Plant-specific calculations using fuel decay

heat based on the actual plant operating history and spent fuel configurations could yield significantly shorter critical decay times. COBRA-SFS calculations performed using checkerboard fuel loadings indicate that the critical decay time can be significantly reduced if the highest power fuel is interspersed with low powered fuel or empty rack spaces.

The building ventilation flow is unknown since the dominating initial event is a well beyond design basis earthquake. The consequences of such an event on available ventilation for air cooling is highly variable. Power will not be available to run forced ventilation systems, the fuel pool may contain debris, an unspecified amount of water may remain in the pool, and the fuel racks may not be in a well defined geometry. Since the availability of air cooling is assumption driven a probability distribution on parameters such as available air flow rate would be required to properly integrate the T/H analysis with the risk analysis. Reducing the building ventilation flow will increase fuel temperatures at a given decay time. Since the building ventilation rate may be close to zero after the postulated accident, the critical decay time could essentially extend indefinitely. Calculations of the partial draindown indicate that consideration of this event will extend the critical decay time to well in excess of 5 years.

Spent fuel pool reracks have continuously increased the fuel power density and decreased the downflow area available for cooling flow. Since the design basis for spent fuel pools involves water cooling only, no restrictions have been put on parameters that increase severe accident risk. Recent reracks have left as little as 2 inches of downcomer width available for air downflow. The downcomer width assumption was approximately 6 inches in the FLUENT calculations. Smaller downcomer widths will extend the critical decay time at a given building flow rate beyond the times listed in the NUREG-1726 study.

The critical decay time will depend on the specific pool configuration and accident scenario being analyzed. The critical decay time will be a function of building configuration, building ventilation flow rate, fuel pool loading, rack geometry and the oxidation modeling. Because of all of these possible variations, a generically applicable critical decay time can not be readily defined.

1.7 Fire Propagation

The staff has not performed a sufficient amount of research to fully understand and predict the propagation of zirconium fires in a spent fuel pool. Based on the limited amount of work performed to date, the propagation is probably limited to less than 2 full cores at a time of 1 year after shutdown. This estimate is based on lowering the GSI 82 estimate of the 6 KW/MTU fire threshold to 3KW/MTU to account for building ventilation effects. This does not consider potentially important effects such as rubble formation after loss of fuel integrity. The actual propagation will probably be dependent on the actual fuel loading configuration in the spent fuel pool. A long term experimental and analytical research program would be required to reliably predict the propagation of a zirconium fire in a spent fuel pool.

1.8 Conclusions and Recommendations

The staff has concluded that it is not possible to perform a generically applicable analysis to determine heatup times or critical decay times. Therefore the times cited in this report should only be considered as rough estimates of what the staff believes are realistic conditions for what is possible based on currently available information. The staff also has a poor understanding of the accident progression and source term from a spent fuel pool fire. There

are many specific weaknesses in the present and past studies that have been identified. Some of the primary weaknesses were also identified as weaknesses in the studies for resolution of GSI 82. To support a more accurate assessment of what may occur following the postulated seismic initiating event, the following areas need additional development and study:

1.) A realistic accident scenario for the dominant risk sequence or a number of scenarios with associated probabilities should be defined.

2.)Potential restrictions or limitations on fuel loading patterns, allowable rack configurations, and maximum fuel burnups should be considered.

3.)Additional oxidation data and oxidation model development should be pursued for all zirconium alloys used in fuel cladding under a wide range of clad burnup and temperature conditions.

4.) Properly scaled experimental data should be obtained for code assessment.

5.) A validated computer code to accurately predict the accident progression and the source term from the accident should be developed.

Additional Recommendations

1.) The above issues need to be addressed in order to provide more accurate and realistic information for a risk analysis. A CSAU [Ref 18] like study should be performed after the experimental and code development work has been completed to quantify the uncertainty in the validated analysis code for a fixed pool configuration.

2.) RES should consider whether GSI 82 should be reopened to examine the impact of recent spent fuel pool configuration changes and new information on source terms in the presence of air. The higher rack densities, higher fuel burnups, larger source term, and uncertainties in physical phenomena have the potential to significantly change the conclusions reached in GSI 82.

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Appendix 1.B Temperature Criteria for Spent Fuel Pool Analysis

BACKGROUND

The engineering analyses performed to address spent fuel pool performance during various accidents have, in the past, used a temperature criterion to evaluate the potential for significant fuel damage. This temperature was intended as an acceptance criterion beyond which one would expect the onset of significant, global, fuel damage and substantial release of fission products (e.g., 50-100% of inventory of volatiles) associated with such damage. Further, the temperature criterion cited (generally about 900°C) has been selected on the basis that it represented a threshold for self sustained oxidation (Ref. 1) of cladding in air and on that basis it has been argued that if cooling of the spent fuel could limit fuel temperatures in equilibrium below this threshold then large releases of fission products need not be considered. Self sustaining reaction in this sense means the reaction rate and thus heat generation rate is sufficient, to roughly balance heat losses for given cooling mechanisms, resulting in an isothermal condition. Once the fuel temperature exceeds this threshold temperature (alternatively identified as an ignition or autoignition temperature) it was presumed that subsequent heat up and further increases in reaction rates would be escalating and rapid and that serious fuel damage would ensue. The temperature escalation associated with oxidation in this regime would not be balanced by any reasonable cooling afforded by natural circulation of air. While it was not expected that fission product releases associated with core melt accidents would immediately emerge at this temperature (based on reactor research in various steam and hydrogen environments) it was recognized that the time window for subsequent fuel heating would be relatively small once oxidation escalated. This also did not preclude gap type releases associated with fuel failures below the threshold temperature but these generally were not considered to be significant compared to the releases associated with higher fuel temperatures and significant fuel damage.

In the report, "Draft Final Technical Study of Spent Fuel Pool Accident Risk at Decommissioning Nuclear Power Plants", February 2000, the temperature criterion selected, 800°C, was used in two ways. First, it was used to determine the decay heat level and corresponding time at which heat generation and losses for complete and instantaneous draining of the pool would lead to heating of the fuel (to 800°C) after 10 hours. This time period would allow for the implementation of effective emergency response without the full compliment of regulatory requirements associated with operating reactors. Secondly, the temperature criterion was also used to evaluate the decay heat level and time ("critical decay time") at which heat generation and losses for a fully drained fuel pool would result in an equilibrium temperature of 800°C (typically this critical decay time has been on the order of 5 years) On that basis it was reasoned that since serious overheating of the fuel had not occurred, the fission product release associated with core melt need not be considered.

AIR OXIDATION AND TEMPERATURE ESCALATION

The NRC has received a number of comments related to the use of this temperature criterion and has reassessed the appropriateness of such a value for both its intended purposes. At the outset RES acknowledges that an ignition temperature, or more precisely in this case a temperature for incipient temperature escalation is dependent on heat generation and losses

which in turn is dependent on system geometry and configuration. In fact, much of the data onoxidation is produced in isothermal tests up to near the melting temperature of zirconium. In examining an appropriate criterion, it is useful to consider the range of available data including core degradation testing in steam environments, since it is likely that many spent fuel pool accidents may involve some initial period during which steam oxidation kinetics controls the initial oxidation, heatup, and release of fission products. In various experimental programs around the world (e.g., PBF-SFD, ACRR, CORA, NSRR, PHEBUS and QUENCH) repeatable phenomena have been observed for the early phase of core degradation (in steam) which proceeds initially at temperature increase rates associated with decay heat (at levels characteristic of reactor accidents) until cladding oxidation becomes dominant and a more rapid temperature escalation occurs. The point at which the escalation occurs, which does vary between tests, has been attributed to heat losses (Reference 2) characteristic of the facility and to phase changes of ZrO_2 over a temperature range. The threshold at which temperature escalation occurred has been reported to vary from approximately 1100°C to 1600°C. In a CORA test performed with a lower initial heat up rate (to simulate reduced decay heat during shutdown conditions) it was reported that uncontrolled temperature escalation did not occur, raising the prospect that heating rate may be a factor. (This is probably due to the formation of a thicker oxidation layer built up over the protracted time at lower temperature such that when higher temperatures are attained, the thicker scale results in a lower oxidation rate relative to a thinner scale at the same temperature.) In more recent QUENCH tests (Quench 04 and 05) the effect of preoxidation was evaluated for its effect on hydrogen generation and temperature escalation. In Quench 04 temperature escalation was reported to occur at 1300°C; in Quench 05 with approximately 200μm preoxidation temperature escalation was reported to be delayed until the fuel rod temperature reached 1620°C.

Because of interest in air ingression phenomena for reactor accidents, recent severe accident research has also examined oxidation in air environments. Publication of results from the DRESSMAN and CODEX test programs (Reference 3) has provided much of the transient data on fuel rod and rod bundle behavior for air kinetics as well as data on fuel oxidation and volatility. Early studies of zirconium oxidation in air (References 4 and 5) were performed by comparing isothermal oxidation and scaling of fresh samples to determine the influence of different atmospheres and materials as well as to examine potential for fire hazards. The general observation is that, at least at higher temperatures(>1000°C), the oxidation rate is higher in air than in steam. Another observation of the early studies was, under the same conditions, oxidation in an air environment produced an oxide layer or scale less protective than that for steam owing to the possible instability of a nitride layer beneath the outer oxide layer leading to scale cracking and a breakaway in the oxidation rate. The onset of this breakaway in the oxidation rate occurred at about 800°C after a time period of 10 hours in the studies performed by Evans et al(Ref. 4) and after a period of approximately 4 hours in studies by Leistikow (Ref. 9). The Leistikow studies were performed on fresh cladding, however, and it is expected that breakaway would occur after a longer time delay with preoxidized cladding. As breakaway oxidation occurs the oxidation behavior observed no longer reflects a parabolic rate dependence but takes on a linear rate dependence. Also, at lower temperatures the kinetics of reaction indicate near cubic rate dependence thus the representation of the oxidation behavior at both high and low temperatures with a parabolic rate dependence may introduce unnecessary simplification and an understatement of the low temperature behavior. Breakaway scaling in an isothermal test may not translate to similar behavior under transient heatup conditions where initial oxidation occurs at lower temperatures and may involve steam oxidation. The presence of hydrides in the cladding may also increase the potential

forexfoliation and a breakaway in the oxidation; the effect of this has, however, been seen more clearly in testing conducted with steam and high hydrogen concentrations. Also, zirconium hydride will be dissolved at 700°C and above, thus its contribution to exfoliation and breakaway will be minimal.

Autoignition is known to occur in zirconium alloys and zirconium hydride, especially when clean metal or hydride is suddenly exposed to air. The temperature of ignition is highly dependent on the ratio of surface area to volume and the degree of surface cleanliness. Generally, spent fuel rod cladding is covered with a relatively thick oxide layer (20-100μm), therefore, unless ballooning and burst occur in the cladding during heatup, clean high-temperature Zircaloy metal will not be exposed to air in a spent fuel pool accident. However, if there is cladding failure by ballooning and burst (expected to occur over a temperature range of 700-850*C), hot oxide-free clean metal will be abruptly exposed to air. Zirconium hydride is expected to dissolve into the metal matrix during the slow heatup to these temperatures. At the moment of burst, some clean surface area of Zr metal will be exposed to air in the location of the rupture. Although data applicable to this situation is quite limited, considering the relatively small surface-to-volume ratio of the exposed metal, likelihood of ignition and subsequent propagation of the burning front of Zr metal is believed to be small (Ref. 8).

In the CODEX tests annular cladded fuel (in a 9 rod bundle) were heated with an inner tungsten heater rod to examine fuel degradation, with preoxidized cladding, in an air environment. Zircaloy oxidation kinetics were evaluated as well as the oxidation of the fuel. In the CODEX AIT -1 test the early phase of the test involved creating a preoxidation using an argon-oxygen mixture. The intent was to achieve a controlled preoxidation at a temperature of 900-950°C, but it was reported (Ref. 3) that preoxidation was started at a slightly higher temperature than planned. What subsequently occurred was an uncontrollable temperature escalation up to approximately 2200°C before it was cooled with cold argon flow. After restabilization of the rods at 900°C air injection was started, electrical heatup commenced, and a second temperature escalation occurred. In the CODEX AIT-2 test, designed to proceed to a more damaged state, the preoxidation phase was conducted in an argon/steam mixture at 820°C and 950°C. (a malfunction occurred during the preoxidation phase resulting in the admission of a small air flow as well). No temperature escalation was seen during the preoxidation phase. Following the restabilization of the fuel rods, a linear power increase was started and a temperature excursion subsequently occurred.

In addition to examining relevant test data RES also looked at determining a temperature based threshold for temperature escalation in an air environment by determining equivalent heat generation from steam transient tests. In this exercise we posited that at equivalent heat generation rates, i.e., accommodating different reaction rates and different heats of reaction for air and steam, we should be able to predict the corresponding temperature for escalation in air based on temperature escalations seen in severe fuel damage tests conducted in steam. Using this approach, the heat generation rate was estimated, assuming parabolic kinetics, and the following equation for a rate constant in air:

$$k_p = 52.67 \exp(-17597/T) \quad \{kg/m^2\}^2 \cdot sec^{-1} \qquad \text{[rate constant of } O_2 \text{ mass produced]}$$

It was predicted that based on an escalation temperature of 1200°C in steam (observed in many of the steam tests), the equivalent heat generation rate in air would produce a temperature escalation at approximately 925°C. The above equation for air kinetics was

identified in Reference 3 as the best fit for the CODEX AIT test data, i.e., it provided the best agreement to the temperature transient in the peak position. For steam kinetics, the rate equation used in MELCOR was selected for calculating the heat generation rate. The prediction of an escalation temperature in air using this approach seems to conform quite well with the observed behavior in the transient CODEX tests and lends further credence to the relative effect of oxidation in air with respect temperature escalation. The assumption of parabolic kinetics is routine in oxidation calculations and has been shown to provide a good match with a wide spectrum of experimental data even though, over select temperature ranges, deviations from that formulation have been observed. At temperatures above 900°C, the reaction rate in air is high, regardless of whether parabolic or linear kinetics is assumed at that point and distinguishing between the rates of escalation is unimportant for our purposes.

ACCEPTANCE CRITERIA

In assessing a temperature criterion for escalation of the oxidation process and subsequent temperature escalation it is necessary to reconsider the intended uses of the criterion: 1) to evaluate the decay time after which the fuel heatup, in the case of complete fuel uncovery, leads to reaching that temperature at 10 hours and 2) to evaluate the decay time after which the fuel heatup, in the case of complete uncovery will never exceed the temperature criterion.

On balance it appears that a reasonable criteria for the threshold of temperature escalation in an air environment is a value of approximately 900°C. This value is supported both by limited experimental data as well as by inference from the more abundant steam testing data. While certain weight gain data indicate the onset of a break away in the oxidation rate at lower temperatures after a period of 10 hours, this additional time period then exceeds the time interval for which the first use of the criterion is intended. With regard to the second use of the criterion, determination of the point at which severe fuel heatup is precluded, the onset of breakaway indicated in certain tests indicates that the temperature criterion should be lowered to 800°C. It is important to stress that, in both instances, the temperature criteria should be used together with a thermal-hydraulic analysis that considers heat generation (i.e., decay heat and zircaloy reactions) and heat losses. For the second use of the criterion, i.e., establishing a threshold for precluding escalation, the analysis must demonstrate that heat losses, through convection, conduction and radiation, are sufficient to stabilize the temperature at the value selected.

In the case of slow, complete draining of the pool, or partial draining of the pool it is appropriate to consider use of a higher temperature criterion for escalation, perhaps as high as 1100 to 1200°C. This would be appropriate if the primary oxidation reaction was with steam. Such a temperature criterion is relevant for the first intended use of the criterion, determining the point at which the temperature is not exceeded for 10 hours, however it is not appropriate for use as a long term equilibrium temperature since over long intervals at such high temperature, one might reasonably expect significant fission product releases.

In addition to comments on the selection of an ignition temperature, the staff received comments related to the effect of intermetallic reactions and eutectic reactions. With respect to intermetallic reactions, the melting temperature of aluminum, which is a constituent in BORAL poison plates in some types of spent fuel storage racks, is approximately 640°C. Molten aluminum can dissolve stainless steel and zirconium in an exothermic reaction forming intermetallic compounds. In the spent fuel pool configuration, zircaloy cladding will be covered

with an oxide layer and unless significant fresh metal surface is exposed through exfoliation there will be no opportunity to interact metallic zircaloy with aluminum (which similarly will be oxidized). Aluminum and steel will form an intermetallic compound at a temperature of 1150°C, (Ref. 5) which is above the temperature criterion selected for fuel damage.

Besides intermetallic compounds, eutectic reactions may take place between pairs of various reactor materials, e.g., Zr-Inconel (937°C), Zr-steel (937°C), Zr-Ag-In-Cd (1200°C), Zr-B_4C (1627°C), steel-B_4C (1150°C), etc. (Ref. 6). Consideration of eutectics and intermetallics is important from the standpoint of heat addition as well as assuring the structural integrity of the storage racks and maintaining a coolable configuration. Noting the eutectic and intermetallic reaction temperatures, however it does not appear that formation of these compounds imposes any additional temperature limit on the degradation of cladding in an air environment.

Since the temperature criterion is also a surrogate of sorts for the subsequent release of fission products it is useful to consider the temperature threshold versus temperatures at which cladding may fail and fission products be released. Cladding is likely to fail by ballooning and burst in the temperature range of 700-850°C, resulting in the release of fission products and fuel fines. At burst, clean Zircalloy metal will also be exposed, leading to an increase in oxidation although the total amount of metal involved will be limited. Creep failure of the cladding at or above 600°C is also a possibility. This temperature limit is roughly associated with the 10 hour creep rupture time (565°C) which has been used as a regulatory limit. While failure of the cladding at these lower temperatures will lead to fission product release, such release is considerably smaller than that assumed for the cases where the temperature criterion is exceeded and significant fuel heatup and damage occurs. Low temperature cladding failures might be expected to produce releases similar to those associated with dry cask accident conditions as represented in Interim Staff Guidance (ISG)-5. This NRC guidance document prescribes release fractions for failed fuel ($2x10^{-4}$ for cesium and ruthenium and $3x10^{-5}$ for fuel fines). Use of these release fractions would reduce the estimated offsite consequences dramatically from the fuel melt cases, early fatalities would be eliminated and latent cancer fatalities would be reduced by a factor of 100. As the temperature limit is increased from 600°C to 900°C there is some evidence that ruthenium releases would be increased based on ORNL test data from unclad pellets. Canadian data indicate though, that in the case of clad fuel the ruthenium release did not commence until virtually all of the cladding had oxidized. By this point it might be surmised that the fuel configuration would more closely resemble a debris bed than intact fuel rods. Selection of a temperature criterion for fuel pool damage also depends on the intended use, i.e., whether it is intended as the criterion for the 10 hour delay before the onset of fission product release or whether it is being used as a threshold for long term fission product release. If the criteria is being used to judge when 10 hours are available for evacuation, then it may be argued that a higher temperature could be adopted, one associated with the significant release of fission products,1200°C, since the release of fission products at lower temperatures will likely be small. However, in air it may be that the oxidation rate above 900°C is sufficient to reduce the additional time gained to reach 1200°C to a relatively small amount. Selection of a temperature criterion for long term fuel pool integrity needs to consider that ruthenium release rates, in air, become significant at approximately 600-800°C, based on the data of Parker et al. (Ref. 7).

Selection of an acceptance criterion for precluding significant offsite release after roughly 5 years, should also consider that ruthenium with a 1 year half life will be substantially decayed and that at 5 years cesium (and perhaps fuel fines such as plutonium) will dominate the dose

calculation. For these reasons RES believes that the long term viability of the pool in a completely drained condition (air environment), if it concerns time periods of approximately 5 years, pool degradation should be assessed for a temperature of approximately 800°C. Again, an analysis needs to be performed to demonstrate that at that temperature an equilibrium condition can be established. While this would result in an offsite release, there would be substantial time available to take corrective action after a 5 year decay time for the most recently loaded fuel. If shorter decay time periods are proposed for achieving the long term equilibrium temperature criterion, then the impact of ruthenium releases would dictate reconsideration of this value.

SUMMARY

In summary, we conclude that for assessing the onset of fission product release under transient conditions (to establish the critical decay time for determining availability of 10 hours to evacuate) it is acceptable to use a temperature of 900°C if fuel and cladding oxidation occurs in air. If steam kinetics dominate the transient heatup case, as it would in many boildown and drain down scenarios, then a suitable temperature criterion would be around 1200°C. For establishing long term equilibrium conditions for fuel pool integrity during spent fuel pool accidents which preclude significant fission product release it is necessary to limit temperatures to values of 600°C to 800°C. If the critical decay time is sufficiently long (>5 yrs) that ruthenium inventories have substantially decayed then it would be appropriate to consider the use of a higher temperature, 800°C, otherwise fission product releases should be assumed to commence at 600°C. These cases are marked by substantial time for corrective action to restore cooling and prevent smaller gap type releases associated with early cladding failures. A tabulated summary of the suggested criteria is listed below.

	Adequacy of 10 hrs for Evacuation	Precluding Large Release Fuel <5yrs	Precluding Large Release Fuel >5yrs
Dominant Air Environment	900°C	600°C	800°C
Dominant Steam Environment	1200°C	N/A	N/A

The degradation of fuel during spent fuel pool accidents is an area of uncertainty since most research on severe fuel degradation has focused on reactor accidents in steam environments. Because of this uncertainty, we have tended to rely on the selection of conservative criterion for predicting the global behavior of the spent fuel pool. It is our recommendation that the modeling of spent fuel pool accidents be performed with codes capable of calculating the heat generation and losses associated with the range of accidents, including phenomena associated with both water boiloff and air circulation. Further, the calculation of critical decay times for establishing both the validity of ad hoc evacuation and precluding fission product release must also include consideration of the exothermic energy of reactions (i.e., reactions with air and steam) with cladding, or alternatively demonstrate that such energy contribution is negligible in comparison to decay heat at that point. Severe accident codes, such as MELCOR, developed for modeling the degradation of reactor cores, would seem to be a reasonable approach for analysis of integral behavior and would possess the general capabilities for modeling liquid levels and vapor generation, air circulation, cladding oxidation and fission product release. Use

of a severe accident code also facilitates the use of self consistent modeling and assumptions for the analysis. The proper calculation of fission product releases depends in large part on the prediction of thermal hydraulic conditions. More detailed CFD modeling would improve the calculation of boundary conditions for air circulation and could be used in conjunction with integral codes to better evaluate convective cooling. The kinetics of cladding reactions should be confirmed with experiments designed to simulate the range of conditions of interest under steady state and transient heating. The experimental database on ruthenium releases under conditions applicable to spent fuel pool accidents is inadequate and we are currently extrapolating data from conditions which tend to maximize such releases.

While there is uncertainty in the analysis of spent fuel degradation, especially for the conditions of air ingression, it is also true that elements of the analysis contain conservatism. The assumption of 75-100% release of ruthenium initiated at lower temperatures is based in large part on tests with bare fuel pellets, testing of cladded fuel indicates that the cladding acts as a getter of oxygen limiting release of ruthenium until virtually all of the cladding has oxidized. Further, before significant ruthenium release occurs (in its more volatile oxide form) the surrounding fuel matrix must be oxidized. During transient heatup of a spent fuel pool with temperature escalation one would expect the ruthenium release to follow the oxidation of the cladding at which point the fuel would more likely resemble a debris bed (the seismic event may also contribute in that regard) limiting the release fraction. The competition between formation of hyperstoichiometric UO_2 and U_3O_8 may also limit the release fraction below that seen in the data. The use of a temperature criterion of 600°C to preclude significant fission product releases is conservative in that it is based in large part on data that discounts the effect of cladding to limit releases. The cladding failures at low temperatures will still allow substantial retention of fuel fines and the presence of unoxidized zircalloy will prevent formation of volatile forms of ruthenium. More prototypic experimental data on releases under these kinds of conditions may reveal that the onset of significant releases, especially ruthenium, would not occur under spent fuel pool accident conditions until fuel rod temperatures reached much higher temperatures associated with complete oxidation of the cladding.

Use of the hottest fuel assemblies to predict global release of fission products from the entire spent fuel inventory is a significant conservatism as well. Transient fuel damage testing indicates that at the time of local temperature escalation not all of the rod bundle undergoes rapid heating, cooler regions can avoid the oxidation transient. Prediction of the propagation of the temperature escalation to the cooler regions of the pool needs to be carefully examined to see if significant benefit can be gained, at a minimum it will lengthen the period of fission product release reducing the concentration of activity in the plume of fission products for offsite consequence analysis.

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